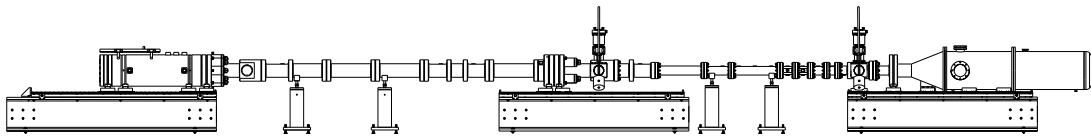


**UNIVERSIDADE DE LISBOA
INSTITUTO SUPERIOR TÉCNICO**



**Development and Qualification of the European Shock
Tube for High Enthalpy Research**

Ricardo João Grosso Marques Ferreira

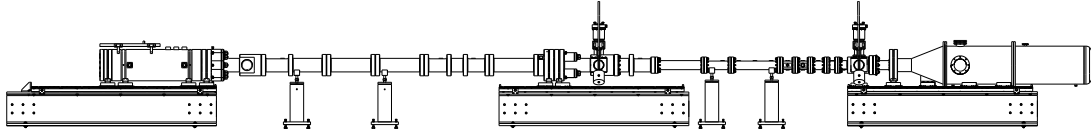
Supervisor: Doctor Mário António Prazeres Lino da Silva

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Funding Institution:

Fundação para a Ciência e Tecnologia

2024

Para os meus pais...

“You cannot always be the best. But you can do your best.”

- Sebastian Vettel

Declaration

I declare that this document is an original work of my own authorship and that it fulfills all the requirements of the Code of Conduct and Good Practices of the Universidade de Lisboa.

Acknowledgments

Despite the single name on the cover of this document, a lot of people contributed directly and indirectly towards its completion. First and foremost, thanks to my parents Serafim and Helena who pushed me to pursue Physics and a PhD against all odds and all pre-established ideas others tried to pass to them. To my longest roommate and brother Bruno, who kept me (and him) sane over the research and writing process. Lastly, to my cats, some of whom are still around while others are not, especially Pepe and Dora, for being a fluffy company during the lockdowns.

A heartfelt acknowledgement and thanks to my supervisor Mário, with whom I could make sense of this work and be part of the fascinating area of Space exploration. We spent lots of time in the lab discussing how to solve the neverending problems of ESTHER, and probably even more on side topics. Among these problems was what title to give to this thesis. It should be stated its apocryphal title is *Cenas do Tubo de Choque*. To the personnel of IPFN (Instituto de Plasmas e Fusão Nuclear), especially Carla Reis, and the team at HPL (Hypersonic Plasma Laboratory), Professor Bernardo B. Carvalho and Engineer Rafael Rodrigues, without whom this shock tube cannot run. Not to forget the leaders of both N-Prime, Professor Luís Lemos Alves (*O Chefe*), and IPFN, Doctor Bruno Gonçalves that keep this “machine” running. A special thanks to Professor Lionel Marraffa who helped not only me but also the ESTHER team during his stay at IST with his vast expertise and knowledge. The ESTHER project and this doctorate could only be possible thanks to the financial support of Fundação para a Ciência e Tecnologia (FCT) under its doctoral program grant, and the European Space Agency (ESA).

To my current and former Ph.D. candidate colleagues within and outside IPFN, for the times where they provided different ideas as perspectives into my problems and the good times we had during our journey. Namely, André Torres, Bernardo Gonçalves and Miguel Levy (a.k.a *Men of Culture*), Duarte Gonçalves, João Vargas, Miguel Oliveira, Rui Calado and Rui Torres. My other friends are not left out, J.C., André “Tilhas” Neves, Cláudio João, Luís Graça Franco, and Raul Marques for their continuous presence and support despite the distance.

Resumo

O Tubo de Choque Europeu para Pesquisa a Alta Entalpia (ESTHER, sigla em inglês) é uma instalação de tubo de choque de última geração capaz de recriar o plasma envolvendo uma nave durante a sua entrada numa atmosfera planetária. ESTHER é um tubo de choque multi-estágio, dividido em quatro seções principais separadas por diafragmas.

Uma câmara de combustão de alta pressão aciona a onda de choque através da ignição de uma mistura diluída de hidrogénio-oxigénio por um laser Nd:YAG, com pressões de enchimento entre 10 e 100 bar. Duas condições operacionais foram desenvolvidas. A primeira para entradas a alta velocidade, de composição $[\text{He}:\text{H}_2:\text{O}_2]$ 8:2:1.2-1.4. A segunda, utilizada em entradas lentas, de composição $[\text{N}_2:\text{H}_2:\text{O}_2]$ 10:2:1.4. Calculou-se o mapa de desempenho, demonstrando que o ESTHER é capaz de gerar ondas de choque entre 4 e 14 km/s em ar.

O vácuo na seção de teste é gerado por um sistema de bombeamento multi-estágio (bomba primária ligada a duas bombas turbomoleculares). Este atinge uma taxa de fuga de $3.94 \times 10^{-5} \text{ mbar l s}^{-1}$ e pressão mínima de $9.8 \times 10^{-7} \text{ mbar}$, obtendo-se operacionalmente uma taxa de contaminação inferior a 1%.

ESTHER tem um sistema de espectroscopia de emissão UV-VUV composto de um espetómetro e uma câmara estraboscópica, com uma resolução de 0.5 nm com uma rede de difração 1,200 gr/mm. Uma estação de espectroscopia infravermelha foi projetada e uma pré-seleção do equipamento para cobrir a região espectral de 1-5 μm proposta. Um interferómetro modular é finalmente usado para estimar a densidade eletrónica atrás da onda de choque. Este pode também funcionar como refletómetro, e examinar diferentes densidades de plasma.

Palavras-chave:

Hipersónico, Tubo de choque, Combustão de alta pressão, Entrada planetária, Espectroscopia

Abstract

The European Shock Tube for High-Enthalpy Research (ESTHER) is a state-of-the-art shock tube facility capable of reproducing the plasma surrounding a spacecraft crossing the upper layers of planetary atmospheres at hypersonic speeds. ESTHER is a two-stage shock tube, divided into four main sections separated by metallic diaphragms.

Its driver is a high-pressure combustion chamber operating a hydrogen-oxygen diluted mixture, ignited by a high-power Nd:YAG laser, with filling pressures ranging from 10 to 100 bar. Two operational mixtures were considered to generate the shock waves. The first is an $[\text{He}:\text{H}_2:\text{O}_2]$ 8:2:1.2-1.4 mixture tailored for high-speed entries. The second, a $[\text{N}_2:\text{H}_2:\text{O}_2]$ 10:2:1.4 mixture, is used for slower shocks. The theoretical performance map of ESTHER was computed, showing it is capable of generating shock waves between 4 and 14 km/s in air.

A two-stage vacuum pumping system (comprised of a primary and two turbomolecular pumps) generates a high-vacuum level in the test section. The setup yields a leakage rate of $3.94 \times 10^{-5} \text{ mbar l s}^{-1}$ and ultimate pressure of $9.8 \times 10^{-7} \text{ mbar}$, meaning a sub-1% contamination rate of the test-gas can be achieved.

ESTHER is equipped with an optical emission UV-VUV spectrometer and streak camera setup with a 0.5 nm resolution on a 1,200 gr/mm grating. A mid-IR spectroscopy setup was also designed to cover the 1-5 μm spectral region, and a pre-selection of the appropriate equipment was carried out. A modular-designed interferometer is finally used to probe the electron density behind the shock. It may also work as a reflectometer, and probe different plasma densities by switching the equipment front-end section.

Keywords:

Hypersonic, Shock tube, High-pressure combustion, Planetary entry, Spectroscopy

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Nomenclature

Greek symbols

δ_r	Boundary layer thickness
γ	Heat capacity ratio
Λ	Detonation cell size
λ	Air to fuel equivalence ratio (chapter 5)
λ	Wavelength (chapters 7 and 8)
λ_0	Laser fundamental wavelength
μ	Viscosity
μ_0	Vacuum magnetic permeability
ν	Kinematic viscosity
ω	Angular frequency
ω_c	Plasma cyclotron frequency
ϕ	Angular phase (chapters 7 and 8)
ϕ	Fuel to air equivalence ratio (chapter 5)
ρ	Mass density
τ	Representative time scale/wave period
θ_a	Acceptance angle
θ_m	Reflection angle
ε_0	Vacuum permittivity

Roman symbols

$\bar{l}$	Mean free path
$\mathcal{D}$	Angular dispersion

$\mathcal{M}$	Mean molecular mass
$\mathcal{N}$	Refractive index
$\mathcal{R}$	Ideal gas constant
$\mathcal{X}$	Molar fraction
Kn	Knudsen number
Re	Reynolds number
$\vec{B}$	Magnetic induction field
$\vec{E}$	Electric field
$\vec{H}$	Magnetic field
$\vec{J}$	Current density
A	Area
a	Speed of sound
C	Conductance
c	Speed of light in vacuum
C_f	Friction coefficient
c_p	Specific gas heat capacity at constant pressure
D	Plasma thickness
d	Diameter
d_G	Groove distance in grating
E	Energy
e	Elementary electric charge
E_d	Minimum ignition energy for detonation
E_f	Minimum ignition energy
F	Focal length
f	Frequency
$f/\#$	Aperture number
h	Specific enthalpy
I	Irradiance

k_B	Boltzmann constant
L	Location of the Friedlander blast wave (chapter 3)
L	Representative length scale
l_m	Boundary length
M	Mach number
m	Mass
m_b	Mass burned
m_e	Electron mass
M_{00}	Beam quality factor
N	Number of particles
n	Number of moles
n_e	Electron density
$n_{e,p}$	Critical electron density
NA	Numerical Aperture
p	Pressure
Q	Gas throughput
q	Gas quantity
R_a	Arithmetical mean roughness
R_y	Maximum height (roughness)
S	Pumping rate
S_L	Laminar flame velocity
T	Temperature
t	Time
u	Flow velocity
V	Volume
v	Velocity
w	Beam half-waist
x	Position

Subscripts

0	Initial
∞	Free stream conditions
$crit$	Critical
des	Desorption
eff	Effective
eq	Equilibrium
eqv	Equivalent
int	Internal modes
k	k^{th} order
lam	Laminar
mol	Molecular
nom	Nominal
pe	Plasma
$stag$	Stagnation point
th	Thermal
tr	Translational modes

Superscripts

0	Pre-shock
1	Post-shock
$\bar{Z}$	Average value of variable Z
$\dot{Z}$	Time-derivative of variable Z
sp	Species

Acronyms

ADC Analog-to-Digital Converter

ADST Arc-Driven Shock Tube

AEL Arbitrary Euler Lagrangian

ARD Atmospheric Reentry Demonstrator

ATEX Explosive Atmosphere

CAD Computer Aided Design

CARDC China Aerodynamics Research & Development Center

CC Combustion Chamber

CCD Charged-Coupled Device

CDR Critical Design Review

CEA Chemical Equilibrium with Applications

CF Con-Flat (connection)

CFD Computational Fluid Dynamics

CJ Chapman–Jouguet

CMOS Complementary Metal-Oxide-Semiconductor

CNC Computer Numeric Control

CS Contact Surface

CT Compression Tube

CUBRC Calspan-University of Buffalo Research Center

DDT Deflagration to Detonation Transition

DLR German Aerospace Center

DT Dump Tank

EAST Electrical Arc Shock Tube

EBS Electron Back-Scattering

EDL Entry, Descending and Landing

EM Electromagnetic

ESA European Space Agency

ESTHER European Shock Tube for High Enthalpy Research

FDTD Finite Differences in Time-Domain

FFT Fast-Fourier Transform

FGE Fluid Gravity Engineering

FMC FPGA Mezzanine Card

FPGA Field-Programmable Gate Array

FWHM Full-Width at Half-Maximum

GASL GASL, General Applied Science Laboratory

HEG High Enthalpy shock tunnel Göttingen

HIEST High Enthalpy Shock Tunnel

HOPE H-II Orbiting Plane

HPL Hypersonic Plasma Laboratory

HVST HyperVelocity Shock Tube

HYFLEX Hypersonic Flight Experiment

iCCD Intensified Charged-Coupled Device

IPFN Instituto de Plasmas e Fusão Nuclear

IR Infrared (light)

IST Instituto Superior Técnico

ISTC International Science and Technology Center

ITAM Institute of Theoretical and Applied Mechanics

IUSTI Institute Universitaire des Systèmes Thermiques Industriels

JAXA Japan Aerospace Exploration Agency

KAIST Korean Advance Institute of Science and Technology

KF/ISO-KF ISO Kleinflansch

KFM Fluoroelastomer (elastomer)

LENS Large Energy National Shock-tunnel

LIF Laser-Induced Fluorescence

MIPT Moscow Institute of Physics and Technology

MWIR Mid-Wave Infrared (light)

NETD Noise Equivalent Temperature Different

NIR Near Infrared (light)

NASA National Aeronautics and Space Administration

OFHC Oxygen-Free High Conductivity

ONERA French Aerospace Lab

OREX Orbital Re-entry Experiment

PCB Printed Circuit Board

PCIe Peripheral Component Interconnect express

PDR Preliminary Design Review

PE Piezo-Electric

PID Piping and Instrumentation Diagram

PLC Programmable Logic Controller

PLIF Planar Laser-Induced Fluorescence

PMT Photomultiplier Tube

PREN Pitting Resistance Equivalent Number

RAM Radio Attenuation Measurement

REFMUL Electromagnetic wave propagation numerical code

RPM Revolutions per minute

RWTH RWTH Aachen University

S/N Signal-to-Noise

SEM Scanning Electron Microscope

SMA SubMiniature Assembly

SPARK Software Platform for Aerothermodynamics Radiation and Kinetics

SPARK-LbL SPARK Line-by-Line

ST Shock Tube

STAGG Shock Tube And Gas Guns

SW Shock Wave

TDRS Tracking and Data Relay Satellite

TPS Thermal Protection System

TsAGI Central Aerohydrodynamic Institute

TsNIIMASH Central Research Institute of Mechanical Engineering

UHF Ultra High Frequency

UHV Ultra-High Vacuum

UMIST University of Manchester Institute of Science and Technology

UV Ultraviolet (light)

VCO Voltage-Controlled Oscillator

VEC Vector Error Correction

VHF Very High Frequency

VIS Visible (light)

VKI Von Karman Institute for Fluid Dynamics

VUV Vacuum-Ultraviolet (light)

Chapter 1

Introduction

Enūma Eliš lā nabû šamāmū	<i>When the heavens above did not exist,</i>
Šapliš ammatsu šuma lā zakrat	<i>And earth beneath had not come into being-</i>
Apsūm-ma rēštū zārūšun	<i>There was Apsū, the first in order, their begetter,</i>
Mummu Tiāmat muallidat gimrišun	<i>And demiurge Tiāmat, who gave birth to them all;</i>
Mêšunu ištēniš ihiqū-ma	<i>They had mingled their waters together</i>
Gipara la kiššurū šuša lā še'û	<i>Before meadowland had coalesced and reed-bed was to be found-</i>
Enūma ilāni lā šūpû manāma	<i>When not one of the gods had been formed</i>
Šuma lā zukkurū šīmatu lā šīmū	<i>Or had come into being, when no destinies had been decreed,</i>
Ibbanū-ma ilū qerebšun	<i>The gods were created within them;</i>
Lahmu u Lahamu uštāpû šumī izzakrū	<i>Lahmu and Lahamu were formed and came into being</i>

Enuma Elish the Babylonian creation myth - adapted from:

Wilfred G. Lambert, *Babylonian Creation Myths*, Eisenbrauns, 2013

1.1 Motivation

Entry, descent and landing (EDL) is one of the most challenging mission phases for planetary exploration/Earth return spacecraft, as one needs to ensure the safe and appropriate deceleration of a spacecraft until the soft landing at ground level. Besides all the specific technological challenges related to the late stages of deceleration (descent and landing), with the definition of appropriate glide systems, parachutes, and landing systems (retro propulsion, inflatables or crushable structures), the atmospheric entry phase remains one of the most complex phases to be tackled. This flight phase occurs at extreme hypersonic speeds with strong deceleration and high heating rates, owing to the so-called *atmospheric entry plasmas*, which are created downstream of strong, detached shock waves typical of hypersonic flow regimes.

These shock waves convert the coherent energy of the flow into thermal agitation energy, rapidly heating it and triggering the internal excitation, dissociation and ionization of the flow species in severe nonequilibrium conditions, ultimately leading to the formation of the entry plasma. This nonequilibrium plasma also radiates significantly besides strongly convecting heat towards the colder spacecraft walls,

triggering endothermic surface reactions, which, in general, lead to the ablation of the wall surfaces, which are protected by a (generally ablative) thermal protection system (TPS). The precise knowledge of the physical–chemical properties of such plasmas is accordingly key to the efficient design of such TPS systems, among other aspects (aerodynamics, flight stability, blackout issues, etc.).

Ground Testing

Apart from flight test experiments, which provide the ultimate benchmark, ground testing is a conspicuous phase for any scheduled planetary exploration mission. Different classes of ground-test facilities are available for such purposes, and the project team foremost needs to select those that are best suited for the mission profile. Ground test experiments occur in a different inertial frame of reference to the spacecraft flight. In the planet frame of reference we and the atmosphere are stationary while the spacecraft travels at a high velocity. If we now move to the spacecraft frame of reference, we are stopped while the planet rotates and we “feel” the atmosphere travelling towards us. Most ground test facilities operate on this principle, and thus, do not have a stationary atmosphere and fast moving model, but accelerate the gas to the desired hypersonic conditions towards the test model.

Shock tubes are the most faithful facilities for adequately reproducing post-shock conditions for such flows, either directly reproducing the stagnation line flow (behind a normal shock), or indirectly reproducing the other forebody flow regions (behind an oblique shock) through straightforward correlations [1]. Such impulsive facilities are complementary with other steady-state plasma wind tunnels, which can, in turn, reproduce the conditions near the spacecraft walls, or afterbody expansions¹. These facilities, primarily designed for material testing, may also reproduce such flow conditions more approximately, owing to their different plasma excitation mechanism (electromagnetic field) and the expansion of this heated gas through a nozzle. This in turn leads to a non-equilibrium condition of the gas upstream of the model and lower flow velocity compared to the entry vehicle. Whereas the excitation mechanism in a shock tube is the exact same one as that in flight conditions (shock wave), thus remaining the better candidate for assessment of radiative fluxes or physico-chemical processes. Figure 1.1 schematically shows the range of applicability for the different facilities. In short, shock tubes are (among others) key facilities in the planetary entry research ecosystem of space-faring nations.

Entry conditions are determined by the spacecraft’s motion into the planet’s gravitational field. These are then reproduced in key points (typically distributed round the peak radiative and convective heating, and peak dynamic pressure points which are obtained through semi-empirical expressions [5, 6]) on adequate ground test facilities, such as the one discussed in this work, the European Shock Tube for High-Enthalpy Research (ESTHER). Figure 1.2 shows the spacecraft’s entry conditions in different atmospheres and the corresponding experimental points for different shock tube facilities.

Once the key trajectory points are known, one may estimate the post-shock temperatures that are reached using simple thermodynamic correlations. Namely, the total stagnation specific enthalpy h_{stag} (enthalpy per unit of mass) of a shock wave system may be expressed as the sum of the internal specific enthalpy h , and a kinetic term $v^2/2$ due to the flow velocity [8]. The stagnation enthalpy of the system

¹that shock tubes may also be deployed as impulsive wind-tunnel facilities, see [2] for more details

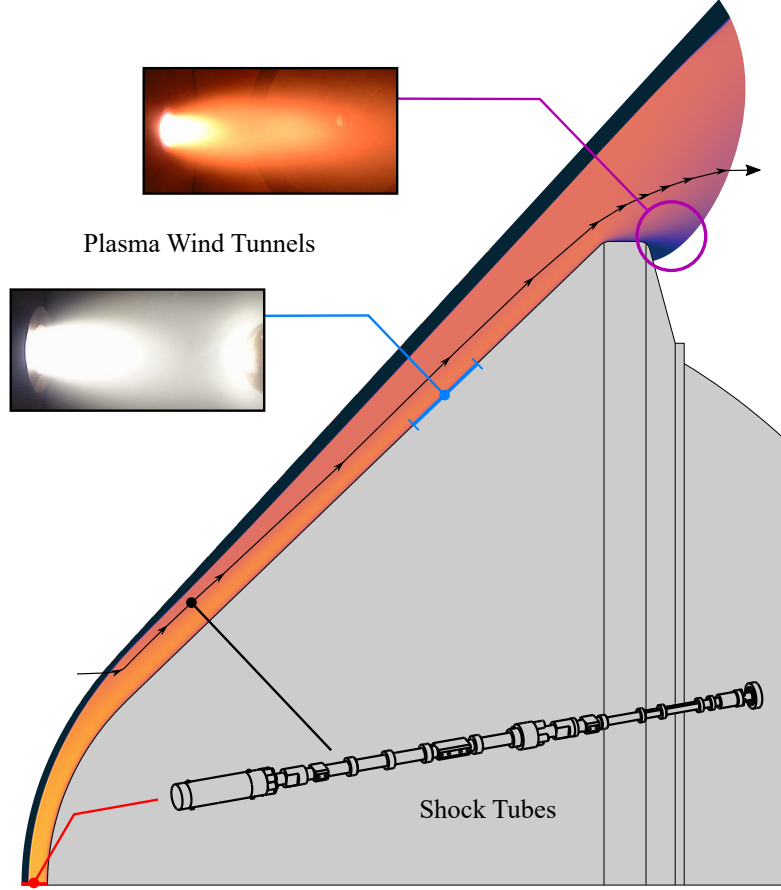


Figure 1.1: Schematic plot outlining the applicability range of ground test facilities for reproducing the different regions of an atmospheric entry flow. Shock tubes directly reproduce the stagnation streamline region where the heat fluxes are typically peaked (red dot), or other flow streamlines in the forebody (black dot and streamline). Free plasma plumes from wind tunnels are tailored for surface ablation tests by placing an obstacle inside the plume (blue dot and line). These can also be used to reproduce the expansion regions (large violet circle) of the hypersonic flow. The plasma plume images (with and without obstacles) are taken from the SR5 arc jet plasma wind tunnel [3]. The simulated flowfield is adapted from a previous work reproducing the 1995 Jupiter entry by the Galileo probe [4].

pre h^0 and post-shock h^1 can be assumed to be conserved, yielding Equations 1.1 and 1.2.

$$h_{stag} = h + \frac{v^2}{2} = const. \quad (1.1)$$

$$h_0 + \frac{v_0^2}{2} = h_1 + \frac{v_1^2}{2} \quad (1.2)$$

Prior to the arrival of the shock wave, the gas is in chemical equilibrium at a low temperature, around 300 K for a shock tube experiment, where the internal energy term is dominant ($v_0^2/2 \gg h_0$) on the left-hand side of Equation 1.2. After the shock, most of the kinetic energy is converted into thermal energy, which both excites the internal energy modes and heats the gas. Thus, on the right-hand side of Equation 1.2, the internal energy term is dominant over the kinetic one $h_1 \gg v_1^2/2$. These approximations yield Equation 1.3, where c_p is the gas specific heat capacity at constant pressure,

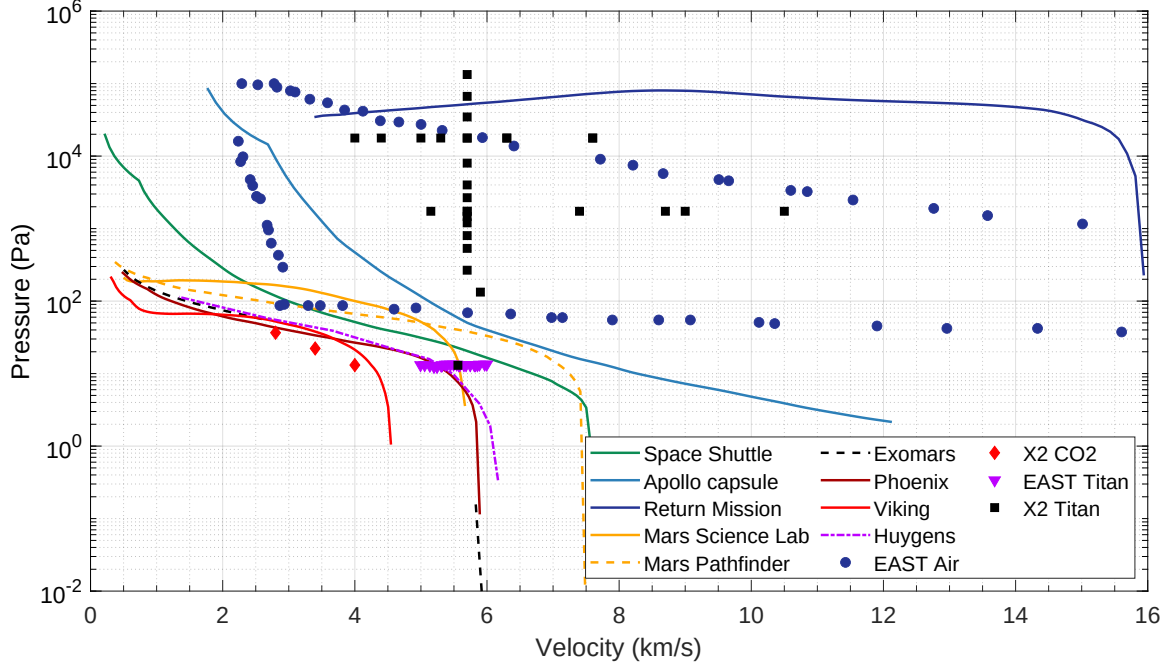


Figure 1.2: Entry conditions for different spacecraft. Space Shuttle, Apollo and Return mission in Earth ($\text{N}_2\text{-O}_2$); Mars Science Lab, Mars Pathfinder, ExoMars, Phoenix and Viking on Mars ($\text{N}_2\text{-CO}_2$); and Huygens in Titan ($\text{N}_2\text{-CH}_4$). Adapted from [7].

$$\frac{v_0^2}{2} \approx h_1 = c_p T_{eq} \quad . \quad (1.3)$$

The gas total specific heat capacity can be split into two contributions, a constant frozen-gas term c_{pf} (for an ideal gas), and a contribution from the internal degrees of freedom of each chemical specie i ,

$$c_p = c_{pf} + \sum_i h_i \left(\frac{\partial c_i}{\partial T} \right)_p \quad , \quad (1.4)$$

where c_i and h_i are the mass fraction and specific enthalpy of specie i . Inserting Equation 1.4 into 1.2, and solving for T , we get Equation 1.5a and both its upper and lower temperature limits, T_{max} 1.5b, and T_{eq} 1.5c, respectively.

$$T = \frac{h}{c_{pf} + \sum_i h_i \left(\frac{\partial c_i}{\partial T} \right)_p} \quad (1.5a)$$

$$T_{max} = \frac{v_0^2}{2c_{pf}} \quad (1.5b)$$

$$T_{eq} = \frac{v_0^2}{2c_p(T_{eq})} = \frac{h}{c_{pf} + \sum_i h_i \left(\frac{\partial c_i}{\partial T_{eq}} \right)_p} \quad (1.5c)$$

The expected post-shock temperature range of the gas is shown in Figure 1.3. The upper limit (full line) is the so-called frozen limit, where all the chemical reactions are ignored, and thus $c_p = c_{pf}$; and the lower limit (dashed line) is the final temperature after chemical equilibrium is reached.

Planetary exploration missions have significantly broadened in scope to include missions as far as the

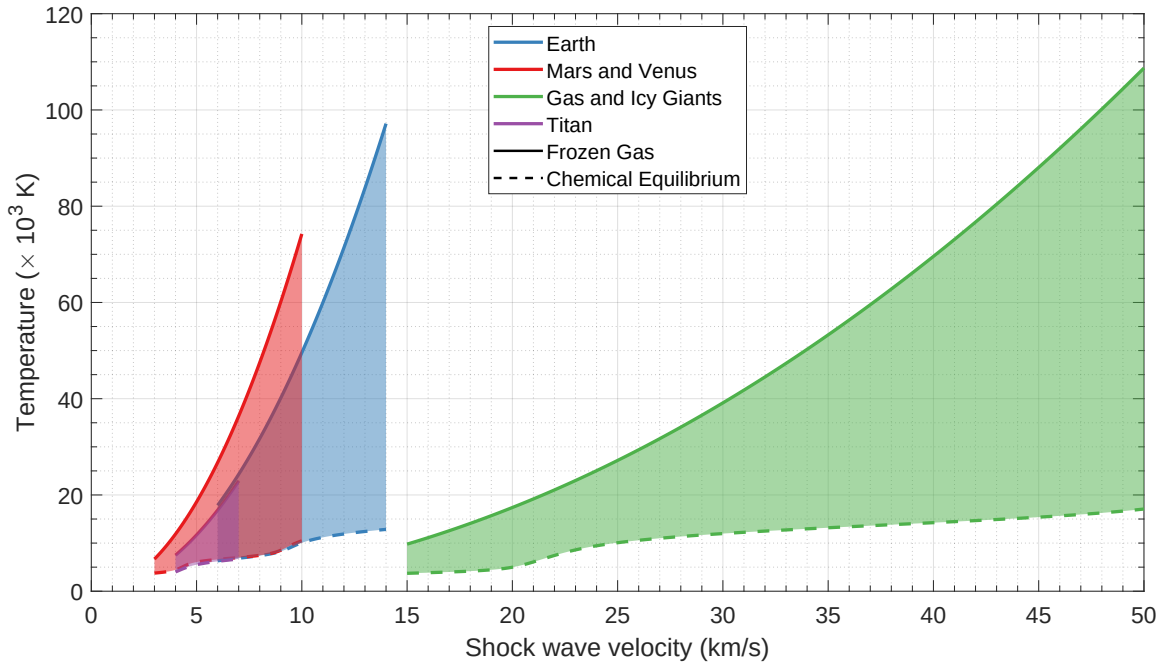


Figure 1.3: Temperature range for different entry conditions and atmospheres. The upper and lower limit corresponds to a calorifically perfect and chemical equilibrium gas, respectively. Full line for the frozen gas condition and the dashed line for the chemical equilibrium one.

Icy Giants or sample return missions from Earth's neighbour planets. Such mission profiles mandate trajectories that entail large entry speeds. On the other end of the spectrum lighter celestial bodies, such as Mars and Titan, mandate lower entry speeds. This means that the scope and range for impulse facilities have been significantly extended beyond the traditional Earth orbital entries.

Akin to the other world's space agencies, the European Space Agency (ESA) supports fundamental and applied research on atmospheric entry plasmas to advance European planetary exploration endeavours. The earliest concerted studies on atmospheric entry flow at European level were carried out in the scope of the HERMES space shuttle development program [9], where two shock tube facilities were developed to support this endeavour: the HEG shock tunnel at Göttingen, which acted as an aerodynamic shock tube facility [10], and the TCM2 shock tube at Marseilles, which was devoted to fundamental studies on kinetic and radiative shock-induced processes [11]. The latter could only reach velocities of about 8–9 km/s, which was enough for reproducing a return from Earth's orbit, the Huygens mission (which successfully entered Titan at a velocity of 5.15 km/s in 2004) [12–14] or Mars exploration missions [15–17], but not enough for recreating an Earth superorbital entry (11–12 km/s) [18]. However, the renewed ambitions at the beginning of the century, namely the Mars Sample Return mission, required a higher performance facility. In consequence, a competitive tender for the development of a novel facility was launched by ESA in 2009.

1.2 Scope

The development of a new state-of-the-art shock tube facility was awarded in 2010 by ESA to an international consortium led by the Institute for Plasmas and Nuclear Fusion (IPFN), a research unit

of Instituto Superior Técnico (IST) [19, 20]. The European Shock Tube for High-Enthalpy Research (ESTHER), was developed by an international consortium which was comprised (besides IPFN) of Fluid Gravity Eng. (FGE), Université de Provence, Ingénierie et Systèmes Avancés, and Instituto de Soldadura e Qualidade, among others. The facility is in its final qualification phase [21] as of 2023.

ESTHER is a combustion-driven two-stage shock tube capable of reaching superorbital shock velocities, its main focus is the study of shock layer radiation. The collected data is implemented into Aerothermal databases, which are then used for the validation of computational fluid dynamics (CFD) codes. The numerical simulation from CFD help design the overall spacecraft geometry. Alongside this, plasma wind tunnels are used to test the TPS materials ensuring these can withstand the expected convective and radiation heat. A diagram of this procedure is shown in Figure 1.4.

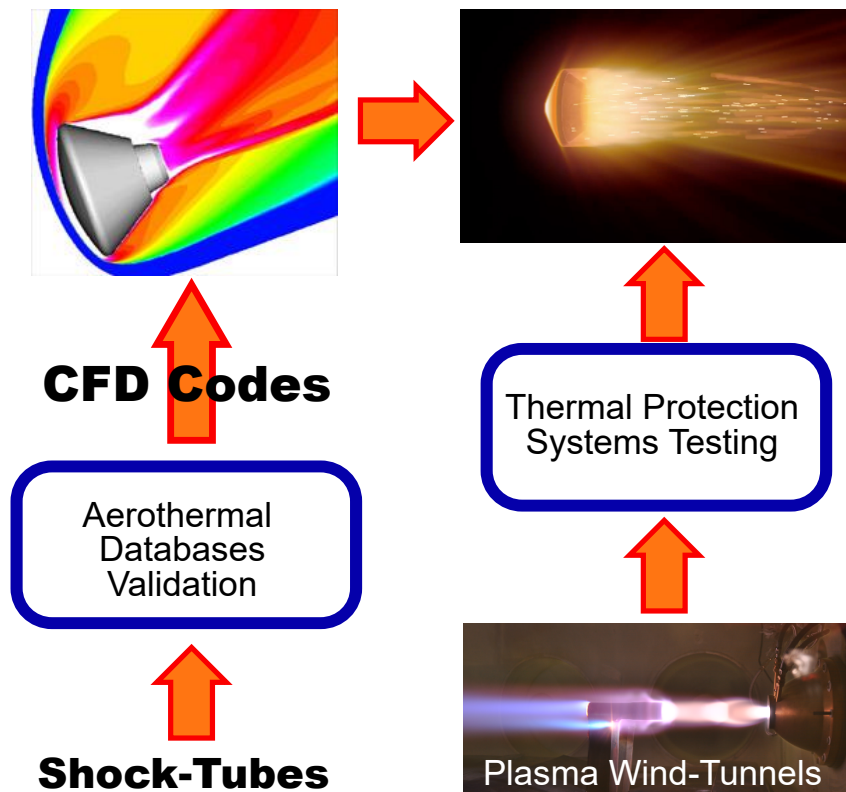


Figure 1.4: Thermal protection system design steps and procedure.

A shock tube, in its simplest form, is composed of a high-pressure driver section and a low-pressure driven section, separated by a diaphragm. The separating diaphragm will rupture at a pre-set pressure value, creating the shock wave which will excite the test gas simulating the desired entry conditions. The performance of a shock tube is mostly dictated by the performance of its driver and thus requires a high-performance driver. Alongside the high-pressure driver, the associated diagnostics and instrumentation must also be high-performance and state-of-the-art.

This work focus around the qualification of the individual parts and sub-systems of ESTHER, and their integration for the complete and fully operational ESTHER facility. It addresses the high-pressure combustion driver, and the low-pressure shock tube section and its associated diagnostics. The qualification results are compared to the specifications and requirements, and correction and/or mitigation

strategies are applied when needed. Additional parts and diagnostics equipment were also designed and manufactured.

The layout of this thesis is as follows: Chapter 2 presents a review of the different types of hypersonic ground test facilities, their driver technologies and associated research. A special focus is put on reviewing shock tube facilities directly comparable to ESTHER. The mathematical formulation for shock wave formation and propagation in a shock tube is detailed in Chapter 3. The overall mechanical design for ESTHER and its auxiliary systems is discussed in Chapter 4. Direct contributions include participation on the water proof test and design and drawing of the shock wave detectors used on the compression and shock tube section. A detailed view on the combustion driver design, its gas filling system and laser ignition system are presented in Chapter 5. The results of the driver qualification campaign, high-pressure combustion physics, and ESTHER predicted performance envelope are also discussed there. Laser alignment, data processing and visualization of the driver combustion process and effects were done by the author. The performance map computations and visualization were also done by the author. Chapter 6 addresses general vacuum technology and its application to the ESTHER compression tube and test sections. The corresponding vacuum tests are also discussed. The author took part in designing mechanical parts for the gas filling system in the compression tube and vacuum tests, as well as, participating on the multiple vacuum tests and employing of the respective mitigation strategies. The state-of-the-art diagnostics setup and qualification are presented in two separated Chapters. The first is the optical emission spectroscopy, presented in Chapter 7, and the second one is a microwave interferometer/reflectometer that is discussed in Chapter 8. The author participated in the design and acceptance tests of the optical plug for the UV-VUV spectroscopy setup. Alongside it, the author adapted a design from the X2 to be used as the optical plug for the mid-IR spectroscopy and made a pre-selection of the setup equipment parts. Regarding the microwave diagnostics, the author computed the specifications and requirements of the equipment, through CFD simulations, took part on the equipment design and data visualization of the wave propagation code results, and performed the proof-of-concept test and subsequent results. Lastly, the main conclusions and outcomes of this work are summarized in Chapter 9, alongside future steps to take on the development of ESTHER.

1.3 Outputs

This work has yielded the following peer-reviewed articles:

1. “*Reflectometry diagnostics for atmospheric entry applications: state-of-the-art and new developments*” - R. Grosso Ferreira, J. Vicente, F. Silva, B. Gonçalves, L. Cupido, M. Lino da Silva, CEAS Space Journal, February 2023
2. “*VUV to IR Emission Spectroscopy and Interferometry Diagnostics for the European Shock Tube for High-Enthalpy Research*” - R. Grosso Ferreira, B. B. Carvalho, L. L. Alves, B. Gonçalves, V. F. Villace, L. Marraffa, M. Lino da Silva, Sensors, June 2023
3. “*Unfocused laser ignition of high-pressure He-H₂-O₂ combustible mixtures*” - R. Grosso Ferreira,

B. B. Carvalho, J. Rodrigues, R. Rodrigues, A. Smith, L. Marraffa, M. Lino da Silva, International Journal of Hydrogen Energy, March 2024

4. “*Combustion of high-pressure hydrogen diluted mixtures in the ESTHER shock tube*” - R. Grosso Ferreira, B. B. Carvalho, R. Rodrigues, L. Marraffa, L. L. Alves, B. Gonçalves, M. Lino da Silva, submitted to the Journal of Thermophysics and Heat Transfer

Furthermore, parts of this work were also presented as an oral presentation at the following scientific conferences,

1. “*A Compact FMCW Reflectometer for the Monitoring of Atmospheric Entry Flows*” - R. Grosso Ferreira, J. Vicente, F. Silva, J. Dias, A. Silva, M. Lino da Silva, L. L. Alves, and B. Gonçalves - 8th Workshop on High-Temperature Gases in Atmospheric Entry (RHTGAE) - March 2019
2. “*Qualification of the European Shock-Tube for High Enthalpy Research*” - M. Lino Da Silva, R. Grosso Ferreira, R. Rodrigues, L. L. Alves, B. Gonçalves, A. Smith, J. Merrifield, V. Villace and L. Marraffa - American Institute of Aeronautics and Astronautics Science and Technology Forum and Exposition (Scitech) - January 2020
3. “*Qualification Campaign of the European Shock-Tube for High-Enthalpy Research Combustion Driver*” - R. Grosso Ferreira and M. Lino da Silva, 8th Edition Space Propulsion Conference - May 2022
4. “*High-pressure Combustion and Qualification of ESTHER Shock tube Driver*” - R. Grosso Ferreira, M. Lino da Silva, B. Brotas Carvalho, and R. Rodrigues - 2nd International Conference on Flight Vehicles, Aerothermodynamics and Re-entry Missions Engineering (FAR) - June 2022
5. “*High-pressure Combustion and Qualification of ESTHER Shock tube Driver*” - R. Grosso Ferreira, M. Lino da Silva, B. Brotas Carvalho, and R. Rodrigues - 9th Workshop on High-Temperature Gases in Atmospheric Entry (RHTGAE) - September 2022
6. “*Reflectrometry Diagnostics for Atmospheric entry applications: State-of-the-art and new development*” - R. Grosso Ferreira, M. Lino da Silva, J. Vicente, F. Silva, B. Gonçalves, and L. Cupido - 9th Workshop on High-Temperature Gases in Atmospheric Entry (RHTGAE) - September 2022

or at the following ones as a poster:

1. “*VUV to IR Spectroscopy and Interferometry on ESTHER Shock-Tube*” - R. Grosso Ferreira - PhD Open Days - April 2019
2. “*VUV to IR Spectroscopy and Interferometry on ESTHER Shock-Tube*” - R. Grosso Ferreira, M. Lino da Silva, R. B. Gomes, L. L. Alves, B. Gonçalves, and V. F. Villace - 3rd European Conference on Plasma Diagnostics (ECPD) - May 2020
3. “*VUV Spectroscopy validation for the ESTHER Shock-Tube*” - R. Grosso Ferreira - PhD Open Days - April 2020

Chapter 2

Hypersonic Facilities around the globe

“No matter the era, whichever the society, people always gaze at the stars when young. Then they stretch out their hands and try to catch them. And then one day, they realize their arms are not long enough to catch the stars, and that’s when they grow up.”

- Reinhard Von Lohengramm, The Legend of the Galactic Heroes

This Chapter presents an overview of hypersonic facilities similar to ESTHER around the globe. A summary of their overall and driver designs, working conditions, and test campaigns is presented. A focus is put on shock tubes and similar-style facilities, as well as their diagnostic techniques. Different driver types (cold and heated gas, free-piston, combustion, and electric arc) are discussed and compared. Lastly, an overview of the most commonly deployed diagnostics in shock tube facilities is presented.

2.1 Classes of hypersonic test facilities

Understanding high-enthalpy chemical kinetic and plasma radiation phenomena in atmospheric entries needs dedicated ground test facilities owing to their high energy characteristics. Due to the small time scales for these events, the best suited facilities for these studies are shock tubes, reflected shock tunnels, expansion tubes, hot shot tunnels, and ballistic ranges [22]. In general terms, a shock facility is comprised of three primary assemblies: a driver tube, either free-piston, combustion, or electric arc; a driven tube/test section; and a dump tank. Each section is isolated from the next by a diaphragm and filled with different gas mixtures at different pressures. The driver is filled with a light gas mixture and compressed/heated in some form that it will rupture the diaphragm forming a strong shock wave travelling downstream through the driven tube. This shock will compress and accelerate the test gas forming the hypersonic flow. At the end of the driven tube, the test models may be placed in the test section or the dump tank, if it is the case, and the respective diagnostics. Finally, the dump tank collects all the gases flowing during the experiment. The theoretical framework and how to optimize the working conditions of a shock tube-like facility is discussed in further detail in Chapter 3.

Hot shot tunnels

In hot shot tunnels, a fixed test gas volume is heated by an electric arc discharge in the arc chamber [22]. Pressure and temperature quickly rise, and the test gas is then evacuated towards a low-pressure test chamber [23, 24]. The gas initial pressure, arc power, and arc temporal duration can be adjusted to obtain the desired stagnation conditions. A diaphragm or fast-acting valve separates the arc chamber from the test one, opening when the desired reservoir flow conditions are reached. The arc chamber flow expands through a nozzle, and the gas reservoir pressure decreases. The experimental run time ends when the reservoir is depleted or the test gas conditions fall below a threshold. The major advantages and limitations of hot shot test facilities are presented in Table 2.1.

Table 2.1: Major advantages and limitations of hot shot test facilities, adapted from [22].

Advantages	Disadvantages
• Large Mach and Reynolds numbers map • Relatively long test time • Moderately high enthalpy • Test gas choice • Wide range of available test techniques	• Time varying test conditions • Flow contaminants • Productivity • Cold wall conditions only • Limited air chemistry • Free-stream uncertainties

Shock heated

Heating a gas by processing it with a shock wave has long been identified as an effective way of producing high temperatures in a fluid [22]. The primary shock wave created by the diaphragm rupture suddenly accelerates and compresses the driven gas, increasing its pressure, density and temperature [25]. All shock tubes, shock tunnels, and expansion tubes work within those principles, yet small differences distinguish the three of them. When compared to hot shot facilities, shock heated facilities have advantages and limitations as shown in Table 2.2.

Table 2.2: Major advantages and limitations of shock test facilities, adapted from [22].

Advantages	Disadvantages
• High enthalpy • Wide range of free-stream conditions • Wide range of available test techniques • Choice of test gas	• Short test times • Flow contaminants • Productivity • Dissociated free-stream • Cold wall conditions only • Free-stream uncertainties

Both reflected and non-reflected shock tunnels are limited in terms of the stagnation enthalpy they can simulate. This is due to all the energy added to the flow through shock waves. At very high shock speeds, the flow exists as a highly dissociated plasma, useful for post-shock planetary entry radiation studies, but not for aerothermodynamic studies. These types of test facilities are normally limited to speeds up to, and including, Earth orbital velocity (8 km/s) [26].

2. HYPERSONIC FACILITIES AROUND THE GLOBE

Non-reflected shock tubes like the Electric Arc Shock Tube (EAST) at NASA Ames [27–29], and ESTHER [21] are typically used for planetary entry radiation studies. This is due to the non-reflected shock tubes’ ability to generate faithful post-shock conditions. However, the low-densities of shock tube flows [30–32] mean that the test times are very short, hundreds of microseconds at most. These small test times make it more difficult to establish a useful flow over a test model when compared to other hypersonic facilities, and thus shock tubes are usually reserved for radiation and chemical kinetics studies. Mudford and Stalker [33–35] developed a technique allowing to generate useful aerothermodynamic flows over test models in shock and expansion tubes. By creating a flow at the hypersonic nozzle before the shocked test gas arrival, in this way a flow around the model could be established despite the low test time and be used on aerodynamic studies.

A reflected shock tunnel works similarly to a non-reflected shock tube, as the test gas is shocked and reflects off a wall, and shocked again, creating a reservoir of gas at high temperature and pressure. This gas reservoir can be directly probed for high temperature gas behaviour studies, or slowly bled out through a de Laval nozzle, creating hypersonic flows. The bleeding of the gas reservoir allows for reflected shock tunnels to produce much longer test times than non-reflected facilities. As a downside, these require different nozzles to generate different flow velocities. At the nozzle exit, the flow both accelerates hypersonically and cools down, enabling the reproduction of flight conditions [25]. Despite the temperature drop at the nozzle expansion, the test gas may not recombine [36, 37]. This “frozen” flow is not fully representative of flight conditions and CFD is needed to accurately computed what were the experimental flow conditions. Reflected shock tunnels have been used for a variety of hypersonic tests, namely, planetary entry heat transfer [38–43], scramjet flight [44–46], or different fundamental studies of hypersonic flow-fields [43, 45, 47, 48].

The expansion tube, first proposed by Resler and Broxson [49], is a conventional shock tube with an additional expansion section downstream of the driven tube containing a low-pressure light acceleration gas [50]. After the initial shock processing of the test gas, an unsteady expansion fan adds further energy to the test flow. This is done by accelerating it, increasing its kinetic energy while maintaining a relatively low-temperature [51]. In this way, the flow accelerates, increasing its enthalpy without dissociation or ionization [52]. These type of facilities have the drawbacks of having shorter test times than reflected shock tunnels and having large boundary layers, yet they nonetheless allow for very high Reynolds number flows and high enthalpy flows, like those of a planetary entry [52].

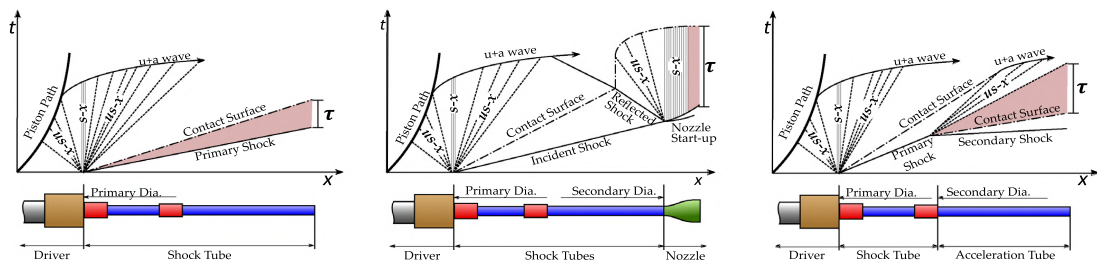


Figure 2.1: Diagrams for different types of hypersonic shock tubes, adapted from [53]. Non-reflected shock tube (left), reflected shock tunnel (middle), and expansion (tube) with the respective $x - t$ diagrams

Other

The other types of ground testing facilities considered to study hypersonic phenomena operate at relatively lower velocities and longer test times. Among them we have arc-jets [54, 55], inductively-coupled plasma (ICP) torches [56–58], and plasma wind tunnels [59]. These are high enthalpy test facilities whose test times are long enough for the test model to reach steady-state temperatures, where hot-wall and ablation tests can be performed. Despite being able to re-create flow stagnation enthalpies, they do not reproduce the velocity, and (accordingly) neither the real aerodynamic flowfield. Their upstream flow thermal and physico-chemical state is also not representative of the expected flight conditions. Because of this, such long-duration facilities are more often used for materials testing, as they allow representative heat loads to be re-created and the test model to be measured before and after the experiment, quantifying the material lost through the surface recession. Arc jets and plasma wind tunnels can generally only produce test flows with low Reynolds numbers and low stagnation pressures owing to the way the flow is generated. Besides, this results in high levels of thermal non-equilibrium in the test flow. On the other hand, plasma torches typically operate at much higher pressures, and Laux in [56] shows that the test flow may be in thermochemical equilibrium. Examples of experiments on the interaction of a test flow with ablating test models are found in a plasma torch [57], and in a plasma wind tunnel [60].

2.2 Driver types

A shock tube may generate a shock wave by multiple methods, each with its advantages and disadvantages. A shock tube driver’s goal is to increase the local sound speed and pressure as high as possible. This will yield faster shock waves in the test section. To do so, one must increase the driver gas pressure and temperature while keeping a low molecular weight. Chapter 3 presents the mathematical models describing the shock wave behaviour in a shock tube environment. The most common drivers are:

- Cold gas driver
- Heated driver
- Free-piston
- Combustion/detonation driver
- Electric arc driver

Cold and heated drivers In a cold driver, gas is injected into the driver, increasing the pressure until the burst of the diaphragm occurs. It is the simplest type of driver. Since the gas temperature remains approximately constant, this type of driver yields the lowest performance of all [49]. In an electrically heated driver, the gas is heated up by an external or internal source, such as an electric blanket, or heating filaments [49]. Since external heating depends on the vessel walls’ heat conductivity, this can limit the peak pressure. Internal heat sources can be damaged by the expansion waves moving in the

driver, nonetheless these heat sources do not exert a thermal stress on the vessel walls contrary to external ones. The added complexity of an internal heating mechanism may rule it out as a viable option [49]. Typically, heated gas drivers have driver temperatures in the 700 K region [27]. Cold and heated gas drivers are generally not capable of reaching the required temperature and pressures for the highest shock speed regimes.

Free-piston driver A free-piston driver works by compressing the driver gas with a piston in a fast process. A very high-pressure gas reservoir will push the piston towards the driver gas, compressing it. As the driver gas reaches its peak pressure and temperature, with the piston near the end wall, the diaphragm ultimately ruptures [61]. The compression process is sufficiently fast to be considered an adiabatic, with the gas theoretically capable of achieving temperatures of 15,000 K at a compression ratio of 200 [61]. The compression process lasts for hundreds of milliseconds, where the piston and vessel materials are under mechanical stress which may lead to damage and fatigue of the driver. Free piston drivers have the added complexity of having the largest number of moving parts compared to the other driver types. This makes these drivers difficult to be tuned so the desired flow conditions are achieved [62]. This design also leads to very long driver lengths.

Combustion driver Combustion drivers use a combustible mixture in the driver to achieve a high temperature and pressure gas. This type of driver may operate in deflagration or detonation mode. While the former is highly repeatable, the latter can reach higher peak pressures, generating faster shocks. Hydrogen-oxygen combustible mixtures are used due to hydrogen's low molecular mass, however a dilutant gas such as helium must be added to avoid strong detonations in the vessel [49]. As the combustion process progresses, the pressure and temperature in the driver chamber rise leading to the diaphragm rupture. Usual temperatures for combustion drivers go to 2,500 K region [27]. ESTHER operates as a deflagration combustion driver. Its design and performance are discussed in Chapter 5.

Electric arc driver Electric arc drivers heat the gas through an electric discharge, with temperatures up to 8,000 K in hydrogen, or 20,000 K in helium [27]. Typically, a copper wire connects an outer ring of the diaphragm to the driver end wall crossing the driver. The discharge energy source is often a capacitor bank, and the trigger can be a mechanical actuator or an electrical switch (lovotron). When the trigger is pulled, a strong electrical discharge passes through the wire heating the gas and rupturing the diaphragm. The voltage difference is usually high enough to form an electric arc between the wire and electrode when these are sufficiently close [27]. Because of this electric arc, ionization of the driver gas can occur, which in turn further heats it to the previously mentioned temperatures. The short discharge time duration helps prevent damage and ablation of the driver's vessel [27]. Electric arc drivers have the highest performance owing to the very high temperatures they reach, however, the stochastic nature of an electric arc limits its repeatability between experimental runs. Pre-ionization of the test gas flow may also occur, owing to the strong radiation from the ionized driver gas.

2.3 Review of main hypersonic facilities

The following review is focused primarily on the worldwide facilities used to investigate chemical kinetics and plasma radiation, similar to the ones encountered during atmospheric reentry at superorbital velocities. Other ground test facilities capable of reaching orbital and suborbital velocities will also be briefly mentioned. Reynier in [63] performed a very thorough review of hypersonic ground test facilities. A global outlook of the hypersonic facilities is depicted in Figure 2.2.

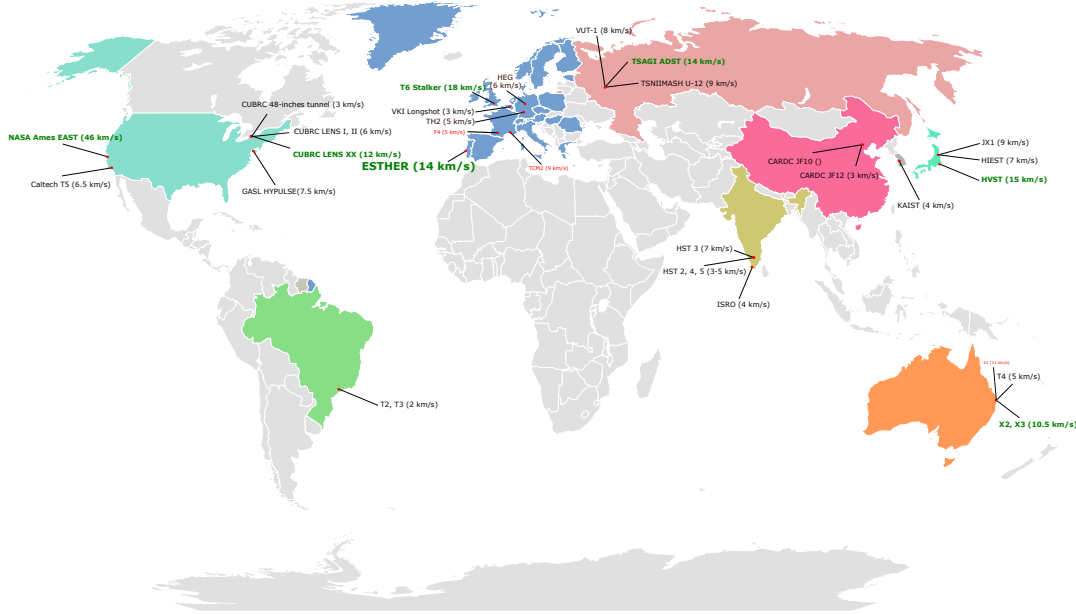


Figure 2.2: World map with hypersonic facilities, adapted from [7].

2.3.1 Europe

Europe owns a large number of shock tubes and shock tunnels. Due to their large number, this review will only focus on the facilities whose performance is close to that found in atmosphere entries. A review of these facilities can be found in [64–66]. The only non-shock tube/tunnel facility discussed is the F4 Onera hot shot that was extensively used for experimental simulation of Mars entry conditions, which adds contributions to aerochemistry and aerothermodynamics [67, 68]. The review will focus on:

- DLR HEG shock-tunnel;
- HTG HHK Ludwig tube;
- IUSTI TCM2 (today closed);
- ONERA F4 hot shot;
- RWTH Hypersonic shock tunnel TH2;
- UMIST shock-tubes;
- VKI Longshot;

- T6 Stalker

DLR's HEG shock tunnel The High Enthalpy shock-tunnel Göttingen (HEG) is the DLR shock tunnel located in Göttingen. The facility is a free piston-driven shock tunnel for the experimental simulation of entry flows up to 6 km/s. It is capable of working with both air and CO₂ test gases. HEG nominal operational conditions may be found in [63]. The facility is equipped with a wide range of diagnostics, pressure transducers and thermocouples, as well as dynamic measurements through the use of a force balance. Non-intrusive techniques such as Schlieren visualisation system, diode laser absorption spectroscopy, and holographic interferometry for density field measurements are also available.

HEG has been extensively used for European projects involving Earth atmospheric entry, in the frame of the ARD mission [69], X-38 project [70], and more recently on SHEFEX project [71]. Tests on CO₂ flows have been done in the frame of the ExoMars project [72] for building the aerodynamic database [73]. The measurements performed with HEG focus mostly on aerodynamic characteristics associated with entry capsules, their forces and moments. Other experimental measurements concern capsule surface pressure and heat-fluxes [73] as well as the assessment of flow topology.

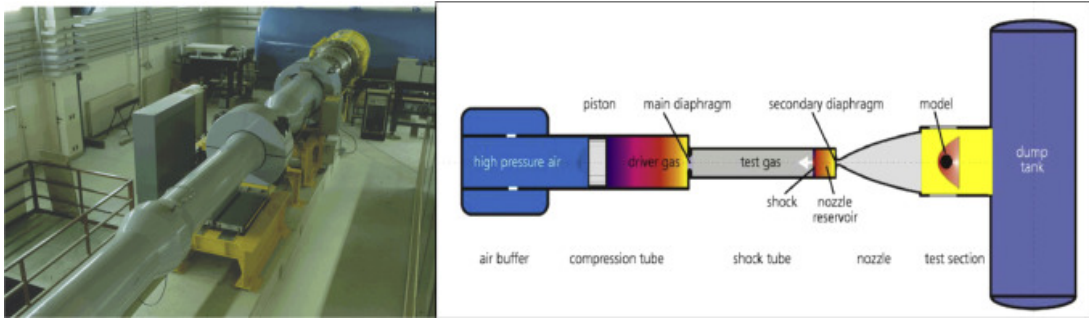


Figure 2.3: Left: View of HEG; Right: Facility layout with shock tube (grey), nozzle (metallic), test section (yellow) and dump tank (blue), adapted from [63].

HTG HHK facility The HHK is a Ludwig shock tube owned by the HTG company located near Göttingen, Germany. A Ludwig tube is a simplified blow-down wind tunnel whose storage tank is replaced by a tube, upstream of the convergent-divergent nozzle. These allow running steady flows for 60 to 100 ms and different gases [74]. The HHK has a 250 mm test section running cold hypersonic flow conditions, up to now of air, CO₂ and CF₄ gases. The nominal working range for HHK is between Mach 6 and 9 in air, and up to Mach 7 in CO₂.

The facility instrumentation includes a pressure distribution, 3-axis component forces, and thin skin heat transfer; additionally, infrared thermography with an external camera for Schlieren flow visualisation is available. HHK has been used for multiple ESA aerothermodynamics activities related to atmospheric entries of capsules and instrumentation testing [75]. In particular, the IRDT project had an experimental campaign performed at HHK, where the flow topology around the capsule was analysed at Mach 9.

IUSTI TCM2 Shock Tube TCM2 was a free piston shock tunnel built at IUSTI in Marseille, according to the Stalker principle [61]. Experiments had a running time of 1 ms, with enthalpies from 3.5

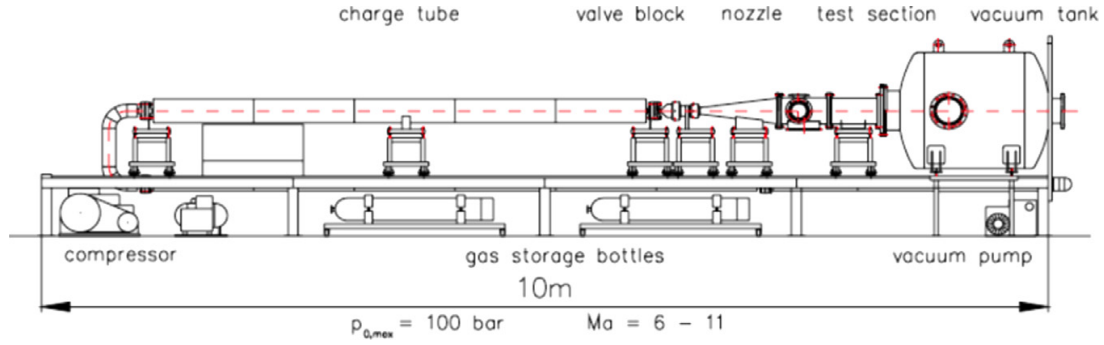


Figure 2.4: HHK Ludwig tunnel facility, adapted from [63].

up to 11 MJ/kg. TCM2 had the following available instrumentation and diagnostic techniques:

- NO behaviour in shock layers by Laser Induced Fluorescence (LIF);
- N₂ behaviour in shock layers by Raman scattering;
- Time-resolved spontaneous emission spectroscopy using a streak camera in near UV (CN), visible (C₂) and infrared (CO₂);
- Laser absorption spectroscopy (CO₂).

A multitude of different gas mixtures could be tested in TCM2, including air, CO₂, N₂, CH₄, and argon. The local climatic conditions in Marseilles, owing to the low moisture level in the air, allow TCM2 to have improved cleanliness when compared to similar-size facilities. A photograph of TCM2 is shown in Figure 2.5.

The TCM2 shock tube at Marseilles was devoted to fundamental studies on kinetic and radiative shock-induced processes [11]. It could reach velocities of about 8–9 km/s, enough for reproducing an orbital Earth return, the Huygens mission [12–14] or Mars exploration missions [15–17]. The optical emission visible spectroscopy setup used in the TCM2 shock tube facility was ceded to ESTHER after the de-comission of TCM2, and is described in Chapter 7.

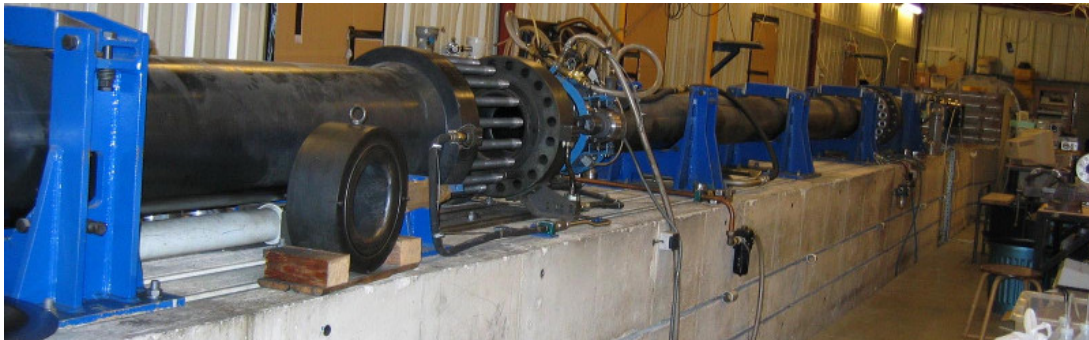


Figure 2.5: TCM2 shock tube

ONERA F4 hot-shot The ONERA F4 hot-shot wind tunnel is based in Le Fauga, near Toulouse. It was built between 1988 and 1991 for supporting the HERMES project, was used to research hypersonic

spacecraft re-entries with real gas effects, and is presently cocooned. An impulse generator with 150 MW of power supplies electrical energy to the electrodes. F4 has an usual blow-down duration of 200 ms with an enthalpy of 16.5 MJ/kg. The flow conditions can range from Mach 7 to Mach 21, with stagnation temperatures up to 7,500 K. Onera F4 can be equipped with different nozzles and has 18 wall pressure and heat fluxes measurement points are available [76].

The aerodynamic forces and moments, flow topology, and composition are analysed. Balances for force measurements with three and six-components are used for blunt body capsules and complete space plane models respectively. Besides these, measurements of wall pressure, heat-fluxes, and hinge moments on the model elements and control surfaces through all three-components are also accessible. The flow characterization is done through non-intrusive techniques, capable of measuring velocity, temperature, and density of NO, CO, CO₂, and O₂ species. These include:

- Line of sight techniques;
- Schlieren photography and high-speed camera imaging;
- Emission spectroscopy;
- Laser diode emission spectroscopy;
- Electron Beam Fluorescence and Coherent anti-Stokes Raman scattering techniques;
- Infrared thermography.

ONERA has greatly developed the DLAS (Diode Laser Absorption Spectroscopy) technique for CO₂ flows [67, 77]. This technique can give measurements of velocity, temperature and species density (for NO, CO, CO₂, and O₂).

F4 hot shot main studies are those of aeroheating and measurements of aerodynamic efforts. Multiple European projects, such as ARD, Mars Premier, Pre-X, IXV, Expert and ExoMars have been investigated in F4 [68]. Particular important results for the ExoMars project were obtained due to the extensive experience of tests with CO₂ for the Mars Premier project [78]. The facility has been adapted for scramjet testing up to Mach 12 in 2011 and 2012 [79, 80]. A nozzle-throat of F4 hot shot is shown in Figure 2.6.

RWTH hypersonic shock tunnel TH2 The RWTH Aachen University operates a hypersonic shock tunnel named TH2 [81]. It has a running time of between 2 and 10 ms, for an enthalpy range from 1.25 to 14.6 MJ/kg. The flow velocities go up to 4.8 km/s, yielding a stagnation temperature up to 7,400 K. TH2 instrumentation includes pressure probes, thermocouples and infrared thermography for heat-flux measurements, shadow and Schlieren imaging using high-speed cameras, and force balances.

The majority of the test campaigns in TH2 have been carried out with air. Tests in this facility have been focused on Earth re-entry, in European projects such as Hermes, HOPPER [82] and FESTIP. Furthermore, fundamental simulations like investigation on film cooling and shock wave/boundary-layer interaction [83] have also been carried out.



Figure 2.6: ONERA F4 nozzle-throat.

UMIST shock-tubes The University of Manchester possesses several shock facilities, with the HST2 operating in the hypersonic regime. It has a short state test time of 500 μ s-10 ms, and can operate in air or argon. Free stream velocities are in the 1.5 km/s region, with enthalpies around 4 MJ/kg. The shock tunnel is a stainless steel shock tube with 50 mm diameter connected to a convergent/divergent de Laval nozzle of 300 mm exit diameter. The test section is 450 mm long and has a 300 $\times$ 300 mm² square cross-section.

The facility is equipped with the following instrumentation and measurement techniques: a three-component force balance, pressure transducers, liquid crystals, pressure-sensitive paints, LIF, PIV (Particle Image Velocimetry), pitot-static tubes, thermal probes, optical skin friction measurement, heat transfer measurement, laser sheet, double exposure holographic interferometry, thermographic phosphors, high-speed Schlieren and shadowgraph imaging.

HST2 is mostly used for investigating aerodynamic phenomena in the low supersonic regime. It has allowed the investigation of Göertler vortices for hypersonic Mach numbers [84], and the development of measurement techniques, such as phosphor thermometry or pressure sensitive paint [85, 86].

VKI Longshot The VKI Longshot is a free piston wind tunnel with very high stagnation pressure and short test times. It has been designed to reach very high Reynolds number hypersonic flows and is operated with air, nitrogen or carbon dioxide [87]. A schematic of this facility is shown in Figure 2.7.

It can measure the aerodynamic forces and moments of shuttle-type vehicles using a six-component strain gauge balance, or a three-component strain gauge balance for capsule-like models. The surface model heat transfer and pressure distribution can also be determined with thermocouples and pressure transducers. Flow characterization through Schlieren visualization and heat flux and pitot probes is also possible. Diode Laser Absorption Spectroscopy has more recently been applied [89].

Longshot has performed studies on laminar and turbulent heat transfer, transition to turbulence,

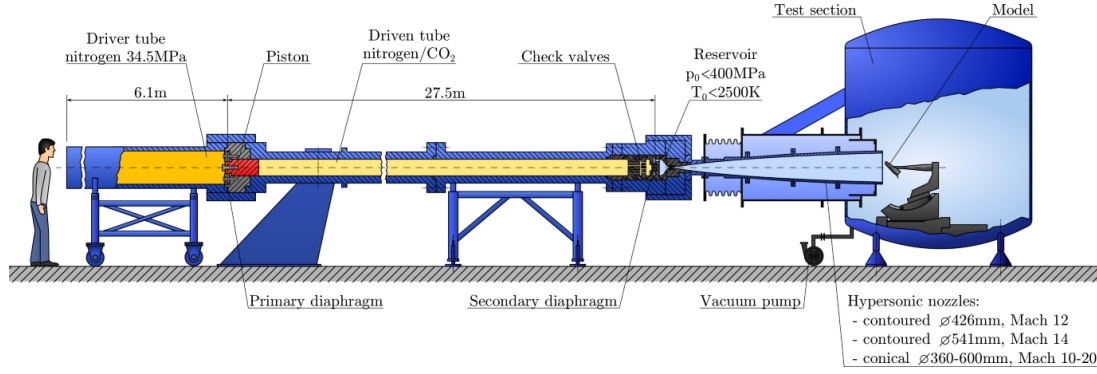


Figure 2.7: VKI Longshot schematic, adapted from [88].

static and dynamic stability, flow-field topology around aerodynamic bodies, and aerodynamic forces and moments. The facility has been broadly used in European projects involving entries of capsules and demonstrators, namely MSRO [78], Expert [90] and IXV [91].

T6 Stalker Tunnel The T6 Stalker tunnel is a collaboration between the University of Oxford and the University of Queensland Centre for Hypersonics [92]. T6 is formed by coupling the decommissioned T3 free-piston driver, to the barrels, nozzle, and test section of the Oxford Gun Tunnel. Its design allows the facility to operate as either a reflected shock tunnel, an expansion tunnel, or a shock tube, with two test sections in steel or aluminium, [92]. Shock speed ranges from 4 to 18 km/s (the latter in lighter hydrogen/helium mixtures), depending on the configuration and test gas.

The shock tube modes are suited for shock-layer radiation. The steel shock tube has the highest performance and is designed for Earth return superorbital entries, or entries into ice giants planets. The aluminium shock tube was specially designed for radiation studies to reduce carbon contamination when compared to a steel one, and by having large 200 mm long optical windows. Besides this, its large diameter helps twofold: firstly by having a boundary layer that is relatively thinner in comparison with the tube diameter, resulting in a more uniform core flow, less shock deceleration and a longer slug of test gas. Secondly, the larger diameter yields a greater integration path length for optically thin radiation, which increases the signal-to-noise ratio. The reflected shock tunnel is designed for longer test times but has the drawback of having a lower test speed.

The reflected shock tunnel operation mode is primarily designed for scale model testing. These include scramjet engine testing, boundary layer transition and hydrogen ionisation studies. In this mode, a typical test time of 1 to 10 ms is expected, and the shock speed does not exceed 6 km/s [92]. Lastly, the expansion tube mode, as discussed before, allows for the test of high-enthalpy flows at greater velocity and pressure by sacrificing the useful test time, 10–100 μ s, [53, 92]. Figure 2.8 depicts an illustration of the T6 Stalker tunnel and its different operational modes.

Oxford's T6 has several diagnostic equipments for the different operational modes. Most of the equipment is interchangeable and adaptable between operational modes and setups, thus no specific setup will be discussed. The operational/running T6 equipment is, extracted from [92]:

- High-Speed Pressure PCB 132 high-speed transducers, Kulite XTL-140 high-speed transducers;

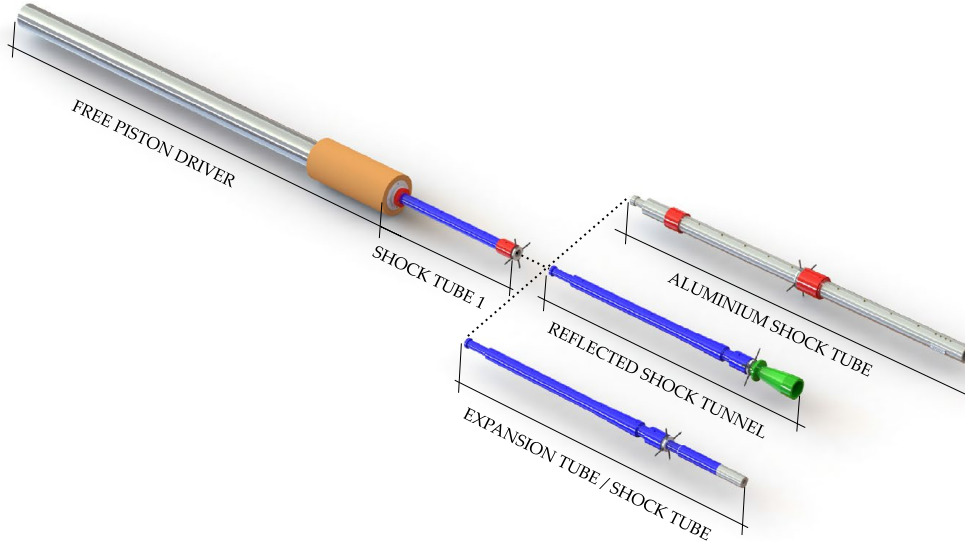


Figure 2.8: Oxford university T6 Stalker tunnel, adapted from [53].

- High-Speed Video Specialised Imaging Kirana (5 MHz for 250 frames) and Photron Mini UX-100 (1 MHz, reduced capture window, 3 s) and 1 MHz LED light source;
- National Instruments PXE-8135 with several PXIE-6368 (2 Hz/channel) and PXIE-6363 cards (2 MHz aggregate). Free-flight DAQ (Data Acquisition) system, 6 channels, 20 kHz with signal conditioning;
- Oscilloscope Teledyne Lecroy 5 GHz, 4 channel;
- Laser Continuum Powerlite Nd:YAG laser, Low noise Oxxium DPSS at 532 nm, and Laser Quantum DPSS at 671 nm;
- Hammamatsu streak camera C4880 on loan from ESTHER.

The hypersonic flow and radiation are mainly characterized by a pitot rake, and different spectroscopy setups, as shown in Figure 2.9. The pitot rake has 11 measurement points separated by 20 mm each. It can be used in expansion tube or reflected shock tunnel mode [93, 94]. T6 has a series of UV-enhanced aluminium mirrors (Thorlabs series F01) [95], two spectrographs (a Princeton Instruments Isoplan-320 and a McPherson 218), respectively coupled to an Andor Intensified sCMOS camera and a Princeton Instruments PI-Max2 iCCD [96]. Different diffraction gratings can be mounted in the spectrographs. These are 150, 180, or 200 gr/mm, and 500 nm longpass filters [95, 96]. The exact spectral range of the setups varies depending on the filters and gratings but can range from 210 to 885 nm. Calibration is done using a Bentham Instruments SRS8 integrating sphere, known spectral sources (deuterium or mercury lamps), or a neon-argon laser.

The T6 facility has been involved in a recent number of studies for the Mars Sample Return missions [53, 95], exploration to Ice Giants [93, 97, 98], and general shock layer radiation measurements [96, 99, 100].

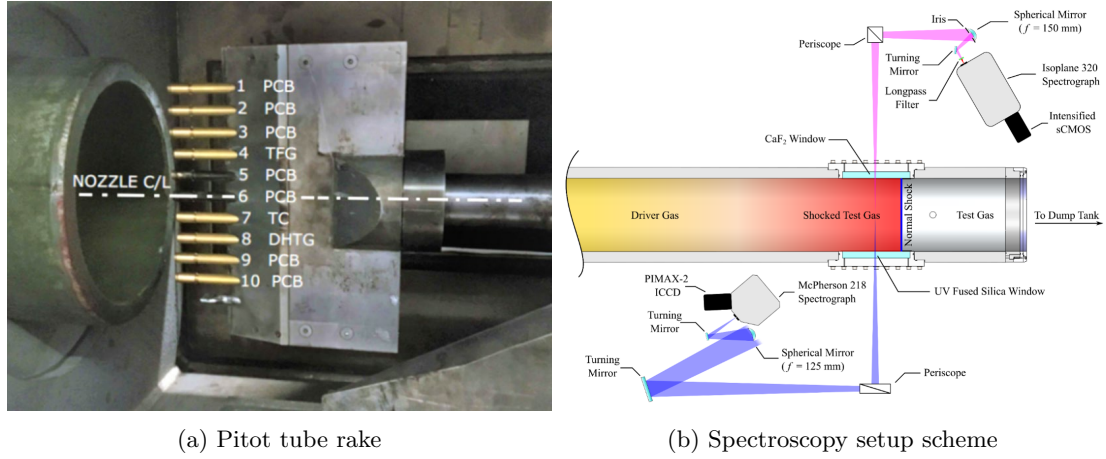


Figure 2.9: T6 Stalker tunnel diagnostics, pitot tube rake (left), and Ultraviolet-Visible (UV/VIS) and Visible-Near Infrared (VIS/NIR) spectroscopy setup (right), adapted from [53] and [96], respectively.

Other facilities A recently developed shock tunnel is operating since 2010 at the University of the Federal Armed Forces in Munich [101], capable of reaching velocities of 5 to 7 km/s for a total enthalpy of 20–25 MJ/kg [102]. Other shock tubes operate at ISL, Gas Dynamics Limited (UK), TU Braunschweig, and several universities and research centres, but are not capable of reproducing Mach number and shock velocities suitable for simulating chemical kinetics and radiation during hypersonic entries.

2.3.2 U.S.A

The US is home to a large number of hypersonic test facilities, for chemical kinetics, radiations, aerodynamic, or material testings. Nonetheless, this review will focus on facilities largely used for radiation and chemical kinetics studies, particularly the NASA Electric Arc Shock Tube (EAST) since it has been extensively used for such purposes. Schneider in [103] presents a summary of US hypersonic ground test facilities. The following ones will be discussed in more detail:

- CALTECH T5 shock tunnel.
- CUBRC LENS facility.
- Expansion tube from Stanford University.
- NASA EAST shock tube.
- GASL HYPULSE facility.

CALTECH T5 shock tunnel T5, located in CALTECH, is a free-piston reflected shock tunnel capable of producing high stagnation pressures and temperatures. It can produce shock flows with enthalpies in the 4–27 MJ/kg region in nitrogen and air, or 3–10 MJ/kg for CO₂. Gas temperatures can reach 10,000 K and shock velocities 6.5 km/s [104, 105]. It yields a test time of 1–5 ms [104]. The facility instrumentation consists of thermocouples, static pressure gauges and multiple optical diagnostics. These include high-speed cameras, Schlieren high-speed imaging, holography, and PLIF (Planar Laser-Induced Fluorescence) for OH measurements.

T5 is extensively used in aerothermodynamic studies of real gas effects around cones or blunt bodies and flows around ballutes for aerocapture. This facility has been extensively used in support and preparation of the Mars Surface Laboratory [105] mission. They assessed heating levels and laminar to turbulent transition [38]. T5 has been largely concentrated on the hypervelocity boundary layer in air and nitrogen, mostly on laminar to turbulent transition [106, 107], boundary-layer noise [108], and shear-layer instability [109]. Besides it, T5 was also used for research on gaseous detonation initiation and stabilisation of hypersonic projectiles [110].

CUBRC LENS facility The Calspan-University of Buffalo Research Center (CUBRC) hosts the Large Energy National Shock tunnel (LENS) [111], a pair of short-duration, high enthalpy shock tunnels, named LENS I and II. The former can produce shocks with a velocity above Mach 15, while the latter can only reach Mach 4-8, yet its 610 mm diameter allows for larger test models and longer test time durations [111]. Both tubes share some common equipment, such as the main compressor, ballast tank, data acquisition systems, and some control and safety measures. An upgrade to the LENS facility allows it to run as an expansion tube named LENS-XX, whose flow can reach Mach 25. LENS works mostly with air or nitrogen as a driven test gas, while employing a helium, hydrogen, or nitrogen mixture as driver gas. Tailoring of the driver conditions allows for LENS to reach a maximum test time of 4 ms.

The expansion tube LENS-XX is capable of simulating flows in the enthalpy range 10–100 MJ/kg, at flow velocities of 2.4 to 12.2 km/s. LENS is equipped with temperature, component friction gauges, static and pitot pressure probes, gas sampling probes, and a six-component aerodynamic balance for aerodynamic measurements [112]. Thin-film heat transfer gauges, coaxial thermocouples, calorimeters, and surface pressure sensors are used for model surface measurements. Optical and non-intrusive diagnostics like high-speed Schlieren imaging, electron beam fluorescence, laser interferometry, and emission spectroscopy are also available.

The spectroscopy setup is divided into two parts, one for the UV-visible region, and another for the visible-near infrared region. The first is comprised of a McPherson 218 monochromator coupled to a Princeton Instruments intensified CCD array. A diffraction grating of 247 gr/mm grants a 140 nm spectral window over the 200 to 500 nm region [113]. Other gratings of 150 and 1,200 gr/mm are also available [114]. A UV quartz window is used here. The second system uses a custom made fibre bundle to transport the light from a Lexan window to an Acton 150 monochromator equipped with a 300 gr/mm diffraction grating covering the spectral windows between 560 and 880 nm. The system is triggered by a Stanford Research delay generator [113]. The LENS facility has been used in the study of viscous flow interaction between air and high-speed flight vehicles, high-speed ramjet type supersonic combustors performance, or entry phenomena into planetary atmospheres [111]. This facility has been part of the support activities of the Orion vehicle through ablation and roughness studies [115, 116]. It also performed radiation measurements of superorbital velocity entries on Earth [113]. Turbulent heating for support of the Mars Science Laboratory (MSL) mission was tested in LENS [117, 118]. The HIFiRE flight experiment instrumentation definition and selection were done with the support of LENS experimental data [119, 120].

Stanford University Expansion tube The Stanford University hosts a 12 m long, 89 mm inner diameter expansion tube [121, 122]. It is a double diaphragm expansion tube with a high-pressure helium driver. The facility is capable of reaching stagnation enthalpies of 3–6 MJ/kg, Mach numbers of 8–12, and a test time of 400 μ s, for an air test gas [122].

Diagnostics-wise, the facility is equipped with lasers and optical arrangement for PLIF [123, 124], tunable Diode Laser Absorption-Spectroscopy (DLAS) [125], and a high-speed camera for Schlieren imaging. A detailed review of this instrumentation is done in [122]. Besides this, the expansion tube has seven piezo-electric pressure transducers to measure the wall pressure and shock velocity along the tube, and a pitot rake in the test section [122].

The expansion tube has been used for combustion studies on scramjets [50, 121], using PLIF and chemiluminescence with investigation of OH in flames [123, 124].

NASA Ames EAST The Electric Arc Shock Tube (EAST) facility, located in the NASA Ames research centre, is an arc-driven shock tube built in 1965 [126, 127]. Its electric arc driver can be connected to a 10.16 cm diameter shock tube or to a larger 61 cm exit diameter nozzle for reflected shock tunnel experiments. The initial driver section was a cylindrical one with 76 cm of length and 19.16 cm of diameter. The diaphragm can be made of steel, aluminium or mylar materials, and it is designed to rupture at one-half to one-third of the expected ultimate pressure [128]. The driver is filled with a light gas, either helium or hydrogen, up to a pressure of 2–14 bar. Shock wave velocities may reach a maximum of 16 km/s in air, however higher velocities were sought, and those required an upgrade to the driver to be able to reach higher gas pressure and temperatures.

This upgrade to the arc driver was made in 1975, replacing the cylindrical driver with a conical one [129]. The lower volume and operational technique, when compared to the original cylindrical one, allows for an energy density five times higher using the same capacitor bank. Figure 2.10 illustrates EAST's electric arc driver. Shortly after the electric circuit is closed, the electric arc will cross the gas and heat it to nearly 10,000 K in tens of microseconds. The diaphragm will either rupture (by pressure expansion) or vaporize (by high gas temperature), or a combination of both, where the pressure discontinuity will create a fast-moving shock wave. Further details on the discharge and driver operation are discussed in [29]. EAST is currently the only ground test facility capable of reproducing a jovian entry, with a shock speed of 46 km/s [27, 28]. For lower shock velocities experiments, below 8 km/s, the cylindrical driver is the preferred choice [128]. The operating conditions of EAST have been detailed in [27, 28].

As previously stated, the driver assembly can be used interchangeably with either of the two driven tubes by physically moving and attaching the driver assembly to the selected one, illustrated in Figure 2.11. The smaller non-reflected shock tube is a 10.16 cm diameter, 15 m long, aluminium tube. Its test section is located 7.5 m downstream of the diaphragm, with the dump tank next. The larger tube, used mostly in reflected shock tunnel mode, is made of 23.5 m long steel with a 76.2 cm of inner diameter. A 2 m long nozzle connects the 10.16 cm diameter driver section to the larger 61 cm driven tube.

In both driven tubes, the shock wave progress is monitored by a series of pressure sensors (PCB 132A35), fibre-optic coupled photodiodes (Hamamatsu H6780-20), or in-house ionization gauges. These

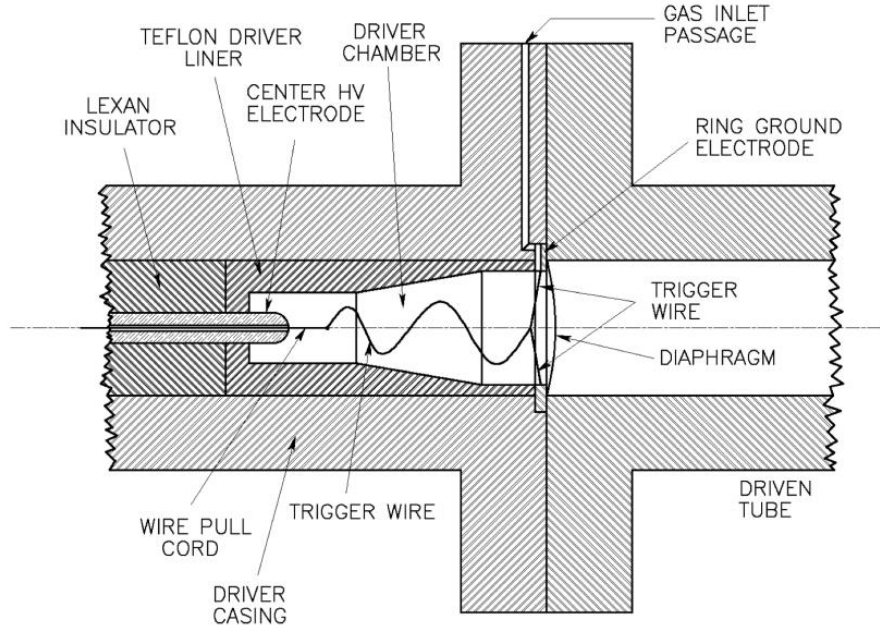


Figure 2.10: Schematic of electric arc driver for EAST, adapted from [126].

gauges are placed in permanent ports along the tube and detect the shock wave by a sudden discontinuity in the gas phase (be it pressure, light emission or electrical conductivity).

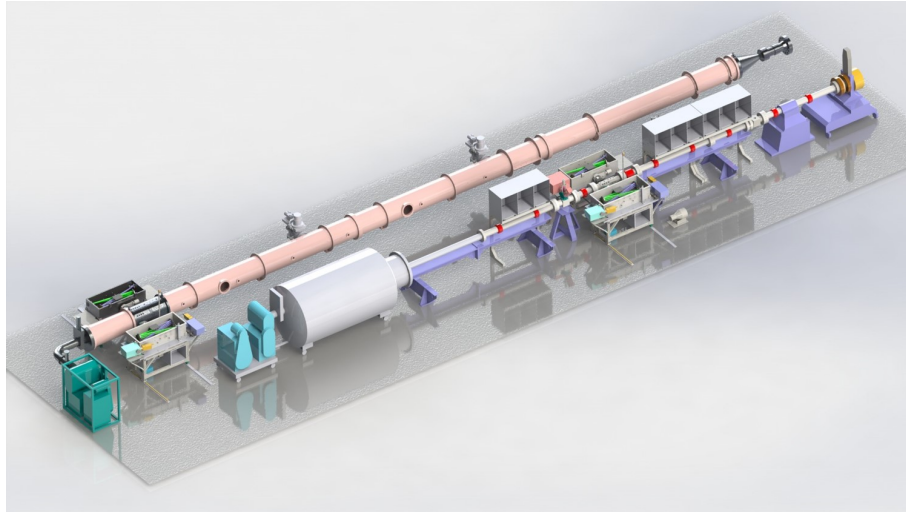


Figure 2.11: Schematic view of the EAST facility, with its two driven tubes. The closer tube is the 10.16 cm (silver colour) shock tube, and the further one is the 76.2 cm reflected shock tunnel (pink) equipped with the expansion nozzle. Credits R. Martinez, NASA Ames, adapted from [130].

The EAST test section was upgraded in late 2008, to expand its data collection capability and improve operation. The test section, depicted in Figure 2.12, has two 12.1 cm long slot windows for imaging spectroscopy, and 32 round ports, either for time-of-arrival sensors or future expansions. These ports are placed over 11 different axial positions. Both the round and long slot window ports were designed to reduce the recess between the inner surface of the window and tube wall by using tapered windows [126].

The main diagnostic of EAST is a set of four imaging spectrometers that can cover the VUV to mid-IR (120–5000 nm) regions simultaneously. These are named Vacuum Ultraviolet (120–500 nm, “VUV”),

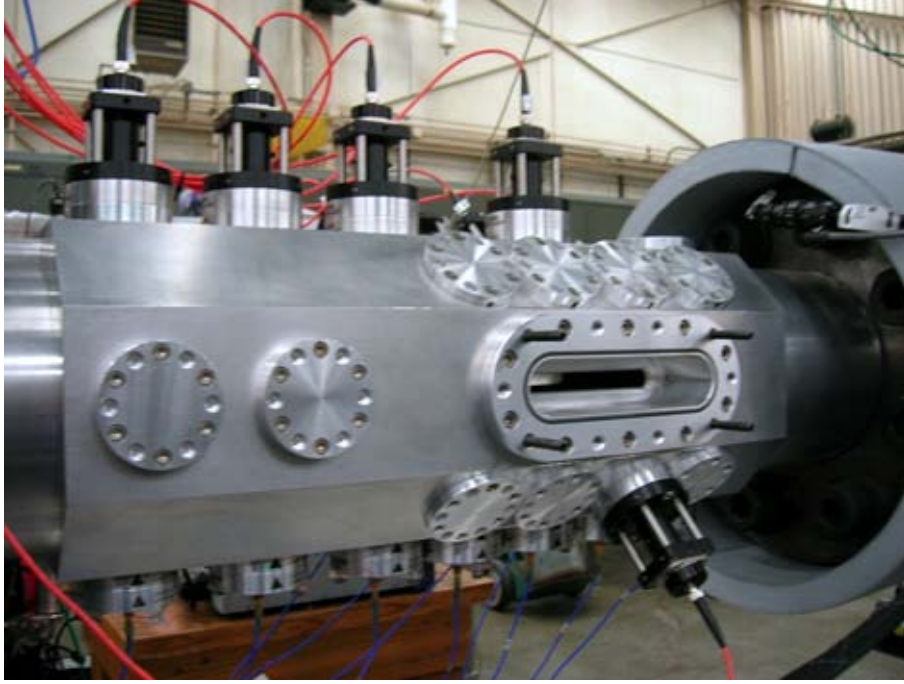


Figure 2.12: NASA Electric Arc Shock Tube test section, adapted from [126].

UV/Visible (200-600 nm, “Blue”), Visible/Near Infrared (480-900 nm, “Red”) and Infrared (800-5500 nm, “IR”). The setup is held under a high vacuum to reduce ambient absorption of radiation, most important for the VUV region. Optical windows are made of fused silica, or lithium fluoride when studying radiation between 120-185 nm. Two identical optics boxes, held under vacuum, house the imaging optics that direct the shock emitted radiation to the spectrographs. Each optical box is “responsible” for two spectral regions, one for the VUV and Blue, the other for the Red and IR. Light is captured at four different azimuthal angles but at the same axial position, to ensure the consistency between different spectral regions. Figure 2.13 depicts the schematic for the complete emission spectroscopy setup of EAST. A detailed description of the optics box and mirror assembly of the EAST spectroscopy setup can be found in [131].

Each spectral region is diagnosed with an imaging spectrograph equipped with a triple-grating turret, and a camera. The triple-turret in each spectrometer allows the adjustment of the resolution and spectral window at will. Currently, three Princeton Instruments (PI)/Acton imaging spectrographs model SP2500i are employed in the Blue, Red and IR setups, a PI VM-504 spectrograph is used in the VUV one. A multitude of different diffraction gratings are available with grooves densities of 3,600, 2,400, 1,200, 600, 150, 50, 17 gr/mm, and different blaze wavelengths. The maximum possible resolution in EAST spectroscopy setups are 0.1, 0.14, 0.2, and 0.6 nm, for the VUV, Blue, Red, and IR regions, respectively. An iCCD camera is used to capture the light signal, except in the IR region where an InGaAs or InSb detector must be used. A more detailed description and characterization of the EAST spectroscopy equipment can be found at [132, 133]. Spectral calibration is performed using line source lamps such as Hg, Ne, or Ar. Spatial calibration is done by a transparent plexiglass sheet scribbled with 1 cm increments. Spectral emission calibration uses one integration sphere, for wavelengths above 300 nm,

and a deuterium lamp for wavelengths below 300 nm. A detailed description and methodology of the calibration procedure used at EAST can be found in [29, 132].

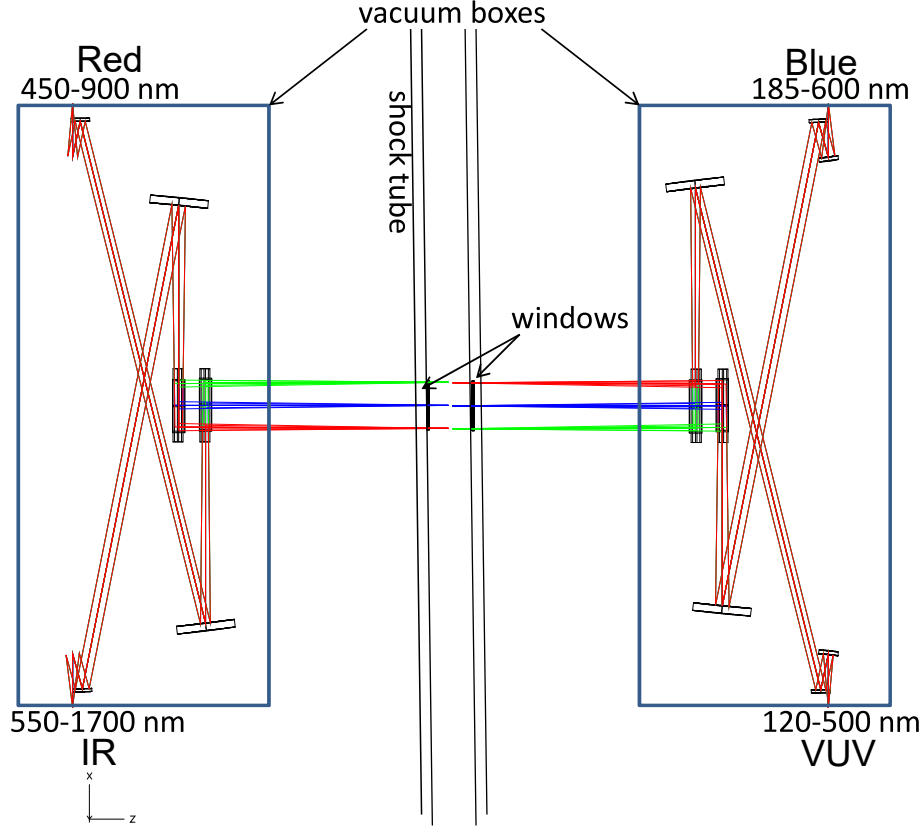


Figure 2.13: Schematic for the emission spectroscopy setup of NASA Electric Arc Shock Tube, adapted from [131].

EAST has been used for a wide range of applications and studies and directly supports NASA missions. The facility was used to test the substitution of an $\text{N}_2\text{O}/\text{N}_2$ mixture as the working fluid to increase test time [134]. Other studies include vibrational and rotational relaxation in expanding nitrogen flows [135, 136], demonstration of a magnetohydrodynamic accelerator [137], or measurements of the electrical conductivity of shock heated air [138]. Park in [139] investigated ablation through the measure of nitridation in solid carbon. Radiative heating experiments of the Galileo probe to Jupiter were performed in the late 1970s [140]. One of EAST's early reports shows the facility application to the examination of hydrometeor interaction with shock waves [141], a phenomenon encountered by ballistic missile entries through clouds and droplets. Alongside this, a significant effort was made in characterizing and increasing EAST performance, and shock tubes, in general, [142–144].

More recently, EAST has been focused on shock radiation spectroscopy for planetary entries. Recent tests have been conducted for Earth (N_2 and air) [29, 145–148], measuring UV emission of NO , N_2 and N_2^+ . Experiments on Mars and Venus atmospheric entries ($\text{CO}_2\text{-N}_2$) were done in [149–153], with special attention to the CN , C_2 , CO radiative systems. The CN radiative system was also investigated in Titan entry conditions [154–156]. Electron density measurements, through Stark broadening techniques, have also been investigated in EAST for Lunar return missions [157]. EAST and the X2 expansion tube have successfully compared results, for Earth re-entry conditions [158], and Titan entries [159].

NASA HYPULSE Facility The NASA HYPULSE is a large-scale test facility capable of operating either as a reflected shock tunnel or as an expansion tube. The facility's total length is around 37 m, which is a large test chamber with 2.13 m in diameter. Its driver has two modes, the first is a high-pressure cold helium driver, and the second is a detonation driver using a stoichiometric hydrogen-oxygen mixture, diluted by a noble gas [160]. The latter driver mode is needed for shocks above Mach 7. In reflected shock tunnel mode test times can reach 3 to 7 ms in Mach 5–12 conditions, whereas in expansion tube mode, Mach 25 can be achieved [161]. An upgrade to the facility makes it capable of reaching Lunar return entry conditions at 12.5 km/s [162].

The HYPULSE facility is equipped with pressure transducers and thin-film thermocouple heat-flux gauges. Optical diagnostics include laser holographic interferometry, laser-based fuel plume planar imaging system, and high-speed Schlieren. This facility is part of the NASA Langley scramjet complex [163], and is extensively useful for recreating the flight conditions of Hyper-X [164]. Aerothermodynamics studies for atmospheric entries are also done in HYPULSE, like the reproduction of entry conditions for the Mars Pathfinder mission. Hollis and Perkins [165–167] investigated the wake flow of the Mars Pathfinder capsule, measuring its wake and forebody heating rates. They were focused on the heat transfer to the capsule in both forebody and afterbody regions. This facility was decommissioned, and donated in 2020 to Purdue University in Indiana [168].

Other facilities Other facilities, such as the expansion tube from the University of Illinois have been used in the frame of the Mars Science Laboratory [38], testing CO₂ flows at 3 km/s. The now decommissioned NASA 42-inch reflected shock tunnel was used for the Mars Pathfinder mission. It investigated the convective heating in CO₂ and other gases [169]. Test flows were similar to the ones in HEG [170]. Stanford University has several shock tubes alongside its expansion tube. Other than measurements on radiation in Titan-like mixtures at 5.75 km/s [171], few results are available.

2.3.3 Australia

The University of Queensland in Australia is host to several hypersonic facilities. These account for a large shock tunnel (T4) capable of reaching suborbital re-entry velocities, a Drummond tunnel, a small shock tunnel operating up to Mach 7, and multiple expansion tubes (X1, X2, and X3) capable of reaching sample return mission conditions. This survey will be limited to these expansion tubes as they are the ones capable of simulating superorbital re-entry conditions. The X1, X2 and X3 expansion tubes all use a free piston driver.

Each of X1 to X3 expansion tubes are comprised of three primary assemblies: a free piston driver; a driven tube, divided into isolated compartments as the secondary driver, shock tube and acceleration tube; and the test section & dump tank [172]. The driver section is a large-diameter steel tube containing a heavy piston separated off the driven section by a steel diaphragm. A low pressure light gas (like helium) fills the volume between the piston and diaphragm. The piston compresses it until diaphragm rupture forming a strong shock wave. The shock wave will move down the shock tube compressing and accelerating the test gas, reaching a second diaphragm (only strong enough to sustain the pre-shock pressure gradient)

which ruptures easily. The shock wave velocities increase in the acceleration tube because of the lower density medium [173]. At the end of the driven tube is the test section, followed by the dump tank.

X2 superorbital expansion tube Commissioned in 1995 by Doolan and Morgan [174–176], X2 was initially a two-stage free-piston driven expansion tube. Later in 2004, the original two-stage free-piston driver was replaced by a single-stage free-piston driver using a 35 kg piston [177–179]. Another upgrade to the X2 driver was done in 2011, where Gilfind et al. [180, 181] found out that the facility could not maintain a steady flow with constant pressure for long enough for the simulation of the slower but much higher total pressure scramjet test conditions. To solve this, Gilfind et al. designed a new 10.5 kg piston and a new “tuned” driver conditions that corrected the causes for the low test times [180, 181].

The X2 expansion tube has a total length of 20 m, a driven section diameter of 85 mm, and a nozzle exit diameter of 201.8 mm. X2 is typically used for blunt-body planetary entry radiation studies or to investigate new techniques for the improved simulation of planetary entry in ground testing facilities. The facility is capable of reaching shock velocities of 3 to 12 km/s in air and up to 18.6 km/s in hydrogen/helium [182], with a test time in the range of tens to hundreds of microseconds [63]. X2 is equipped with twelve wall-mounted pressure transducers along its length, which are also used to measure the shock speed. Test models can be equipped with thin film heat gauges for temperature assessments. A series of pitot tubes are used to measure the flow pressure and velocity, impact pressure probes can be mounted to the pitot rake whenever new test conditions are being validated to measure the test flow pressure, test flow uniformity, and experimental test time [183]. Multiple optical techniques have been developed for the X2 expansion tube, among there are high-speed imaging, VUV to NIR emission spectroscopy [184–187], and holographic interferometry [188]. The flow can be visualized with a Shimadzu Hypervision HPV-1 high-speed camera, and a bandpass filter can be used so that only the atomic oxygen 777.55 nm multiplet is recorded onto the camera [184].



Figure 2.14: X2 expansion tube, adapted from [179]

2. HYPERSONIC FACILITIES AROUND THE GLOBE

The X2 expansion tube spectroscopy system [184, 186, 187] consists of a normal incidence spectrometer and an intensified charge-coupled device (iCCD) with enhanced sensitivity in the VUV spectral range camera. The Ultraviolet spectral region can also be observed using the same setup. Their setup is capable of observing the shock radiation from the front by using a slab with a small window, or from the side with a normal optical window. Figure 2.15 shows a schematic with the X2 VUV-UV spectroscopy system. An optics box with two light entry ports has a set of mirrors that focus the incoming light into the spectrometer grating. The design allows the setup to switch views without moving it through the use of optical rails. The spectroscopy system is comprised of a McPherson NOVA 225 spectrometer with 1 m focal length, $f/10.4$ normal incidence VUV monochromator with automatic focusing. The spectrometer has a concave focusing grating with a 15° angle between entry and exit slits. The small turning angle reduces aberrations and the long focal length allows for a higher resolution. Two gratings are available with 1,200 and 300 grooves/mm, the latter allowing for a 60 nm spectral window. The system has a lower spectral limit at 115 nm. An iCCD camera Andor iStar generation 2 with enhanced sensitivity in the VUV spectral range is used to capture the light signal. It possesses a 18 mm S20 UV enhanced photocathode using P43 phosphor [185–187]. All the optical windows for VUV-UV are made of calcium fluoride (CaF_2) to allow for higher transmissivity in the VUV region. The system has a theoretical resolution of 0.06 nm/pixel and 60 nm of spectral range. The system is held under vacuum to reduce absorption by oxygen, nitrogen or water vapour molecules.

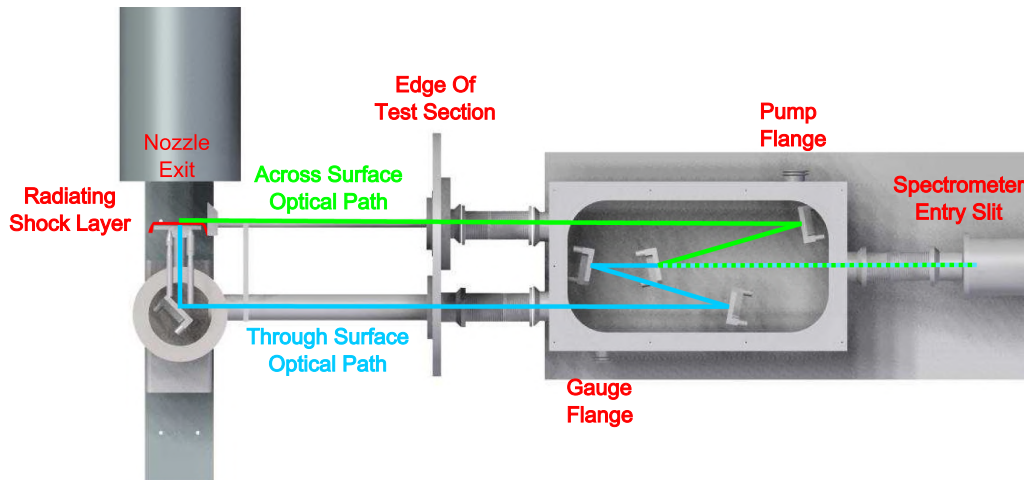


Figure 2.15: X2 expansion VUV-UV spectroscopy setup, adapted from [186]

The X2 near-infrared spectroscopy setup uses an Acton SpectraPro 2358i spectrometer, with a focal length of 300 mm and $f/3.9$, coupled to a Princeton Instruments PI-MAX iCCD camera. The system is equipped with a 150 gr/mm diffraction grating yielding a wavelength covering the 695 to 880 nm region with a resolution of 0.55 nm/pixel [186]. Figure 2.16 illustrates an experimental setup where both VUV and NIR regions were observed at the same time. Adaptations of the described setups may be made, such as switching between diffraction gratings with different groove densities and blazing, or centre the spectrographs/monochromators position into different wavelengths. This allows for setups to focus on different emission lines, depending on the test gas and model composition. Examples of this are: Zander et al. [189] using two spectrometers one with 600 gr/mm grating blazed at 300 nm, centre at 390 nm, the

second with a 150 gr/mm blaze at 500 nm and centred at 700 nm, which yield a total spectral window of 318–460 nm + 400–850 nm. Banerji et al. [190] used a setup that covered the UV to NIR regions by interposing the results of three experimental shots each one focused on a spectral region. Other examples of spectroscopy setups in X2, where gratings with 150, 300, 600, and 1,200 gr/mm were used, can be found in [191, 192].

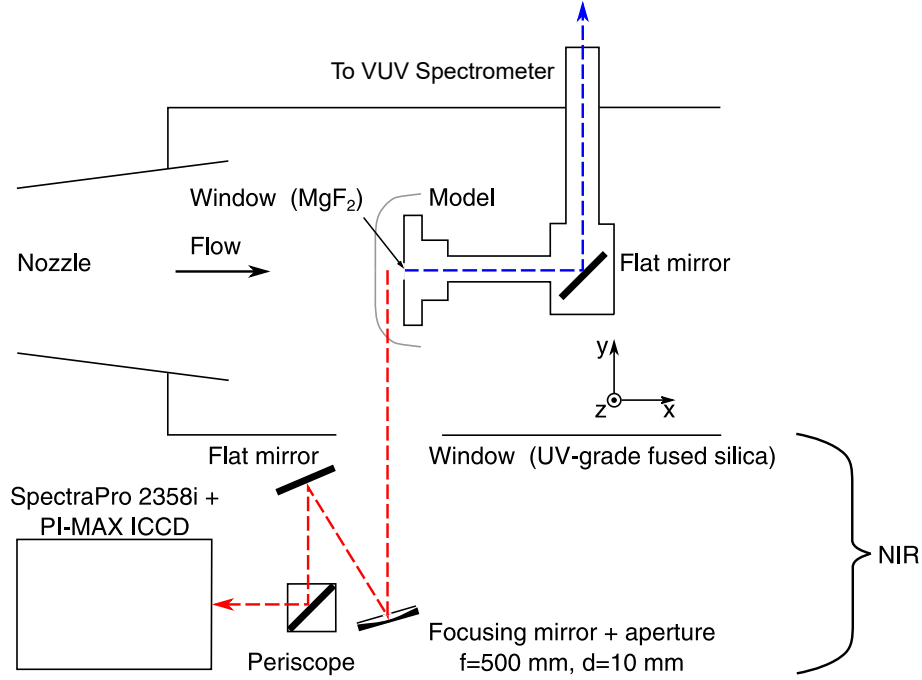


Figure 2.16: X2 expansion tube IR spectroscopy setup, adapted from [184]

Besides the emission spectroscopy diagnostics, X2 has a Nd:YAG (355 and 532 nm) optical interferometry instrumentation equipment capable of measuring density, ionization levels, species concentrations and temperatures [187, 193–195]. X2 has largely been used for investigating shock radiation in Mars [188] and Titan [17, 159, 196] mixtures. Besides it, X2 can simulate flow characteristics of superorbital Earth entry conditions, which led it to being used for simulating the Hayabusa [197] and Stardust [198] probe re-entries radiation. This facility has also been used for investigation on the ablation-radiation coupling over carbonaceous test models [189, 198, 199], scramjet testing [44, 200] and flow over a toroidal ballute [201].

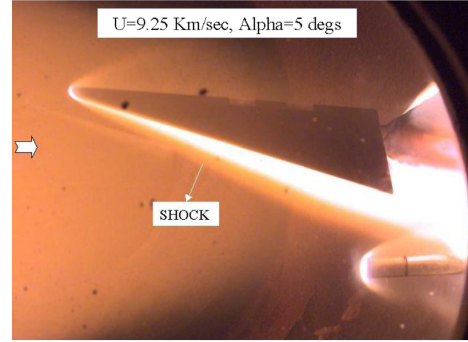
X3 expansion tube The X3 is a larger version of the previously discussed X2, also using a free-piston as its driver. Originally commissioned in January 2001, it has 69 m of total length, divided as follows: a 14 m compression tube, a 10 m driven tube, a 12 m secondary driver, a 25 m acceleration tube and a 2 m test section. The test section exit diameter is 180 mm, or 440 mm with the contoured nozzle [202]. X3 has a running time of 1 ms and a speed flow of 10 km/s, with a 100 MJ/kg stagnation enthalpy [173]. This facility is mainly focused on reproducing atmospheric entries of hyperbolic interplanetary entries (above 8 km/s).



(a) X3 test section, adapted from [203]



(b) X3 pitot tube rake, adapted from [203]



(c) Blunt waverider in X3, test with air at 9.25 km/s, adapted from [63]

Figure 2.17: X3 expansion tube and diagnostics, adapted from [63, 203].

2.3.4 Japan

The main hypersonic shock facilities in Japan are the Hiest shock tunnel and HVST shock tube, operated by the Japanese Space Agency (JAXA), and the JX1 expansion tube at Tohoku University.

JX1 Expansion tube The Shock Wave Research Center at Tohoku University hosts the JX1 expansion tube. It is a 13.6 m long free-piston expansion tube working only with dry air as test gas, further details of this installation can be found in [204, 205]. This facility is capable of reaching shock velocities up to 9 km/s with a maximum enthalpy of 37 MJ/kg.

The shock wave velocity is monitored with nine piezo-electric pressure transducers and a high-speed imaging system. A xenon flush light illuminates the shock while the imaging system captures a sequence of six frames. JX1 was used in preparation for the Hayabusa mission as it was capable of simulating its radiative environment.

High Enthalpy Shock Tunnel (HIEST) HIEST is a high-enthalpy free-piston shock tunnel located at Kakuda Space Center. Its performance is similar to that of the HEG shock tunnel in Germany, as both were developed for experimental simulations of re-entry flight vehicles such as HERMES, in the case of HEG, and HOPE (H-II Orbiting Plane) for HIEST. It has a total length of around 60 m and an exit nozzle diameter of 1.2 m, [206]. The facility is capable of reaching shock velocities in the 4–7 km/s range with a stagnation enthalpy of 25 MJ/kg, using nitrogen or dry air as test gas, and has a test duration of

2–10 ms.

HIRST instrumentation is comprised of an aerodynamic balance [207] for force and moments measurements, Schlieren photography and holographic interferometry for flow visualization. Boundary-layer transition [208], aeroheating [41, 208, 209] studies, as well as surface pressure and temperature measurements, can be performed at HIRST [210].

Hyper Velocity Shock Tube JAXA's Hyper Velocity Shock Tube (HVST) is a large free-piston double diaphragm shock tube with a square test section of $70 \times 70 \text{ mm}^2$. It is capable of achieving shock velocities of 15 km/s and can run dry air, nitrogen, or carbon dioxide as test gases. HVST, depicted in Figure 2.18, has a total length of 18 m.

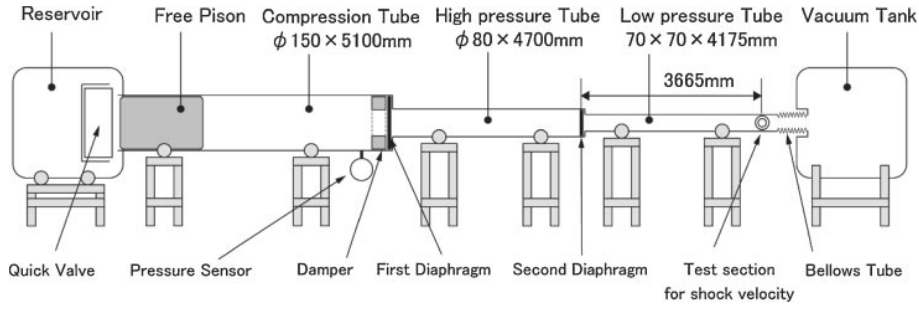


Figure 2.18: Schematic of the HVST facility, adapted from [211].

This facility is equipped with piezo-electric pressure transducers, photodiode detectors, a Schlieren imaging system with two He-Ne lasers, and a spectrometer capable of covering the VUV to NIR regions [212, 213]. The VUV-UV equipment is comprised of a McPherson 218 imaging spectrometer with 300 mm of focal length, which can be equipped with different diffraction gratings [213]. An optical reflective condenser, McPherson 615, connects the MgF_2 optical window to the spectrometer maximizing the collected signal. The calibration is performed with a deuterium lamp, and all the systems are held under vacuum to prevent radiation absorption by oxygen and water vapour molecules. For the VIS-NIR setup, a set of mirrors focus the light into an optical fibre array, which then connects to a Burkoukeiki HK-300 spectrometer, with a focal length of 300 mm. Calibration in the aforementioned VIS-NIR region uses a quartz tungsten halogen lamp. An iCCD camera Andor DH720i-18F-90H captures the light signal. Pressure and photoelectric sensors monitor the shock wave and are connected to a digital oscilloscope Yokogawa DL9040 [214]. The spectral range is 115–240 nm, and 350–600 nm for the VUV-UV and VIS-NIR setups, respectively. An illustration of these emission spectroscopy setups is shown in Figure 2.19. An absorption spectroscopy diagnostic was also used in HVST, as well as Stark broadening techniques [215], for temperature and electron density measurements, respectively.

The capability of HVST to reach superorbital re-entry conditions was used in preparation for the Hayabusa mission [215–218]. Experiments were done for re-entry conditions in pure nitrogen [212, 216], dry air [217, 218], and Mars atmosphere [213, 219]. Recently, an upgrade for higher performance velocities –above 14 km/s– was made, and a test for the Trojan mission was done in air [220].

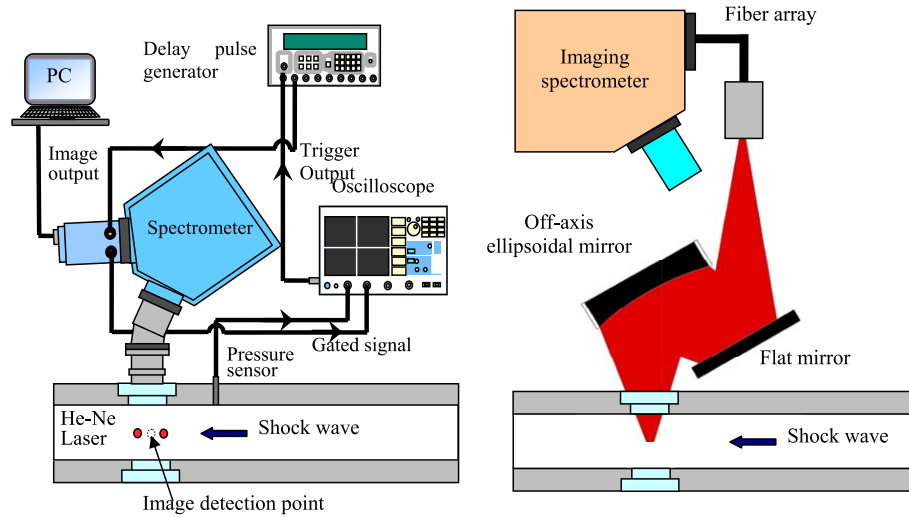


Figure 2.19: Schematic of the HVST emission spectroscopy setup, adapted from [213]. VUV-UV variant (left), and VIS-NIR (right).

2.3.5 Rest of Asia

India India is host to multiple shock tube and shock tunnel facilities. These are mostly used for aerothermodynamics studies and scramjet development, and were reviewed by Reddy [221]. A list of the available facilities, and its details on Mach number, velocity range and total enthalpy can be found in [222].

Five shock tunnels (HDST1 to HDST5) are located at the Indian Institute of Science Bangalore. Both HST1 and HST2 are shock tubes with 50 mm diameter tubes and a test section of $300 \times 300 \text{ mm}^2$. The HDST3 is a free piston-driven hypersonic shock tunnel which can be converted into an expansion tube. The HDST4 was updated to a free-piston shock tube, and can now reach Mach 15 flows. Lastly, there is the HDST5 with a 300 mm diameter shock tube and a 1.0 m diameter dump tank. It can reach flow speeds of 3 km/s at 6 MJ/kg of total enthalpy. These facilities measure aerodynamic pressure, force and moments transfer. Flow visualization and heat transfer rates are also measured.

Another shock tunnel is operated at the Vikram Sarabhai Space Center and is used for aerothermal studies of returning capsules/vehicles. It is a combustion-driven shock tunnel, to be upgraded to a free piston shock tube, and can reach a peak temperature of 5,000 K [63].

The different facilities in India are well suited for sub-orbital and orbital re-entry simulations [221]. Most literature publications are from the HDST2 facility. It has been used to research hypersonic flow-field and aeroheating phenomena during re-entry [223–225]. Scramjet studies have also been carried out in HDST2 [221, 226]. HDST1 performed experiments to establish heating correlations for re-entry applications [227], and for chemical kinetics on scramjet combustion [228].

South Korea The Korean Advance Institute of Science and Technology in South Korea (KAIST) operates a shock tube facility. It has a 68 mm diameter stainless steel driver operating with cold hydrogen gas up to 100 atm. The driven tube is 3 m long with a 47.5 mm diameter and is equipped with two 3 mm thick, 25 mm diameter sapphire windows. The shock speed is monitored by five ionization gauges placed

along the driven tube, and a pressure gauge at its end [229].

An optical emission spectroscopy system is the main diagnostic of the KAIST shock tube. It comprises an Oriel MS125 spectrometer with a 600 gr/mm diffraction grating, yielding a spectral resolution of 1.8 nm. The KAIST shock tube can operate as a reflected shock tube as well, this may be done as the distance between the test section and the end wall is small. The reflected shock is capable of heating the test gas before the arrival of the contact surface [229].

This facility has been used for radiation studies in N_2 -CO mixtures [229]. Carbon nanotube production using shock tube experiments [230], and surface catalysis over copper and silicon oxides in air [231] were also performed in the KAIST shock tube.

China The China Aerodynamics Research & Development Center (CARD) operates two shock tunnels [232]. The shock tunnels have nozzle exits with 0.6 and 2 m of diameter and can reach Mach numbers of 6–20, and 6.5–24 (conic nozzle) or 6–16 (contoured nozzle), respectively. These facilities are adapted to orbital and sub-orbital re-entry aerothermodynamics. Piezo-electric balances and strain balances measure forces. Pressure and heat flux distribution can be measured over the test model surface. Free flight techniques are also available for derivative measurements and forces, as well as flow-field visualization with both Schlieren imaging and high-speed video cameras. Its main areas of research are aerodynamic characteristics for aircraft development, shock wave/boundary layer interference, shock wave oscillation and pressure fluctuations in separated flows, jet/flow interference, scramjet engines, aero-optics, boundary layer transition, and particle corrosion.

The Institute of Mechanics of the Chinese Academy of Sciences in Beijing has multiple hypersonic facilities. These are mostly used for aerothermodynamics and combustion applications [233–235]. Among them, the JF-10, JF-12, JF-16, and JF-22 are detonation-driven high-enthalpy shock tunnels.

JF-10 performance has been investigated in [236, 237]. It has around 40 m in length with a 500 mm diameter exit nozzle. For instrumentation, JF-10 has a pitot rake, pressure and heat-flux sensors, and a Schlieren visualization system. This facility has a total enthalpy range of 15–25 MJ/kg, achieving nominal Mach numbers of 10 and 12, with test times ranging from 2 to 8 milliseconds, depending on the specific test conditions. Two photographs of JF-10 are shown in Figure 2.20.

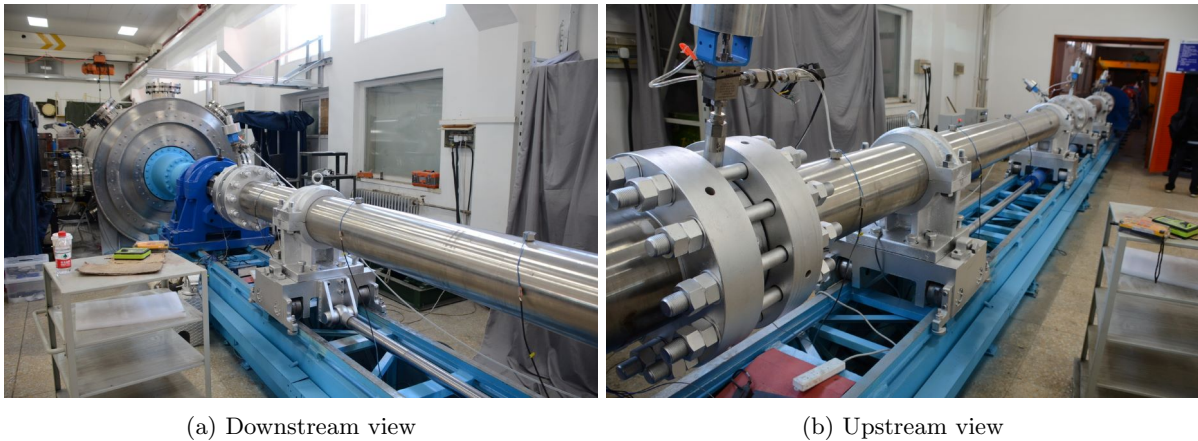


Figure 2.20: JF-10 shock tube

JF-12 operates between Mach 5 and 9 and has a test time of up to 100 ms. Its larger 2.5 m diameter nozzle allows for the deployment of large-scale models. The total facility has 265 m in length and is equipped with three six-component force and moment balances. Heat-transfer and pitot measurements are also available [234]. This facility was used to investigate boundary layer transition on a plate [238].

JF-16 is a 17 m long detonation-driven expansion tube [239]. Pressure transducers and ionization gauges monitor the flow along the tube. It is capable of shock speeds up to 8 km/s, with a total enthalpy of 40 MJ/kg, and a test time of 50–70 μ s. Schlieren visualization experiments were performed at JF-16 by Zhou et al. [240].

The JF-22 (Hyper-dragon II) [241] is the latest hypersonic facility from China. The high-enthalpy, hypervelocity detonation-driven shock tunnel is capable of covering Mach numbers from 10 to 25 and flight altitudes from 30 to 90 km. It was built with support from the National Natural Science Foundation of China.

The last facility is the 170 m long free piston FD-21 shock tube located at the China Academy of Aerospace Aerodynamics in Beijing. Pressure sensors are located along the shock tube for monitoring total enthalpy and pressure. The facility is equipped with a rake capable of measuring pitot and static pressure, stagnation pressure, and stagnation point heat transfer. A small test model can be also used to validate the flow properties by measuring the heat transfer rate [242, 243].

2.3.6 Russia

Similar to the European survey, this analysis will focus on the main Russian shock tube, shock tunnels, and hot shot capable of hypersonic range. From references [65, 66], the facilities of interest are:

- ITAM hot-shot IT-302M;
- MIPT VUT-1 shock tube;
- TsAGI ADST shock tube;
- TsAGI IT-2 hot shot
- TsAGI UT-1M shock-tunnel;
- TSNIIMASH U-12 shock-tunnel;
- Moscow State university shock tube

MIPT VUT-1 The VUT-1 shock tube of the Moscow Institute of Physics and Technology (MIPT) has been largely used for ESA and International Science and Technology Center (ISTC) research activities [244–247]. It is a stainless steel shock tube with a 6 m long, 76 mm cross-section working channel. VUT-1 has an operational range between Mach 5 and 25. Its driver can work either as a free-piston (helium is used as a push gas up to 50 atm), or as a combustion one (explosive diluted He:H₂:O₂ mixture, shock pressure is around 300 atm). Experiments on air and CO₂-N₂-Ar mixtures have shock wave velocities in the 4–8 km/s with gas test pressures from 0.1 to 6 torr. Figure 2.21 shows a photograph of the VUT-1

shock tube and the technical drawings showing a side-view of the facility. The following measurement techniques equip this shock tube:

- Spectroscopic equipment: Infrared spectrometer, VUV–IR monochromators, interference/disperse optical filters and optics. Light emission detectors for different spectral regions, lasers (He-Ne, Ar⁺, YAG, dye 580–630 nm, 205–380 nm, ArF 193 nm) and spectral lamps;
- 94 GHz microwave interferometer to infer electron densities behind shock waves;
- iCCD camera for obtaining spatial plasma distribution and a high temporal resolution with a wavelength sensitivity of 200–800 nm;
- Non-linear laser spectroscopy (Coherent anti-Stokes Raman Scattering CARS) method.

The current diagnostics are capable of determining the test gas’s initial pressure, molecular, ionic, and electronic densities, shock velocity, gas temperatures (vibrational, rotational and translational), and molecular population levels. The experimental results from VUT-1 are used to assess rate constants of chemical and plasma chemical gas reactions, and molecular energy exchange; relaxation times of inelastic collision processes; and spectral and integral absorption coefficients.

ITAM hot-shot IT-302M The Institute of Theoretical and Applied Mechanics (ITAM) possesses a hot-shot wind tunnel named IT-302M. Its driver operates by an electric discharge of a 1.7 MJ capacitor and can run on nitrogen or carbon dioxide gases. A high-pressure compressor, capable of 150 atm, and a vacuum system are part of IT-302M. Contamination of the flow is between 1.5 and 2%. Shock wave velocities range from Mach 5 to 15, depending on the nozzle, while having a test time of up to 250 ms [248]. Typical measurements in IT-302M focus on heat-transfer studies on the model surface with heat-flux probes of various types and the “thin wall” method; and the aerodynamic characteristics of models of flying vehicles by a fast-response strain-gauge balance [63]. Topologic flow analysis via Schlieren photography is also available, as well as the determination of combustion efficiency through the intensity of OH radicals in the ultraviolet range. IT-302M is also used for the study of combustion processes in air-breathing engines at both subsonic (ramjets) and supersonic velocities (scramjets). The facility is shown in Figure 2.22.

TsAGI ADST shock tube The Central Aerohydrodynamic Institute (TsAGI) hosts the arc-driven ADST shock tube, whose configuration and current status are unknown. Information on this facility has only been obtained through activities carried out in the frame of the ISTC project [249]. The facility allowed the study of ionization and radiation processes [64], as well as electron density and temperature measurements behind a shock wave in air in the range of 8 to 14 km/s [65].

TsAGI hot-shot wind tunnel IT-2 The IT-2 hypersonic wind tunnel (150 MW impulse generator) is an intermittent facility with exhaust into a vacuum reservoir. It is similar to ONERA F4, yet with some differences regarding the temperature that can be reached [250]. An electric discharge of 550 kJ capacitor heats the test gas in a constant volume chamber to an equilibrium state. Gas contamination

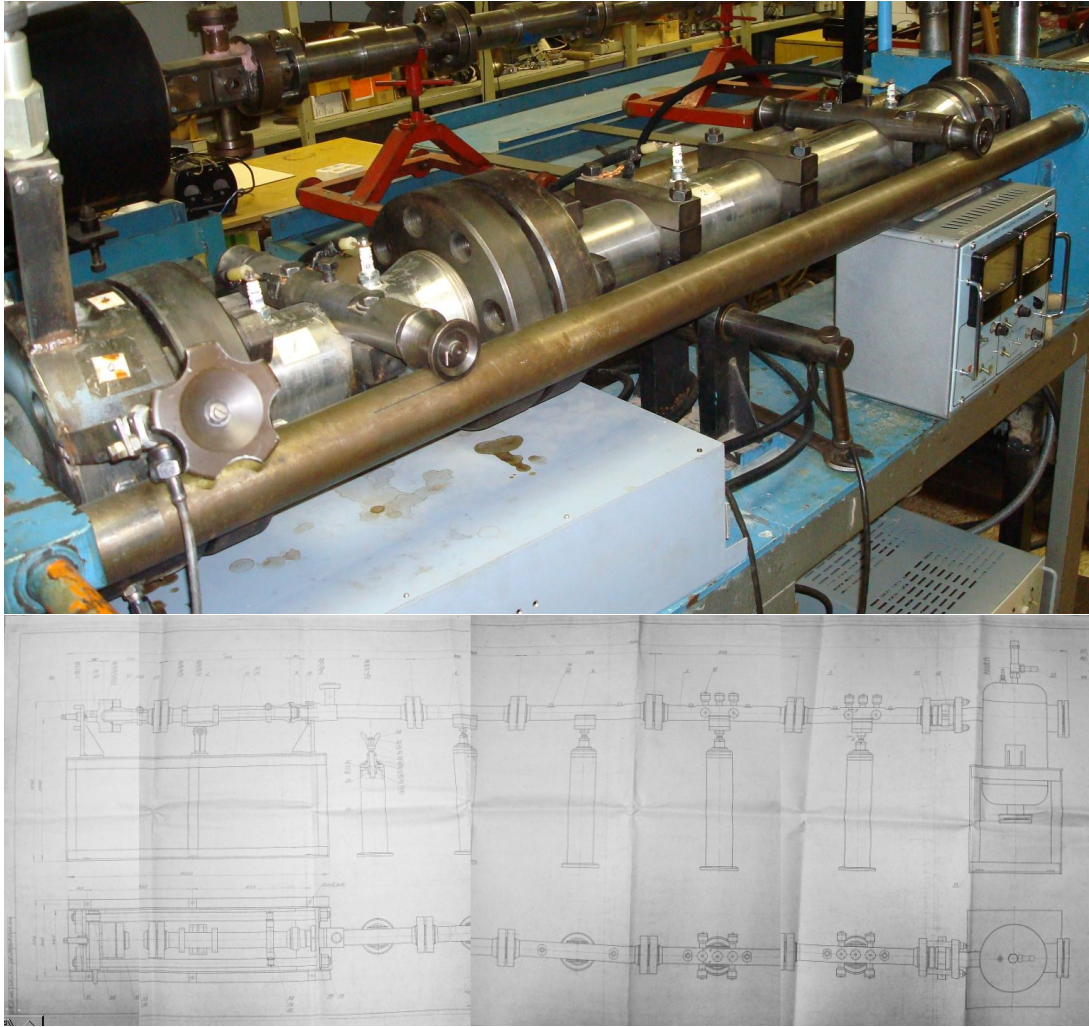


Figure 2.21: MIPT VUT-1 shock tube driver (top), and overall CAD drawing (bottom).

by erosion of electrode and insulation products is less than 2%. The wind tunnel is supplied by a system for gas injection at high pressures, a vacuum system, and a system for jet tests. Its main focus is the investigation of aerodynamic characteristics of hypersonic aircraft models for a wide range of Mach and Reynolds numbers. The hypersonic flow can run up to 0.1 s, with a total pressure ranging from 12 to 150 MPa, while the stagnation point temperature is between 1,500 and 7,500 K. The facility can use air, N_2 , CO_2 , and Ar as test gases.

The diagnostic equipment for the measurements of aerodynamic characteristics of flying vehicle models is a fast-response strain-gauge balance, operating for a wide range of angles of attack. Heat-flux probes study the heat transfer on model surfaces, and the thin-wall method with a high spatial resolution is also available. Schlieren photography is used to picture the flow around the models with a time resolution of around 1 ms. Electron and ion concentration in the shock layer and the near wake of the model are determined via a set of electric probes. The facility has been extensively used for testing different models of spacecraft, namely the Soyuz, Buran [251], Bor, Pathfinder and Mars Sample Return vehicles with models tested in the frame of ISTC grants [249].



Figure 2.22: ITAM IT-302M hot-shot, adapted from [248]

TsAGI shock tunnel UT-1M A modular, two variant, shock tunnel facility named UT-1M is operated in TsAGI, illustrated in Figure 2.23. The first mode is a Ludwieg tube, operating from Mach 5 to 10, the second is a classical shock tunnel operating in a 10 to 16 Mach number range. UT-1M can perform jet-flow studies by injecting various gases into the model, and even solid particles. Different gas mixtures can be used, namely air, N_2 , CO_2 , He, and Fr. Depending on test conditions, running time is between 5 and 40 ms. The facility is supplied with the following instrumentation:

- A 96-channel hot-wire measurement system;
- A system for model illumination in the UV range;
- Multiple thermocouples, thermistors, and calorimeter gauges, as well as temperature-sensitive coatings for heat-transfer investigation on the model surface;
- Pressure probes and pressure-sensitive coatings for measuring the pressure distribution over the model surface;
- Shadowgraphs linked to a high-speed camera;
- Optical setup for dusty flows employing a light plane.

This shock tunnel is largely used for aerodynamic heating study of models of flying vehicles over a wide range of Mach and Reynolds numbers, in laminar, transitional, or turbulent flows. Alongside it, studies on pressure oscillations in the free stream and on the model surface, jet aerodynamics problems, and

electrification of bodies in the presence of solid particles are also carried out. There is also the possibility of studying model processes of combustion of gaseous and liquid fuels with non-aggressive components.

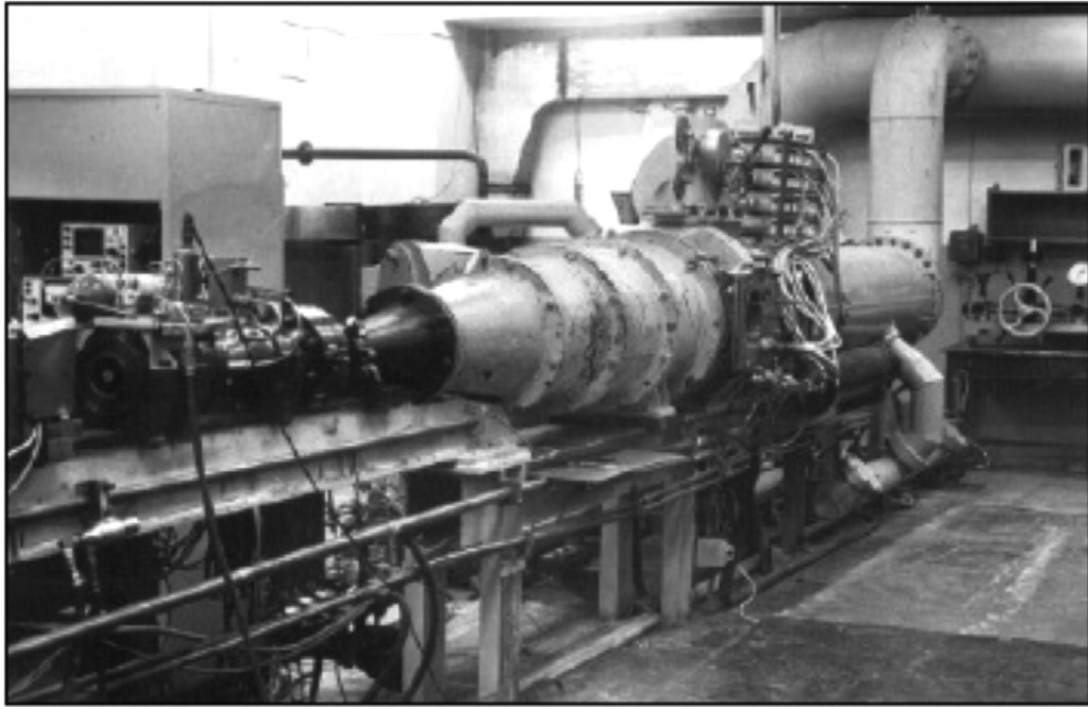


Figure 2.23: TsAGI UT-1M shock tunnel, adapted from [63]

TsNIIMash U-12 shock-tunnel The U-12 is a hypersonic shock tunnel located at TsNIIMash (Central Research Institute of Machine Building). It has four working sections. Three of these are located in the cylindrical part of the channel and have hatches for setting the model and double glass 150 mm diameter observation windows. The last section is a gas receiver placed beyond the nozzle exit with three horizontal rows of windows, 360 mm in diameter. Explosive oxygen-hydrogen mixtures diluted with helium, nitrogen or other gases act as the driver. These explosions are initiated by a special electrical system or shock wave. U-12 has a running time between 25 and 100 ms, with shock speeds of 3.5 to 9 km/s, depending on test conditions. The facility can operate with air, N_2 , CO_2 , and other gas mixtures.

The available instrumentation is comprised of a measuring system of dynamic (fast varying) pressure sensors, heat-flux probes, three-component piezo-electric and six-component strain gauge balances, and a system of optical-physical measurements and high-speed filming. The UT-1M shock tunnel is utilized for investigating shock wave interference, hypersonic flow (including pressure and heat-flux distribution) over re-entry vehicles, shock and detonation waves interaction with vehicles, and radio-wave propagation through the plasma formation on the vehicle surface at supersonic speeds.

Moscow State University Shock Tube The shock tube is comprised of two chambers, a high-pressure and a low pressure chamber connected by a conical nozzle. The first one has 90 mm of internal diameter and 3 m of length, the second one has 493 mm of diameter and is 12 m long [252]. A copper or aluminum diaphragm separates them, and a cross wedge is used to cut open the diaphragm into four

petals, preventing metallic shrapnel from flying into the test region. It can use compressed hydrogen or helium, or a diluted stoichiometric oxygen–hydrogen mixture as driver gas.

Shock wave speed is measured with two thermal (film) sensors, mounted on a single removable insert in the side wall of the measuring section. Several piezo-electric pressure sensors are located in its side wall, these can also measure shock speed with less accuracy than film sensors. Their main purpose is to probe the pressure and lifetime of the unperturbed reflected wave before it enters the nozzle. An absorption spectroscopy setup was also used to investigate the rate of deactivation of different chemical species, like oxygen [252]. A photograph of the low-pressure side is shown in Figure 2.24.



Figure 2.24: Moscow State University Shock Tube

2.3.7 Performance envelopes

Different hypersonic facilities’ operational range capabilities can be compared through their performance map. It illustrates the achievable shock wave speeds at different test gas filling pressures (p_∞ vs. v_∞). Figure 2.25 shows the performance envelope of the main global hypersonic facilities in dry air ($\text{N}_2\text{-O}_2$) and pure nitrogen atmospheres. The expected ESTHER performance map is also depicted, for which a complete discussion is found in Chapter 5. The performance map for Gas giants ($\text{H}_2\text{-He}$) and pure helium atmospheres is shown in Figure 2.26. Data was extracted from: Grinstead et al. [126] for EAST facility; Holden [253] –and converted from altitude to pressure recurring to NASA’s “Earth Atmosphere Model” [254]– for the CUBRC LENS facilities; Kendall et al. [255] and Smith and Mee [256] for the X1 expansion tube; Banerji et al. [190]. Brandis [17], Gu et al. [257], Jacobs [258], McGilvray

[259], Sheik et al. [186] for the X2 expansion tube; Abe et al. [260], Fujita et al. [217], Jacobs [258], and Yamada et al. [214] for HVST.

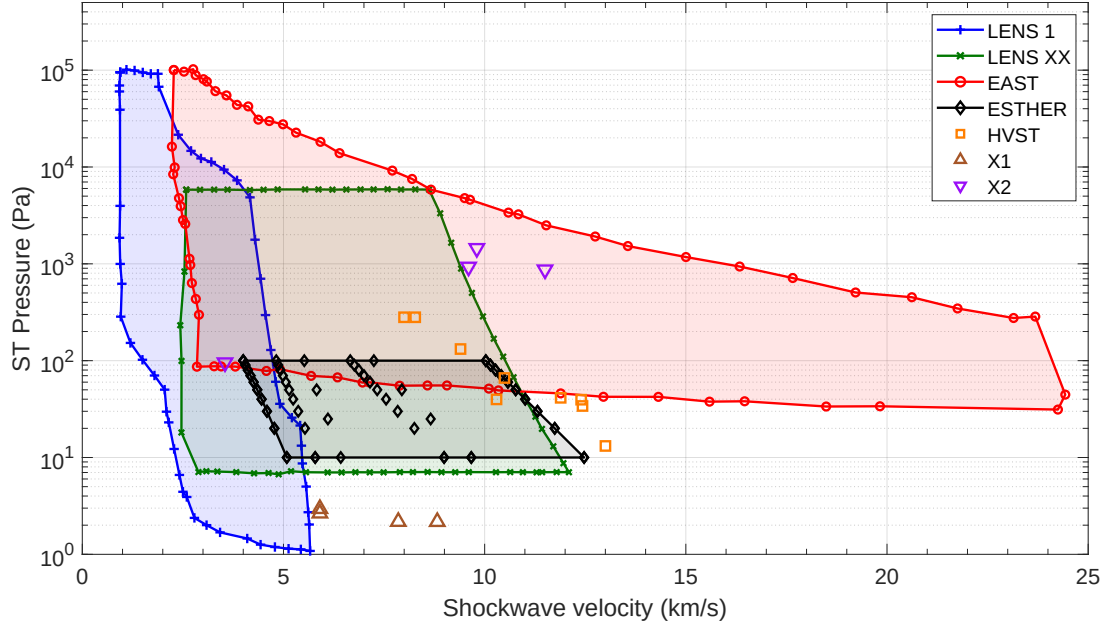


Figure 2.25: Performance map of different hypersonic facilities in dry air and pure nitrogen atmospheres

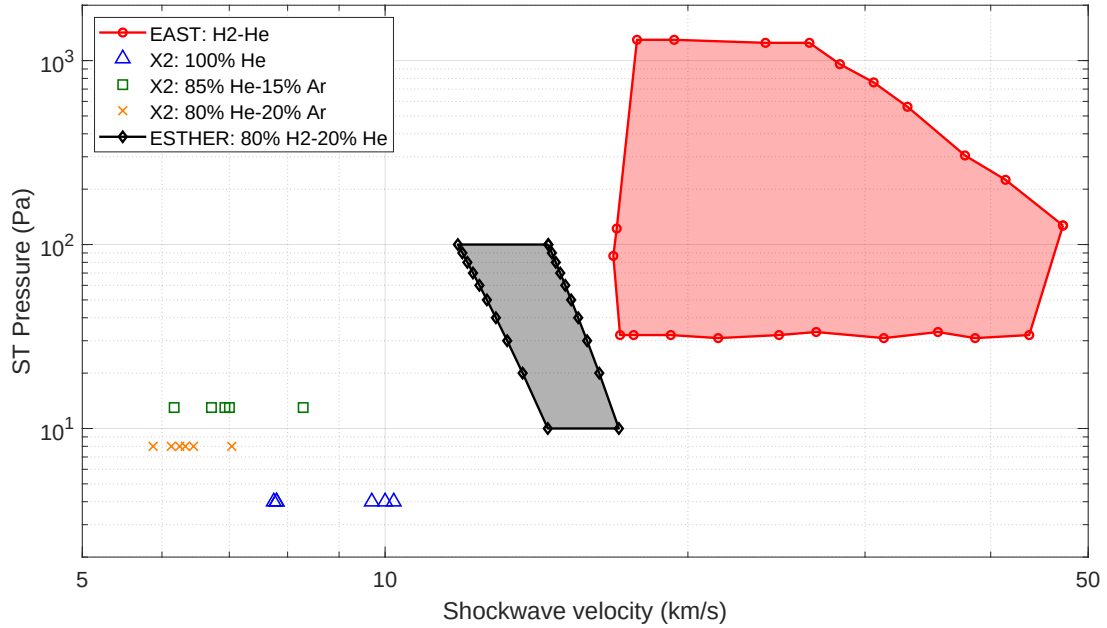


Figure 2.26: Performance map of different hypersonic facilities in Gas giants and pure helium atmospheres

2.4 Diagnostics on Shock Tubes

Experimental measurements in hypersonic facilities are a challenging task. The main difficulties are the necessity to use non-intrusive, fast response systems, and sturdy measurement techniques. Firstly, any intrusive measurement used along the facility length will change the flow conditions and thus the

downstream measurements. Secondly, the very fast (3–12 km/s) shock speeds means the test times are on the $\sim 10\text{ }\mu\text{s}$ timescales¹, and this requires fast response sensors and electronics working on the 10^6 – 10^7 samples per second region. Lastly, the moving high-pressure shock wave will exert both thermal and mechanical stress on the sensors. Besides it, the hot driver gas and any diaphragm debris entrained in the flow may damage the sensor after the test gas has already passed. Some common diagnostics used in hypersonic facilities are, but not limited [28, 53, 69, 183, 261] to:

- Force balances
- Heat transfer gauges
- Pitot tubes
- Piezo-electric/piezo-resistive sensors
- Ionization gauges
- Photomultipliers
- Shadowgraph and Schlieren imaging
- Optical and microwave interferometry
- Absorption and/or emission spectroscopy

Force balances and heat transfer gauges are typically deployed in larger facilities used for aerothermodynamic model testing. The former is used to measure the aerodynamic forces acting on the model [262, 263], while the latter measure the heat transfer to the model [264]. Since these diagnostics require longer test times, they are not used in facilities focused on kinetic reaction experiments like non-reflected shock tubes. Pitot tubes can measure the flow velocity and stagnation pressure. These often use two pressure sensors to measure the stagnation and static pressures, from where the flow Mach number can be derived [261]. With this said, as the pitot rake is an intrusive technique, it may be not possible to use it in conjunction with a scale model, as it disturbs the downstream flow.

Piezo-electric materials produce a voltage difference between their faces when subjected to a pressure variation. This voltage difference is proportional to the pressure difference across its faces. These materials are used to create piezo-electric gauges or piezo-resistive gauges, in the case of measuring the electric resistance [261]. Most pitot tubes are formed by piezo-electric sensors. The shock wave moving along the tube can be detected by a sudden pressure rise. Piezo-electric pressure transducers are commonly mounted along the test section to monitor shock wave position and speed. Ionization gauges measure the electric conductivity of a gas and are used to identify the passing shock wave through the change in electric conductivity [28]. Alternatively, the passing shock wave may be identified through the light emitted by the shock, in this case photomultiplier tubes (PMT's) can be used. Photomultipliers collect and amplify the received light, which is then converted into an electric signal by a photodetector [183].

¹when observing a moving shock wave

The moving flow can be imaged through the use of shadowgraph or Schlieren imaging. These are based on the dependence of the gas refractive index on its density and therefore detect density variations across the flow. As a parallel light beam crosses the test section, its deflection angle is proportional to the density gradient of the medium [265]. Shadowgraph and Schlieren yield an integrated signal, however, in shock tube facilities this is not a large drawback due to the small boundary layer and flowfield radial and polar symmetry. These techniques depend on the density gradient over the medium, so a small gradient may lead to unresolved images. The Schlieren technique enhances the image contrast when compared to the simpler shadowgraph technique. A knife-edge at the lens focal point eliminates the fundamental light, keeping only the harmonics. Schlieren imaging requires higher gas densities for gradients to be identified [261]. Fundamentally, shadowgraph methods are sensitive to the refraction index second spatial derivative, while Schlieren's are sensitive to the first derivative [265].

Optical interferometry, similar to the shadowgraph technique, uses the change in the medium refractive index to measure the density of the gas. However, an interferometer may provide quantitative measurement of the flowfield density whereas a shadowgraph cannot. One of the simplest interferometer designs is the Mach-Zehnder interferometer [265]; A light beam is split in two with one beam passing through the medium, and the other through a compensation chamber so both beams are in-phase within one another [261]. The phase difference between the two signals will be caused by the change in the medium diffraction index integrated over the optical path. In microwave interferometry, the phase shift is related to the change in refractive index caused by the electron density [265]. Compared to optical interferometry this technique is better suited for electron density measurements. This technique and application to ESTHER will be discussed in greater detail in Chapter 8.

Spectroscopy is used to analyze the electromagnetic (EM) radiation emitted or absorbed behind the shock wave. These emission and absorption spectra stem from the transitions between internal levels of the chemical species. In absorption spectroscopy, the density of different chemical species may be probed by measuring the amount of light absorbed by the medium [265]. These are inferred from the changes on the absorption spectrum. A variation of this technique is called laser-induced fluorescence (LIF), where a monochromatic light source (laser) or a monochromator is used so a specific wavelength can probe the shocked flow, analysing a particular species density. The species in study will absorb the laser wavelength and then emit two photons via stimulated emission, yielding a very good signal-to-noise ratio. In emission spectroscopy, one observes the light signal emitted by the excited plasma. Typically, an imaging spectrometer is used to separate the collected light into its components, and then an iCCD camera captures a time- and spectrally-resolved signal of the shock [157]. Emission spectroscopy is one of the most commonly used techniques in shock tubes focused on chemical kinetics studies. It gives information on the state densities of the excited species, collisional electron-atom, atom-atom and ion-atom effects, energetic distribution of the species, or temperature of the species, or electron density [266]. Chapter 7 discusses the practical implications and applications of an emission spectroscopy setup to ESTHER. Both absorption and emission spectroscopy are non-intrusive diagnostics making them well-suited for hypersonic facilities.

Chapter 3

Shock tube theory

“Gotta go fast!”

- Sonic the Hedgehog

The present Chapter presents the mathematical model and physical theory for a working shock tube facility. An ideal gas one-dimensional fluid dynamic model is used to estimate the shock tube thermodynamic performance. Non-ideal effects, such as wall boundary layer effects, blast wave formation, and non-instantaneous diaphragm rupture are also discussed.

A shock tube is designed to create high-temperature gas flows during a very short time. In its simplest form, a shock tube is comprised of two sections separated by a diaphragm allowing for a pressure difference to be set up. This configuration is incapable of generating shock flows with high Mach numbers in a practical fashion. A solution to this is using a double diaphragm shock tube, where an auxiliary section further heats the driven gas via a secondary shock wave.

A wide range of operating conditions can be reached by using different gases and pressures. However, this versatility is balanced by the short test times of a shock tube. It is key to know the experiment time duration as a function of the tube dimensions, geometry and gas operational parameters.

3.1 Fundamental principles of an ideal shock tube

3.1.1 Single diaphragm with constant area shock tubes

The most rudimentary shock tube design consists of two chambers, with equal cross area, the driver section on the left and the working section on the right. These sections are split by a single diaphragm. Initially, both chambers are filled with gases at rest, the driver (4) side at a higher pressure than the working (1) side.

When the diaphragm ruptures the gas in the driver section will rapidly expand towards the working section, forming a normal shock wave with velocity u_s . As it moves the gas pressure behind it (region 2) rises and induces a mass motion with velocity u_2 . The different gases are separated by a contact surface moving at velocity u_2 and pressure p_2 . Pressure and velocity are conserved in the contact surface, $p_2 = p_3$ and $u_2 = u_3$, but entropy has a discontinuity. Simultaneously, an expansion wave moves to

the high-pressure section, continuously decreasing the pressure in region 4 to a lower pressure value, p_3 , behind the expansion wave. The flow field and $x - t$ diagram of a simple constant area shock tube are depicted in Figure 3.1.

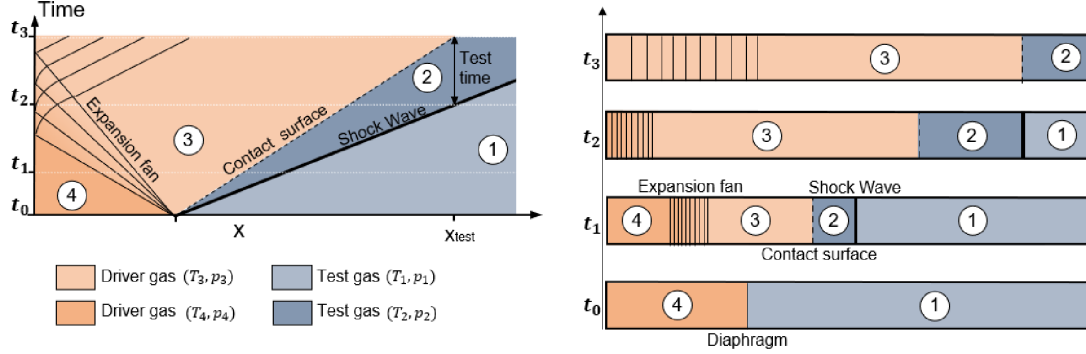


Figure 3.1: $x - t$ diagram (left), and flow field diagram for a constant area ratio (right) [not at scale], adapted from [267]. 1-Undisturbed driven gas; 2-Steady flow of shocked gas under uniform conditions (hot flow); 3-Steady flow of shocked gas under expansion conditions (cold flow); 4-Undisturbed driver gas.

Moving normal shock waves

The one-dimensioned set of equations that governs a normal shock wave is shown in Equations 3.1, where ρ is the gas mass density, u the flow velocity, p the pressure, and h the specific enthalpy. We get the equation for conservation of mass 3.1a, conservation of momentum 3.1b, and conservation of energy 3.1c. A more detailed description and derivation of the shock wave governing equations can be found in [7]. Another important parameter to be taken into account is the sound velocity a in different gas conditions. For an ideal gas, it can be computed from Equation 3.2, where γ is the heat capacity ratio, $\mathcal{R}$ the perfect gas constant, $\mathcal{M}$ the gas mean molar and T the temperature.

$$\rho_1 u_s = \rho_2 (u_s - u_2) \quad , \quad (3.1a)$$

$$p_1 + \rho_1 u_s^2 = p_2 + \rho_2 (u_s - u_2)^2 \quad , \quad (3.1b)$$

$$h_1 + \frac{1}{2} u_s^2 = h_2 + \frac{1}{2} (u_s - u_2)^2 \quad , \quad (3.1c)$$

$$a = \sqrt{\gamma \cdot \frac{p}{\rho}} = \sqrt{\gamma \cdot \frac{\mathcal{R} \cdot T}{\mathcal{M}}} \quad . \quad (3.2)$$

The shock velocity can be expressed as a function of the pressure ratio, p_2/p_1 , and sound speed in the test section a_1 , it is given by

$$u_s = a_1 \sqrt{\frac{\gamma_1 + 1}{2\gamma_1} \left(\frac{p_2}{p_1} - 1 \right) + 1} \quad (3.3)$$

To maximize the shock velocity u_s , one needs to maximize the pressure ratio p_2/p_1 . Said pressure

3. SHOCK TUBE THEORY

ratio relates to the ratio p_4/p_1 , the pressure ratio at the diaphragm rupture moment. The strength of the incident shock and expansion waves is also determined by the ratio p_4/p_1 . Which can be expressed by the following equation,

$$\frac{p_4}{p_1} = \frac{p_2}{p_1} \left\{ 1 - \frac{(\gamma_4 - 1) \left(\frac{a_1}{a_4} \right) \left(\frac{p_2}{p_1} - 1 \right)}{\sqrt{2\gamma_1 \left[2\gamma_1 + (\gamma_1 + 1) \left(\frac{p_2}{p_1} - 1 \right) \right]}} \right\}^{-\frac{2\gamma_4}{\gamma_4 - 1}}. \quad (3.4)$$

The incident shock strength p_2/p_1 can also be maximized by minimizing a_1/a_4 . As stated by Equation 3.2, the speed of sound is higher in hot light gases vs. cold heavy ones. To maximize the incident shock strength, one must choose a low molecular weight gas at high temperature for driver gas (yielding a high a_4), and a high molecular weight at low temperature for the working section (yielding a low a_1). The expansion wave strength is given by the ratio p_3/p_4 , since $p_2 = p_3$, it yields,

$$\frac{p_3}{p_4} = \frac{p_3}{p_1} \frac{p_1}{p_4} = \frac{p_2}{p_1} \left(\frac{p_4}{p_1} \right)^{-1} = \left\{ 1 - \frac{(\gamma_4 - 1) \left(\frac{a_1}{a_4} \right) \left(\frac{p_2}{p_1} - 1 \right)}{\sqrt{2\gamma_1 \left[2\gamma_1 + (\gamma_1 + 1) \left(\frac{p_2}{p_1} - 1 \right) \right]}} \right\}^{\frac{2\gamma_4}{\gamma_4 - 1}}. \quad (3.5)$$

Incident and reflected expansion waves

As aforementioned, as the diaphragm is removed, an expansion wave is created. This expansion wave head moves to the left into region 4, where the mass-motion velocity is zero. The head of the expansion wave will thus propagate to the left with a velocity $u_4 - a_4 = 0 - a_4 = -a_4$. Inside the expansion the temperature (and thus sound velocity) reduces, spreading the wave down the tube. Within the expansion wave, the induced mass velocity motion is $-u$ (directed towards the left). Inside the wave, the temperature, and hence a , is reduced. Meaning, although the head of the wave advances into region 4 at the speed of sound $-a_4$, the remaining parts of the wave propagate at slower velocities. The expansion wave will, thus, spread out as it propagates down the tube, “growing” over time.

At the driver end wall, the head wave will reflect back, moving now towards the right. As the reflected wave head interacts with the incident left-running wave it accelerates. This mixed left and right-running waves region is called a non-simple region. A simple region can only occur when the wave is propagating into a uniform region. There, it only possesses one family of characteristic curves, given by straight lines. Whereas a non-simple region has the two families of characteristics as curved lines. Numerically this is a problem, with the system’s characteristic lines and the compatibility conditions (the Riemann invariants) being pieced together point by point.

Because the head of the reflected expansion wave moves faster than the incident shock wave, it can cross the shock wave before the observation point. The lengths of the driver and driven sections must then be chosen to ensure that the reflected expansion does not “overtake” the shock wave and is delayed as much as possible.

Reflected shock wave

Ahead of the incident shock, region 1, the mass is stationary thus $u_1 = 0$. Behind the shock, the mass moves at velocity u_2 towards the right. A reflected normal shock wave, subscript 5, is created when the incident shock hit the right wall, assumed flat. To conserve the zero-velocity boundary the mass motion behind the reflected shock wave must thus be zero $u_5 = 0$. The reflected shock propagates into the moving gas with a Mach number M_r given by,

$$M_r = \frac{(u_r + u_2)}{a_2}, \quad (3.6)$$

Applying a variation of Equation system 3.1 and taking a calorically perfect gas, a relation between M_r and M_s can be written as

$$\frac{M_r}{M_r^2 - 1} = \frac{M_s}{M_s^2 - 1} \sqrt{1 + \frac{2(\gamma_1 - 1)}{(\gamma_1 + 1)^2} (M_s^2 - 1) \left(\gamma_1 + \frac{1}{M_s^2} \right)}. \quad (3.7)$$

Therefore, the reflected wave velocity u_r is given by

$$u_r = a_2 M_r - u_2. \quad (3.8)$$

Shock tube length

The useful flow duration is determined by the distance between the diaphragm and test section, x_{test} . Nonetheless, the driver length x_4 should be large enough to prevent the reflected wave (at the driver wall) from affecting the useful flow. Alongside it, the working section length x_1 must also be sufficiently long to prevent the reflected wave (at the working wall) to cross the test flow at the test section.

The minimum length for the low-pressure section, x_1 , will depend on test section length, x_{test} , shock Mach number M_{s1} , as well as the flow characteristics in regions 1 and 2. For strong shocks, the minimum length for the driver section, given by Walenta in [268], is

$$\left(\frac{x_4}{x_{test}} \right)_{min} = \frac{2}{4u_3/a_4} \left[\frac{\gamma_4 + 1}{(\gamma_4 - 1) + 2} \left(1 - \frac{\gamma_4 - 1}{2} \frac{u_3}{a_4} \right) \right]^{\frac{\gamma_4 + 1}{2(\gamma_4 - 1)}}. \quad (3.9)$$

A semi-empirical relation states that stable shock conditions will be reached for a test section length equal to 40 times its diameter [269].

3.1.2 Single diaphragm with variable area shock tubes

A cross-section area reduction may be applied at the diaphragm to increase the shock wave velocity. This area reduction is equivalent to an increase in the driver temperature. Figure 3.2 shows a schematic diagram of a shock tube with a convergent geometry at the diaphragm. When the diaphragm is removed, energy from the driver gas is extracted through an unsteady (time-dependent) expansion, from state 4 to 3a. This transition can have three possibilities.

3. SHOCK TUBE THEORY

1. Uniform - if the connecting areas are equal, states 3a and 3b coalesce and it corresponds to the diaphragm location.
2. Supersonic cold flow (region 3) - if the flow is supersonic, this is the region where Mach equals one.
3. Monotonically convergent transition - flow from 3a to 3b is a steady nozzle flow. The flow at 3b may be subsonic or sonic ($M_{3b} \leq 1$). Following it, one gets either an uniform flow with $M_3 \leq 1$ or an unsteady supersonic expansion $M_3 > 1$

Lastly, if the transition section is a convergent-divergent nozzle, i.e. if region 3b' has the lowest area, one has a subsonic or supersonic nozzle between states 3a and 3b. For the supersonic case, the exit flow at state 3b may be subjected to a further unsteady expansion to state 3. This case is not very important in our facility.

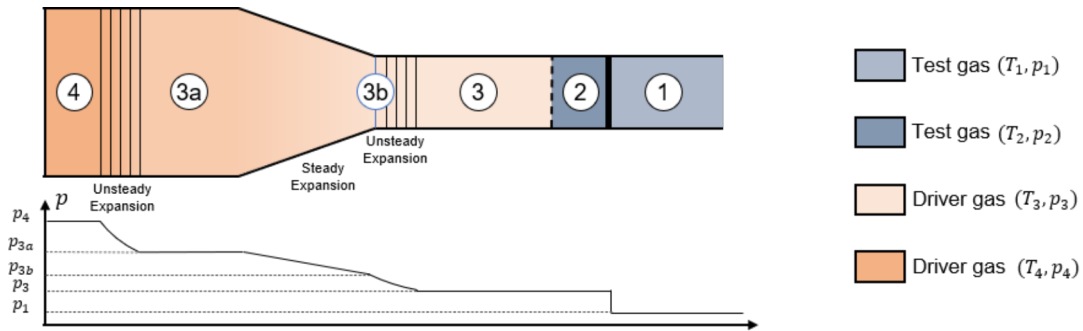


Figure 3.2: Flow field diagram for an area reduction shock tube [not at scale], adapted from [267]. 1- Undisturbed driven gas; 2-Steady flow of shocked gas under uniform conditions (hot flow); 3a-Steady flow under uniform conditions 3a; 3b-Unsteady expansion to conditions 3 (if $M_3 > 1$); 3-Steady flow of shocked gas under expansion conditions (cold flow); 4-Undisturbed driver gas.

For the following analysis, except across the shock itself, we consider isentropic processes and ideal gas behaviour. Based on the works of Alpher and White [270] and considering the general case of a convergent-divergent diaphragm section, the pressure ratio p_4/p_1 can be expanded as,

$$\frac{p_4}{p_1} = \frac{p_4}{p_{3a}} \frac{p_{3a}}{p_{3b'}} \frac{p_{3b'}}{p_{3b}} \frac{p_{3b}}{p_3} \frac{p_3}{p_2} \frac{p_2}{p_1} \quad (3.10)$$

Where p_4/p_{3a} is the ratio required to accelerate the driver gas from rest to the Mach number M_{3a} through an unsteady expansion; $p_{3a}/p_{3b'}$ the ratio required to bring the driver gas by a steady expansion from M_{3a} to $M_{3b'}$; $p_{3b'}/p_{3b}$ the ratio required to bring the gas by a steady expansion from $M_{3b'}$ to M_{3b} through an area ratio $A_{3b'}/A_{3b}$; and p_{3b}/p_3 is the ratio needed from an unsteady expansion from M_{3b} to M_3 . At the contact surface, we have $p_3 = p_2$.

Applying the Riemann invariant across an unsteady isentropic wave results in the non-steady expansion wave (regions 4 – 3a and 3b – 3), and recalling $u_4 = 0$, we get,

$$\frac{p_4}{p_1} = \frac{1}{g} \frac{p_2}{p_1} \left[1 + \frac{\gamma_4 - 1}{2} M_3^2 \right]^{\frac{2\gamma_4}{\gamma_4 - 1}} = \frac{1}{g} \frac{p_2}{p_1} \left[1 - \frac{u_2}{a_1} \frac{a_1}{a_4} \frac{\gamma_4 + 1}{2} g^{-\frac{\gamma_4 - 1}{2\gamma_4}} \right]^{-\frac{2\gamma_4}{\gamma_4 - 1}}, \text{ where} \quad (3.11a)$$

$$g = \left\{ \left[\frac{2 + (\gamma_4 - 1)M_{3a}^2}{2 + (\gamma_4 - 1)M_{3b}^2} \right]^{1/2} \left[\frac{2 + (\gamma_4 - 1)M_{3b}}{2 + (\gamma_4 - 1)M_{3a}} \right] \right\}^{\frac{2\gamma_4}{\gamma_4 - 1}}. \quad (3.11b)$$

The factor g is the equivalence factor defined by Alpher and White [270]. A more complete description and derivation of the different equations used here may be found at [7].

For a subsonic cold flow (the previous assumption of isentropic does not exclude a transition to subsonic flow via a shock), the transition section is a subsonic nozzle with $M_{3b} = M_3$, $p_{3b} = p_3$ and $a_{3b} = a_3$. Equations for A_4/A_1 , M_3 and g , see [7] for the explicit form, can be solved iteratively from an initial guess of a_4 , a_1 and M_s . In this subsonic case, only the ratio A_4/A_1 matters and the existence of a minimum section is irrelevant.

For supersonic cold flow, $M_3 \geq 1$, one must have $M_{3b'} = 1$ (or $M_{3b} = 1$ if the section is monotonically convergent, where $A_{3b} = A_1$ is the minimum area). The previous relations between area ratio and Mach number in supersonic nozzles apply, as used in obtaining 3.11b, to yield M_{3a} and M_{3b} . From these M_3 , g and p_4/p_1 follows.

As previously discussed, the distance between the diaphragm and the test section, x_{test} , determines the useful flow duration in the shock tube. For this type of configuration, according to Walenta [268], the minimum ratio between the driver section and the test section is given by,

$$\left(\frac{x_4}{x_{test}} \right)_{min} = \frac{M_{3a} + 1}{4 \frac{u_3}{a_4}} \left[\frac{\gamma_4 + 1}{(\gamma_4 - 1)M_{3a} + 2} \left(1 - \frac{\gamma_4 - 1}{2\bar{k}} \frac{u_3}{a_4} \right) \right]^{\frac{\gamma_4 + 1}{2(\gamma_4 - 1)}}, \quad (3.12)$$

where $\bar{k}$ is given by Equation 3.13,

$$\bar{k} = \left[\frac{2 + (\gamma_4 - 1)M_{3a}^2}{\gamma_4 + 1} \right]^{\frac{1}{2}} \left[\frac{\gamma_4 + 1}{2 + (\gamma_4 - 1)M_{3a}} \right]. \quad (3.13)$$

3.1.3 Double diaphragm shock tube

A simple shock tube cannot create shocks with extremely high Mach numbers and, in consequence, the attainable gas temperature is low. To improve the shock Mach number, the ratio of sound speeds between the driver and working gases must be increased. This can be done by using different gases and heating the driver gas. Area reduction at the diaphragm is equivalent to an increase in the driver gas temperature, as explained before. This area reduction causes a steady expansion wave in the driver gas in the subsonic region of the flow which is thermally more efficient than an unsteady expansion.

When the first diaphragm ruptures, a primary shock wave with velocity u_{s7} is formed, followed by a contact surface with velocity u_6 , that creates backwards moving expansion waves. When this primary shock wave reaches the second diaphragm, it is reflected and the pressure and temperature increase. Once the diaphragm opens, a main shock wave is created with a velocity u_{s1} , greater than the velocity of the primary shock wave u_{s7} . From the initial conditions, before the diaphragms are removed, the properties

3. SHOCK TUBE THEORY

of regions 11 (driver section), 7 (intermediate section) and 1 (working section) can be known. In a double diaphragm shock tube, the length of both driver and intermediate sections must be long enough, so that the disturbances do not affect the useful flow.

Shock tube with cross-section area reductions at both diaphragms

In the most general case of a double diaphragm shock tube with two cross-section area reductions, according to Walenta [268], it is assumed that the second diaphragm is sufficiently weak, that it bursts instantly when hit by the primary shock wave. Furthermore, both primary and main shock waves are assumed strong enough that the flow in regions 3 and 8 are supersonic and the primary shock reflection from the area reduction is also a shock. Finally, steady acceleration to the sonic speed in the cross-section area reductions is assumed to occur isentropically. Figure 3.3 depicts a representation of the different zones after the burst of both diaphragms. The full equation derivation can be found in [7, 268].

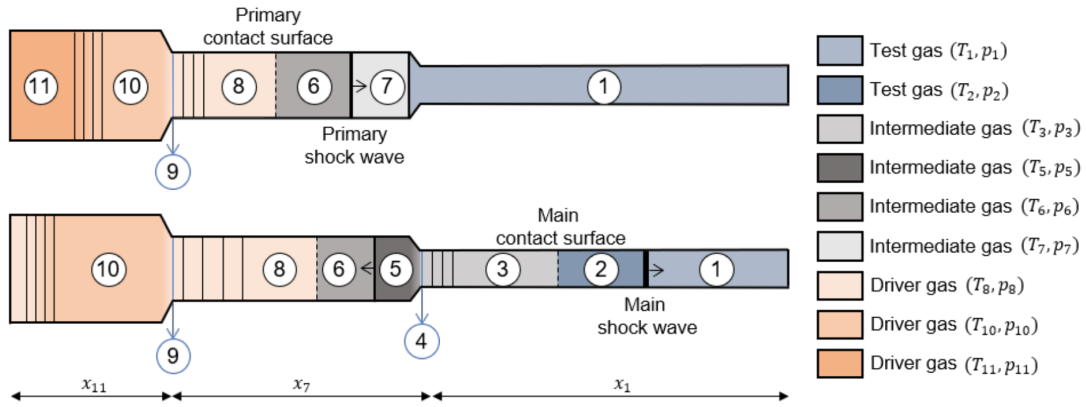


Figure 3.3: Flow diagram of a double stage shock tube with two cross-section area reductions [not to scale], adapted from [267]

In the case of a double diaphragm, double cross-section area reduction shock tube, the following conditions need to be satisfied, to avoid flow interferences:

- the expansion wave head, reflected from the driver's section end, must not "overtake" the primary contact surface before it intersects with the shock reflected from the second diaphragm;
- The wave, generated at the intersection of the primary contact surface with the shock reflected from the second diaphragm must not overtake the main contact surface before the test section.

Based on the first condition, the ratio x_{11}/x_7 should be larger than a minimum value, depending on the shock Mach number M_{s7} . This ratio can be written as,

$$\left(\frac{x_{11}}{x_7}\right)_{min} = \left(\frac{M_{10} + 1}{4}\right) \frac{a_{11}}{a_7} \left(\frac{1 + \frac{u_R}{M_{s7} a_7}}{x \frac{a_6}{a_7}}\right) \left\{ \frac{\gamma_{11} + 1}{k[(\gamma_{11} - 1)M_{10} + 2]} \left[k - \frac{\gamma_{11} - 1}{\gamma_7 + 1} \frac{a_7}{a_{11}} \left(M_{s7} - \frac{1}{M_{s7}} \right) \right] \right\}^{\frac{\gamma_{11} + 1}{2(\gamma_{11} - 1)}}, \quad (3.14)$$

where

$$\frac{u_R}{a_7} = \frac{u_6}{a_7} \left(\frac{X'}{M_6} - 1 \right) \quad (3.15a)$$

$$X' = \frac{u_R + u_6}{a_6} \quad (3.15b)$$

$$k = \left[\frac{2 + (\gamma_{11} - 1)M_{10}^2}{\gamma_{11} + 1} \right]^{\frac{1}{2}} \left[\frac{\gamma_{11} + 1}{2 + (\gamma_{11} - 1)M_{10}} \right] \quad (3.15c)$$

The Mach number M_{10} can be computed from

$$\frac{1}{M_{10}} \left[\frac{2}{\gamma_{11} + 1} \left(1 + \frac{\gamma_{11} - 1}{2} M_{10}^2 \right) \right]^{\frac{\gamma_{11} + 1}{2(\gamma_{11} - 1)}} = \frac{A_{11}}{A_7}. \quad (3.16)$$

To obtain a closed form for the expression $(x_7/x_{test})_{min}$, one has to assume that the intersection of the primary contact surface with the shock reflected from the second area reduction, creates a wave which behaves like a characteristic line. With this assumption, $(x_7/x_{test})_{min}$ can be calculated from

$$\left(\frac{x_7}{x_{test}} \right) = \frac{1}{2 \frac{u_3}{a_7} \phi} \left(\frac{\gamma_7 + 1}{2} - \frac{\gamma_7 - 1}{2} \frac{\frac{u_3}{a_7}}{\frac{a_4}{a_7}} \right)^{\frac{\gamma_7 + 1}{2(\gamma_7 - 1)}}, \quad (3.17)$$

where

$$\phi = \frac{\gamma_7 - 1}{\gamma_7 + 1} \frac{\left[1 + \frac{2}{(\gamma_7 - 1)M_{s7}^2} \right]}{X \frac{a_6}{a_7}} \left[1 + \frac{\frac{u_R}{a_7}}{(1 + M_5) \frac{a_5}{a_6} \frac{a_6}{a_7}} \right] \quad (3.18)$$

The above equations, and deductions for the conditions presented here, can be found in Walenta [268], and are valid if the following conditions are satisfied:

a) The flow in region 8 is sonic or supersonic:

$$M_{s7} \geq \frac{\gamma_7 + 1}{\gamma_{11} + 1} \frac{a_{11}}{a_7} \frac{k}{2} + \left[1 + \left(\frac{\gamma_7 + 1}{\gamma_{11} + 1} \frac{a_{11}}{a_7} \frac{k}{2} \right)^2 \right]^{1/2}; \quad (3.19)$$

b) The primary shock wave reflects at the second diaphragm as a shock:

$$M_6 \geq M_5; \quad (3.20)$$

c) The flow in region 3 is sonic or supersonic:

$$\frac{u_3}{a_7} \geq \frac{a_4}{a_7}; \quad (3.21)$$

d) No liquefaction (change of gas into liquid phase) occurs in any region of the flow.

Shock tube with cross-section area reduction only at the second diaphragm

In this case, the same two assumptions are required as in the previous more general case, although, the flow in region 8 does not need to be supersonic. The same set of equations can be used, now considering that the variable k becomes constant and equal to 1. Thus, Equations 3.15 and 3.16 are no longer needed to solve the system.

Shock tube with cross-section area reduction only at the first diaphragm

In this case, only the first assumption is necessary, considering that the primary shock wave is strong enough, such that the flow in region 8 is supersonic. No further details will be presented here, for more information resort to [7, 268].

Shock tube with constant areas

For this case, equations from the previous case can be applied, now replacing the variable k by 1, whenever necessary. Therefore, Equations 3.15, 3.16 and condition 3.19 are no longer needed. This means that only the condition to guarantee is that the liquefaction of the gas does not occur. Moreover, the assumptions concerning the flow Mach number in regions 3 and 8 are not necessary.

3.2 Performance enhancement through gas substitution techniques

Entry flight into outer gaseous planets whose atmospheres are predominantly composed of hydrogen and helium happens at extremely high velocities owing to the planets strong gravitational potential. In these cases, the flow is fully dissociated and significantly ionized when compared to an orbital or rocky planet entry. Except for the EAST shock tube, the majority of ground test facilities are incapable of reaching such velocities, typically in excess of 20 km/s. Henceforth, alternatives were studied to possibly simulate these entry conditions. Stalker and Edward [271] proposed two solutions: a) replace helium with a heavier gas, neon p.e.; or increase the portion of helium in the test section compared to an actual entry.

The first idea is to use a hydrogen and neon mixture instead of the original hydrogen and helium. If used in sufficient concentrations, the molecular weight of the test gas would increase due to neon's heavier mass. This would mean a higher post-shock temperature and the possibility of simulating realistic dissociation and ionization of hydrogen. The substitution was valid as long as the neon did not begin to ionize hence radiating and polluting the flow, due to high post-shock temperatures.

The second proposal from [271] is increasing the helium percentage in the flow field. It was shown that the helium's large ionization energy would make it act as a "inert" diluent in the hydrogen-helium flow. The helium atoms would serve as collision partners for the hydrogen atoms and molecules. The authors report an equivalent velocity flow of approximately 19 km/s. These are enough to simulate entries into

Saturn and Uranus. Both techniques were studied by James et al. [183, 272], and the author reports them as effective for simulating Saturn and Uranus entries.

3.3 Disturbance effects

In an ideal shock tube, both the shock wave and contact surface propagate at a constant speed, u_s and u_2 , respectively. The presence of a wall boundary layer in a real shock tube alters both these velocities. The boundary layer present between the shock and contact surface would remove mass, making the shock decelerate and the contact surface accelerate. The flow becomes non-uniform, and the separation distance l drops below the ideal value, reducing the effective test time.

3.3.1 Wall boundary layer

The wall boundary layer formation and effect behind a shock wave have been investigated by several authors. Trimpi and Cohen [273], Mirels [274], Dem'yanov [275] and, Spence and Woods [276] presented wall boundary layer theories focused on the unsteady motion of the shock. All represent the displacement effect of the boundary layer through a small perturbation of the flow variables.

Both Trimpi and Cohen [273], and Mirels [274] considered a small one-dimension acoustic perturbation to the flow. Trimpi and Cohen [273] inserted a wall friction term to the momentum equation. An entropy production term arises derived from the wall heat transfer and viscous dissipation in the energy equation.

They demonstrated that viscosity and heat transfer in the flow field behind the shock continuously formed unsteady waves, which damped the shock. Mirels [274] argues that these effects were confined to the wall boundary layers, whereas Trimpi and Cohen [273] convected them downstream with the ideal flow velocity. Mirels introduced only a mass addition term in the continuity equation for the linearized system of equations. Furthermore, he stated that the unsteady waves created at the inviscid core should be attributed to the radial component at the boundary layer edge. This presumed a thin boundary layer, whereas Trimpi and Cohen's model implied a thick one.

Nonetheless, both formulations foresee the same order of attenuation and agree with weak shock experiments. These two models are however deficient in reproducing the behaviour of strong shocks, as they neglect the flow in the rarefaction wave, important for increased strength shocks.

Dem'yanov [275], used the same formulation as Mirels [274], and obtained a closed form solution for the case of the laminar boundary layer between the shock and contact surface, yet he ignored disturbances originating in the cold flow. He demonstrated that the growth of the hot flow boundary slowed down the shock and accelerated the contact surface.

Spence and Woods [277] presented a theory just considering the hot flow. A derivation of linearized unsteady conservation equations was done assuming axial symmetry. They confirmed the Mirels' formulation that the boundary layer thickness is much smaller than the tube diameter. An extension of Dem'yanov's method of solution was developed covering the turbulent hot flow boundary layer case.

Duff [278] observed that, at a certain point in a low pressure shock tube, the distance between the shock wave and contact surfaced reached a maximum. After this limiting value both shock and contact

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surface starts moving at an equal and regular velocity. This implies that the test time/hot flow duration approaches a finite limit as the distance to the diaphragm increases. The author deduced that the limiting time grows with the square of the shock tube diameter for laminar boundary layer growth behind the shock. Anderson [279] proposed the same process, after some point the test time would decrease. This assumed the shock moved at constant velocity.

Roshko [280], Hooker [281], Mirels [30, 31] and Ackroyd [282] theories wanted to predict the duration of the hot flow, simultaneously satisfying the condition of mass continuity between the shock and the contact surface.

Roshko [280], and later Hooker [281], studied the limiting flow effect both theoretically and experimentally. Roshko's model assumed that the contact surface moved at constant velocity and that the free stream has a uniform velocity profile. These were later expanded and modified by Hooker. Mirels [30, 31] later modified this model to include acceleration of the contact surface, flow non-uniformities and real gas effects. The theoretical predictions of Ackroyd [282] show good agreement with experimental measurements, for the shocks in Argon at low Mach number, obtained by Duff [278].

Mirels theory

Mirels [30] states that in a conventional shock tube, the separation distance between the shock wave and contact surface will start to shrink after a certain time. His theory accounts for the non-uniform flow in the region between shock and contact surface. This effect is illustrated in Figure 3.4. In its coordinate system, the shock wave is stationary and the wall moves with velocity u_w . The flow between the shock wave and the contact surface is steady and the boundary layer is thin when compared to the tube radius.

Suppose the shock wave is positioned at $l = 0$ and the contact surface at $l = l_m$. Using Mirels notation, the flow upstream, (subscript ∞) the unperturbed gas, moves towards the shock with velocity u_w . The subscript e represents the free stream conditions in the region between the shock wave and contact surface, with the subscript 0 conditions immediately downstream of the shock. The mass flow percentage in the boundary layer increases with the distance l . At $l = l_m$, all the mass flow is in the boundary layer, and the free stream is stationary at that point.

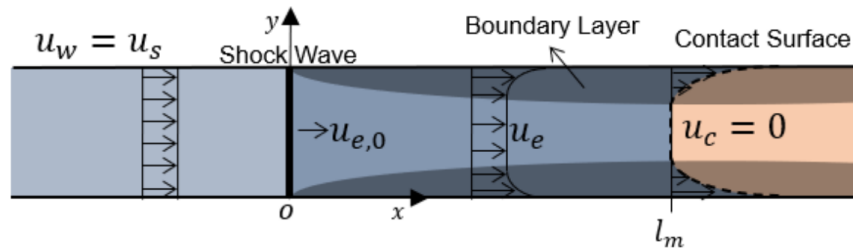


Figure 3.4: Boundary layer development diagram [not to scale], adapted from [267]

The mass flow passing the contact surface $\dot{m}_c$ is given by,

$$\dot{m}_c = \pi d \rho_{w,0} (u_w - u_{e,0}) \delta_r \quad , \quad (3.22)$$

where d , $\rho_{w,0}$ and δ_r are the tube diameter, the boundary layer density and thickness, respectively.

For a laminar boundary, δ_r can be written with function of an adimensional parameter β , kinematic viscosity $\nu_{w,0}$, boundary length l_m

$$\delta_r = \beta \left(\frac{\nu_{m,0} l_m}{u_w - u_{e,0}} \right)^{1/2}. \quad (3.23)$$

The mass flow into region 2 (between shock wave and contact surface) is,

$$\dot{m}_s = \pi d^2 \rho_{e,0} u_{e,0} / 4, \quad (3.24)$$

Concurrently, the mass flow out of region 2 by the boundary layer is given by,

$$\dot{m}_c = \pi d \beta \rho_{w,0} (u_w - u_{e,0}) \left(\frac{\nu_m l_m}{u_w - u_{e,0}} \right)^{1/2}, \quad (3.25)$$

Equating equations 3.24 and 3.25, gives the boundary layer length l_m

$$l_m = \frac{d^2}{16\beta^2} \left(\frac{\rho_{e,0}}{\rho_{w,0}} \right)^2 \frac{u_{e,0}^2}{u_w - u_{e,0}} \frac{1}{\nu_{w,0}}. \quad (3.26)$$

Mirels estimate the value of β in a twofold approach. First considering the boundary layer to develop in a uniform external stream, and then, using the concept of local similarity. Applying the uniform free stream, l_m is such that it equates the excess mass flow through the boundary layer and the mass flow through the shock. The author achieved a correlation for the first estimation β_0 within 2% based on numerical values, given by,

$$\frac{\beta_0}{C_{e,0}^{0.37}} = 1.135 \left(\frac{2(W-1)}{1+1.022W} \right)^{\frac{1}{2}} \left(1 + \frac{1.328 + 0.890W}{ZW - 1} \right). \quad (3.27)$$

In it, $C_{e,0} = [(\rho_e \mu_e)/(\rho_w \mu_w)]_0$ corrects for the variable $C = \rho \mu / (\rho \mu)_w$, and $W \equiv u_w / u_{e,0} = \rho_{e,0} / \rho_\infty$, is valid for all Prandtl number σ , between 0.72 and 1 [30, 283]. The parameter $Z = (\gamma + 1)/(\gamma - 1)$ is used for an ideal shock, and is replaced by W for the case of a strong non-ideal shock ($W \gtrsim Z$). Mirels' calculations were performed for air, yet he recognizes this relation as sufficiently accurate for the majority of gases found in shock tubes. The value β_0 is found to be too small, overestimating l_m .

The β estimation can be improved by considering the variation of free stream conditions. The local similarity approximation considers that the development of the boundary layer is simultaneous with the variation in free stream properties.

The local similarity concept assumes that the boundary layer profile at each l matches a boundary layer associated with a uniform free stream and wall velocity u_w . The boundary layer originates at l_i , initially an unknown function of l . Besides this, it is assumed that the boundary layer growth at each section is the same as for the corresponding uniform free stream boundary layer. The improved value of β , β_1 , can be estimated for strong shocks based on an approximate analytical integration as follows,

$$\frac{\beta_1}{\beta_0} = \left\{ \frac{1 + 1.002W}{2(W-1)^2} \frac{1}{(3.022W - 1) \ln \left(\frac{W}{W-1} \right) - 3.022} \right\}^{\frac{1}{2}} = \frac{W}{W-1} [1 + O(W^{-2})], \quad (3.28)$$

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where $O(W^{-2})$ are second order terms, we may neglect. The values of β_1 are larger than β_0 , as expected. When $W \rightarrow \infty$, $\beta_1 \rightarrow \beta_0$.

Test time

The test time of a shock tube flow, defined by Hooker [281] as “the spatial or temporal extent of shocked gas uncontaminated by driver gas”, is always smaller in a real shock tube than in an ideal one. In an ideal shock tube, both shock wave and contact surface move with constant velocities, in a real one, the boundary layers alters both of these. This effect is illustrated in Figure 3.5.

Mirels in [30] defines the test time, τ , equating it to the difference in time between the arrival of the shock and the arrival of the contact surface, at a position value of x . Defining $\bar{\tau} \equiv u_w \tau / l_m$ as the test time at x normalized to the test time when $x \rightarrow \infty$. In this model, where the shock moves at a constant velocity, it can be shown [284] that $\bar{\tau}$ and T_b are related through,

$$\bar{\tau} = T_b, \quad x = x_b - \frac{t_b}{W} . \quad (3.29)$$

The parameters X_b and T_b are the values of X and T computed by the following equations, from [30]. They are the dimensionless flow duration T , and dimensionless distance of the shock wave from the diaphragm X . A detailed derivation and analysis of these parameters is found in [280].

$$X \equiv \frac{x_s}{W l_m} \equiv \frac{u_{e,0} t}{l_m} = \frac{\int_0^{\bar{\delta}} \frac{\bar{\delta} d\bar{\delta}}{V H_e^2}}{\int_0^1 \frac{\bar{\delta} d\bar{\delta}}{H_e^2}}, \quad T \equiv \frac{l}{l_m} = \frac{\int_0^{\bar{\delta}} \frac{\bar{\delta} d\bar{\delta}}{H_e^2}}{\int_0^1 \frac{\bar{\delta} d\bar{\delta}}{H_e^2}} . \quad (3.30)$$

where $V = u_e / u_{e,0}$, $\bar{\delta}$ is the ratio of excess mass flow through the boundary layer to the mass flow through the shock, and H_e a function of the gas properties [30]. For an ideal shock, as there are no viscous losses, $W \rightarrow \infty$, and thus, $X = T$. In strong shocks, the external free stream remains reasonably uniform, and the excess mass flow in the boundary layer grows approximately as $l^{\frac{1}{2}}$. A relation between X and $\bar{\tau}$ can be attained, allowing for the calculation of the test time, $\bar{\tau}$, at a diaphragm distance x . It is given by,

$$-X = 2 \left[\ln \left(1 - \bar{\tau}^{\frac{1}{2}} \right) + \bar{\tau}^{\frac{1}{2}} \right] + \frac{\bar{\tau}}{W} . \quad (3.31)$$

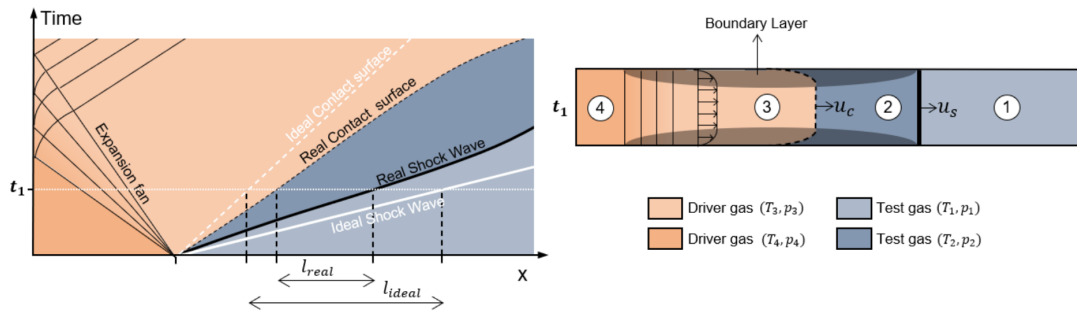


Figure 3.5: Ideal and real shock tube $x-t$ diagram [not to scale], adapted from [267]

3.3.2 Wall drag effects on shock velocity

As the boundary layer expands, the shock wave velocity is reduced through drag effects. Milne [285] established a simplified theory to estimate the effects of the shock-boundary layer interaction. It was done by including a source term to the ideal case. For the working section of a circular shock tube with diameter d_1 , the momentum source term per unit length of tube S_w is given by

$$S_w = \frac{4 \int_0^{d_1} \tau_w dz}{d_1^2} = \frac{4\tau_w}{d_1} \quad , \quad (3.32)$$

where τ_w is the wall friction. It can be expressed as a function of the friction coefficient C_f , of the ideal shock speed u_s and density ρ_s ,

$$\tau_w = C_f \frac{\rho_s u_s^2}{2} \quad . \quad (3.33)$$

The friction coefficient is parameterized in terms of the Reynolds number, Re , by

$$C_f = \begin{cases} 64/Re & \text{if } Re < 1200 \\ 0.316/Re^{\frac{1}{4}} & \text{if } 1200 < Re < 10^5 \\ 0.00332 + 0.221/Re^{0.237} & \text{if } Re > 10^5 \end{cases} \quad . \quad (3.34)$$

Reynolds number Re can be expressed as function of kinematic viscosity ν , and tube diameter and shock wave velocity as,

$$Re = Re_0 u_s = \left(\frac{d_1}{\nu} \right) u_s \quad . \quad (3.35)$$

The kinematic viscosity is related to the shock temperature T_s and viscosity μ through,

$$\nu = \mu \frac{\sqrt{\frac{T_s}{3000}}}{\rho_s} \quad . \quad (3.36)$$

where μ is a function of the gas in the shock tube, typically approximated by 10^{-4} [285]. Given these parameters, the momentum source term can be added to the velocity equation, and is calculated iteratively by,

$$u_{losses}(i) = u_{losses}(i-1) - \frac{S_w dx}{\rho_s} \quad (3.37)$$

with $u_{losses}(i-1)$ the velocity in the previous step, each distanced by a value dx from the previous. In this way, shock velocity can be predicted accounting for the wall drag effects.

A numerical code is often needed to compute the shock wave conditions in a shock tube, especially if real gas or wall drag effects are considered. Through this work, the ESTHER performance was estimated using the STAGG (Shock Tube And Gas-Gun) numerical code [286] developed by Fluid Gravity Engineering. It can model single or double diaphragm gas driven shock tubes, with or without convergent sections at either diaphragms with a physical model based on the works of [30, 268, 270]. The code

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computes the shock speed and pressure, at various points in the shock tube, and assumes an isentropic and inviscid gas, the conservation of mass and energy and the sonic condition at the narrow points of convergent sections [286]. Real gas effects can be included in both the driver and test section, based on the chosen gas type, temperature and pressure. Losses effect can either be ignored or included through the Mirels model [30].

3.3.3 Blast wave formation

As discussed before, in its traditional form a shock tube is comprised of a high-pressure gas chamber, whose gas is abruptly released by the rupture of a diaphragm membrane. This leads to the formation of a shock structure, comprised of a shock wave, a contact discontinuity and a family of expansion waves. For sufficiently long tubes, it was experimentally observed that this wave structure will evolve into a shape similar to an air blast wave [287].

The blast wave formation starts with the reflection of the head expansion wave at the driver end wall. As previously discussed, this reflection will propagate faster than the shock and can reach it. When the reflected head wave reaches the right-propagating shock, it begins the formation of a Friedlander waveform in the shock tube.

To study the formation of the blast wave, the wave interactions taking place upon the reflection of the head expansion wave at the left end wall need to be considered. When the reflected head wave reaches the right-propagating shock, it marks the onset of the Friedlander waveform in the shock tube. The location of this wave “crossing” which onsets the waveform simply requires tracking the head wave position, as illustrated in Figure 3.6. The algorithm here presented is only valid for a single diaphragm configuration, thus the use of the notation of section 3.1.1

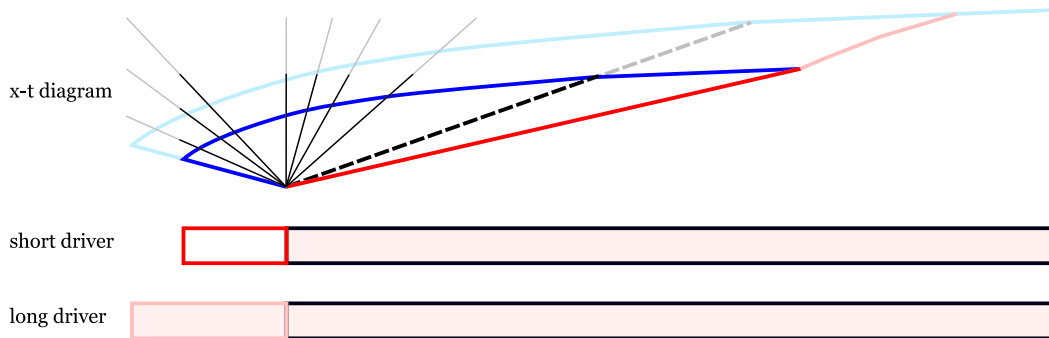


Figure 3.6: Blast wave formation [not to scale], adapted from [7]

The location L of the Friedlander wave is given by,

$$L = L_4 + u_s t_d \quad , \quad (3.38)$$

where L_4 is the length of the driver section, u_s is the shock speed and t_d is the time of onset of the Friedlander wave. The onset time can be computed from,

$$t_d = \frac{2L_4 a_2}{a_4(u_2 + a_2 - u_s)} \left(\frac{p_4}{p_2} \right)^{\frac{\gamma_4+1}{4\gamma_4}}, \quad (3.39)$$

where a_2 and a_4 are the sound speeds, p_2 and p_4 the driver pressures, respectively behind the shock and initially at the driver section, and γ_4 the specific heat ratio in the driver section. As the pressure ratio p_4/p_1 is known from Equation 3.4, the shock speed u_s can be obtained from Equation 3.3. The flow and sound velocities, u_2 and a_2 , behind the shock can be computed through Equations 3.40 and 3.41, respectively.

$$u_2 = \frac{a_1}{\gamma_1} \left(\frac{p_2}{p_1} - 1 \right) \sqrt{\frac{\frac{2\gamma_1}{\gamma_1+1}}{\frac{p_2}{p_1} + \frac{\gamma_1-1}{\gamma_1+1}}}, \quad (3.40)$$

$$a_2 = a_1 \sqrt{\frac{p_2}{p_1} \frac{\frac{\gamma_1+1}{\gamma_1-1} + \frac{p_2}{p_1}}{1 + \frac{\gamma_1+1}{\gamma_1-1} \frac{p_2}{p_1}}}, \quad (3.41)$$

solving these equations would close the equation system, and yield the position of the onset for the blast wave formation.

3.3.4 Secondary diaphragm rupture

Several theories have been proposed to model how a diaphragm ruptures. The first approach is typically the *ideal rupture* model (figure 3.7a). In it the secondary diaphragm is infinitely thin and massless, meaning it ruptures instantaneously on impact by the primary shock and, thus, has no impact on the flow. In reality, the diaphragm interferes with the flow during and after rupture, affecting both the primary shock reflection strength, and the test gas rate of expansion, which is forced to stagnate. Therefore, the test flow conditions diverge from the ideal case. The two most common approaches are the *diaphragm inertia*, and *holding time* rupture models, which take into account the finite opening rate of the secondary diaphragm.

The *diaphragm inertia* model (figure 3.7b) was proposed by Morgan and Stalker [288], it acknowledges that the diaphragm inertia limits the rate of expansion. Their model assumes that the diaphragm breaks instantaneously as the primary shock hits it, and then stays together as an obstacle in the flow field. The reflected shock weakens over time through the interaction with expansion waves, caused by diaphragm acceleration. Kendall et al. [255] developed an experiment in the X1 expansion tube to determine the effect of heat losses and the non-ideal rupture of the light secondary diaphragm on the driver performance.

They compared experimental shock speed data near the secondary diaphragm to the original inertia diaphragm model from Morgan and Stalker [288], and to the numerical inertial diaphragm model of Petrie-Repar [289], which solved the unsteady one-dimensional Arbitrary Euler Lagrangian (AEL) equation for inviscid compressible flow during the shock reflection process. The authors concluded that Petrie-Repar's model was able to better reproduce the diaphragm rupture than Morgan and Stalker's [288]. Nonetheless, the trajectories computed by the AEL code started to diverge after the first 30 μ s, underestimating the shock speed. Kendall et al. conclude that this means "*the diaphragm inertial model, in its current form,*

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is not complete”.

Wegener et al. [290] investigated the rupture of a light cellophane diaphragm in an expansion tube. They subjected the diaphragm to a large pressure differential, via a shock wave, and recorded holographic images along the rupture process. The initially curved diaphragm broke peripherally after the shock wave impacted it. Upon rupture, the diaphragm flattened out, becoming planar and propagating downstream. After moving around a quarter of the tube diameter, the diaphragm starts to fragment in the centre. The authors concluded that the cellophane diaphragm ruptures through fragmentation. The authors in [290] also observed that the diaphragm trajectory and gas interface were well modelled by the diaphragm inertial model for a short time after rupture, gradually losing accuracy. After propagating half a tube diameter downstream, the interface accelerated more than predicted due to a mass reduction in the diaphragm, derived by fragments falling behind.

Furukawa et al. [291] experimented with the Japanese JX-1 free piston-driven expansion tube, using the shadowgraph technique and wall pressure measurements, to examine the effect of diaphragm thickness and material on flow disturbance due to diaphragm rupture. The secondary diaphragm, separating the acceleration from the shock tube, was a sheet made of either Mylar or cellophane. They tested Mylar diaphragms with 3 and 25 μm thickness, and cellophane with 23 μm thickness. Furukawa et al. [291] observed radiation emitted behind the cellophane 23 μm and Mylar 25 μm thick diaphragms, which unambiguously mark stagnation of the test gas. The ruptured diaphragm pieces react with the shock wave, causing it to slow down. For the thinner 3 μm Mylar diaphragm the shock wave remained planar and the transmitted shock wave nearly maintained its speed. Furukawa et al. [291] considered that the *diaphragm inertia* model reproduced the experimental trajectory, despite an underestimation of the shock wave speed.

Haggard [292] suggested the *holding time* rupture model, (figure 3.7c). The model marks the time of arrival of the primary shock at the secondary diaphragm, after a set time, it is instantaneously removed. This model undervalues the test gas recombination through the unsteady expansion [173]. Nonetheless, it can give a simple pre-rupture flow field. Wilson [293], Roberts et al. [294] and, Bakos and Morgan [295] applied the *holding time* model and verified that it produces pressure disturbances near the secondary diaphragm, which are identified in experimental work. Besides it, Roberts et al. [294] studied the effects of the secondary diaphragm thickness and deformation. They estimated the diaphragm rupture time both numerically and experimentally. Furthermore, Bakos and Morgan [295] demonstrated the limitations of the *holding time* model by comparing it with an inertial diaphragm model.

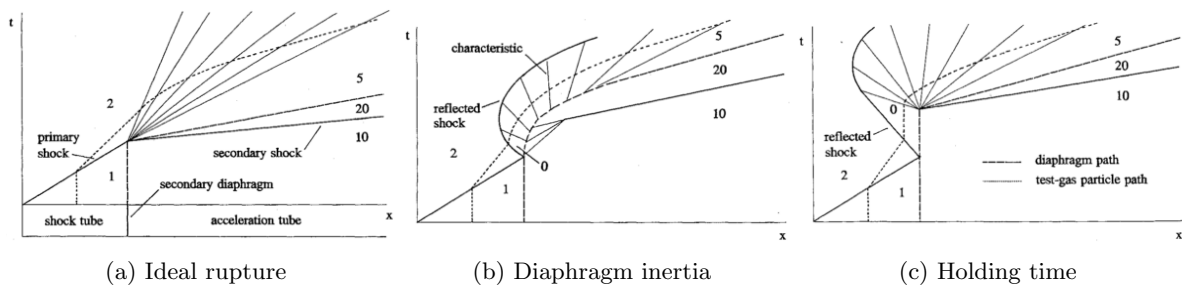


Figure 3.7: Diaphragm rupture models illustrations by Bakos and Morgan [295]

3.4 Nozzle flow

A shock tube is capable of being used as an aerodynamic facility, by placing an expanding nozzle section within the entrance to the dump tank and including the required windows. The larger cross-section enables the inclusion of scale models facing the hypersonic flow. A diaphragm separates the shock tube section with the test gas from the nozzle side in vacuum. The shock wave ruptures the diaphragm and the already shocked test gas expands and accelerates at the expansion zone. As it expands, the gas pressure, density and temperature decrease. The gas pressure at nozzle entry is generally tailored to achieve the desired free-stream conditions after the expansion, see Figure 3.8. The steep decrease in temperature and density quickly freezes the gas chemical reactions, so the gas composition on a first approach is equal to the pre-expanded one. The useful test time is, as in a normal shock tube, the time between the shock wave and contact surface. Figure 3.9 illustrates an expanding nozzle with the useful test flow time marked between the shock wave (solid thick line) and contact surface (dashed line).

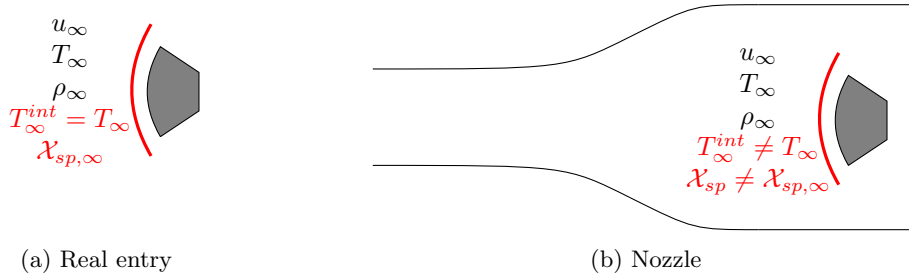


Figure 3.8: Real entry vs. nozzle properties, adapted from [7].

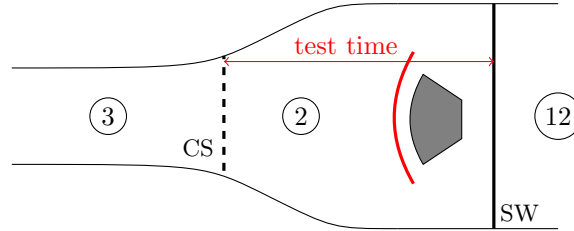


Figure 3.9: Nozzle design, adapted from [7]

The gas conditions after expansion can be estimated, in a first approach, recurring to the quasi-1D nozzle equations:

$$\frac{A_2}{A_{12}} = \frac{M_{12}}{M_2} \left(\frac{1 + \frac{\gamma-1}{2} M_2^2}{1 + \frac{\gamma-1}{2} M_{12}^2} \right)^{\frac{\gamma+1}{2(\gamma-1)}}, \quad (3.42)$$

$$\frac{p_2}{p_{12}} = \left(\frac{1 + \frac{\gamma-1}{2} M_{12}^2}{1 + \frac{\gamma-1}{2} M_2^2} \right)^{\frac{\gamma}{\gamma-1}}, \quad (3.43)$$

$$\frac{T_2}{T_{12}} = \left(\frac{1 + \frac{\gamma-1}{2} M_{12}^2}{1 + \frac{\gamma-1}{2} M_2^2} \right). \quad (3.44)$$

If a smaller effective nozzle cross-section is taken, by accounting for the boundary layer effect, this

3. SHOCK TUBE THEORY

approach is normally sufficiently good [183]. This is quite similar to the operation of an expansion tube, depicted in Figure 3.10, where the gas is expanded and accelerated as it enters a second test section with a larger area A . This expansion results in faster shock waves and better performance.

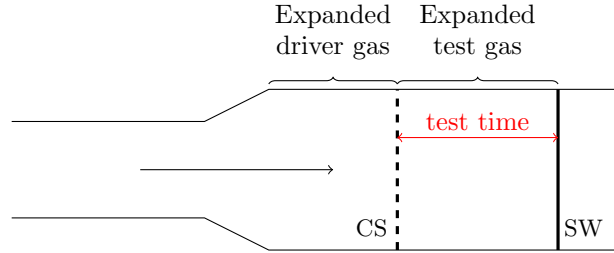


Figure 3.10: Expansion tube design, adapted from [7].

Whenever expansion nozzles are contemplated for a shock tube facility some difficulties arise. The gas expansion throughout the nozzle of the tube cross-section leads to an inevitable decrease of density (in the supersonic regime), which decreases the rate of collision at a microscopic level. Further, the gas particles, atoms and molecules, have both translational and internal degrees of freedom. The internal ones are, within the Born-Oppenheimer approximation, electronic excitation (atoms and molecules), vibration and rotation (just molecules). The total gas energy account for both these contributions [8],

$$E_{gas} = \sum_{sp} E^{sp} = \sum_{sp} (E_{tr}^{sp} + E_{int}^{sp}) \quad . \quad (3.45)$$

When there are enough collisions between the gas particles, the translational and internal degrees are said to be in equilibrium, with an equal characteristic temperature T . Nonequilibrium between the different degrees of freedom starts to appear when the collision rates are sufficiently low. The translational mode effectively redistributes energy, reaching thermodynamic equilibrium, in a much lower number of collisions than the internal modes [296]. In a first approach, one can describe the relaxation of the internal modes of a gas particle with the Landau-Teller equation, assuming a Boltzmann equilibrium for the internal states [297],

$$\frac{dE_{int}^{sp}}{dt} = \frac{E_{int}^{sp}(T) - E_v^{sp}(T_{int})}{\tau_{tr-int}^{sp}} = \rho_{sp} \frac{e_{int}^{sp}(T) - e_v^{sp}(T_{int})}{\tau_{tr-int}^{sp}} \quad , \quad (3.46)$$

where τ_{tr-int} is the characteristic relaxation time between the translational and internal modes, and e^{sp} the specific energy per unit volume of each specie. As the expansion of a gas progresses, its density decreases, lowering the energy exchange rate between translational and internal modes. As a consequence of the ρ_{sp} term getting smaller, the relaxation time, τ_{tr-int} , increases lowering energy exchange rate, dE_{int}^{sp}/dt , which will cause T_{int} to start lagging behind T . The internal “temperature” T_{int} may even freeze at an intermediate value between T_2 and T_{12} .

This exposes a conceptual difficulty with expansion tubes and similar facilities: the thermodynamic state of the moving, post-shock gas is uncertain as it could be disturbed during the expansion and no longer in thermodynamic equilibrium. Notice that the reduction in the area cross-section is not a problem. When the gas is just compressed, ρ can only increase, and the energy exchange processes are even faster,

maintaining the thermodynamic equilibrium of the moving gas. For an expansion tube, the test gas can only be rigorously considered in thermodynamic equilibrium in the region between the shock wave and contact surface. The gas moving behind the contact surface experiences an expansion (as shown in Figure 3.10), thus its thermodynamic equilibrium condition cannot be completely guaranteed.

Altogether, the previous remarks do not entail that these facilities necessarily provide inaccurate or erroneous results. Additional experimental and numerical diagnostics may be deployed to accurately determine the gas flow internal temperature at the nozzle. Experimental results still provide accurate test cases, as long as T_{int} is reported and correctly used in the numerical models. An identical situation can be encountered whenever one tries to determine the plasma plume properties in a continuous plasma wind tunnel. In these facilities, hot plasma is expanded via a nozzle mostly designed for TPS testing. A comparison between the in-flight situation and the situation of a model placed downstream of a nozzle is presented in Figure 3.8.

Whenever a nozzle is present one must start the nozzle flow before the experiment. A family of unsteady waves travel through the nozzle, followed by the formation of steady expansion field and a quasi-steady nozzle boundary layer. Initial boundary layers must be formed over the model, including separated regions if needed, as well as the bow shock with the sub and supersonic regions of the post-shock flow. This starting process depends on the length-scale and velocity of the flow. For example, windward shock layers in hypersonic flow have short-length scales and high local sound speeds, thus are quickly developed. Detached regions are, on the other hand, characterized by long-length scales and low sound speeds, thus take several flow lengths to be fully established. Fundamentally unsteady phenomena like vortex shedding, for example, need time scales of multiple cycles to provide useful data.

The passage of the high-speed and temperature accelerator gas, before the arrival of the useful test gas, creates a prior flow that helps the establishment of the steady flow in the consequent test gas [298]. It is comparable to the “prior-steady flow” technique developed by Mudford and Stalker [33] to initialize nozzles in high-speed non-reflected shock tunnels. This technique was latter analyzed in greater detail by Mudford et al. [34] and, Stalker and Mudford [35].

Chapter 4

The European Shock Tube for High Enthalpy Research: Design, Manufacturing and Metrology

“It was too big to be called a sword. Massive, thick, heavy, and far too rough.

Indeed, it was a heap of raw iron.”

- Kentaro Miura, Berserk, Vol. 1

The present Chapter discusses the overall ESTHER design and the choices behind it. These include, the sizing of the components, the hydraulic system and the diaphragm design, as well as the control and shock wave detection system and instrumentation. Details on the combustion chamber and test section are discussed in later Chapters.

The high level user requirement are connected to the entire design and manufacturing process of the ESTHER facility. While some specifications can be mostly traced back to the particular requirements, others have broader implications. The facility’s performance is dictated mostly by its design choices, driver dimensions, single or double stage shock tube, etc. The driver choice play a major role on the operation of the shock tube, repeatability, turnover rate, and performance. Shot pollution is highly correlated with material choice. Connecting all these choices is the manufacturing process with the associated dimensional and geometric tolerances.

4.1 Overall design

ESTHER is expected to reach shock wave velocities in the range of 4 to 14 km/s in air, and above 18 km/s in H₂-He mixtures [7, 267]. Different drivers are better suited for different applications. Since ESTHER’s main focus is the study of kinetics and radiation, pollution needs to be minimized. Tables 4.1

and 4.2 compare the different driver options and materials used for the test section. Nonetheless, a high repeatability and test turn rate are required [19, 299], and accordingly, a combustion driver was selected as the better option. Although a free-piston driver has a high repeatability rate, the risk of moving parts was enough of a deterrent to leave it (specially owing to the incident that befell TCM2 in 2007). For a better repeatability rate, and despite the lower peak performance, ESTHER was designed to operate with a combustion driver in deflagration mode (instead of detonation mode). Concerning the test section, typical materials of choice are steel and aluminium. The former can lead to pollution from the carbon atoms released from the steel, while the latter oxidizes with greater facility. Steel will outgas carbon atoms, especially when subject to heat, like a shock wave or a combustion process. The outgassed carbon atoms will then pollute the test section leading to contamination of any experimental data. In aluminium shock tubes the surface can oxidize with relative ease, forming a layer of Al_2O_3 . The surface, owing to its lower Young's modulus, may be scratched or damaged more easily by the shrapnel of the ruptured diaphragms, ending up with a peel off of a chunk of the oxide layer. This can in turn pollute the spectral data, similar to the carbon in steel shock tube. In short, the bitter debate of steel vs. aluminium as the best shock tube material still rages on among the community.

Table 4.1: Different shock tube drivers advantages and disadvantages.

	Strong recoil Moving parts	Pollution in the Driver	Repeatability	Performance (high p, T)	Example
Free-Piston	yes	no	good	excellent	X2 [183]
Electric Arc	no	yes	bad	excellent	EAST [27]
Comb. Detonation	no	yes	moderate/bad	good	HYPULSE [160]
Comb. Deflagration	no	no/yes	good	good	ESTHER [21]

Table 4.2: Different shock tube test section materials advantages and disadvantages.

	Surface pollution from carbon	Surface damage and pollution Al_2O_3	Example
Aluminium	no	yes	X2, EAST [28, 183]
Steel	yes	no	VUT-1 [247]
Low-carbon Steel	needs good chemical and vacuum cleaning	no	ESTHER [20, 300]

The mechanical design has been subjected to several evolutions corresponding to the following different versions.

Design evolution

The overall ESTHER dimensions and of its individual sections were dimensioned to account for a multitude of issues. A non-extensive and non-exhaustive list these dimensional considerations is presented in Table 4.3.

Initial version (September 2009) This was the single-stage (Combustion chamber and test section) version, submitted in the original bid proposal. This version, observed in Figure 4.1, was abandoned

4. THE EUROPEAN SHOCK TUBE FOR HIGH ENTHALPY RESEARCH: DESIGN, MANUFACTURING AND METROLOGY

Table 4.3: ESTHER dimensional considerations, problems and objectives.

Dimension	Issue	Objective	Reference
Driver length	Blast wave formation	Avoid reflection wave crossing the main shock	Equations 3.38 to 3.41
Ignition channel for the driver	May cause detonations	Ensure a smooth pressure rise	Chapter 5
Area ratio		Increasing performance	Chapter 3.1.3
Test section length	Well-formed shock	About 40 times its diameter	References [2, 258]
Test section length		Maximize test time	Chapter 3.3.1
Dump tank volume	Damage to the mechanical arms	Lowering final overpressure	Reference [301]

in favour of an upgraded version one the Preliminary Design Review (PDR) performance sensitivity calculations, predicting only a marginal performance, just exceeding the 10 km/s shock velocity threshold of the requirements.

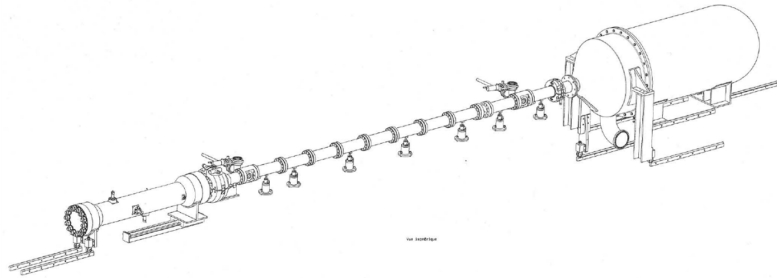


Figure 4.1: ESTHER design in September of 2009

Second version (November 2011) The version presented at Critical Design Review (CDR) included a second stage placed between the combustion chamber and the test section, discussed in Chapter 3, designed to operate at an intermediary pressure. This is a well-known technique, allowing higher shock-speeds to be reached, at the expense of a shorter test-time. Figure 4.2 shows this second ESTHER version.

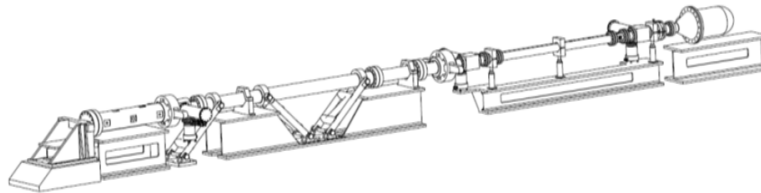


Figure 4.2: ESTHER design in November of 2011

Third version (January 2013) This third version, shown in Figure 4.3, retains the major features of the second version. The differences lying essentially in the use of shorter elements for the different tube

stages and redesign of the support stands and the design of an hydraulic closure system for connecting the different sections of the shock tube (combustion chamber/compression tube and compression tube/test section).

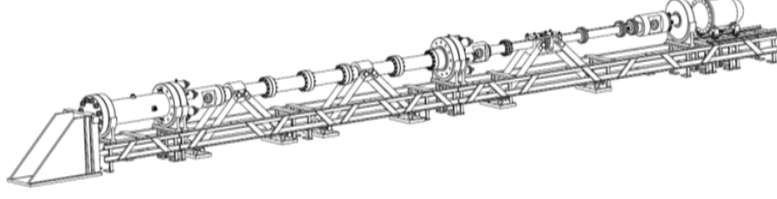


Figure 4.3: ESTHER design in January of 2013

Fourth Version (September 2013) An iteration identified a significant issue with the previous design, being that no standard sized super-duplex steel (thus at reasonable cost) could be found for the required diameters of the combustion chamber (800 mm). As such, another redesign was necessary for bringing the maximum diameters to compatible dimensions (580 mm maximum). This iteration is illustrated in Figure 4.4.

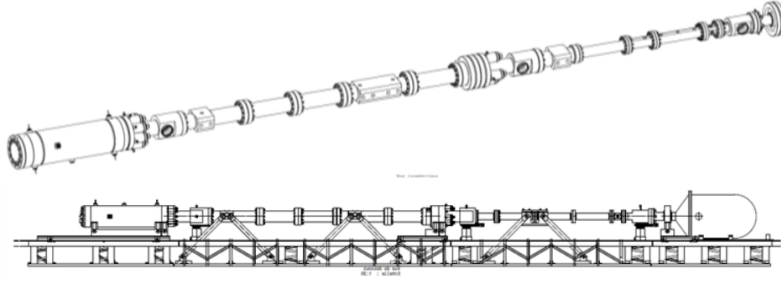


Figure 4.4: ESTHER design in September of 2013

Manufactured version (2017-2018) Following the testing of the “bombe” scaled driver, and during the manufacturing process, a certain number of additional tweaks were carried out to the design. A few of them were the consequence of further performance design iterations, others suggestions from the manufacturer for rationalization of the design, and finally the combustion chamber final design took into account the lessons from the “bombe” campaign. Figure 4.5 depicts a schematic and a photograph overview of the manufactured ESTHER facility. It can be divided into four sections, separated from each other by diaphragms. These are the combustion chamber/driver (CC), the compression tube (CT), the shock tube (including the test section) (ST), and the dump tank (DT). Its total length is 18 m and is mounted atop a concrete slab to reduce the recoil during operation. The main dimensions, length, inner and outer diameter of each section is also outline in Figure 4.5. ESTHER is a double-stage shock tube with area reduction in both its diaphragms. As discussed in Chapter 3, an additional intermediate stage between the driver and test section further accelerates the shock wave yielding higher performance. Alongside it, an area reduction also accelerates the gas through an unsteady expansion. Greater area ratios will generate faster shocks, but the performance gain will start to decrease as illustrated in Figure 4.6. The final area ratio, between the driver and test section, for ESTHER was set at 6.25 [7]. Larger

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area ratios will also mandate larger facilities that carry higher maintenance and manufacturing costs, with little performance gains. The intermediate area ratios were optimized to be around 2, as discussed in [7]. In summary, ESTHER area ratios are 2.37, 2.64, and 6.25, for Combustion Chamber-Compression Tube, Compression Tube-Shock Tube, and Combustion Chamber-Shock Tube, respectively. The different sections are connected through three diaphragm sets. These are held in place with a system of hydraulic bolts for a faster opening and closure when compared to mechanical ones. Table 4.4 summarizes the major design evolution on each review of ESTHER design.

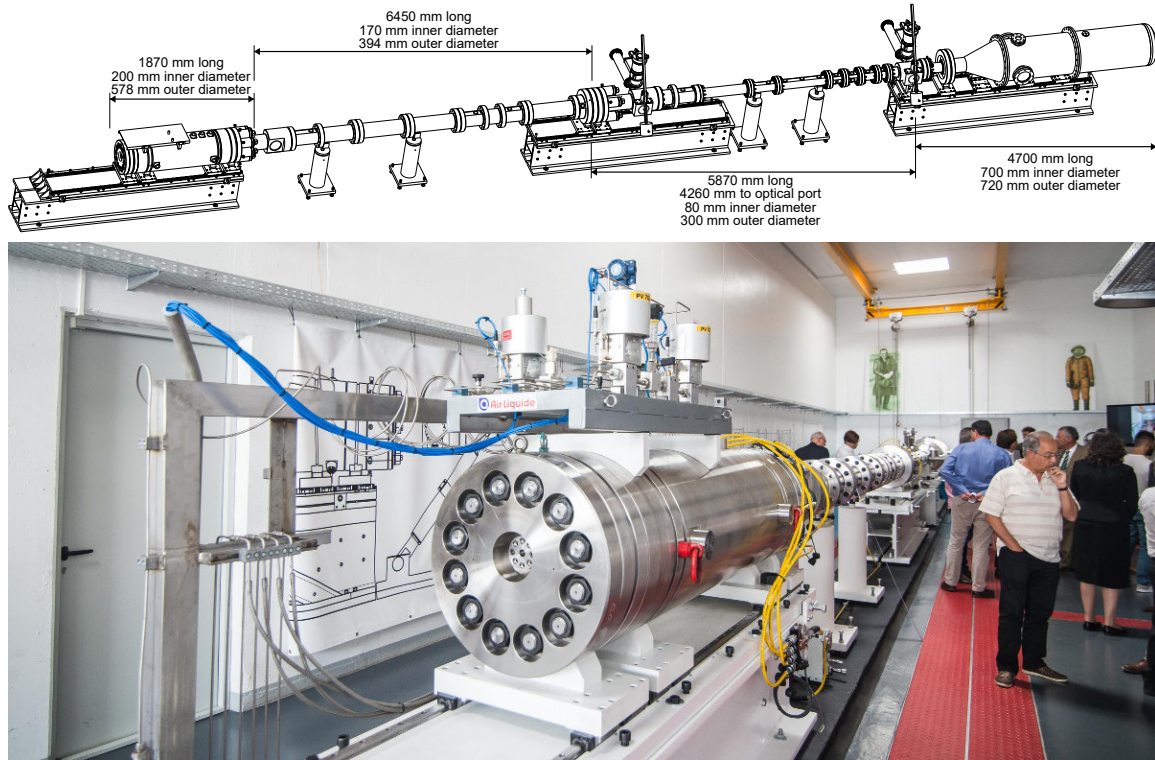


Figure 4.5: European Shock Tube for High-Enthalpy Research (ESTHER). Driver section to the left, test section on the right side. Shock wave moves from left to right.

Table 4.4: ESTHER design evolution.

Date	Design Phase	Changes
September 2009	Initial design	• Single-stage • Combustion driver
November 2011	Critical Design Review	• Added second stage
January 2013		• Redesign of support stands • Hydraulic closure system
September 2013		• Standardization of steel parts
January 2018	Manufactured	• Area reduction at diaphragms • Added laser ignition • Redesign of support stands • Extended dump tank

The fast nature of shock tube experiments requires fast diagnostics and electronics to control said diagnostics. Taking a shock wave moving at 10 km/s, or 10^7 mm/s, ESTHER test section windows have

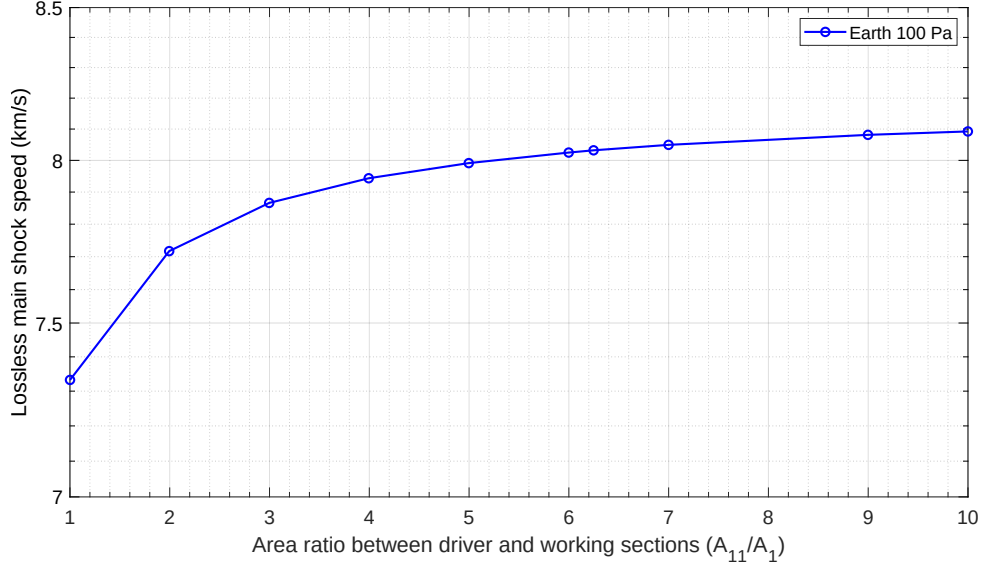


Figure 4.6: Effect of area ratio on shock speed - single diaphragm configuration, adapted from [7].

10 mm of diameter, meaning an exposition time of about 10^{-6} s, or 1 μ s. A typical CCD camera has about 1000 pixels in each direction, thus, dividing the exposure time by the resolution, yields about 1 ns per pixel. The integration time of fast CCD and streak cameras is short enough (sub-1 μ s) to work in these conditions. A detailed discussion of the chosen equipment is shown in Chapters 7 and 8.

The trigger system should be able of computing the estimated shock velocity and time of arrival at the test section to signal the streak and iCCD cameras. It uses four sensors, three before and one after the test region, connected to an FPGA (field-programmable gate array) rapid development board, an FMC (FPGA Mezzanine Card) fast ADC (analog-to-digital) converter and a signal conditioning system.

An FPGA is a semiconductor device based around a matrix of configurable logic blocks connected via programmable interconnects. A KC705 evaluation board provides a hardware environment for developing and evaluating designs targeting the Kintex-7 FPGA. The FMC-based development board, model AD-FMCJESDADC1-EBZ, is a high-speed data acquisition board with four 14-bit ADC channels at a sampling rate of up to 250 MSamples/s. A maximum of four ADC channels can be connected to the FPGA array. In reality, two sensors are sufficient to trigger the diagnostics and measure the shock speed, however, the third sensor is a cross-check to confirm the time threshold before sending it to the camera. The sensor after the test region is used to validate and correct the shock speed results, downstream of the camera. The sensors cannot be connected directly to the FMC development board, thus the need for the “signal conditioning” system. The FPGA board is installed on a PCIe slot of the Linux PC and connected by an Ethernet cable. It generates the trigger signals by digital pulse processing (DPP) and sends the trigger signal to the streak and CCD cameras via SMA connectors. An in-depth explanation of the trigger and auxiliary system was done by Luís in [7].

The ESTHER driver is a 1900 mm long, 578 mm diameter cylinder made of F55 steel. It houses the combustion chamber, a cylindrical chamber with a diameter of 200 mm and 1600 mm of internal length. This yields an internal volume of 47 l, and is filled with a He:H₂:O₂ mixture between 10 and 100 bar of filling pressure. Post-combustion pressures are on the 40–660 bar region in deflagration mode. Ignition

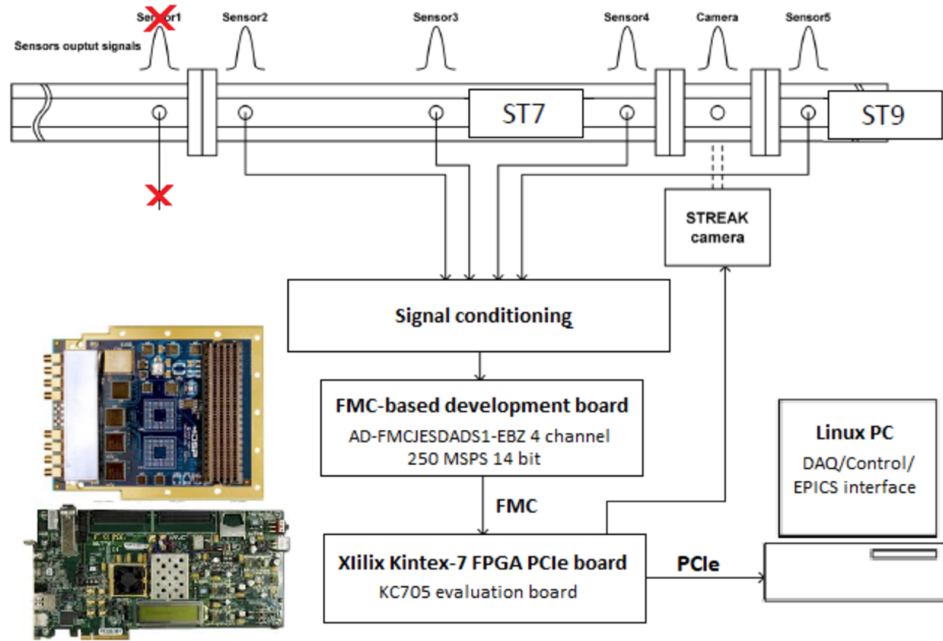


Figure 4.7: ESTHER trigger system diagram, adapted from [7].

is performed by a high-power Nd:YAG laser. The use of a combustion driver helps reduce the recoil because the expanding gas momentum is lower compared to that of a fast-moving piston. The expected driver recoil has an impulse of 2000 N·s, with a velocity of 2 m/s, a final displacement of 1–2 mm after an overshoot of 4 mm, [302]. Due to the use of hydrogen gas as fuel, ESTHER driver must be placed in a (*Atmosphere Explosive-Explosive Atmosphere*) ATEX zone. The laboratory building has a high ceiling, and near the driver where hydrogen is injected, any desired (or undesired) ignition sources are isolated from the room air. Both these requirements are to prevent an unwanted ignition from creating an explosion inside the laboratory. The gas bottles are stored in an isolated room to be compliant with ATEX requirements, and the electric pumps operating the hydraulic system are near the dump tank [303]. Appropriate gas detectors are installed to monitor any hydrogen or other gas leaks. The driver section and its working conditions are further detailed in Chapter 5.

The compression tube is a removable section of ESTHER, designed to further accelerate the shock wave if needed. It is made of 6 pieces of F51 steel, has a total length of 5 m, and outer and inner diameters of 150 mm and 130 mm, respectively [300]. The length is about 45 times the inner diameter, as this allows for the formation of a steady shock after the burst of the diaphragm [2, 258]. Three general-purpose plugs are mounted on the top of the tube, as well as a high-vacuum port on its side. The compression tube is also used to fine-tune the shock wave speed in the test section. It is filled with helium or nitrogen gas at a pressure between 20 and 1000 mbar. Chapter 5 details the expected shock speeds and the effects of the compression tube filling pressure.

The shock tube (or working) section is where the experiment takes place. It is filled with the test gas simulating the atmosphere of interest. Test gas pressure range from 10–1000 Pa (0.1–10 mbar). The shock tube block comprises 10 main sections of F51 steel, 140 mm of outer diameter and 80 mm of inner diameter. The total length is 5.4 m, with the distance between the diaphragm and the test section being

4 m. Similar to the compression tube, the distance to the diagnostic region is about 50 times the inner diameter to allow for a well-formed shock wave. A total of 16 ports and plugs are present in this section, with half of them in the test region. Three shock speed measurement stations are taken during operation, two before the test section, and one after. Four plugs are assigned to this, three before and one after the diagnostics windows. The test section is comprised of two pieces, each equipped with four ports. These are mounted in the four directions at the same longitudinal position to ensure consistent diagnostics. Each port has a cylindrical shape with 10 mm diameter and is customized to a particular diagnostic. ESTHER is set to operate with a VUV to IR emission spectroscopy setup, and a microwave interferometer [304]. Chapter 7 presents the chosen equipment for optical emission spectroscopy, the manufactured plugs, and their details. Chapter 8 discusses the newly designed compact microwave reflectometer/interferometer equipment and how to mount and operate it in ESTHER.

The ESTHER dump tank is made to collect the test, compression and driver gases post-experiment. Alongside it, the dump tank can be equipped with a hypersonic nozzle to test scale models. A set of three windows are built for this purpose. The dump tank is made of 316 steel, and has a cylindrical shape with a conic connection to the shock tube section, with 3.7 m in length and an inner diameter of 700 mm, yielding 1100 l of volume. Vacuum specifications and pumping system for the compression, shock tubes, and dump tank are discussed in Chapter 6.

4.2 Sizing of the shock tube components

Here we will briefly outline the design considerations for the sizing of the different shock tube sections. These were mostly driven by analysing the maximum operational pressures for each section, and the corresponding engineering margins. The driver was made out of a single F55 steel billet, the largest available, with 600 mm of outer diameter shaved to 578 mm. The combustion chamber length of 1600 mm was chosen to account for the reflection of the expansion wave as detailed in Chapter 3, Figure 3.1. The wall thickness was chosen to withstand transient pressure from a detonation without being damaged. The highest pressure comes from a Chapman-Jouget (CJ) detonation [305] at maximum filling pressure (100 bar). A theoretical detonation has a compression ratio of 9.3, making a 100 bar filling reach a pressure of 930 bar. The shocks reflect at the chamber wall and when merged reach double the peak pressure to 1.86 kbar. Taking a safety factor of 1.5, the maximum expected pressure is sure to be below 2.79 kbar, or 279 MPa, which is under the yielding strength of F55 steel at 550 MPa. Multiple reflected local detonations can reach pressure peaks above the 2.79 kbar previously described leading to surface hardening. Nonetheless, this is not problematic because it is a transient effect and does not permanent damage the bulk of the combustion chamber. Structural analysis of ESTHER's combustion chamber was done by Otter in [306].

Shock pressures in the compression tube will not exceed 35 bar, or 70 bar for a reflected case. If some unburned driver gas detonates inside the compression tube the shock at 35 bar, and the interacting reflected shocks can reach a peak of 630 bar. Using the same 1.5 safety margin, the maximum pressure is set at 945 bar (94.5 MPa), below the F51 steel yielding strength of 448 MPa. Lastly, the pressure

transient behind the shock in the test section has a maximum pressure of 10 bar, thus a reflected shock maximum pressure of 20 bar. However, when working in reflected mode, the pressure inside the shock tube settles to the average pressure over the total volume, which is about 400 bar. Taking the safety factor of 1.5, we get a maximum load of 600 bar. In the case of a detonation due to unburned driver gas, the maximum achievable pressure is 360 bar, or 540 bar when factoring in the safety margin. Once more, F51 steel is capable of working in both the static and transient pressure cases [306].

4.3 Manufacturing

The high level user requirement are connected to the overall major design choices and material selection as previously described, but also to the system mechanical tolerances and manufacturing process. The major design choices were already addressed, as was the material selection which accounted for the expected pressures inside the vessel. All of these points are linked by the manufacturing process and the associated dimensional and geometric tolerances of each piece/assembly. Tight tolerances were put in place to ensure good flow quality and reduced losses on any undesired curvature of the interior surface. An example of this care are the plugs over the total length of the facility. These were manufactured in simultaneous with the respective section pieces to guarantee a smooth surface without seam lines, minimizing the surface drag. Of particular interest is ESTHER's design target of having clean/low contaminants experimental data, hence surface treatment and roughness is a key factors of particular interest is to achieve very good surface finish properties in the shock tube test section. Indeed, cleanliness of the future tests (for example limiting pollution from carbon or other particles) will depend on achieving a surface with very small desorption. This is even more critical as steel was selected instead of aluminium for the working sections. Lower surface roughness reduces the accretion of molecules to the walls of the shock tube. It also reduces the outgassing pressure when vacuuming the shock tube, leading to an overall lower pressure and lower contamination of the test gas with atmospheric air and water (from the combustion process), detailed in Chapter 6. Alongside this, sealing cavities and faces, such as O-ring cavities, need good surface treatment to achieve a good sealing, improving the aforementioned vacuum level.

The manufacturing process for ESTHER was mostly done using manual drilling and cutting instead of Computer Numerical Control (CNC) machining. There is a borderline “hyping” of CNC methods as “solve-all” cutting-edge technology. As with many other things in life, the reality is often much more complex and nuanced than one may initially assume. In our specific case, the uncommon characteristic of the selected steel (high-tensile duplex and super-duplex low carbon) mandate the utmost care in manufacturing operations. These steels have higher tensile strengths and are harder to machine than conventional ones. Parameters for turning such as cutting tool pitch, cutting speed and feed rate, are not immediately obvious and require some trial and error by a skilled operator. An operator is capable of noticing anomalous phenomena in the steel/machine such as an increased local vibration of the tools which ultimately may lead to the cutting tool breakage. The operator can disengage the cutting tool before any permanent damage. No machine is currently capable of feeling these small pre-failure vibrations and

replicating this behaviour.

Larger pieces were bored instead of turning due to their weight, the combustion chamber being the best example. The steel block was held in place statically, while the cutting tool rotated in a custom support. If the steel block was turning it would oscillate and vibrate with a large amplitude. This behaviour may lead to an amplification of the cutting depth, which can machine the parts below unacceptable tolerance levels. The boring process of the combustion chamber piece is photographed in Figure 4.8.

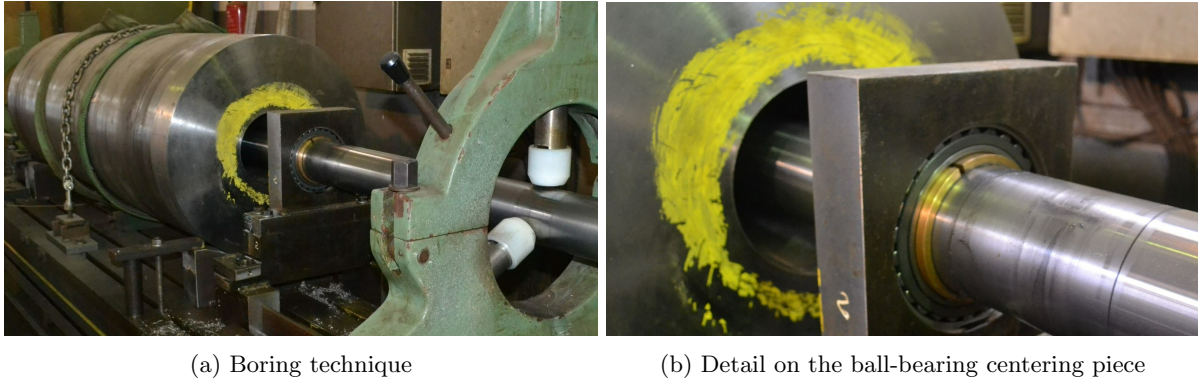


Figure 4.8: ESTHER combustion chamber manufacturing.

A special TT9225 grade tungsten carbide cutting tool was utilized, presented in Figure 4.9. The recommended manufacturer advance and rotation parameters were selected, and a small cutting depth of 0.2 mm was chosen as per recommendations. A coarse cut tool was initially used, then switched to another better suited for finishing the surface at the end of machining. A test was done comparing machining with cooling through sloshing of the tool in a coolant bath, halfway through the diameter of the cylinder, and without cooling. Results showed better tool resistance to wear without cooling. This can be explained by the fact that no thermal cycles occur if there is no cooling, whereas if coolant is present, the tool will be cooled during half a revolution, and then heated up, leading to thermal oscillations that shorten the tool lifetime.



Figure 4.9: Tungsten carbide cutting tool.

The initial results showed a strong degradation of the surface quality as we progress towards the combustion chamber end. This is to be expected, owing to the great length to be machined in one pass, as the cutting tool can either break or be blunted, in which case strain hardening occurs. Machining on one pass can be achieved, and the surface state at one end seems acceptable (estimated arithmetical mean

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roughness level around $1\text{ }\mu\text{m}$, explained in greater detail in a upcoming section) but becomes unacceptable on the other end. This can even be attested in the pictures of the combustion chamber billet, Figure 4.10. Polishing was needed to achieve the desired tolerances, see section 4.5.2.

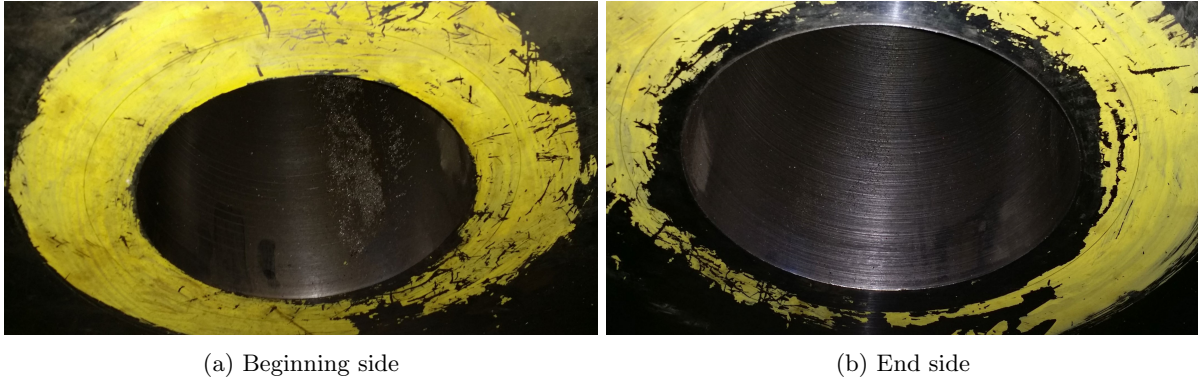


Figure 4.10: Surface finishing of the combustion chamber. Surface roughness difference is apparent on the photographs.

Plugs seal cavities have been manufactured using a reference seal cavity. A reference cavity is carefully manufactured to the required specifications and tolerances, then placed inside the lathe for calibration, and finally is replaced by the plug whose cavity is to be manufactured. Figure 4.11 exhibits photographs of the manufacturing process of the reference seal cavity and of a plug seal cavity.

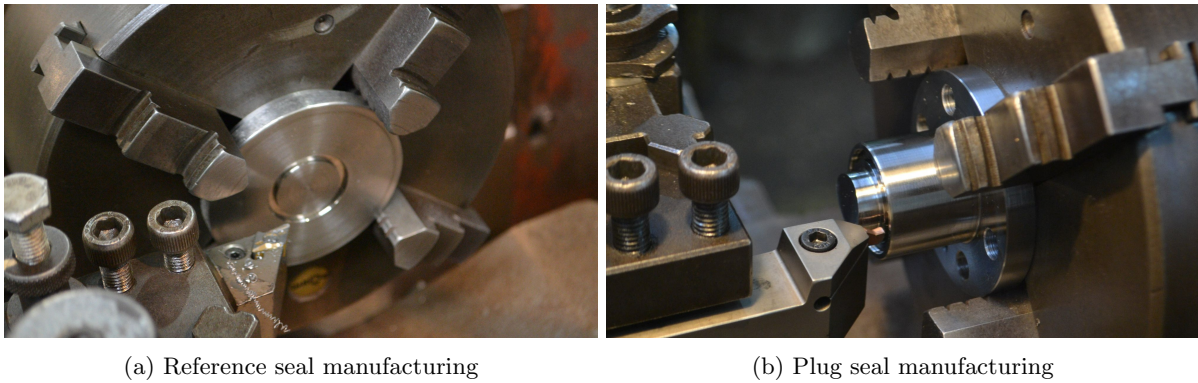


Figure 4.11: Reference seal cavity manufacturing and usage.

Metrology for both dimensional and geometric tolerances (e.g. co-linearity, co-planarity, co-centricity) was done by a dedicated company (S4METRO) by a laser Coordinate Measuring Machine (CMM) model DEA Brown & Sharpe Scirocco 20.13.10, with a measuring range of 2000 mm, 1300 mm and 1000 mm in x, y, z -directions, respectively. The machined pieces had a maturation period of 24 hours in a controlled temperature room at $20 \pm 1^\circ\text{C}$. Due to it, no thermal compensation was needed. The surface state was also measured and is discussed further in section 4.5.

4.4 Diaphragms and hydraulics

ESTHER uses three sets of diaphragms, each separating one main section from the next one. These connections have a conic shape to account for the area reduction in each stage. The main diaphragm

disks, separating the driver and compression tube, are made of stainless steel with varying thicknesses designed to rupture at a pre-determined pressure, about 90-95% of the maximum driver pressure. The other two diaphragm disks are thin metallic sheets, either tin or aluminium, set to burst nearly instantly when hit by the shock wave.

The main diaphragm is scored with a cross mark to help it open in a controlled, petal-shaped manner. Larger rupture times lead to longer distances for shock establishment [307]. The equation of motion for the diaphragm rupture is,

$$J \frac{d^2 \theta}{dt^2} = M_p - M_h \quad , \quad (4.1)$$

where J is the diaphragm moment of inertia, θ the opening angle, and M_p and M_h the pressure and bending torques, respectively. Integrating Equation 4.1 from $\theta = 0$ to $\theta = 90^\circ$, for a 3 mm thick diaphragm holds an opening time between 243 and 268 μs , depending on the steel.

Table 4.5 shows the chosen main diaphragm thickness, and cross mark depth. All cross marks have 200 mm in length and are 60° in angle. Figure 4.12 shows the Computer Aided Design (CAD) drawing a primary diaphragm for post-combustion pressure of 600 bar. The diaphragm material was selected to be 316 stainless steel and the cross was to be scored at a 45° angle to the laminating direction of the material sheet. This avoids accidentally having a preferred direction for shear stress, easing the opening process and ensuring that the four petals open at the same time [307].

Table 4.5: Main diaphragm parameters. All of them are made of 316 stainless steel, with a 200 mm long cross mark at a 60° angle.

Peak p (bar)	Rupture p (bar)	Thickness (mm)	Mark depth (mm)	Reference
600	580	3	1	[307]
250	238	1.5	0.3	[308]
100	94.5	0.5	0.15-0.1	[308]*

* extrapolated from

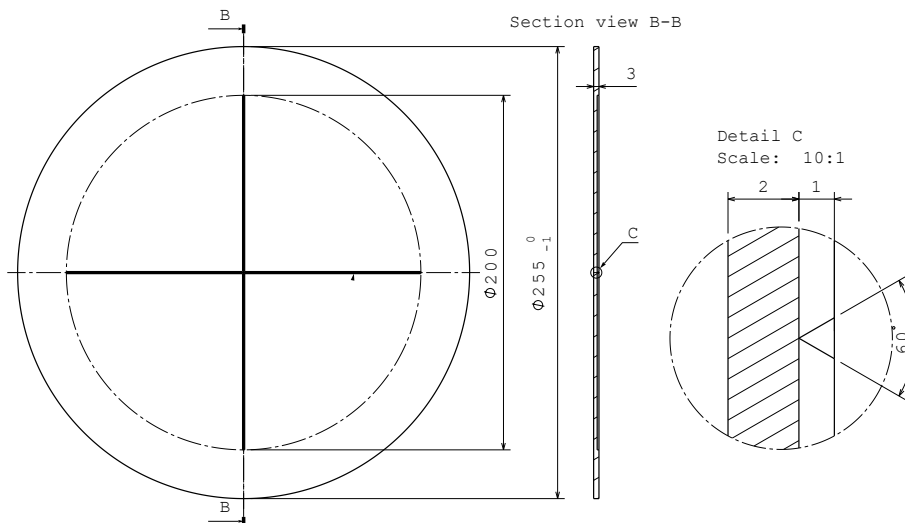


Figure 4.12: CAD main diaphragm example

4.4.1 Hydraulics

As previously stated, a set of hydraulic bolts compresses and connects the different ESTHER sections. One electric pump pressurizes the hydraulic oils in the piping system. Figure 4.13 depicts the piping and instrumentation diagram (PID) of the hydraulic system for ESTHER. Three-way piping connects the electric pump to the ST-DT, CT-ST, and CC-CT diaphragm connections. The CC-CT connection is further divided into two lines, each one linked to four hydraulic cylinders. The CT-ST diaphragm is held in place by four cylinders, while the ST-DT is held by six cylinders. Clamping forces will vary depending on the connection, greatest for the CC-CT, and lowest for the ST-DT.

The held force needed for the CC-CT diaphragm assembly not to open was computed as the maximum pressure, 1.45 kbar, acting in a 200 mm diameter area. It yields a force of 4.55×10^6 N, or 464 tonnes. Each hydraulic cylinder (model RCH603) is capable of exerting a force of 60 tonnes, totalling 480 tonnes. If a transient detonation wave pressure peak passes the 480 tonnes of hydraulic force the diaphragm assembly should not open. This is because of the short duration of this pressure force, that when integrated yields a relatively small force impulse. Thus, the system can be validated in a quasi-static regime of 1.45 kbar of pressure. The pipeline split is done to prevent an unwanted opening in the case of a hydraulic leak in one of the lines.

The 700 bar of pressure in the compression tube acting on a 130 mm diameter disk in the CT, equates to a force of 9.29×10^5 N, or 94.8 tonnes. A total force of 240 tonnes is possible by the four RCH603 hydraulic actuators. Lastly, the same 700 bar of pressure acting on the ST 80 mm diameter disk yield a force of 3.52×10^5 N, or 35.9 tonnes. Six hydraulic cylinders capable of 13 (model RCH120) tonnes each, totalling 78 tonnes, are used for closing the diaphragm connection.

The hydraulic pump, piping and valves are rated for hydraulic pressure of 700 bar. All the valves are manually operated, open during the pressurized phase, and then closed off. This is done so the hydraulic pump can be turned off during shock tube operation. After the shot, the system is again pressurized before opening the valves to avoid a hydraulic fluid wave travelling upstream to the pump. The hydraulic system is expected to run at 80% of the maximum pressure, around 560 bar.

Proof testing

The different sections of the shock tube were proof tested using a LARZEP HAP1900 water pump and a pressure holding time of one hour. A maximum pressure of 1525 bar is achieved before leakage occurs in the combustion chamber. The compression and shock tubes were proof-tested at 100 bar (considering the maximum pressure of 70 bar in these sections plus $1.5 \times$ safety margin). Finally, the dump tank was proof-tested to 12 bar by the manufacturer. Figure 4.14 shows photographs of the water pump and test.

4.5 Dimensional report and Metrology

The two most common parameters used for quantify a surface roughness level are the arithmetical mean roughness (R_a) and the maximum height (R_y) [309], defined in Equation 4.2.

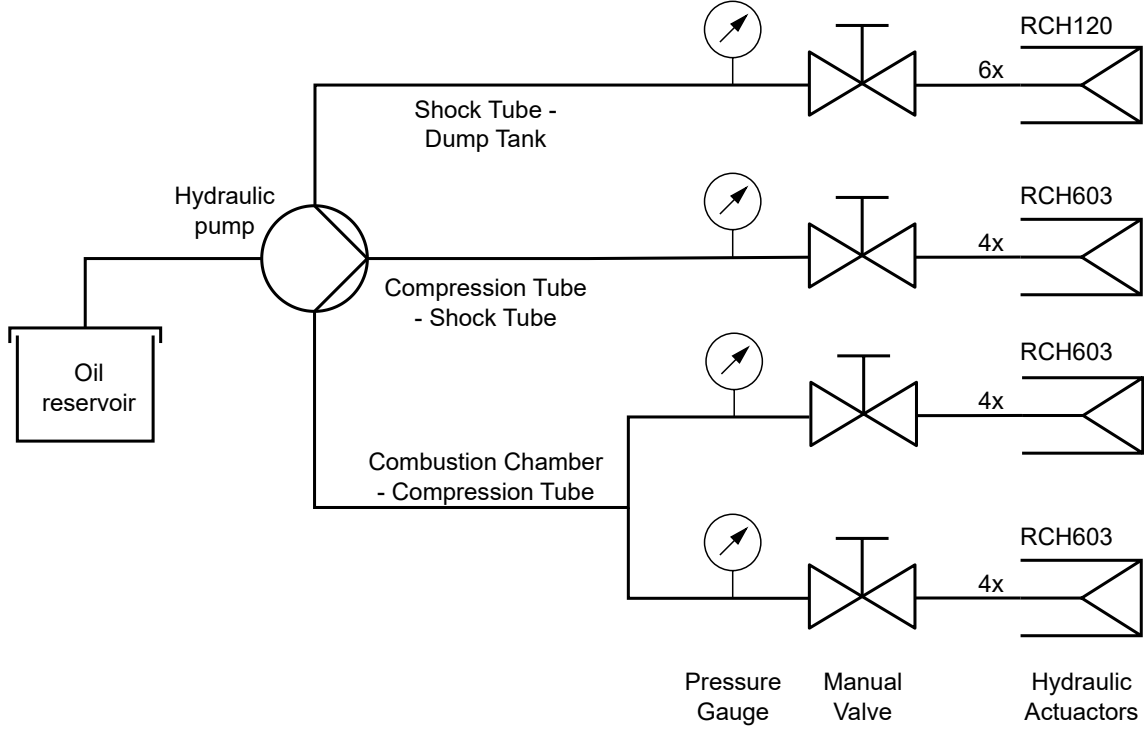


Figure 4.13: PID of ESTHER hydraulic actuator system

$$R_a = \frac{1}{l} \int_0^l |y| dx \quad , \quad (4.2a)$$

$$R_y = R_p + R_v \quad , \quad (4.2b)$$

where l is the length of the profile, y is the deviation from the mean profile height and, R_p and R_v are the height of the highest peak and valley, respectively. An illustration of this is shown in Figure 4.15. Different machining and finishing techniques are capable of achieving different roughness levels, ESTHER surface was set to be finished by appropriate polishing [309]. An overall comparison of the different machining techniques and achievable roughness level is shown in Figure D.1, in appendix D.

4.5.1 Test sample demonstration

A F53 steel test rod was machined with one half, simply machined in a lathe, and the other half also polished afterwards. A scanning electron microscope (SEM), model Hitachi S2400, equipped with electron backscattering (EBS) captured pictures at different scales of the steel test rod for a better understanding of the surface morphology [309]. The images were taken at a 60° angle to allow the evaluation of the surface profile, not only in terms of the size of the peaks and valleys but also in terms of geometry. Figure 4.16 compares the profile of the steel test rod along the axial direction. The R_a was computed at 0.8 and 0.2 μm for the machined and the polished sides, respectively.

Figure 4.17, a significant difference is noticeable between valleys and peaks in the different zones



Figure 4.14: Proof-testing with the water pump. Water filling of the combustion chamber and view of the connection to the water pressure pump.

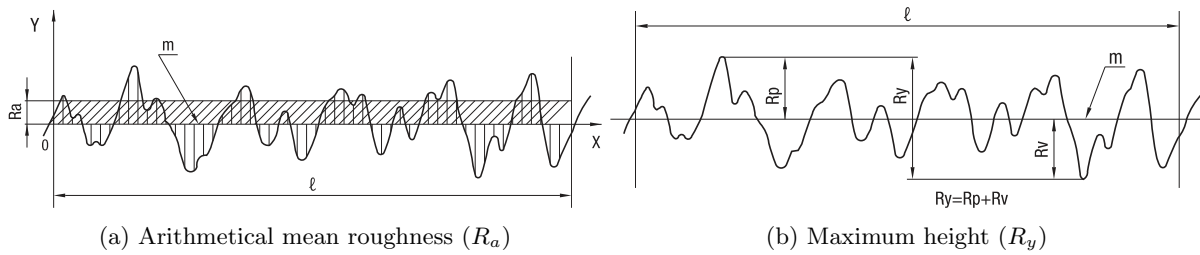


Figure 4.15: Illustration of roughness parameters, arithmetical mean roughness (R_a) and maximum height (R_y).

(polished and unpolished), which reinforces the results obtained from the analysis of roughness profiles and optical microscope images, of Figure 4.16. The SEM images of the unpolished area allowed evaluation of the irregularities in maximum peaks along its circumferential length (perimeter) as well as the observed small valleys, shown in the roughness profiles. It should be noted that the profiles shown in Figure 4.16 are not the same as the images of Figure 4.17.

The polishing allowed decreasing the peaks' height as well as the surface irregularities. These cannot be seen in the roughness profile, which just shows a section parallel to the axis of symmetry of the cylinder. Instead, the SEM pictures allow concluding that the machined surface morphology is very irregular and that the polishing operation decreases the roughness and smooth's the surface morphology. The polishing operation is a good option to standardize the surface morphology of the machined parts.

SEM techniques allow probing the surface material properties of the machined and polished samples through electron back-scattering (EBS). The EBS profiles of both samples are presented in Figure 4.18. The results show that no modification of the surface composition is apparent, and specifically, no carbon and/or silica seems to be deposited on the surface (i.e. no contamination from the polishing material is

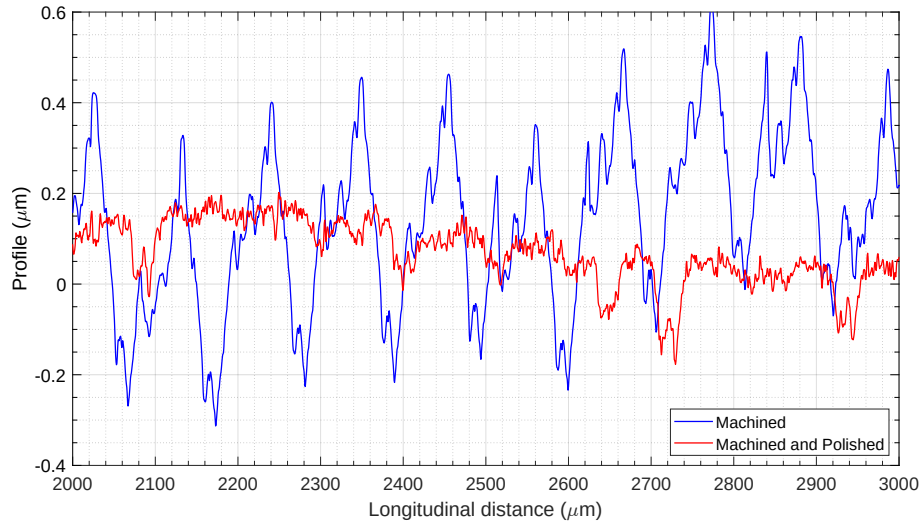


Figure 4.16: Surface morphology by roughness meter, adapted from [309].

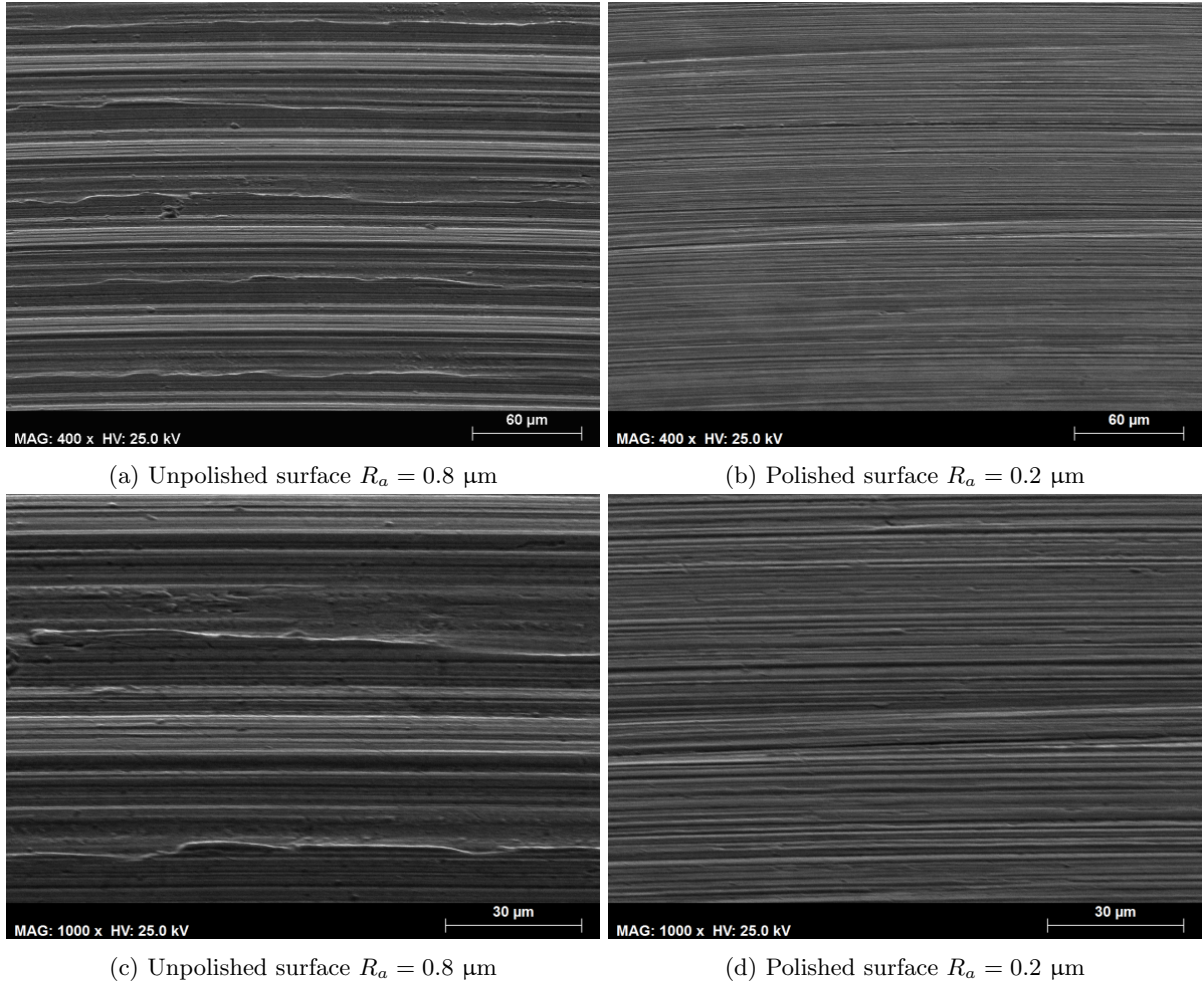


Figure 4.17: Test probe scanning electron images.

apparent).

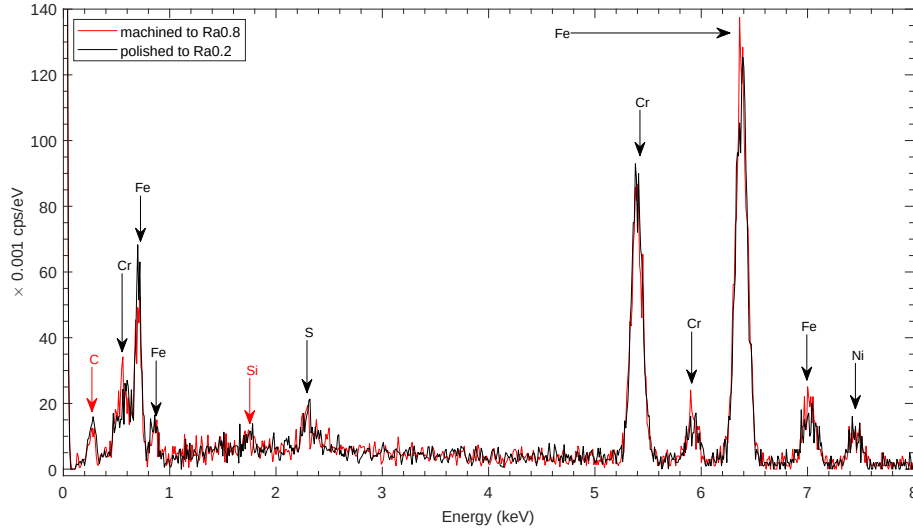


Figure 4.18: Chemical compounds of both machined and polished samples through electron back-scattering, adapted from [309].

4.5.2 ESTHER surface polishing

The surface requirements for the ESTHER internal tube surfaces are $R_a < 0.5 \mu\text{m}$ for the shock tube section, and $R_a < 0.8 \mu\text{m}$ for the other sections [310]. Cavities for O-rings are also required to have $R_a < 0.8 \mu\text{m}$. The interior of the tubes was turned to a relatively fine surface state (around $R_a=1.0 \mu\text{m}$), and the tubes were then assembled in a static position, with the insertion of a FLEX-HONE alumina brush tool, seen in Figure 4.19. Polishing of the seal cavities and other small surfaces was carried out using standard alumina (Al_2O_3) polishing sheets of adequate grit [310]. Silicon carbide (SiC) sheets were considered a non-viable polishing option, because of possible contamination by both silica and carbon atoms.



Figure 4.19: *FLEX-HONE* polishing brush.

The surface state was monitored using the Mitutoyo SurfTest SJ-210 surface roughness tester. The device presents the average surface rugosity, as well as detailed information on the surface topology, seen in Figure 4.20. Surface testing of the shock tube parts was carried along the axis, at 20 mm intervals. The procedure is shown in Figure 4.21.

For the small parts such as seal cavities, direct measurements using the roughness tester are not

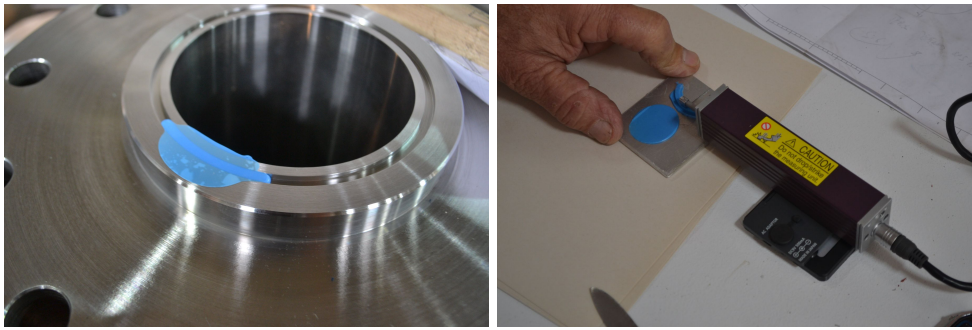


Figure 4.20: Sample surface roughness test using Mitutoyo SurfTest SJ-210.



Figure 4.21: Sample metrology operations of the ST8 interior.

possible. Instead, a polymer paste, brand name PLASTIFORM was used for indirect measurements of the surface state. A first verification was carried out with a roughness sample, showing that the roughness sample ($R_a = 0.2 \mu\text{m}$) was correctly reproduced. Figure 4.22 shows the application of the polymer paste, followed by a measurement of the surface roughness of the imprinted side.



(a) Paste application in the O-ring cavity (b) Indirect R_a measurement of a seal cavity

Figure 4.22: Indirect roughness measurements, images from the ST8 section.

Measurements were taken over a 20 mm step, and then smoothed and an average value for the roughness determined. All parts were compliant with the specifications ($R_a < 0.8 \mu\text{m}$ in the CC and CT sections, and $R_a < 0.5 \mu\text{m}$ in the ST sections). All of the seal cavities and small surface parts were found to also be compliant with the specification ($R_a < 0.8 \mu\text{m}$), except for two plugs, one in the CC2 and another in the ST8-bis sections. The first plug could be accepted since it belongs to the combustion

chamber which already underwent successful test trials, and the latter could be hand polished in future if needed. Figure 4.23 depicts an example of the collected data, and a comparison of the surface state before and after a re-polish of the surface.

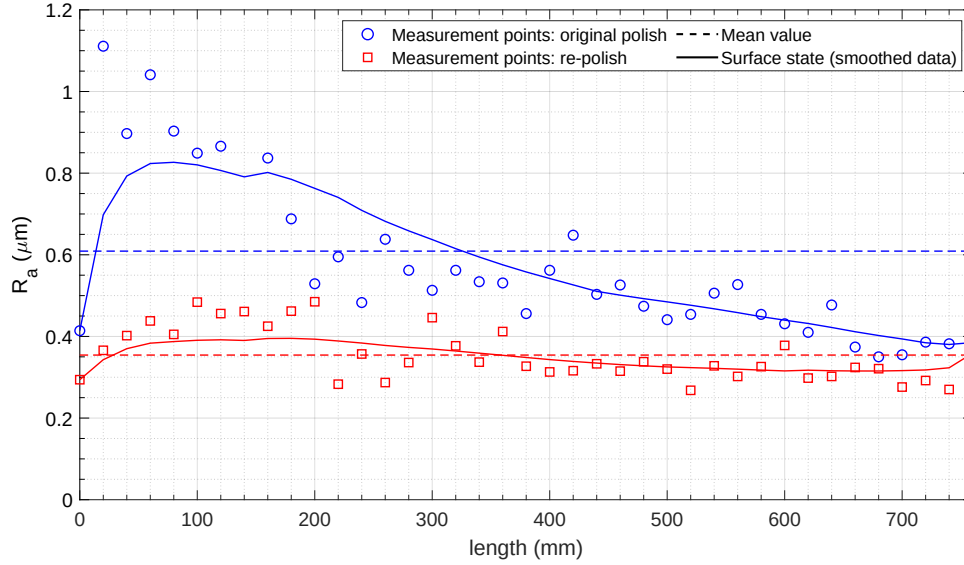


Figure 4.23: Surface state metrology comparison between original polish (blue) and re-polished (red) of the ST2 section.).

4.6 Shock Wave Detection diagnostics

As previously stated, to trigger the main diagnostics at the right time, a fast circuit is used to compute the shock velocity, and then send the trigger signal to the streak and CCD cameras. The fast calculator circuit uses an electric signal provided by either a photodetector or a piezo-electric gauge. Due to the short time window, the detector needs only a qualitative measurement, capable of identifying the shock, ideally a Heaviside function. A photodetector identifies the shock through the light emitted by the shocked gas, while the piezo-electric through the pressure rise in the shock front. The compression tube section has four ports, and the shock tube has twelve ports. These ports allow for one shock speed measurement in the compression tube, and four in the shock tube. Speed measurements of the primary shock in the compression tube are not mandatory. Plugs in the compression tube can be equipped with one of two piezo-electric sensors, either a Kistler 6215 or a Kistler 603C. Meanwhile, shock tube plugs were machined and accessories manufactured so the same plug could fit the optical emission detector, via an optical fibre, or a piezo-electric gauge (Kistler 603C). The optical plug design is adapted from an original design from the X2 facility, kindly shared by Christopher James [183].

Compression Tube Figure 4.24 shows the Computer Aided Design (CAD) drawing of a compression tube plug manufactured to mount the larger Kistler 6215 gauge, identical to the one mounted in the combustion chamber. An adapter was manufactured that mounts on the same thread as the Kistler 6215, with an internal machined hole capable of mounting the smaller Kistler 603C sensor.

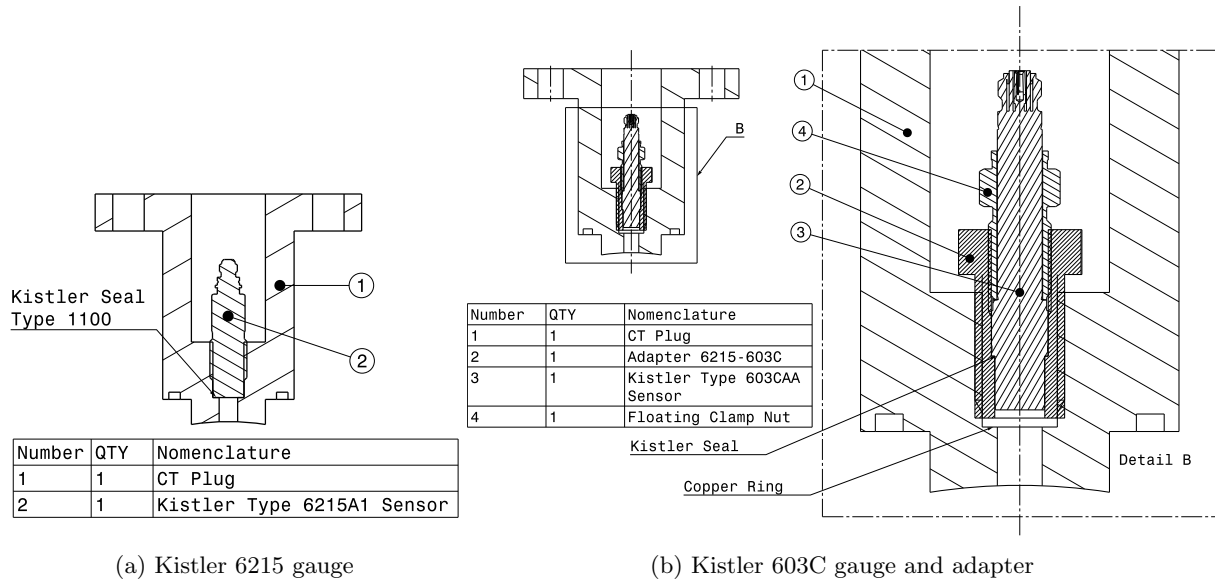


Figure 4.24: CAD drawing of ESTHER shock wave detection plug and different gauge options for the compression tube section.

Shock Tube The shock tube plugs are machined to hold a Kistler 603C sensor, two additional parts were designed to mount an optical window and attach an optical fibre patch cable, as illustrated in Figure 4.25. The optical fibre connects with a SMA (SubMiniature version A) connector to the part that holds the window in place. This system was based on the shock detection system of X2 expansion tube [183]. A threaded part will tighten the connection in place and also deform the copper rings to vacuum seal the system. A comparison of the advantages and drawbacks of the photodetector and piezo-electric techniques is detailed in Table 4.6. Currently, a plug in ST7 part of the shock tube section is already manufactured, and mounted to work in photodetector mode.

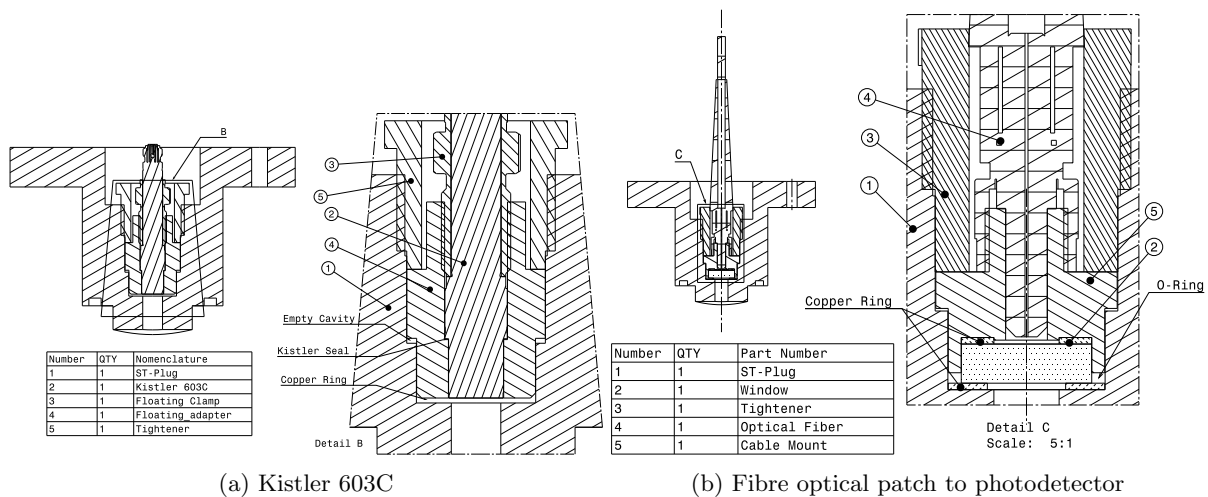


Figure 4.25: CAD drawing of ESTHER shock wave detection plug and different options, piezo-electric or photodetector, for the shock tube section.

Table 4.6: Comparison of selected shock wave detection techniques in ESTHER.

	Requirements	Advantages	Disadvantages
Photodetector	• Optical Fibre • Photodetector • Optical window	• Price • Cleaner signal	• Window may break • Vacuum sealing • Low signal at very fast or slow shocks
Piezo-electric	• Piezo sensor • Amplifier	• Simpler mounting • Easier vacuum sealing	• Price • Cavity ringing can mask the signal

Chapter 5

ESTHER Combustion Chamber Driver

“NO! Power in Firebending comes from the breath. Not the muscles.

The breath becomes energy in the body.”

- Uncle Iroh, Avatar: The Last Airbender

This Chapter addresses the qualification of the ESTHER driver. As previously discussed, ESTHER uses a high-pressure combustion chamber as its driver. A thorough campaign was executed to validate the full-scale driver, previously tested in the small-scale “bombe” [21]. The study evaluated the effects of driver gas pressure, chemical composition and laser ignition focus on the overall compression ratio, flame velocity and combustion mode (deflagration vs. detonation). The operational conditions for ESTHER are summarized. It concludes with an update on ESTHER’s performance map and how to maximize its operational range is also discussed.

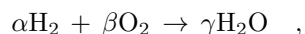
5.1 Introduction

Combustion processes require the use of an oxidizer reactant to react with a fuel. Typically, the oxidizer is oxygen O_2 . The chemical composition of a pre-mixture reaction can be defined by two parameters, the air:fuel equivalence ratio λ , and the dilution $\mathcal{X}_A$, where A is the chemical diluent. Air:fuel ratio is defined as the ratio between the number of molecules n_{air} of air (or oxidizer in a more general way) to fuel n_{fuel} , normalized to the stoichiometric coefficients of the reaction. Another quantity maybe be alternatively used, the fuel:air equivalence ratio $\phi = \lambda^{-1}$. The dilution factor is the fraction of inert molecules in the mixture. These parameters are mathematically defined in Equation 5.1.

$$\lambda = \frac{n_{air}}{n_{fuel}} \bigg/ \left(\frac{n_{air}}{n_{fuel}} \right)_{stoi} , \quad (5.1a)$$

$$\mathcal{X}_{iner} = \frac{n_{iner}}{\sum_{j=1}^k n_j} . \quad (5.1b)$$

A stoichiometric combustion reaction is defined by having the exact amount of both reagents, fuel and oxidizer, such that at the end of the reaction there is neither any fuel nor oxidizer remaining. Taking hydrogen and oxygen as fuel and oxidizer, respectively, we get the chemical reaction,



where α, β, γ are the stoichiometric coefficients. To balance this reaction the atom species, in the reactants (left-hand side) and products (right-hand side) must be equal. This yields α, β, γ equal to 2, 1, 2, respectively. If the mixture has excess air ($\lambda > 1$) it is deemed “lean”, the opposite is called “rich”. Lastly, an ignition source (spark plug, laser beam, heat source) is used to overcome the chemical potential of the reactive species and initiate the reaction mechanisms.

When considering the flame propagation, two different combustion modes have to be taken into account. The first mode is the traditional deflagration mode where combustion proceeds subsonically through heat and mass diffusion, with a smooth increase in pressure until all the reactants have been burned. The second mode is the detonation mode where combustion proceeds supersonically with a very steep and intense rise in pressure (shock wave) which ignites the mixture behind the pressure wave, leading to a much higher peak pressure than the deflagration regime. The conditions for the combustion modes depend on the geometry of the combustion vessel, ignition source, initial pressure, homogeneity, air:fuel equivalence ratio and dilution of the pre-mixture. Higher pressures and small dilutions tend to originate detonations since there are no inert species to absorb the excess energy. The air:fuel equivalence ratio dependence is not monotonous as it depends on the thermal diffusivity of the mixture and different fuels have different diffusivities.

Hydrogen usage as an alternative fuel to conventional hydrocarbon fuels is increasing owing to its cleaner combustion. Hydrogen combustion does not create carbon dioxide or soot, with its only pollutants being nitrogen oxides NO_x , when air is used as oxidizer. The use of lean mixtures [311, 312] whose combustion temperatures are lower helps further reduce nitrogen oxides production. Using hydrogen as fuel in combustion shock tube drivers such as ESTHER has two major advantages over typical hydrocarbons, the lighter molar mass and no soot formation. Drivers with lighter molar masses can achieve higher shock wave velocities, increasing the overall performance. Soot deposition along the walls causes pollution on emission spectroscopy signal with carbon lines, which are known to be strongly emissive at high post-shock temperatures. These advantages make hydrogen a natural solution for a combustion shock tube driver.

As aforementioned, a combustion-driven shock tube can work in two different combustion modes. The first one is the subsonic deflagration mode where combustion proceeds subsonically through heat and mass diffusion, with a smooth increase in pressure up until all the reactants have been consumed. The second one is the detonation regime where combustion proceeds supersonically, with a very steep and intense rise in pressure that ignites the mixture ahead of the pressure wave, leading to higher pressure peaks than the deflagration regime. The conditions for each of the combustion regimes depend on the combustion vessel

geometry, ignition source, and the mixture’s initial pressure, homogeneity, air:fuel equivalence ratio and dilution factor. Despite the higher driver pressure advantage, the inherent instability of the detonation regime yields a lower shock repeatability when compared to the deflagration regime. Besides this, the detonation regime increases the stress levels on the combustion chamber vessel.

Detonations and deflagrations with strong acoustic oscillations are to be avoided to achieve good repeatability between experimental shots. The desired pressure rise should be as smooth as possible to also prevent diaphragm rupture ahead of time. As stated before, a small-scale “bombe” was first used for testing the design and chemical mixture it yields an optimized operational mixture of He:H₂:O₂ 8:2:1.2 [21]. The objective of the campaign was to map combustion initial pressure and gas composition conditions which maximize peak pressure in deflagration mode with low acoustic oscillations.

5.2 State-of-the-Art

Maximum compression ratio is usually found near stoichiometric conditions, where the air:fuel (or oxidizer:fuel) equivalence ratio is $\lambda \sim 1$ ¹. Tang et al. [313] performed experiments measuring compression ratio and velocity for hydrogen-air-nitrogen mixtures. Between 0.1 and 0.5 MPa, peak compression ratio was achieved at $\lambda=1$ ($\phi = 1$). However, the fastest flames occurred in rich (excess fuel) mixtures at $\lambda=0.71$ ($\phi = 1.4$). Due to the hydrogen low molecular weight and high thermal diffusivity, hydrogen-rich mixtures will burn faster than stoichiometric ones [314]. Similar conclusions were reported in [315, 316].

Stoichiometric mixtures are also easier to ignite as shown in [317–319]. Ronney [317] assessed the minimum ignition energy through a thermal hypothesis. The author expressed the minimum ignition energy E_f as a function of the laminar flame speed S_L and flame temperature. In the stoichiometric region, flame speeds are higher which minimizes the minimum ignition energy $E_f \sim S_L^{-3}$. This region is also more prone to detonation as shown by Lee in [320].

Reaction rates of a chemical mixture in a closed vessel are influenced by the mixture filling pressure, as well as its flammability and detonation limits [321]. Neither flame temperature nor compression ratio seem to significantly vary with increased pressure [313]. The small increase in compression may be explained by the faster reaction rates of chain-branching reactions in the overall $2\text{H}_2 + \text{O}_2 \rightarrow 2\text{H}_2\text{O}$ reaction. Shorter combustion times help reduce the wall heat losses, yielding a higher peak temperature/pressure.

Mixtures with lower dilutions have faster flame propagations when their filling pressure rises to 15–20 bar [315, 316, 322–326]. This trend is inverted with an inert gas, like water vapour or a noble gas, at high dilution factors ($\gtrsim 50\%$). Kuznetsov et al. [324] showed that the initial filling pressure has a non-monotonic influence on laminar flame velocity for hydrogen-oxygen-steam mixtures. The authors claim that this trend is due to the kinetics path changing from a chain-branching to a straight-chain one. A similar type of experiment was done in [323], whose results were explained with the same argument.

High-energy pulsed lasers are a successful ignition method for high-pressure mixtures, where as spark plugs mandate very high voltages to work at high pressures. The advantages of laser ignition were reviewed by Ronney [317]. Laser ignition of high-pressure (up to 40 bar) mixtures has been carried for

¹ $\phi = \lambda^{-1}$, ϕ is the fuel:air equivalence ratio

hydrogen-air [327–331], hydrogen-oxygen [332] and methane-air [322, 333–340].

For the majority of practical applications, laser ignition is achieved via laser-induced spark ignition. A high-power laser beam is focused inside the combustion chamber to ignite the gas. The gas is ionized near the focal point of the laser, releasing free electrons in an electron cascade, forming a reactive plasma, which then starts chain-branching reactions, igniting the mixture. The beam’s irradiance I (power per unit area) at the focus is the key parameter controlling the ignition success. If the laser’s irradiance is sufficient to create a gas electrical breakdown, ignition will then follow. The beam’s irradiance can be expressed as,

$$I = \frac{E_{pulse}}{\Delta t_{pulse}} \frac{1}{\pi w^2} \quad , \quad (5.2)$$

where E_{pulse} is the pulse energy, Δt_{pulse} the time duration, defined as the FWHM (Full Width at Half Maximum of a Gaussian pulse), w the beam radius. The beam waist diameter $2w$ of a non-Gaussian beam at the lens focal distance can be calculated by,

$$2w_2 = \frac{4M_{00}^2 \lambda_0 f}{\pi 2w_1} \quad . \quad (5.3)$$

where λ_0 , $2w_1$, $2w_2$, f are the beam wavelength, diameter at the lens, waist at the lens focus, and the lens focal distance, respectively. The factor M_{00} is to correct for a non-gaussian beam, for a Gaussian beam $M_{00} = 1$.

Ronney [317] estimated the minimum ignition energy for developing a flame kernel to be proportional to the gas pressure. However, the mixture thermal conductivity and specific heat would in turn reduce this increase, yielding a very weak pressure dependence. The experimental data disagrees with this model, with Kopecek et al. [341] reporting a dependence of the form $I \sim p^{-1/m}$, $m \approx 1$.

The first studies of combustion propagation inside ducts and tubes were done by Mallard and Le Chatelier in 1883 [342]. This phenomenon was then photographed in 1928 by Ellis [343], where he observed that for tubes with an aspect ratio (length/diameter) greater than two the flame would suddenly change from a fingerlike to a tuliplike shape. Multiple phenomena could occur as the flame propagates in a tube, namely, transition to detonation [344–348], or the aforementioned tulip flame.

The “tulip flame” phenomenon is another well-documented phenomenon of combustion in ducts, studied for example in [349–354]. In it, the flame front evolves to a tuliplike shape, where the centre “falls” behind the region near the tube walls. The explanation for the tulip formation is attributed to different causes, like the flow viscosity [355, 356], flow-pressure wave interaction [357], hydrodynamic instability [351, 358], vortices or Taylor instabilities [357, 359]. The works of Clanet and Searby [360] suggested that a tulip flame is formed by the Taylor instability, not the pressure wave interaction with the boundary layer. An example of a tulip flame photography can be seen in Figure 5.1.

A tulip flame front is constantly accelerating and decelerating. First, the centre region moves faster than the borders, forming a fingerlike shape; then it starts to slow down and the border will catch up and pass the centre region, forming the tuliplike shape. During the transition phase, the propagation speed suffers a sudden decrease. The tulip flame form is sustained until the end of the combustion process.

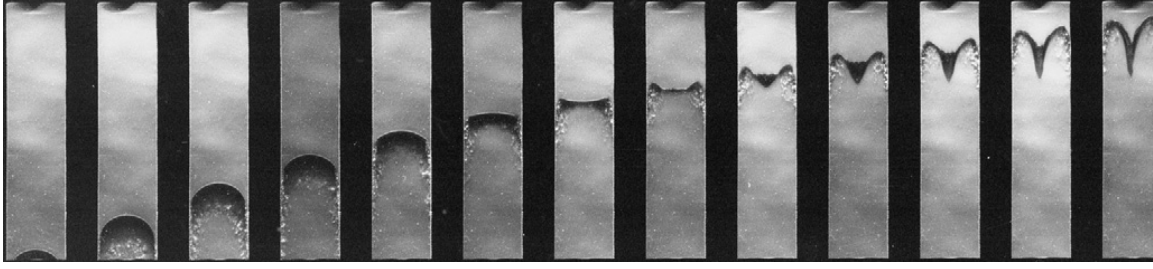


Figure 5.1: Shadow photograph of flame front transition, 0.1 ms between frames, hydrogen-oxygen, $p_0=0.2$ bar, from [346].

Xiao et al. in [350] performed experiments on the influence of ignition position on tulip flame. One important observation was that faster flames have higher acceleration/deceleration during flame front inversion, whose shapes are more pronounced. If ignition starts at the centre of the duct, tulip flames cannot happen. The same authors in [361] investigated the influence of tube dimensions, they report that tubes with higher aspect ratio (length/diameter) lead to faster flames. These faster flames create stronger pressure oscillations and more flame structures, causing the formation of a tulip flame through the Rayleigh-Taylor instability. In very long tubes (aspect ratio above 20) tulip flames collapse, and after a while, a new distorted tulip flame is created [362]. Distorted tulip flames are created when the pressure waves interact with the tulip flame front, creating Taylor instabilities as reported in [363].

The pressure signal of a tulip flame event was described by [346], and it follows: Shortly after ignition, the pressure rises exponentially as the flame reaction area increases; As more gas reacts, it further accelerates the reactions until a constant velocity is reached; The flame front inversion moment may be observed as a second slope appears in the pressure signal rise; The remaining gas is consumed at the constant rate. This description is observable in Figures 5-11 of [364].

Detonation can be initiated directly at ignition time or evolve from a deflagration to a detonation, this is called a deflagration-to-detonation transition (DDT). A detonation region has very high temperatures and pressures, that lead to exponentially growing reaction rates, as these are proportional to the pressure and exponential of the temperature. Outsetting a detonation directly at ignition requires a great amount of energy to be deposited, like through a focused laser. The energy deposition time should be short compared to the characteristic blast time [317]. A strong blast wave will decay to a Chapman-Jouget (CJ) detonation.

The flame speed is key for the DDT phenomena. In it, a tulip flame is always present. As the flame front inversion occurs, a detonation starts in a preheated region ahead of it. This phenomenon was photographed by Liberman et al. in [346], where pressure waves coalesce forming an extremely high-pressure region, initiating the detonation process. The detonation process is sustained by a positive feedback loop, where higher pressure/temperature in the detonation yields faster reaction rates that would further increase the temperature/pressure and so on.

The minimum size of a self-sustained detonation is called the detonation cell Λ . A detonation will decay to a deflagration if the transverse dimension of the tube is smaller than the detonation cell size, ($d > \Lambda/\pi$ in cylindrical tubes). Lee [320] imaged detonation cells in hydrogen-oxygen mixtures, showing a fish scale

pattern formed by the detonation cells. The cells move at an average velocity equal to the Chapman-Jouguet (CJ) velocity. Its velocity varies between 1.6 and 0.6 times the Chapman-Jouguet velocity, in the first and second half of the cell respectively. Mixtures with smaller cells are more sensitive and unstable, meaning they are easier to detonate than mixtures with larger detonation cells.

Lee [320] studied the influence of air:fuel equivalence ratio on the detonation cell size for different fuel mixtures. Detonation cell size has a U-shape curve with a minimum for $\lambda \sim 1$, meaning stoichiometric mixtures are easier to detonate and maintain a sustained detonation. Minimum ignition energy for a detonation, E_d , relates to the characteristic detonation time, τ_d , in the form $E_d \sim \tau_d^3$. Detonation in stoichiometric conditions has faster velocities, which minimize τ_d , and explains why these are easier to detonate than very rich or lean mixtures. Ronney in [317] explains these estimations in more detail.

Knystautas et al.[365] and Bull et al.[366] performed measurements on detonation cells for different pressures on multiple fuel-oxygen mixtures. Their results show that the cell size has a negative linear dependence on increasing pressure. Meaning higher pressure mixtures have smaller detonation cells, which are more detonation prone. Explosion limits of hydrogen-oxygen and hydrogen-air mixtures were studied by Schröder et al.in [367]. Their experiments use an exploding wire for the ignition source and filling pressures up to 200 bar. They report a non-monotonous pressure dependence for both upper and lower explosion limits.

If a detonation wave inside a tube suddenly expands into an unconfined space with the same unburned mixture, it would either decay into a deflagration or continue as a detonation wave. The minimum tube diameter/dimension enabling the detonation to continue as a detonation after the sudden expansion is called the critical diameter d_{crit} . Mixtures with low dilution factors have a critical diameter of 13 times the detonation cell size [365, 368–370], meaning that if the diameter of the tube where the detonation is occurring is 13 times larger than the detonation cell for the mixture ($d_{crit} \geq 13\Lambda$), then the detonation will not decay to a deflagration when it transitions into the wider pipe. For high dilution mixtures the critical diameter increases to 20 to 30 times the detonation cell size [371–373]. The difference in d_{crit} originates from the way the detonation propagates into the larger region. For low-dilution (unstable) mixtures, the re-ignition of the detonation is controlled by the coalescence of pressure waves in the expansion region. Wherein high-dilution (stable) mixtures it is the change in the shock wave curvature in the expansion that controls the re-ignition of the detonation process. A detailed review of these topics can be found in [305]. As an example of this phenomenon, some experimental shots in the “bombe” scale driver were made to decide the format of the ignition channel. Their pressure signal is depicted in Figure 5.2. A cylindrical tube was added to the “bombe” window to emulate a channel expanding into a large vessel. Three configurations were tested, the first two were simple cylinders with 4 and 20 mm of diameter, and a last one a conic shaped tube expanding from 4 to 20 mm of diameter.

In a DDT an initial laminar subsonic flame accelerates and at some point transits to a detonation. The transition point and time are not always the same for the same initial conditions [346], making it a stochastic event. Oppenheim et al.in [374–376] performed experiments on hydrogen-oxygen DDT in tubes. The increased turbulence from the flame acceleration would eventually trigger a DDT.

Recent works [377, 378] show that the DDT phenomena follow these steps: 1) initial exponential

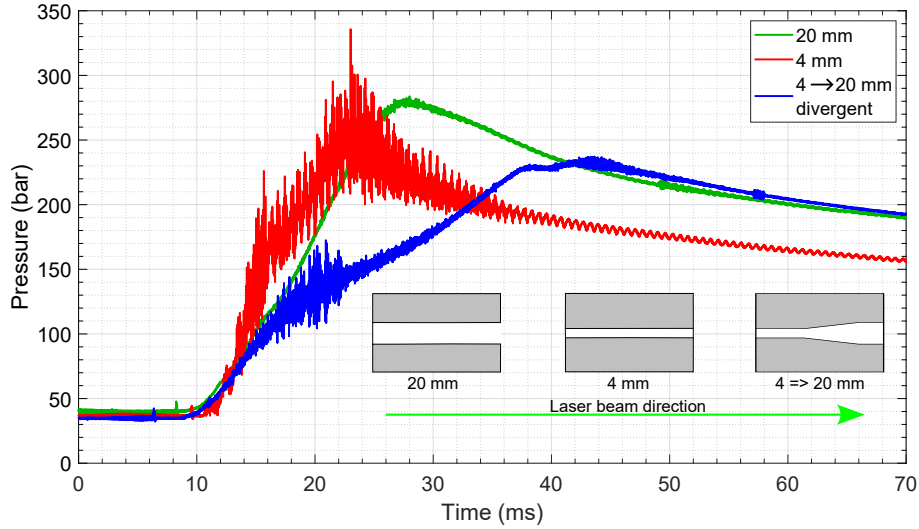


Figure 5.2: Bombe ignition channel tests, shots B148, B149 and B151.

flame acceleration and shockwave creation; 2) flame creates compression waves ahead of its front which steepens the shock; 3) A heated zone of unreacted gases right ahead of the flame is formed, as the flame front reaches it, a chain of reactions starts; The chemical reaction rates in the high pressure/temperature zone are very high, enhancing the heat release, transiting to a detonation. Liberman et al.[346] performed Schlieren photography to identify flame time evolution of stoichiometric hydrogen-oxygen mixtures. They observed the three stages previously presented and detailed them.

During the third stage, the flame speed abruptly rises and forms the transient overdrive detonation, which afterwards decays to a Chapman-Jouget detonation. Their detailed examination showed a reduction in flame tip velocity during the second stage, corresponding to the flame front inversion and creation of a tulip flame, after which the DDT occur. Figure 7 in [346] depicts the DDT in ethylene-oxygen mixtures where the coalescent acoustic waves ahead of the flame front build up to a high pressure/temperature region. Despite all DDT coming from tulip flames, not all tulip flames transit to a detonation.

5.3 Experimental Setup

The ESTHER combustion chamber driver operates nominally with a $\text{He:H}_2\text{:O}_2$ mixture at initial pressures up to 100 bar. Alternatively, helium may be replaced by nitrogen. Figure 5.4 presents the combustion driver setup. The combustion chamber is a low-carbon steel cylinder with an inner diameter of 200 mm, a total length of 1600 mm and a wall thickness of 200 mm. Ignition takes place in a small 144 mm, long 20 mm diameter cylindrical channel, via a remote-controlled high power Nd:YAG laser and its optical system of mirrors and lenses.

The system is divided into three parts, the combustion chamber and its associated optics (1-6), the laser heads (a and b), and the beam conditioning system (c-i). Beam alignment was done using the red diode laser as a guide, so the Nd:YAG laser could be turned off whenever possible, as a safety measure. The high power Nd:YAG laser (Quantel Brilliant, 1064 nm, 180 mJ, 5 ns) pulse beam is reflected by two 45° mirrors (High power CVI mirrors) to adjust its height and azimuthal deviation, passes through

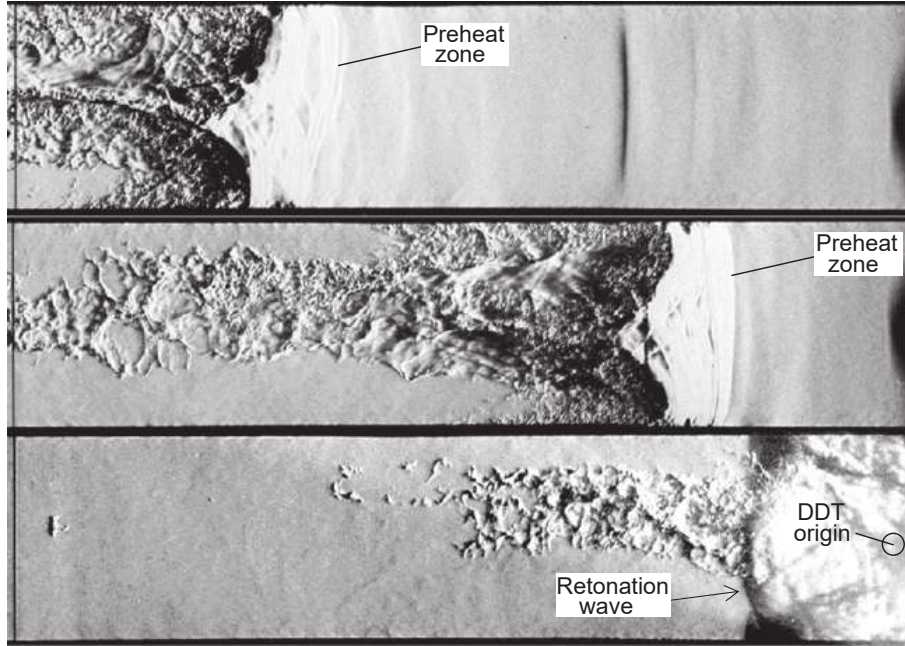


Figure 5.3: Shadow photographs of DDT in an ethylene-oxygen mixture at 0.12 bar, frame times 3.75, 3.85 and 3.95 ms (top to bottom), from [346].

a half-wave plate, a beam splitter cube, and focusing lens, then enters the chamber. It is possible to adjust the laser beam power that is sent to the chamber using the half-wave plate and the polariser cube. The half-wave plate linearly polarizes the light and the beam splitter cube divides it as a function of the polarization, part into a beam dumper and the other in the direction of the combustion vessel. By adjusting the beam polarization we can control the portion that is deviated by the beam splitter cube. The focusing lens can be mounted or unmounted at will.

The pressure evolution is measured by a piezo-electric pressure transducer (Kistler 6215) connected to a charge amplifier (Kistler 5015). This sensor is positioned in the third port of the combustion chamber, number 5 in Figure 5.4, and is protected with a heat shield system (Kistler 6567) as recommended by the manufacturer. The pressure transducer signal is recorded in a digital storage oscilloscope (Tektronics MDO4104B-3) and an FPGA-based acquisition board RedPitaya at 125 Mega Samples per second and variable decimation rate.

The gas filling system (number 3 in Figure 5.4) was supplied by Air liquide and was designed to deliver an arbitrary mixture of $\text{He:H}_2\text{:O}_2$ with a filling pressure of up to 100 bar. The system was later adapted to have the option of filling the chamber with N_2 instead of He baseline. The system was designed with segregated gas lines equipped with individual mass flow controllers (Bronkhorst F-232MI-MGD-33-V) for each gas (H_2 , O_2 and He/N_2) to prevent unwanted flames in the pipeline. A high-pressure zone, rated at 1.8 kbar, equipped with a relief valve system that can block waves travelling upstream into the gas supply lines. These valves have a one-way system so that upstream reflux from gases near the combustion chamber cannot reach the bottle storage zone.

The gas filling system is remotely controlled and monitored from a separate control room, by an industrial standard Siemens Programmable Logic Controller (PLC) system (S7-1200 CPU Module 1215C), associated to an EPICS/CS-Studio higher-level control and monitoring layer. An Ashcroft series A4

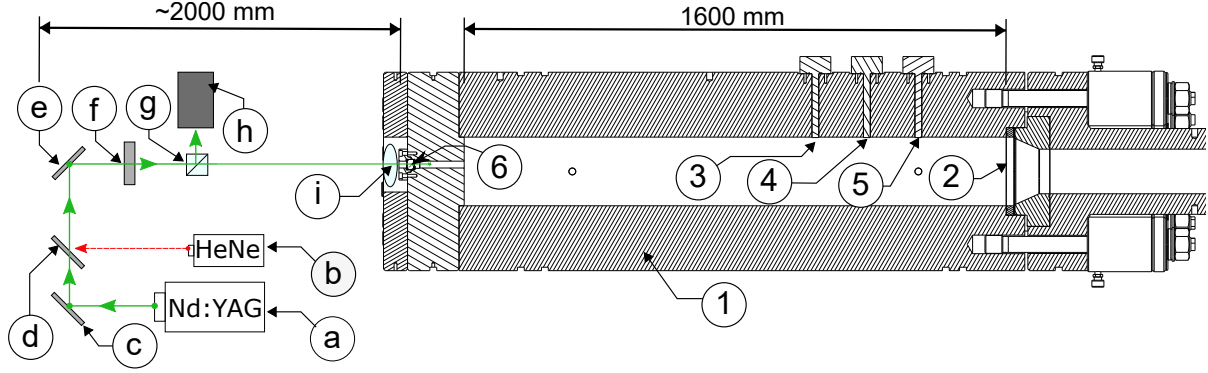


Figure 5.4: Combustion Driver Setup. 1-Combustion Chamber; 2-Diaphragm/Blank; 3-Gas input port; 4-Gas output port; 5-Kistler gauge; 6-Sapphire Optical Window. a-High-power Nd:YAG laser 1064 nm 5 ns 200 mJ; b-Red diode laser (for alignment); c,d,e-45° mirror; f-Half-wave plate; g-Beam splitter cube; h-Beam dump; i-Bi-convex focusing lens 100 mm

model pressure transducer, capable of measuring absolute pressures from atmospheric up to 130 bar, monitors the filling pressure.

Each experimental shot follows these steps: The filling is carried using 200 bar gas bottles in sequence. First O_2 , second He (or N_2), third H_2 and last He (or N_2) again. With this filling sequence, it's possible to flush O_2 and H_2 with He from the gas lines, avoiding combustible mixture in the lines, also it minimizes the time H_2 is present outside the gas bottles. For the ignition system, the laser pulse energy is tuned, and the focal lens is mounted/unmounted, as desired. At the end of each shot the gases are flushed to the atmosphere, with the remaining water vapor manually pumped out. Four variables were studied, filling pressure, helium/nitrogen dilution, $O_2:H_2$ equivalence ratio, and laser lens focal distance.

5.4 Results

Experimental results were evaluated by analysing the temporal evolution of the pressure signal inside the combustion chamber. A sensitivity study of the input parameters (filling pressure, helium/nitrogen dilution, $O_2:H_2$ equivalence ratio and laser lens focal distance) onto the compression ratio, flame velocity, induction time, acoustic oscillation and transition to detonation was performed. The goal is to maximize the pressure ratio while remaining in the deflagration with a low acoustic oscillation. Driver repeatability conditions were also evaluated to ensure future shock tube shots experimental repeatability.

Uncertainty Quantification

Since the main objective of this campaign is to maximize the peak pressure in deflagration mode, a more qualitative approach was chosen to drive the results analysis. Thus, error bars are only presented whenever a quantitative analysis is performed, such as comparing compression ratios or flame velocities. The experimental uncertainties are dominated by the uncertainties of the pressure transducers, both the static one and the dynamic one. The filling pressure uncertainty was taken from the equipment's manual as 0.25% of the static pressure transducer full-scale (150 bar). The peak pressure uncertainty

was also taken from the manual as 0.5% of the full-scale. The exact uncertainty value would vary with the pre-set value before each shot. A charge-drift in the pressure gauge sensor would occur over time, however, this was reset right before the laser firing. Since the charge-drift time-scale is much larger than the combustion process, one could ignore the error caused by it. For the pressure over time signal, its uncertainty can be attributed to signal digitization by digital storage oscilloscope or the FPGA-based acquisition board RedPitaya. The uncertainty was taken as the larger value between the uncertainty of the pressure variation signal, 0.5% of full-scale, and the difference between two consecutive bits of the data acquisition systems. Usually, whenever the RedPitaya was used, the dominant uncertainty was the Kistler one. On the other hand, whenever the oscilloscope was used for data acquisition, its bit-scale was the limiting factor.

The temporal uncertainty in the pressure signal is caused by two factors: 1) the uncertainty of the data acquisition system and 2) the error associated with each “phase” of the combustion process, from the moment the laser is fired until all the gas is consumed. The former is much smaller than the latter, thus, the temporal uncertainty is dominated by the uncertainty in correctly identifying the exact location of some key events, such as combustion begin or inflection points, occur in the pressure signal. The initial conditions of the chemical mixture also has an associated error. It is caused by the uncertainty in the partial pressure of each chemical specie over the gas filling phase. An error analysis for this was not carried out, and as such only the error on the total filling pressure is considered, and thus the uncertainties in the helium/nitrogen dilution or air:fuel equivalence ratios were not considered. A correct estimation of gas filling uncertainties, and the associated parameters air:fuel ratio or helium dilution, would require an extensive set of tests to create a statistically significant data set. All derived parameters errors were computed using linear error propagation methods.

5.4.1 Phenomenological description

A total of 100 shots were performed, 65 using helium as a diluent and 35 using nitrogen. Figures 5.5 and 5.6 illustrate the distribution of experimental shots as for their chemical mixture, and filling pressure map. The gases are treated as an ideal gas mixture, thus each molar fraction is proportional to the gas pressure. Thus, $\chi_{\text{He/N}_2}$ is calculated by the ratio of the partial pressure $p_{\text{He/N}_2}$ of the diluent (He or N₂) to total filling pressure p_0 as,

$$\chi_{\text{He/N}_2} = \frac{p_{\text{He/N}_2}}{p_0} \quad . \quad (5.4)$$

Figure 5.7 illustrates a deflagration shot pressure and mass consumption evolution. The laser firing can be identified by the flash-lamp signal at t_0 , ignition takes place immediately after this. The initial rise in pressure, marked by t_1 , marks what we can identify as the beginning of the combustion process. In truth, there is a time lag on this because the pressure wave needs to cross from the ignition zone to the pressure sensor. Nonetheless, an induction time can be estimated by taking $t_1 - t_0$. The mass consumed m_b and mass consumption rate $\dot{m}_b$ can be obtained from the expressions in [313],

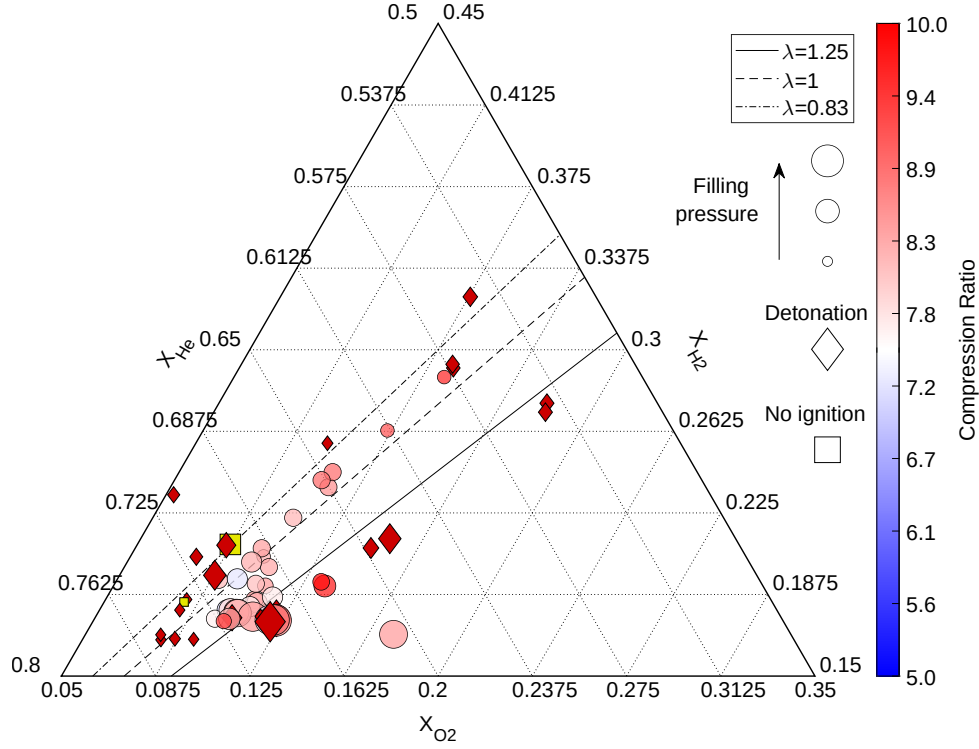


Figure 5.5: Molar fractions and filling pressure map of the performed shots with helium dilution shots.

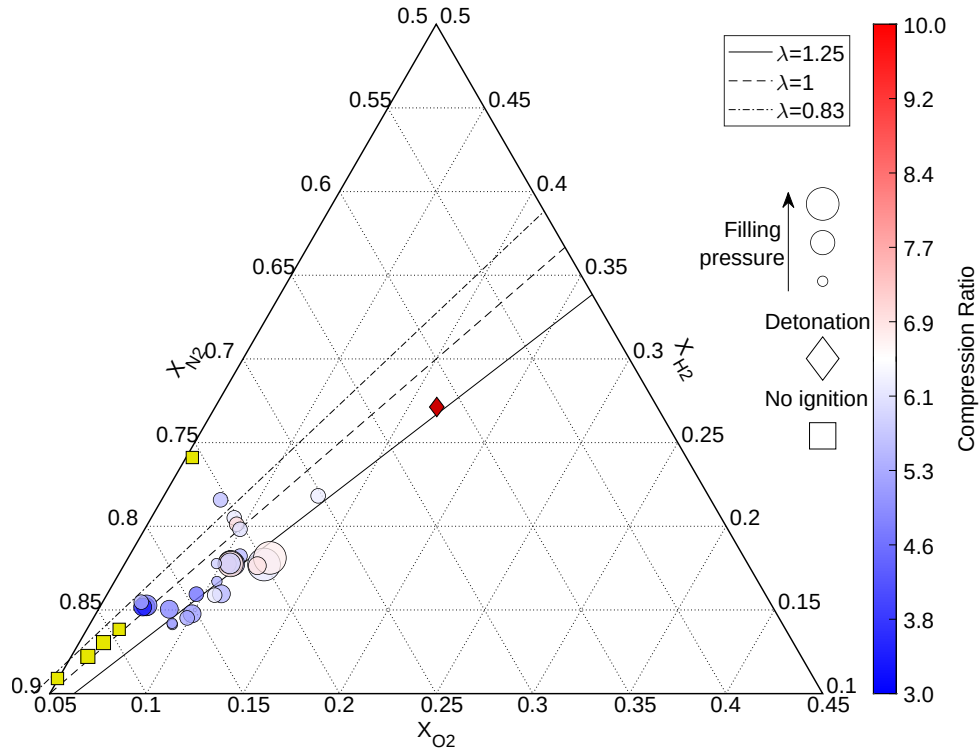


Figure 5.6: Molar fractions and filling pressure map of the performed shots with nitrogen dilution shots.

$$m_b = \frac{p - p_0}{p_{eq} - p_0} m_0 \quad , \quad (5.5a)$$

$$\dot{m}_b = \frac{dm_b}{dt} = \frac{m_0}{p_{eq} - p_0} \frac{dp}{dt} \quad , \quad (5.5b)$$

where p_{eq} is adiabatic equilibrium pressure. When the flame front exits the ignition channel and enters the main chamber channel it accelerates, identified by the higher mass consumption rate. As the flame reaction area increases so does the mass burning rate in a positive feedback loop, as stated in [346]. This acceleration halts when the flame reaches the outer edges of the combustion chamber at t_2 , after which the reaction area remains approximately constant, and so does the flame velocity, until all of the reagents are consumed (t_3).

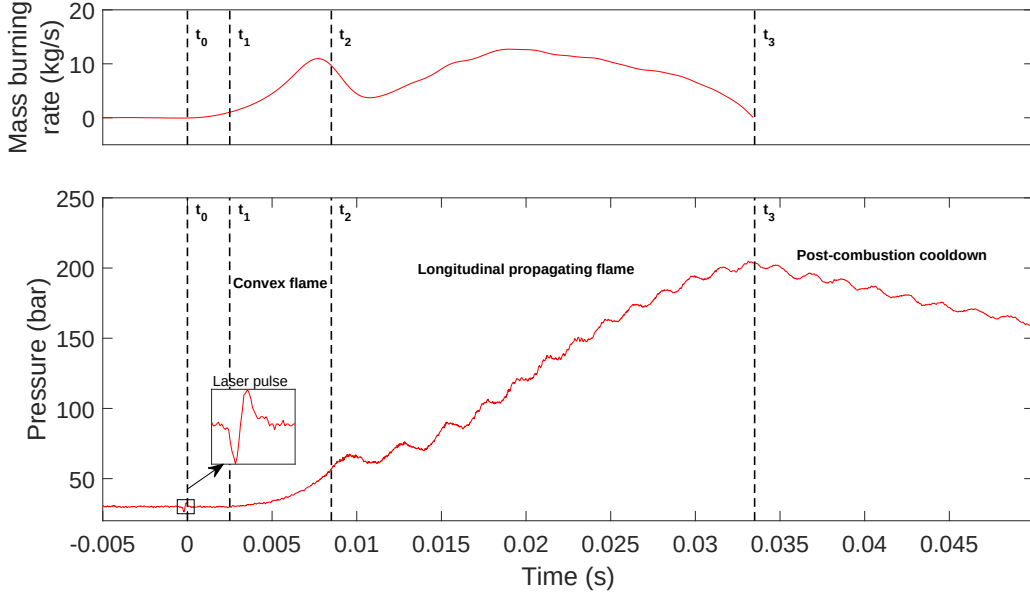


Figure 5.7: Deflagration shot pressure and mass consumption time evolution.

Figures 5.8 and 5.9 depict the pressure signal of shot S017, where the formation of a “tulip flame” occurred, and provide an illustration of its evolution. The uncertainty of the pressure signal over time is marked as a shaded region in Figure 5.8, a detailed window better illustrates this. Laser ignition starts with a highly reactive plasma kernel, as pointed out by [317, 337, 379], near the focal point of the lens (around 40 mm from the sapphire window for the experiment). The flame exits the channel towards the main chamber, where it then radially expands from t_1 to t_2 , until it reaches the chamber’s outer wall. An acoustic pressure wave is created when the flame front touches the chamber walls at t_2 [350, 361]. These then propagate back and forth inside the chamber. Faster flames will induce stronger acoustic waves, as stated by [350]. The same author in [361] stated that larger diameter ducts will originate stronger acoustic waves since the flame front velocity is higher when it reaches the side walls. The flame front inversion occurs at t'_2 , where it transits to a “tulip flame”, denoted by the change in slope. Flame velocities are calculated from the pressure signal and chamber dimensions. The average flame velocity is calculated by taking the distance from the exit of the ignition channel to the Kistler gauge (1346 mm), and the time between the initial rise t_1 and the peak pressure t_3 .

Figure 5.10 shows the Spectrogram (Time-dependent Fast-Fourier Transform) of the pressure signal for shot S084. The combustion process is around 180–190 ms after the signal acquisition starts. It is identified by the rise of an oscillation in the continuum region (< 100 Hz). Soon after, the acoustic

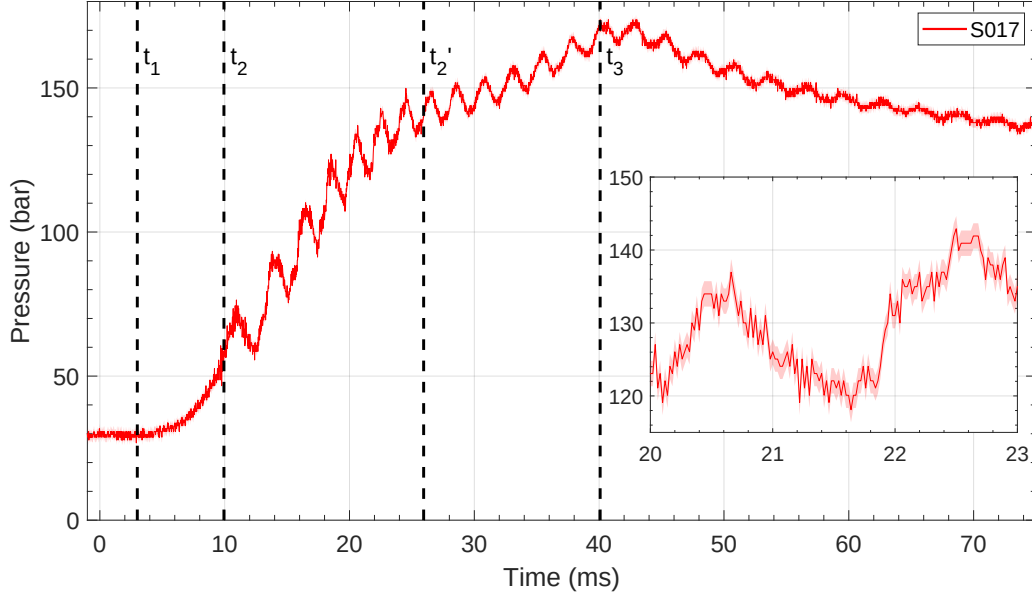


Figure 5.8: S017 pressure signal, $p_0 = 30$ bar, $\lambda = 1.2$, $\chi_{\text{He}} = 71\%$. Characteristic times, at t'_2 occurs a flame front inversion and transition to a “tulip flame”.

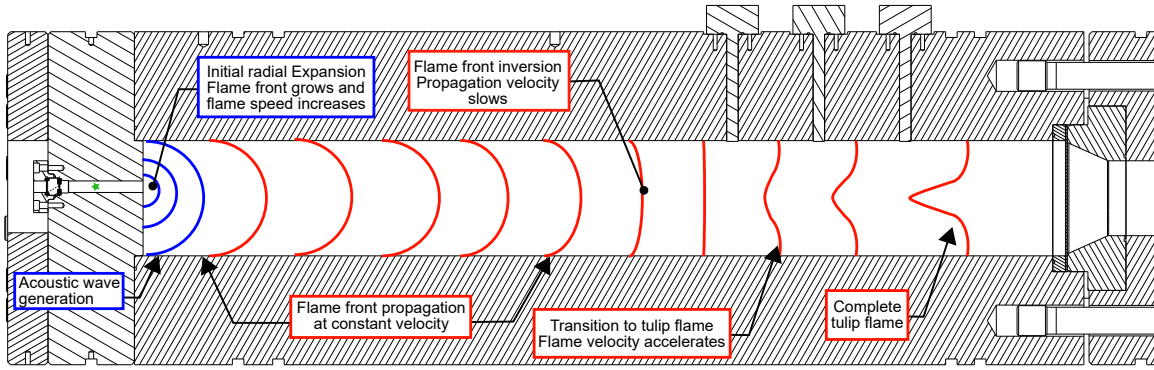


Figure 5.9: Deflagration shot evolution schematic, piezo-electric pressure sensor on the rightmost access plug

oscillations begin. Multiple frequencies can be identified in the spectrogram, however, the dominant ones are in the 0 to 600 Hz region corresponding to the longitudinal acoustic frequency on the driver chamber. As the gas cools down, the peak frequency will over time suffer a “red-shift” to shorter frequencies. Higher order frequencies, 800 to 1500 Hz, decay in about 600 ms after their creation.

The gas temperature inside the chamber can be estimated by two different methodologies. First, through the perfect gas law. The gas temperature after combustion T_{pos} is given by

$$T_{pos} = \frac{p_{peak} \mathcal{M}_{pos}}{\mathcal{R} \rho} \quad , \quad (5.6)$$

where p_{peak} is the peak pressure observed during the combustion process, $\mathcal{M}_{pos}$ the post-combustion mean molar mass, $\mathcal{R}$ the perfect gas constant and ρ the gas mass density. The acoustic waves created during the combustion process form a periodic component in the pressure signal “observed” by the Kistler gauge. The wave’s frequency can be analysed via a Fast Fourier-Transform (FFT) to get another temperature estimation. Equating the acoustic frequency f_k to that of a standing acoustic wave with a

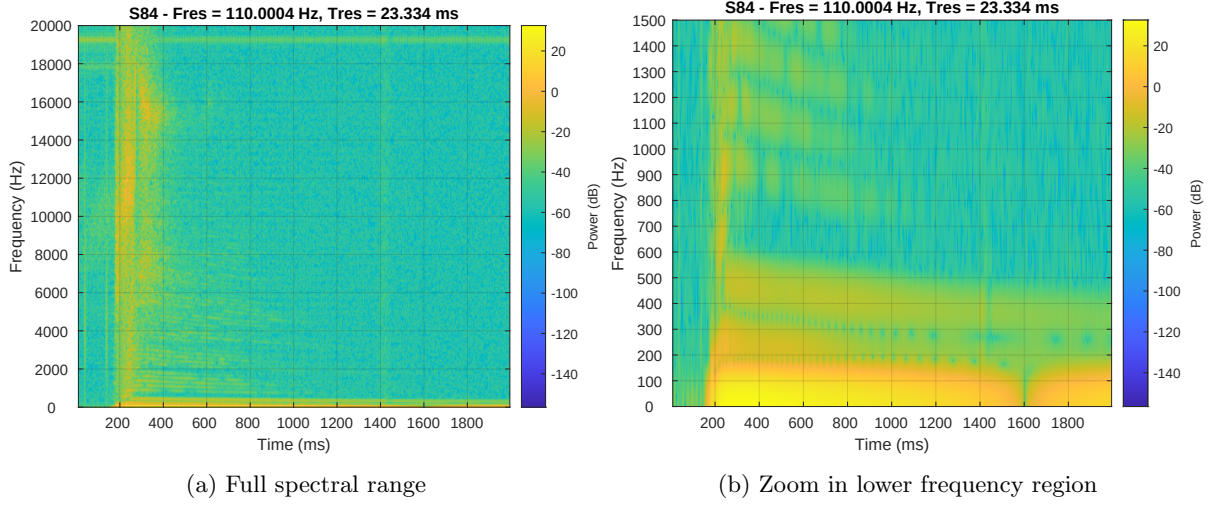


Figure 5.10: Spectrogram (Time-dependent Fast-Fourier transform) of shot S084 ($\text{N}_2:\text{H}_2:\text{O}_2$, $\mathcal{X}_{\text{N}_2}=72\%$, $\lambda=1.1$, $p_0 = 10$ bar) pressure signal. A zoom on the region up to 1500 Hz is depicted. The region of 150 Hz has a peak corresponding to the acoustic wave pressure oscillation.

sound velocity a , one gets,

$$f_k = \frac{a}{\lambda_k} \quad , \quad (5.7)$$

where $k = 1, 2, \dots$ is the acoustic wave harmonic, and λ_k the corresponding wavelength. A standing wave has a minimum wavelength equal to half of the chamber distance l , and by recalling Equation 3.2, one can then re-write Equation 5.7 in the form,

$$f_k = \sqrt{\frac{\gamma \mathcal{R} T_{pos}}{\mathcal{M}_{pos}}} \frac{1}{(k+1)l} \quad . \quad (5.8)$$

Taking the dominant and lowest order term $k = 1$, and acoustic l to be the distance between the combustion chamber wall and Kistler gauge ($\simeq 1325$ mm), one can invert Equation 5.8 to estimate the gas temperature from the dominant acoustic frequency identified by the Fast Fourier-Transform as follows

$$T_{pos} = \frac{4\mathcal{M}_{pos}l^2f^2}{\gamma\mathcal{R}} \quad . \quad (5.9)$$

5.4.2 Repeatability Testing

Figure 5.11 compares the pressure signal for shots S024, S025, and S026 with identical conditions. The pressure signal evolution is very similar between all three experiments, with differences in the cooldown phase. These differences may be explained by different charge drifts in the Kistler piezo-electric sensor, which is only accurate in the pressure rising phase. The experiment repeatability is demonstrated, as identical initial conditions yield identical output signals. Note that this is remarkable owing to the strong acoustic waves and localized detonations observed between 2 and 7 ms.

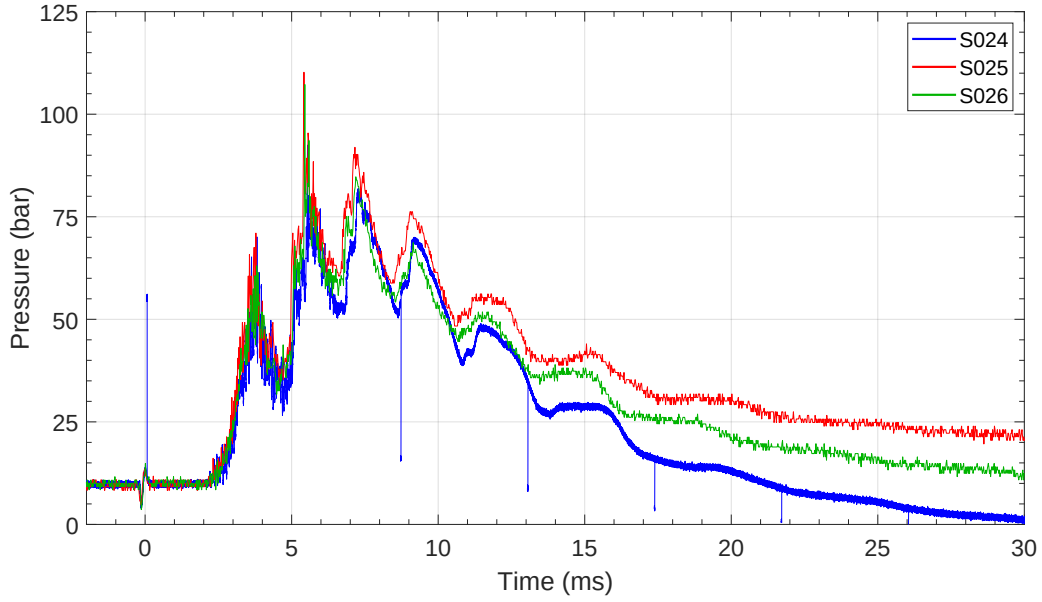


Figure 5.11: Repeatability comparison (shots S024, S025, and S026). $\text{He:H}_2\text{:O}_2$, $p_0=10$ bar, $\mathcal{X}_{\text{He}}=57\%$, $\lambda=0.92$, and 100 mm focal lens.

5.4.3 Parameters influence

The influence of each parameter in the study (p_0 , λ , $\mathcal{X}_{\text{He/N}_2}$, and beam focus) on the combustion process is presented in Table 5.1. A thorough discussion of the effects of each parameter on the combustion process is presented in the next section. Table 5.2 summarizes the operational results from the qualification. In it, the operational gas mixtures for the full pressure range of the ESTHER driver are shown. Nitrogen diluted mixtures with filling pressures above 50 bar are too unstable and acoustically noisy to be of operational use.

Table 5.1: Lookup table, influence of experimental parameters on compression ratio, flame speed, induction time and propensity to detonation.

	Compression ratio	Flame speed	Induction time	Acoustic wave	Detonation ease
$p_0 \uparrow$	$\simeq$	$\downarrow$	$\simeq$	$\uparrow$	$\uparrow$
$\mathcal{X}_{\text{He}} \uparrow$	$\downarrow$	$\downarrow$	$\uparrow$	$\downarrow$	$\downarrow$
$\text{He} \rightarrow \text{N}_2$	$\downarrow$	$\downarrow$	$\uparrow$	$\uparrow$	$\downarrow$
No Focus lens	$\uparrow$	$\uparrow$	$\simeq$	$\uparrow$	$\uparrow$

Table 5.2: ESTHER driver operational mixture for the working pressure range 10-100 bar. Results from the qualification campaign.

Filling pressure (bar)	10	30	50	60	70	80	100
Mixture $\text{He:H}_2\text{:O}_2$	8:2:1.2	8:2:1.2	8:2:1.2	8:2:1.2	8:2:1.3	8:2:1.4	8:2:1.4
Peak Pressure (bar)	61.5	200	334	400.5	425	528	660
Mixture $\text{N}_2\text{:H}_2\text{:O}_2$	10:2:1.4	10:2:1.4	10:2:1.4	—	—	—	—
Peak Pressure (bar)	38	114	190	—	—	—	—

5.5 Discussion

5.5.1 Inert gas dilution

Figure 5.12 depicts the compression ratio as a function of helium dilution, with the $O_2:H_2$ equivalence ratio in the colour scheme. The compression ratio was computed taking the ratio of peak to filling pressure in each shot, a more detailed analysis accounting for the pressure fluctuations is later presented in this section. Still, this first analysis allows us to estimate the overall effect/tendency of the different parameters on compression ratio. Increased helium dilution lowers the compression ratio as helium is an inert species in the reaction. It does not contribute to the exothermic reaction and yet absorbs part of the released heat. Detonations, identified by compression ratios above 8, may occur for cases with very low dilution ($\lesssim 60\%$). Figure 5.13 shows the average flame velocity for the experimental shots with a detail window for the 68-76% region. Similar to the compression ratio, increased helium dilution decreases the average flame velocity. The reason for this is similar to the compression ratio one. The helium will absorb part of the released energy, decreasing the overall temperature and thus the flame velocity. Tang et al in [313] (in Figure 5c) illustrates the normalized pressure signal for three different dilution mixtures. Their observations agree with ours, where higher dilution by an inert gas decreases the compression ratio and increases combustion time, thus decreasing average flame velocity. For low dilutions ($< 65\%$) one may observe shots where the velocity is above 200 m/s. For these cases, a DDT probably occurred during the combustion process. It can be hinted by the average flame velocity being two to six times above the mean flame velocity (~ 100 m/s). A direct detonation, where all/majority of the gas is consumed by a shock wave, is unlikely to have occurred as it would need the average velocity to be above the sound speed (~ 720 m/s for most helium diluted shots).

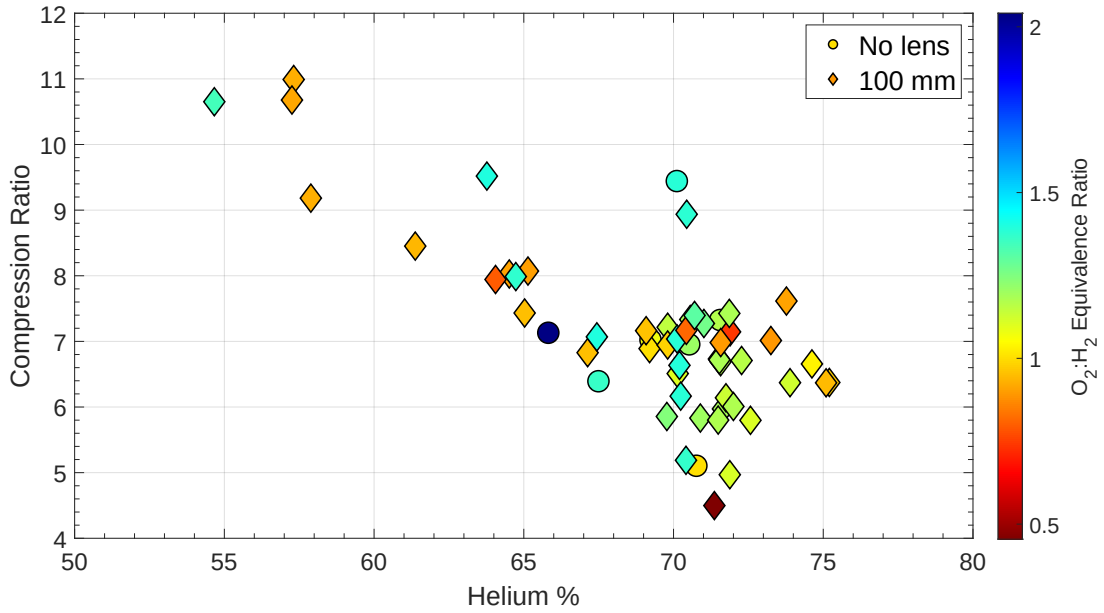


Figure 5.12: Scatter plot of compression ratio function of helium dilution, $O_2:H_2$ equivalence ratio in colour-axis, focal lens distance in markers symbol.

Figure 5.14 compares the pressure signal evolution for shots S034 and S035, with $p_0 = 50$ bar, $\lambda = 1.40$

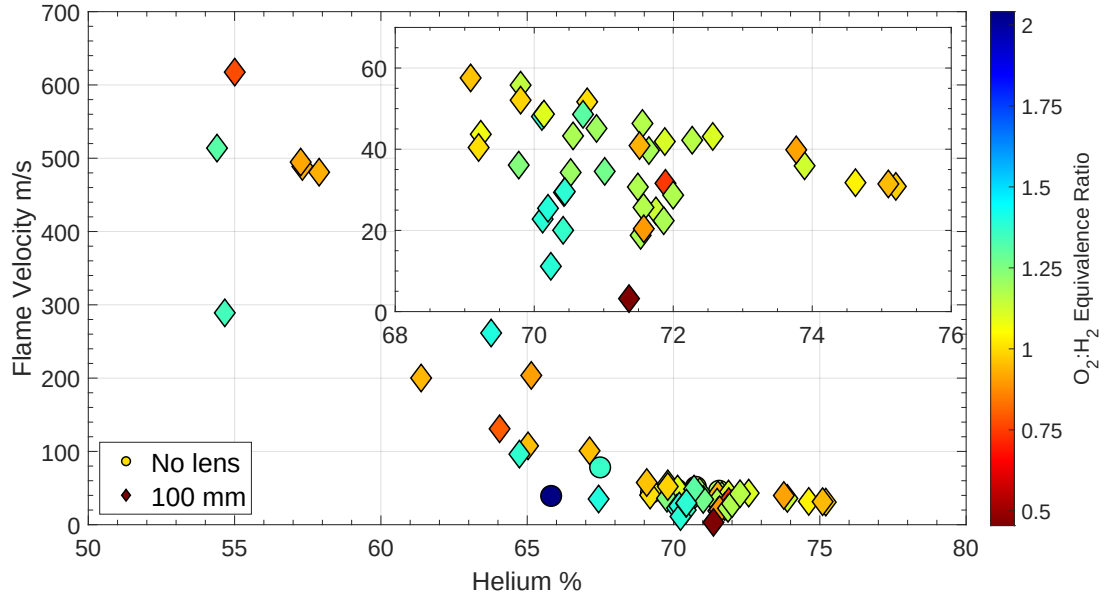


Figure 5.13: Scatter plot of average flame speed function of helium dilution, O₂:H₂ equivalence ratio in colour-axis, focal lens distance in markers symbol.

and helium dilutions of 64 and 67% respectively. In S035 the combustion time is longer and the peak pressure is lower, meaning a lower velocity and compression ratio. Shot S034 was a shorter induction time, seen by the faster rise in pressure. Alongside it, the acoustic wave is clearly stronger in S034. A DDT can be observed at around $t \approx 8$ ms for S034. Despite the small difference in dilution, from 64 to 67%, it is enough to avoid a detonation in the mixture. The relation between helium dilution and acoustic wave strength is not linear, as it affects the chemical reaction rates. The acoustic wave amplitude is related to the flame front velocity, as reported in [350]. The authors observed that when the flame was faster, the variation in flame tip speed was higher. The same author [361] stated that in larger diameter ducts, where faster flames could be achieved, flame and pressure oscillations were larger. However, the acoustic wave also depends on the O₂:H₂ equivalence ratio, and filling pressure, which in turn affects the flame velocity. Nonetheless, increasing helium dilution decreases the acoustic wave amplitude and detonation ease of a combustible mixture as shown in Figure 5.14.

A second set of experiments was performed using nitrogen, in place of helium, as the mixture diluent gas. As expected, both gases present similar behaviours. Increasing the diluent molar fraction reduces the acoustic waves, flame speed and compression ratio. Nitrogen has a higher molar mass and a lower specific heat capacity than helium. These characteristics translate into a slower flame velocity and lower compression ratios. The slower combustion also increases the wall heat losses which in turn further decreases the combustion peak pressure when compared to the helium diluted shots. Nitrogen diluent shots have stronger acoustic waves when compare to the helium ones. Figure 5.15 compares two similar shots with different diluent gases S031 (helium) and S081 (nitrogen). Shot S081's total combustion time is around 300 ms, around four times longer than S031. The induction time was also around four times longer for the nitrogen shot (10-20 ms) compared to the helium mixture (5 ms). The acoustic oscillations of nitrogen dilution have a clearly higher amplitude and lower frequency than for helium. The lower frequency is explained by the heavier molar mass, which lowers the gas sound speed. Strong acoustic waves

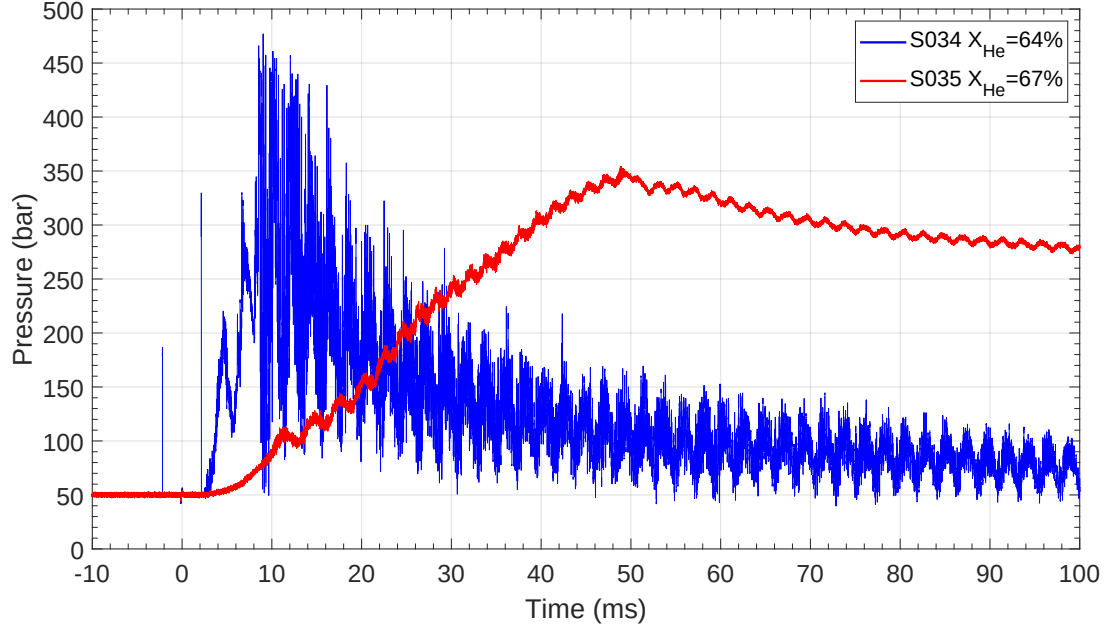


Figure 5.14: Pressure signals for different diluent molar fractions, S034 64% (blue) and S035 67% (red). $p_0 = 50$ bar, $\lambda = 1.40$ and lens focal distance 100 mm.

are generated near the pressure peak region. Owing to these strong oscillations the operational mixture changes from 8:2:1.2 [He:H₂:O₂] to 10:2:1.2 [N₂:H₂:O₂] in nitrogen diluted shots. These observations are in agreement with Burke et al. in [323], where they studied the influence of different diluents on mass burning velocity.

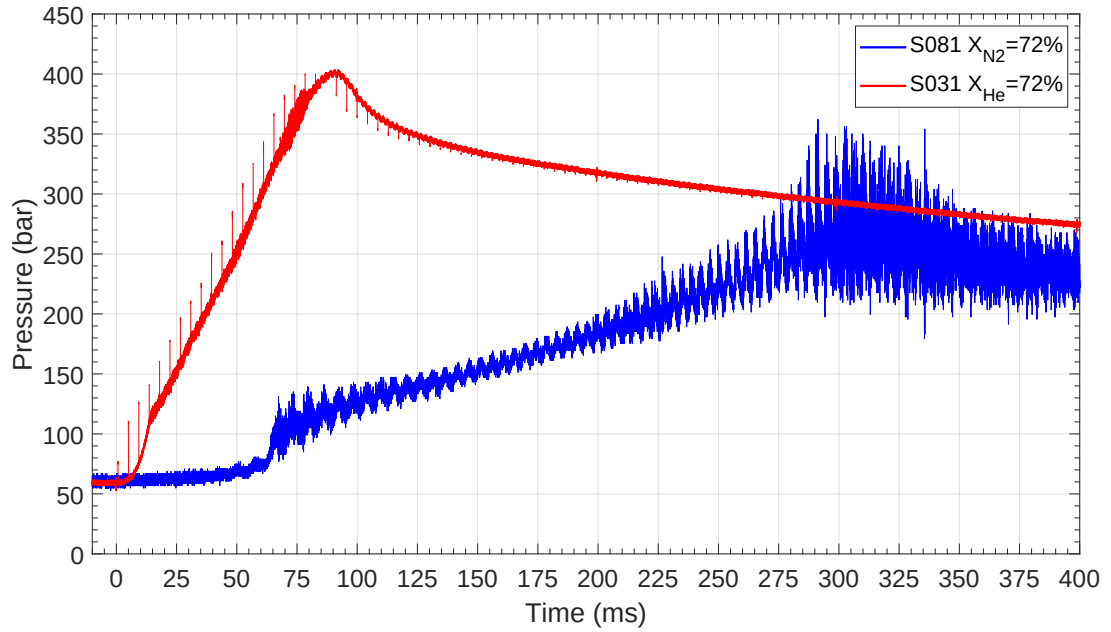


Figure 5.15: Pressure signal comparison for different diluent gases, helium in S031 (blue) and nitrogen in S081 (red). $p_0 = 60$ bar, $\lambda = 1.20$ and lens focal distance 100 mm.

5.5.2 Oxygen-to-Hydrogen Equivalence Ratio

Figure 5.16 compares compression ratio and flame velocity for different $O_2:H_2$ equivalence ratios. Compression ratios are shown to be higher for stoichiometric and slightly rich mixtures. Despite the theoretical maximum energy release being at stoichiometric conditions, slightly-rich mixtures appear to contradict that. The explanation may lie in the wall heat losses and flame velocity. Slightly-rich mixtures are faster, thus reducing the energy losses at the wall. The higher velocity can be justified by the higher diffusivity of hydrogen, as the works of [313, 316, 322, 323, 326] show. However, if the mixture goes very rich the compression ratio will decrease. This is expected, because even if the mixture has higher diffusivity and reduces wall losses, the amount of released heat is lower, and it will not heat the gas as much. This yields a lower peak temperature/pressure (thus lower compression ratios) and lower temperatures will in turn reduce flame velocity.

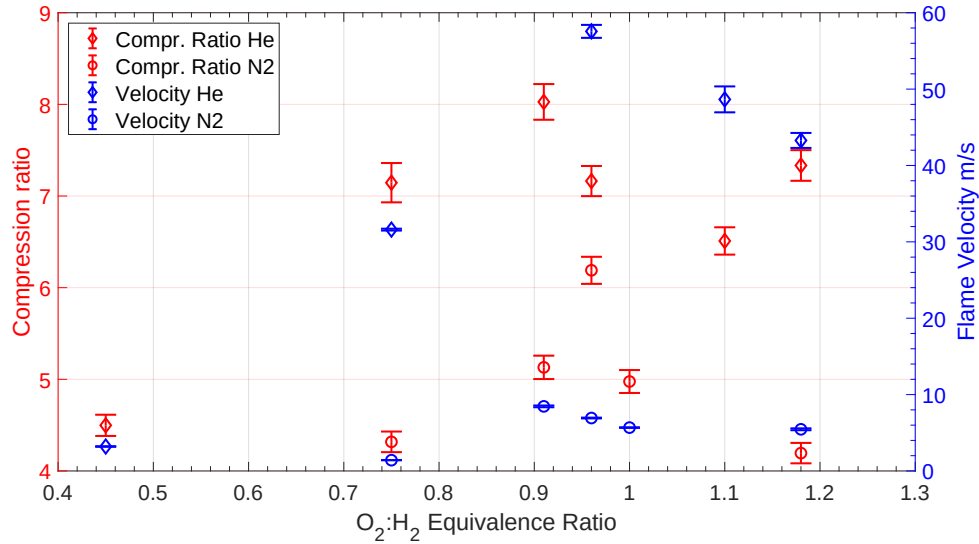


Figure 5.16: Compression ratio function of $O_2:H_2$ equivalence ratio (left axis), average flame velocity (right axis). $p_0 = 20$ bar, $\mathcal{X}_{He/N_2} = 71\%$, lens focal distance 100 mm.

A signal comparison for two shots at 20 bar is shown in Figure 5.17. The image compares two rich mixtures with helium dilutions of 65%. The mixture S016 completes combustion in around 15-20 ms, whereas the closer to stoichiometric S013 completes it in 10 ms. Due to its faster flame, S013 creates stronger acoustic waves, in agreement with the works of [361]. The average peak pressures are similar between S013 and S016. Figure 5.18 shows the pressure signal for three shots at 30 bar with helium dilution of 70% and λ in the range of 1 to 1.24. Leaner mixtures are clearly slower and present a double slope signal, whereas the stoichiometric one does not. Lastly, Figure 5.19 depicts S032 and S033 shots, at 70 bar and 71% helium dilution. Shot S032 at $\lambda = 1.27$ is faster and has a higher peak pressure and stronger acoustic waves than shot S033 at $\lambda = 1.37$. These observations agree with the expected U-curve graph for $O_2:H_2$ equivalence ratio dependence for both compression ratio and flame velocity of [313, 315, 331].

Lee [320] studied the influence of $O_2:H_2$ equivalence ratio in detonations and concluded that transition to detonation occurs easier at near stoichiometric conditions. Our experiments agree with previous works,

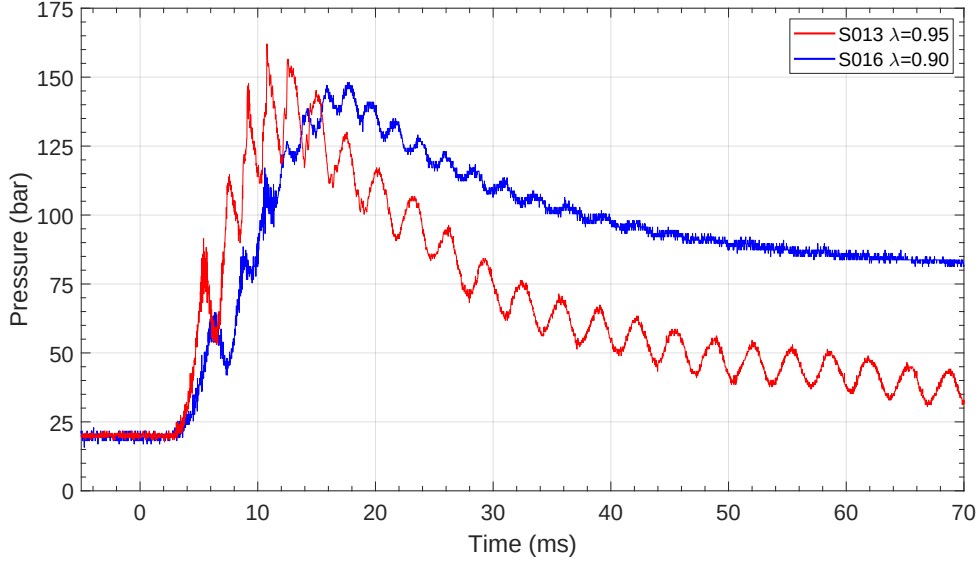


Figure 5.17: Influence of $\text{O}_2:\text{H}_2$ equivalence ratio on pressure signal comparison of shots at $p_0=20$ bar, $\mathcal{X}_{\text{He}}=65\%$, and 100 mm focal lens.

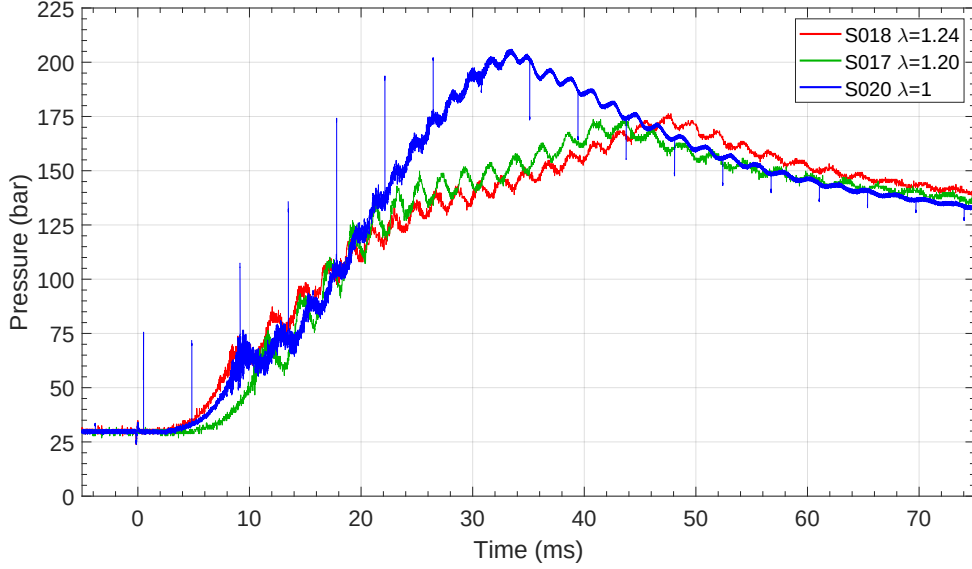


Figure 5.18: Pressure signal comparison for three shots $p_0=30$ bar, $\mathcal{X}_{\text{He}} \simeq 71\%$ and lens focal distance of 100 mm. For lean mixtures ($\lambda > 1$) a double slope signal is observed, meaning a transition to “tulip flame” occurred.

where rich and stoichiometric hydrogen mixtures detonate or transit to detonations more easily than lean ones. This is observable in Figure 5.20, where S054 detonates due to its richer mixture when S056 does not.

5.5.3 Pressure effects

Figure 5.21 illustrates the average flame velocity for the experimental shots, as function of pressure and helium dilution. The increase in filling pressure leads to a decrease in the average flame velocity. This is consistent with Burke et al. [323] which explains that for combustion with high dilution ratios, pressure has a non-monotonic influence on flame velocity. The majority of our performed shots have

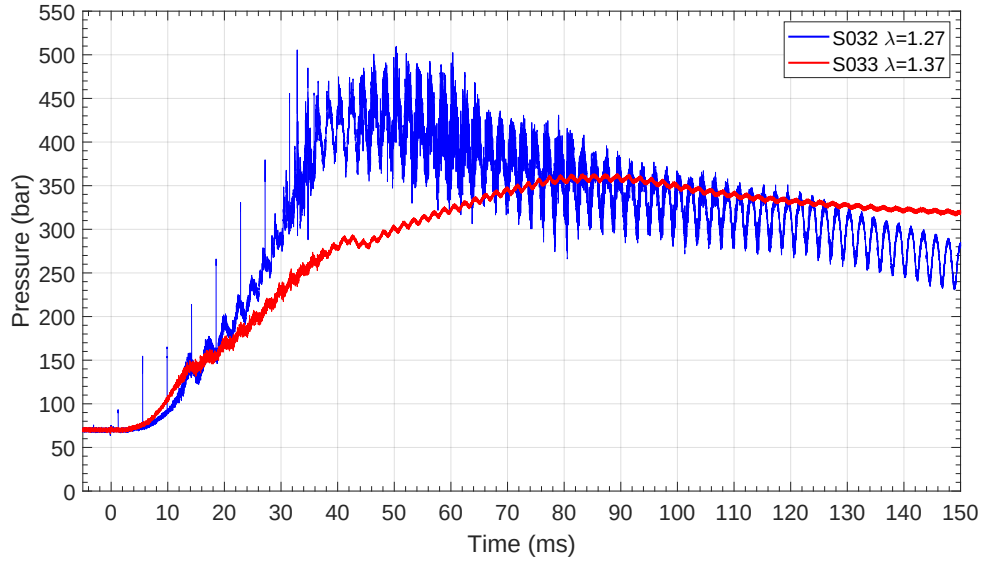


Figure 5.19: Influence of $\text{O}_2\text{:H}_2$ equivalence ratio on pressure signal comparison of shots at $p_0=70$ bar, $\mathcal{X}_{\text{He}}=71\%$. Dumping of the acoustic wave for the leaner mixture with $\lambda=1.37$.

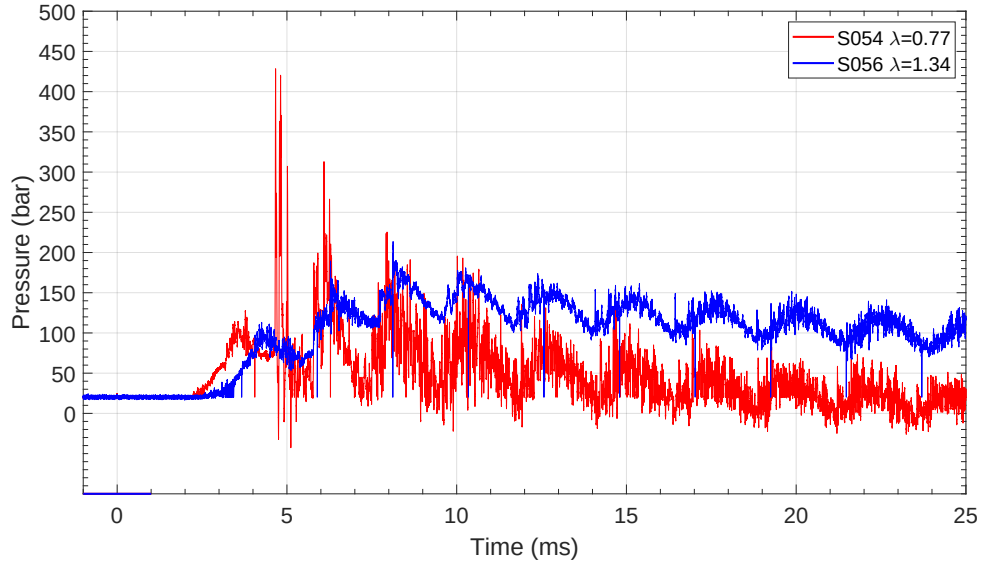


Figure 5.20: Pressure signal comparison for shots at $p_0=20$ bar, $\mathcal{X}_{\text{He}}=55\%$ and lens focal distance of 100 mm. A detonation is observable for S054 ($\lambda=0.77$) but not for S056 ($\lambda=1.34$).

dilutions above 65%, and always above 50%.

A comparison of the compression ratio for different pressures is shown in Figure 5.22. Due to the strong acoustic wave in some of the experiments, mostly nitrogen diluted ones, the peak pressure value was taken as the peak pressure of a moving average signal which could resemble a smooth pressure rise without the acoustic wave. Doing this helps eliminate the acoustic wave, which severely overestimates the peak in some cases. This is discussed in more detail in the following paragraphs. One may observe that the compression ratio does not significantly change with increasing pressure. This is in agreement with [313], where it was observed that the normalized signal for three filling pressures of 1, 2.5 and 5 bar did not significantly differ in its maximum value. The use of a focusing lens seems to slightly decrease the compression ratio, however, such a fact could be explained by the acoustic waves being stronger in non-

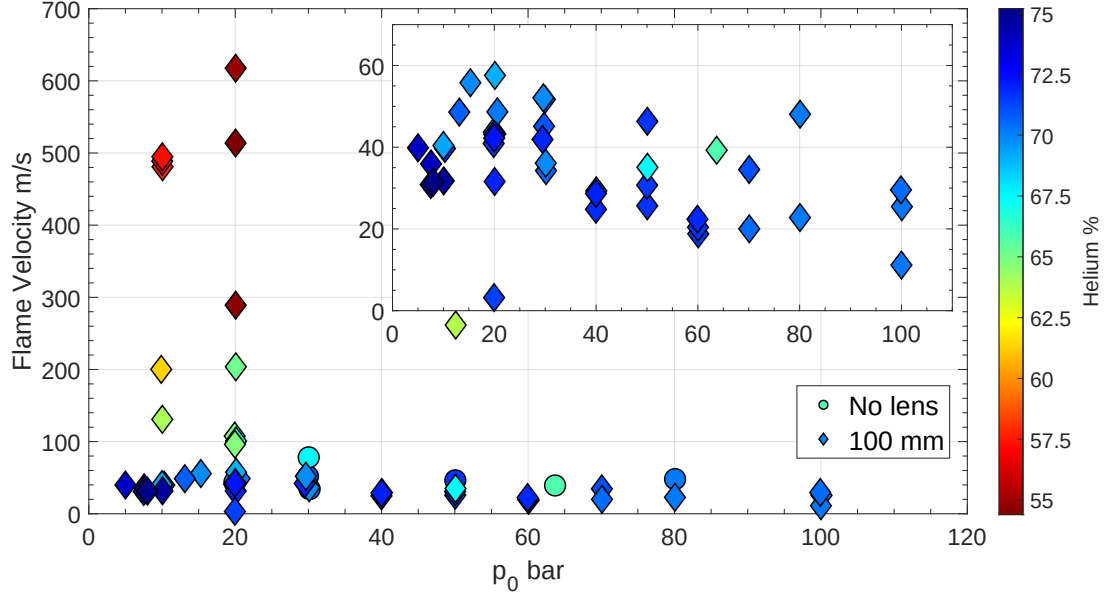


Figure 5.21: Scatter plot of the average flame speed function of filling pressure, helium dilution in colour-axis, focal lens distance in markers symbol.

focused mode. Further discussion on this topic is presented in the next subsection. Figure 5.23 compares the average flame velocity for the same shots of Figure 5.22. The unfocused ignition phenomenon is still not fully understood, therefore a clear picture of the flame propagation and velocity cannot be drawn. Looking only at the focused ignition, a clear trend is observed: the compression ratio is more or less constant and velocity decreases with filling pressure. A stronger trend is shown for helium diluted shots.

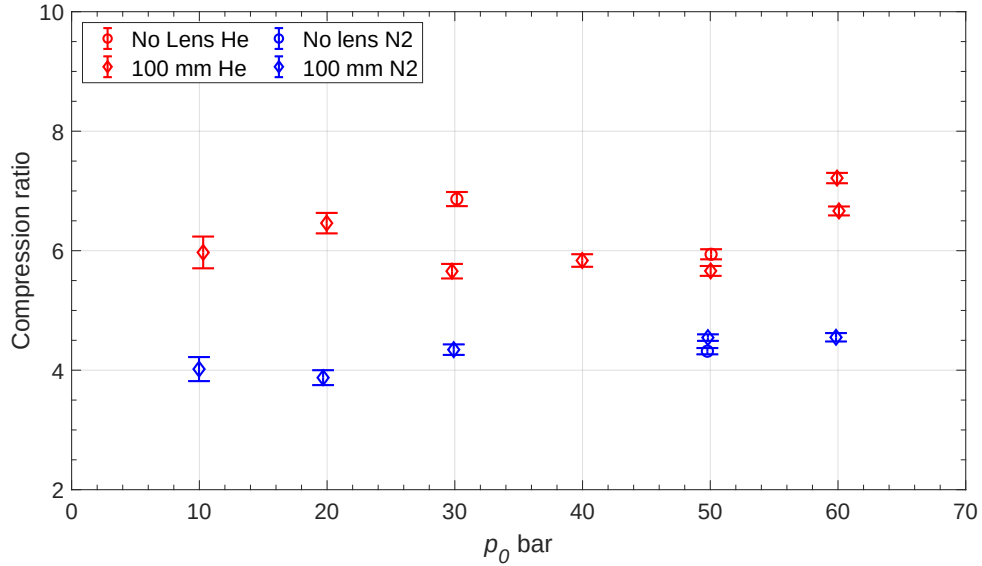


Figure 5.22: Compression ratio comparison for the “typical” $[X:H_2:O_2]$ 8:2:1.2 shot at different filling pressures.

Our observations for the slowing down of flame velocities are in agreement with the works of [315, 316, 322–326]. Since the presented shots have helium dilutions of about 71%, it was expected that increasing pressure would decrease the velocity. The explanation presented by [323, 324] is that the kinetic scheme changes from a chain-branching to a straight-chain one. Figure 5.24 compares the pressure signal for

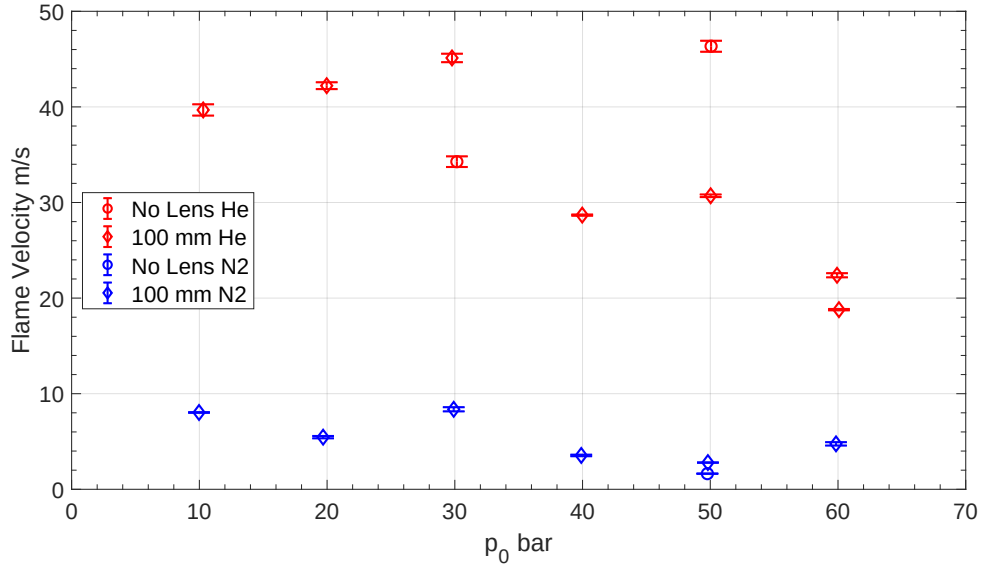


Figure 5.23: Average flame velocity comparison for the “typical” $[\text{He}:\text{H}_2:\text{O}_2]$ 8:2:1.2 shot at different filling pressures.

shots S017, S019 and S031 with a 8:2:1.2 $[\text{He}:\text{H}_2:\text{O}_2]$ mixture at different pressures. Both S017 and S028 evidence a “double” slope combustion, wherein the second slope starts at around $t = 25$ ms and $t = 30$ ms, respectively. The second slope is synonymous with a “tulip flame” formation as pointed by [364]. Shot S031 at a higher filling pressure, $p_0 = 60$ bar, evidences a single slope combustion.

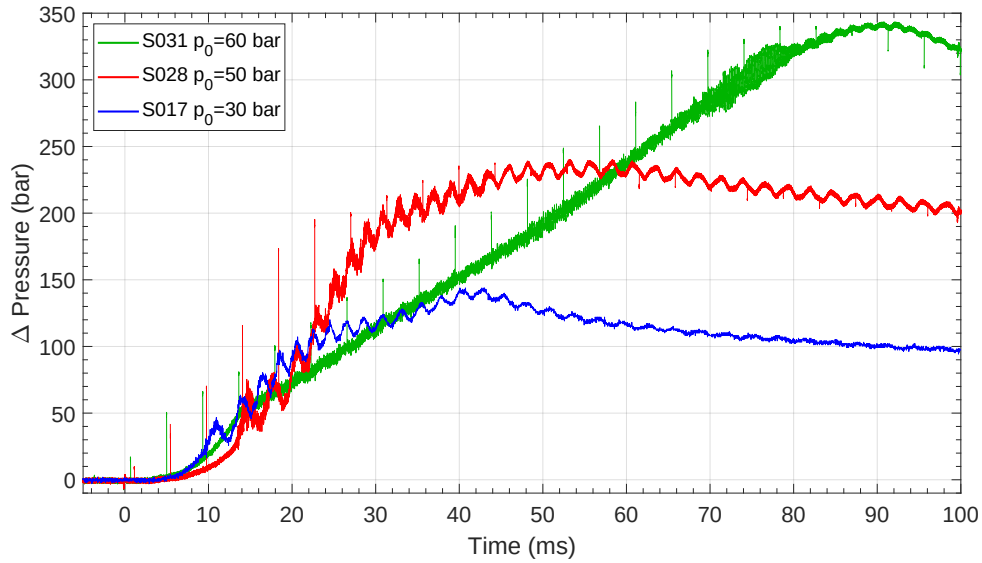


Figure 5.24: Pressure signal comparison of $[\text{He}:\text{H}_2:\text{O}_2]$ 8:2:1.2 shots at different filling pressures

Another observation was that increased pressure would create a more unstable mixture. When increasing pressure from 60 to 70 bar, the rise in acoustic waves mandate us to change the operational mixture from 8:2:1.2 to 8:2:1.3 to reduce said waves. Figure 5.25 depicts the mean acoustic wave, calculated as the absolute difference from the pressure signal to a moving average. On the right-hand side, the mean acoustic wave normalized to the filling pressure is depicted. As expected, higher filling pressures lead to increased amplitudes for the acoustic waves, however, from 60 to 70 bar a steep rise occurs. This

is the region where the operational mixture $\text{O}_2:\text{H}_2$ equivalence ratio needed to shift from 1.2 to 1.3.

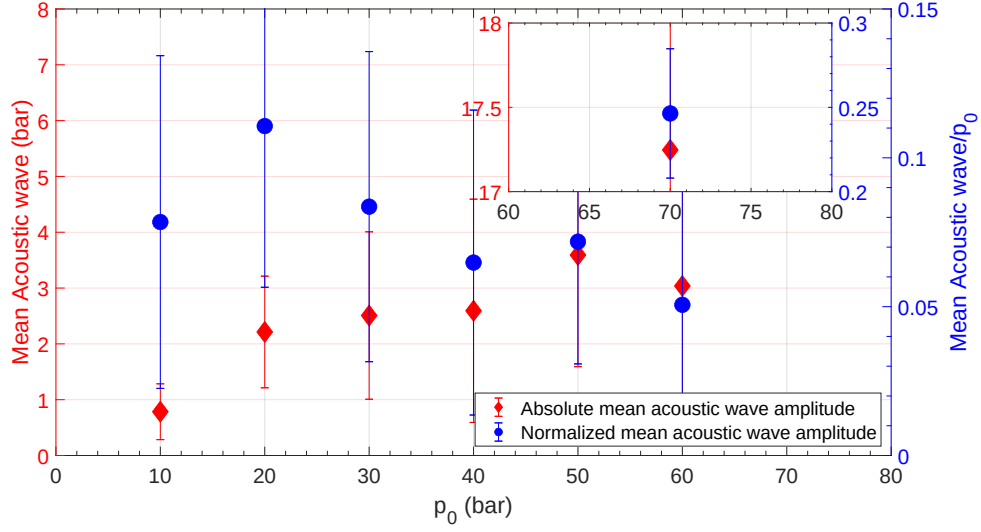


Figure 5.25: Influence of filling pressure on the mean amplitude for acoustic waves. Comparison of 8:2:1.2 $[\text{He}:\text{H}_2:\text{O}_2]$ mixtures.

Figure 5.26 compares two shots with similar chemical composition ($\lambda=1.40$, $\mathcal{X}_{\text{He}}=64\%$) at different pressures. The pressure signal S034 transits to a detonation at $t \approx 8$ ms due to its higher filling pressure. The explanation is that higher pressures have smaller detonation cells, making the mixture more detonation prone, as Knystautas et al. show in their work [365].

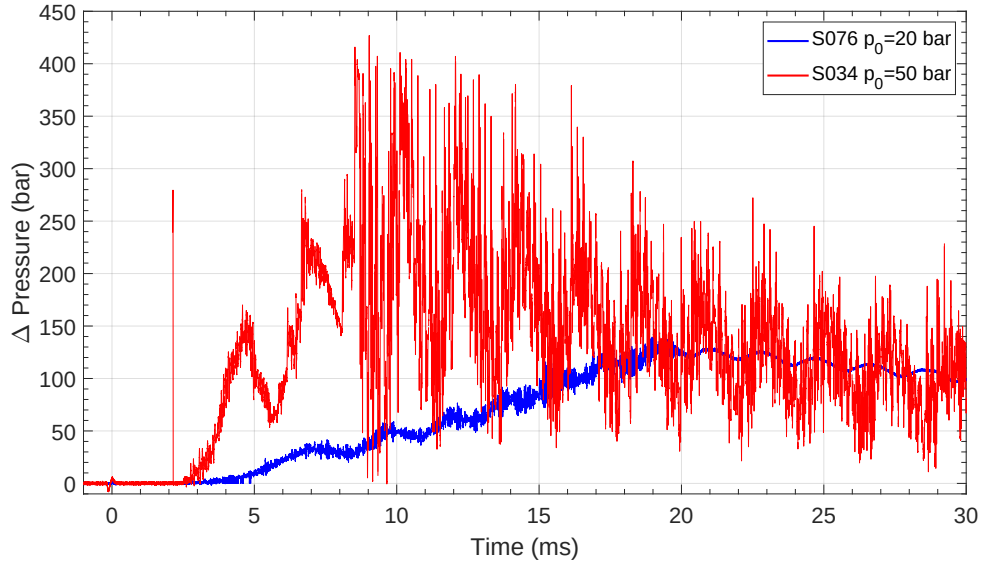


Figure 5.26: Pressure signal comparison for S034 ($p_0=50$ bar) and S076 ($p_0=20$ bar), $\lambda=1.40$, $\mathcal{X}_{\text{He}}=64\%$.

5.5.4 Influence of focusing

The laser ignition system ran in two modes, with a 100 mm bi-convex focusing lens and without any lens. Ignition with the lens occurs near the lens focal point at the ignition channel. For the case without the focusing lens, it is not possible to determine the exact ignition origin. The use of a lens is

necessary for $p_0 < 30$ bar; for $p_0 = [30-60]$ bar ignition may require multiple laser pulses; and $p_0 > 60$ bar ignition happens at the first laser pulse. Figure 5.27 depicts the pressure signal for shots S028 and S030, $p_0 = 50$ bar, $\lambda = 1.17$ and $\chi_{\text{He}} = 72\%$. S028 included the 100 mm lens, whereas S030 did not. The use of a focusing lens reduces the acoustic oscillations, yet increases the time to complete combustion. An explanation for this could lie in that unfocused ignition may create multiple ignition sources, where multiple flame fronts burn the gas and/or create more instabilities which in turn accelerate the burning rate. Another is that unfocused laser radiation ignites the gas along a line rather than at a single point, and therefore the characteristic burn length is the chamber radius and not its length. Further research is needed in this area and it may be unavoidable to include fast imaging diagnostics (Schlieren or other) through dedicated high-pressure optical ports. Figure 5.28 compares shots S046 and S047, $p_0 = 80$ bar, $\lambda = 1.39$ and $\chi_{\text{He}} = 70\%$, with unfocused and focused ignition, respectively. Similar conclusions to Figure 5.27 may be drawn, and also unfocused ignition leads to a higher peak pressure, and the explanation may lie in the faster combustion which reduces wall heat losses increasing the compression ratio. With this said, acoustic oscillations, stronger in the unfocused ignition case, will further mask the “true” peak combustion pressure in comparison to the focused one. The underlying mechanism explaining the unfocused ignition of high-pressure combustible mixtures is still not fully understood. The works of [327, 341, 380] show that increasing the filling pressure decreases the minimum pulse energy and that the electron cascade most likely starts with ionization of microparticles impurities such as dust or soot. A study of these effects in $[\text{He}:\text{H}_2:\text{O}_2]$ was done in the small scale “bombe” [381, 382], supporting this microparticle theory.

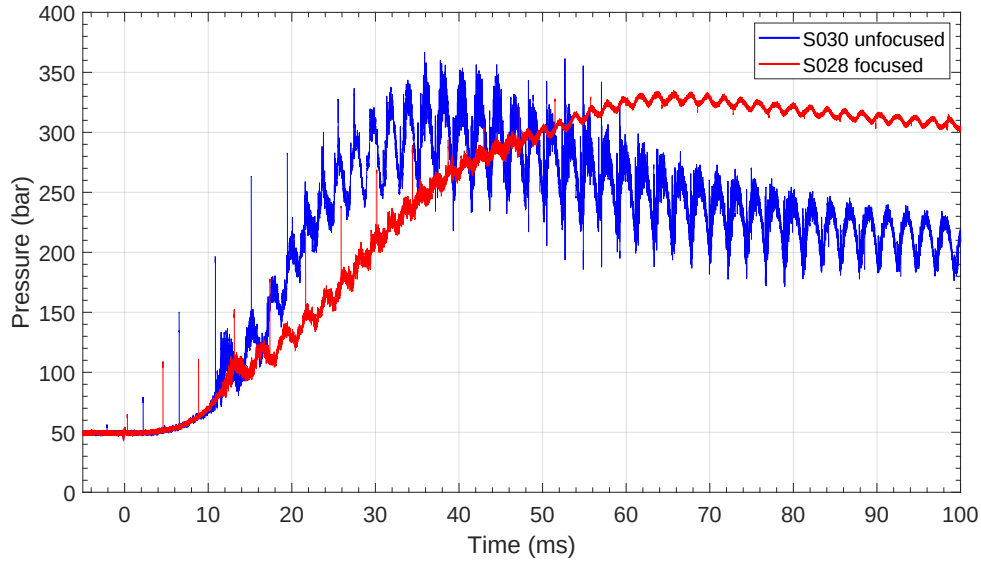


Figure 5.27: S028 and S030 pressure signal comparison. $\text{He}:\text{H}_2:\text{O}_2$ mixture, $p_0=50$ bar, $\lambda=1.17$ and $\chi_{\text{He}}=71\%$.

5.6 Shock Tube And Gas Gun (STAGG) Simulations

The qualification campaign yields an operational combustion chamber gas mixture composition for the filling pressure range 10–100 bar of the ESTHER driver, this mixture is summarized in Table 5.2.

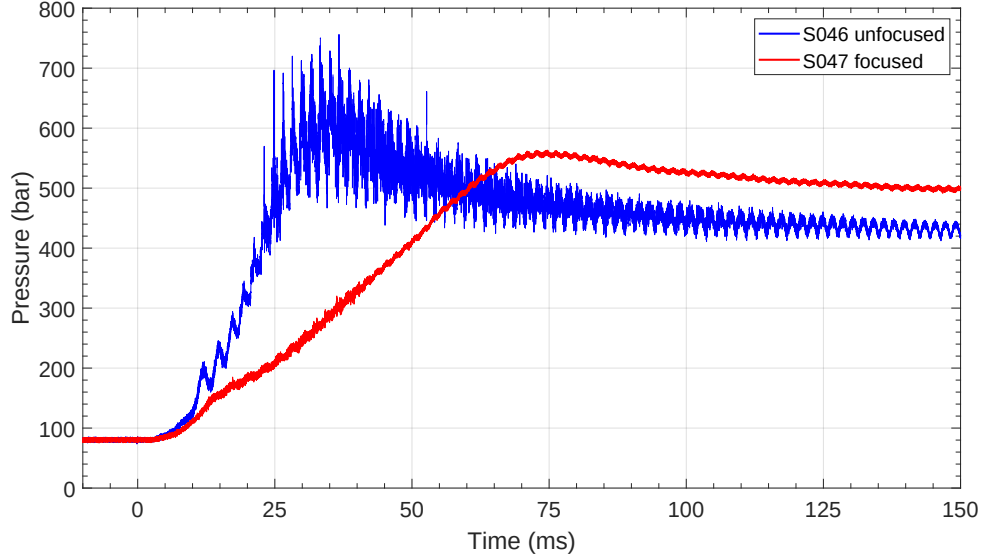


Figure 5.28: Shots S046 and S047 pressure signal comparison. He:H₂:O₂ mixture, $p_0=80$ bar, $\lambda=1.39$ and $\chi_{\text{He}}=70\%$.

From the post-combustion pressure, the gas temperature was computed using the ideal gas law 5.6. The gas composition was computed assuming complete combustion of the reagents. STAGG (Shock Tube And Gas-Gun) simulations were performed to estimate shock wave speed at the test region. Three combustion temperatures were considered for the performance analysis, 2,100, 2,250 and 2,650 K. The first two came from the Equation 5.6, whereas the latter from the an analysis report [269]. The 2,650 K driver temperature was taken from the average values of Hendershot (2,800 K) [383] and LeBoucher (2,500 K) [384]. The 2,800 K value comes from the development of the Convair Shock Tunnel, which accounts for the thermal losses and combustion inefficiency. The work of LeBoucher for the two-stage CEA shock tube assumed a temperature of 2,500 K. Using data from the VUT-1 shock tube at MIPT [247] and a driver temperature of 2,650 K, STAGG was calibrated to reproduce the results of the ESA campaign. The first operational results data should also be used to re-calibrated the STAGG code for ESTHER. An integral validation and update of ESTHER performance should be carried out by the end of the first experimental campaign.

5.6.1 Driver temperature

The first test was to understand the influence of driver gas temperature on the expected performance. For it, the code ran in optimization mode, where it optimized gas pressure at the compression tube (the intermediate stage) to achieve maximum velocity at the test region. The simulation variables are the driver gas temperature, filling/peak pressure, compression tube losses model, and the test gas composition. Ranges for these are shown in Table 5.3,

The temperature of the driver gas right before the diaphragm rupture is dictated by the post-combustion temperature of the combustion chamber. Since the pressure sensor measures a transient pressure, the temperature values from Equation 5.6 may not reflect reality. The driver pressure values were taken from the experimental campaign shots at filling pressures 10, 50, 80 and 100 bar. An adjust-

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Table 5.3: STAGG variables at test for high shock speed scenarios. Driver mixture is $[\text{He:H}_2:\text{O}_2]$ 8:2:1.2 for 10 and 50 bar, and 8:2:1.4 above it.

Driver Temperature (K)	2,100	2,250	2,650	
Driver/CC filling pressure (bar)	10	50	90	100
Driver post-combustion pressure (bar)	61.50	334	600	660
Compression tube Losses	Yes		No	
Shock Tube gas composition	Earth	Mars	Titan	Gas Giants

ment to the driver gas mixture from $[\text{He:H}_2:\text{O}_2]$ 8:2:1.2 to 8:2:1.4 was made for filling pressures above 70 bar. This led to an increase of the gas mean molar mass, which lowers the peak performance. The gas parameters in the Driver and ST sections were generated by a real gas model from the CEA (Chemical Equilibrium with Applications) code [385]. The current version of the STAGG code can only run an ideal gas model for the CT section. To simulate the viscous losses at the CT section two different models were used, the first is derived from the work of Mirels, and the second neglects any viscous effects. Finally, four different gas mixtures were used in the shock tube test region. The code ran for shock tube section filling pressures of 0.1 to 1 mbar.

Figure 5.29 shows the influence of driver gas temperature and pressure on shock wave velocity. As stated, increasing the driver temperature leads to faster shock waves. It is observed that for a fixed filling pressure (color) the diamond markers (2,650 K) is always to the right of the triangles (2,250 K) and circles (2,100 K). Higher filling pressure creates stronger pressure discontinuities leading to faster shock waves. For the experimental upper limit of a filling pressure of 100 bar (660 bar post-combustion), the shock wave velocity for Earth conditions is between 11 km/s (2,100 K) and 14 km/s (2,650 K).

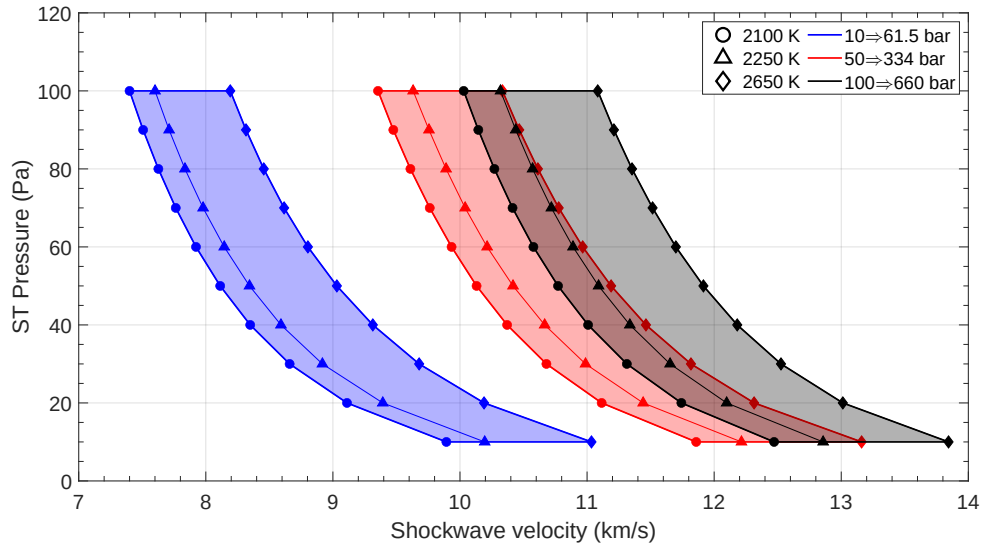


Figure 5.29: STAGG results for different driver temperatures and pressures in Earth's atmosphere ($\text{N}_2\text{-O}_2$).

5.6.2 Compression losses model

Figure 5.30 compares the two simulations with and without compression tube viscous losses at a fixed temperature (2,650 K) and varying pressures for different atmospheres. As expected, losses in the

compression tube will lead to a decrease of the shock wave velocity at the test region. The compression tube losses are modelled after Mirels' work [30]. The losses are computed using a viscosity constant in the input file, or from the real gas file [286]. Because Mirels' theory assumes a asymptotically long compression tube [30], the pressure losses model is modified to account for this by increasing its effect. The exponent in the pressure loss term $(x/D)^{0.8}$ increases to 1 [386]. In sum, the losses model and its implementation are very conservative thus may overestimate the drag effects and underestimate ESTHER performance.

With these losses, the performance decreases by up to 30% at the lower shock tube pressures. Another difference is the shock wave vs. ST pressure plot shape. Without losses there is a monotonic dependence on ST pressure, as increasing ST pressure decreases the shock wave velocity. However, this is not observed in all the cases of Figure 5.30 for the higher driver pressures. Taking a particular look at the simulation in the Earth's atmosphere, shock tube pressures of 100 Pa can reach shock wave velocities of 7.8 km/s, yet at shock tube pressures of 60 Pa, the maximum shock wave velocity is 7.6 km/s. Similar conclusions can be drawn for other atmospheric mixtures.

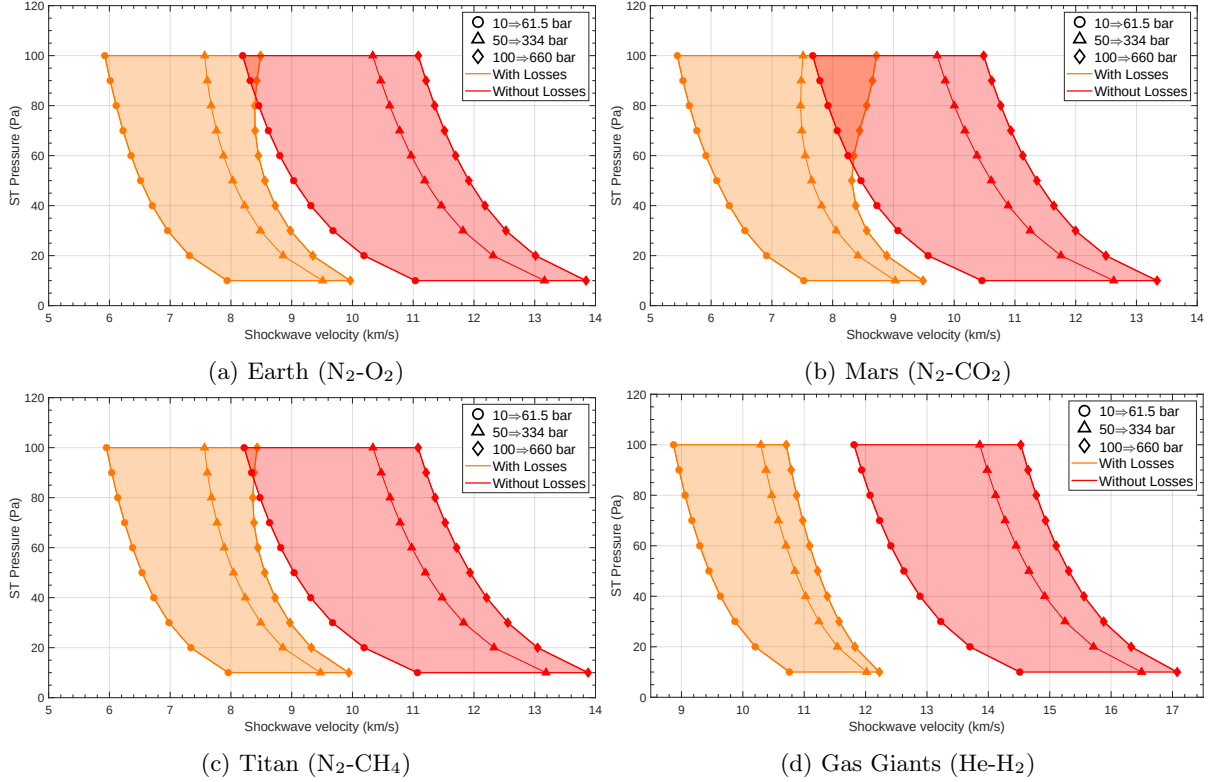


Figure 5.30: Effect of compression tube losses model in STAGG results for ESTHER optimized performance in different atmospheres. Driver gas mixture $He:H_2:O_2$ 8:2:1.2-1.4 at 2,650 K.

5.6.3 Low velocity entries

ESTHER is designed to also operate at lower entry velocities, namely Earth return from orbit or entries in Mars and Titan atmospheres. These velocities are obtained using a driver mixture of $N_2:H_2:O_2$ instead of $He:H_2:O_2$, which affects the shock wave velocity threefold, first by increasing the mean molar

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mass of driver gas, then by reducing the combustion efficiency, lowering both the gas peak pressure and temperature. Due to the combustion effects of nitrogen diluted mixtures, the nominal mixture $\text{N}_2:\text{H}_2:\text{O}_2$ is 10:2:1.4, mostly to prevent/reduce unwanted pressure waves during combustion. Also, the maximum filling pressure in nitrogen diluted mixture is 50 bar, because, above it, the acoustic perturbations are too high for a controlled diaphragm rupture. Further, there is no reason to use nitrogen as a diluent gas in such pressure ranges more suited for high-speed shots. The test variables for low speed entry conditions are presented in Table 5.4.

Table 5.4: STAGG variables at test for low entry velocity mixture $\text{N}_2:\text{H}_2:\text{O}_2$ [10:2:1.4]

Driver Temperature (K)	1,250		
Driver/CC filling pressure (bar)	10	30	50
Driver post-combustion pressure (bar)	38	114	190
Compression tube losses	Yes	No	
Shock Tube gas composition	Earth	Mars	Titan

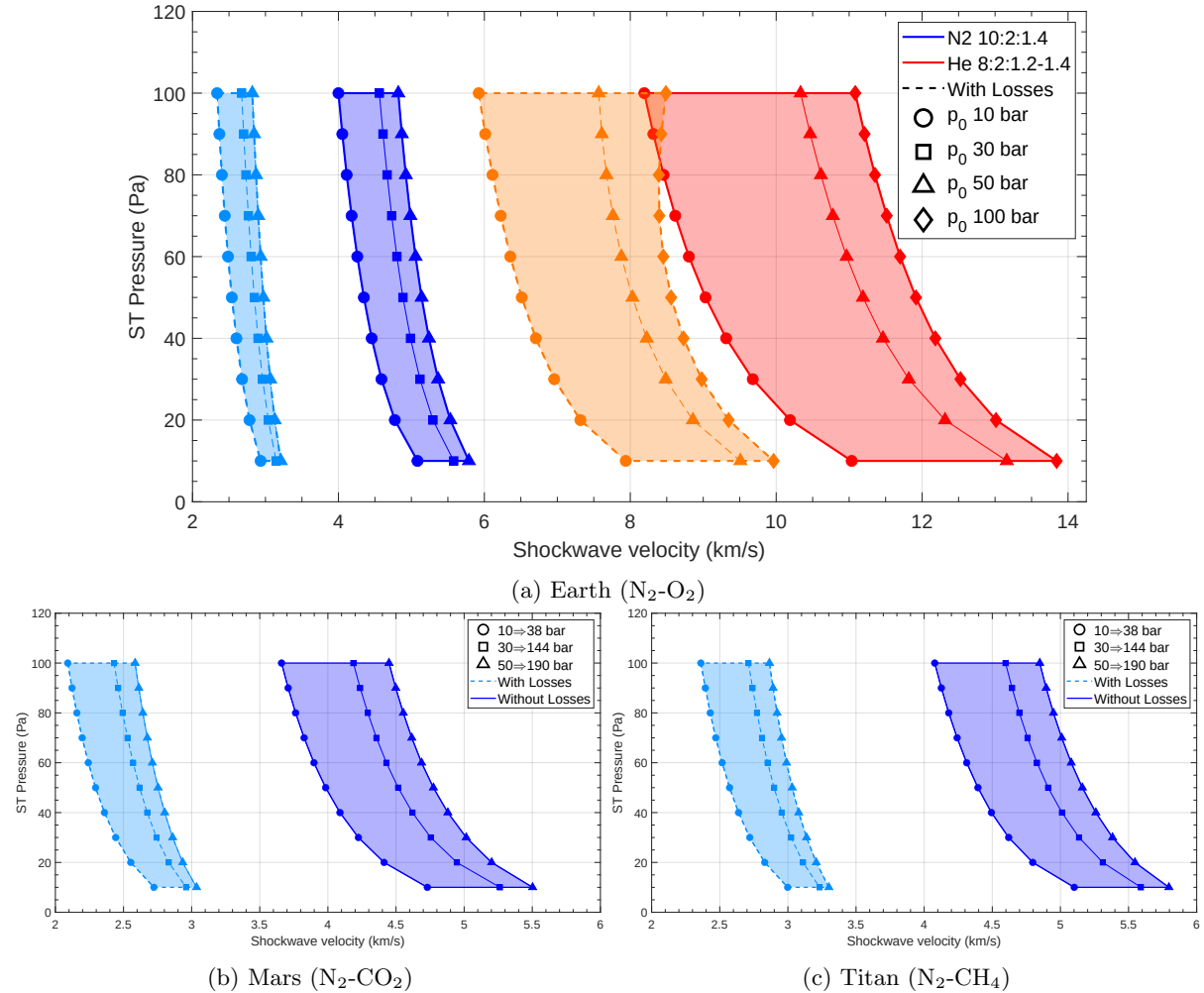


Figure 5.31: Results for ESTHER optimized performance in different atmospheres. Driver gas mixture $\text{N}_2:\text{H}_2:\text{O}_2$ [10:2:1.4] at 1,250 K.

The boundary layer effects of the Mirels' model are greater in nitrogen driver mixtures than in helium diluted mixtures (43% vs. 30%), as shown in Figure 5.31. Typical shock wave velocities are in the range

of 4-5.5 km/s (2.3-3.2 with losses km/s) in Earth conditions, lower for Mars and Titan atmospheres. However, the shock wave velocity presents a performance gap between 5.5 and 10 km/s for the lower ST pressures. To close this performance gap, simulations were computed using a He:H₂:O₂ mixture at 10:2:1.4-1.6, and with filling pressures of 5 and 10 bar. By increasing the amount of oxygen in the mixture, the combustion compression ratio will be lower as well as having a heavier molecular weight. Alongside it, decreasing the filling pressure will lower the absolute peak pressure at the driver. ESTHER optimized performance range is depicted in Figure 5.32, with and without CT losses. The figure shows the maximum shock wave velocity for different driver mixtures and pressures, with CT filled with helium. The effective performance is somewhere in between, as the Mirels model overestimates the energy losses at the boundary layer [30].

Performance with the N₂ diluted driver mixture cannot exceed 5.8 km/s, as the maximum filling pressure pre-combustion should not exceed 50 bar, besides it, the mixture has a lower compression ratio due to a higher air:fuel equivalence ratio (1.4 vs. 1.2) and dilution (71% vs. 74%), with nitrogen having a larger dilution effect (lower compression ratio and velocity) when compared to helium. All these factors limit the performance of nitrogen driver mixtures.

5.6.4 Moderate velocity entries

To have velocities between the nominal 8:2:1.2 helium mixture and the nominal 10:2:1.4 nitrogen mixture a twofold approach was taken. First, by reducing the driver's initial pressure from 10 to 5 bar, and second by changing the mixture from 8:2:1.2 to 8:2:1.4-1.6. Considering the combustion efficiency remains constant, as observed by the qualification campaign results, the driver pressure decreases from 60 to 30 bar. Increasing the amount of oxygen in the driver gas will have the side effect of lowering the overall combustion efficiency. Since these mixtures were not studied during the validation campaign, we use the reasonable assumption that the compression ratio and gas temperature remained similar between 8:2:1.2 to 8:2:1.6. In fact, increasing the O₂:H₂ equivalence ratio from 1.2 to 1.6 can only lower the compression ratio, as it deviates from a stoichiometric mixture.

Comparing the filling pressure reduction and the change of mixture effects, the former has a stronger effect on reducing shock wave velocity, since the increase in molecular weight is only around 4% from 7.30 g/mol to 7.56 g/mol. Nonetheless, the effect of increased oxygen may be underestimated as stated before. With 5 bar filling pressure, the maximum shock wave speed is between 7.3 and 10.2 km/s. Using these driver conditions we may “close” the performance gap in shock wave velocity. To further assess the performance limitations in this middle region, STAGG simulations were performed with a fixed driver, compression and shock tube conditions. The chosen test conditions are shown in Table 5.5.

Figure 5.33 depicts the results of STAGG simulations with fixed conditions in Earth's atmosphere using a driver mixture of He:H₂:O₂ [8:2:1.6] at p_0 of 5 and 10 bar (30 and 60 bar post-combustion). On the top figure, due to the compression tube losses, some points did not converge to a solution, thus being removed. These occur at higher compression and shock tube pressures. From the results, one can see that the performance gap can now be “reduced”. The results range from STAGG optimization and fixed conditions mode runs can be checked in Table 5.6. After the first experimental shots in ESTHER, the

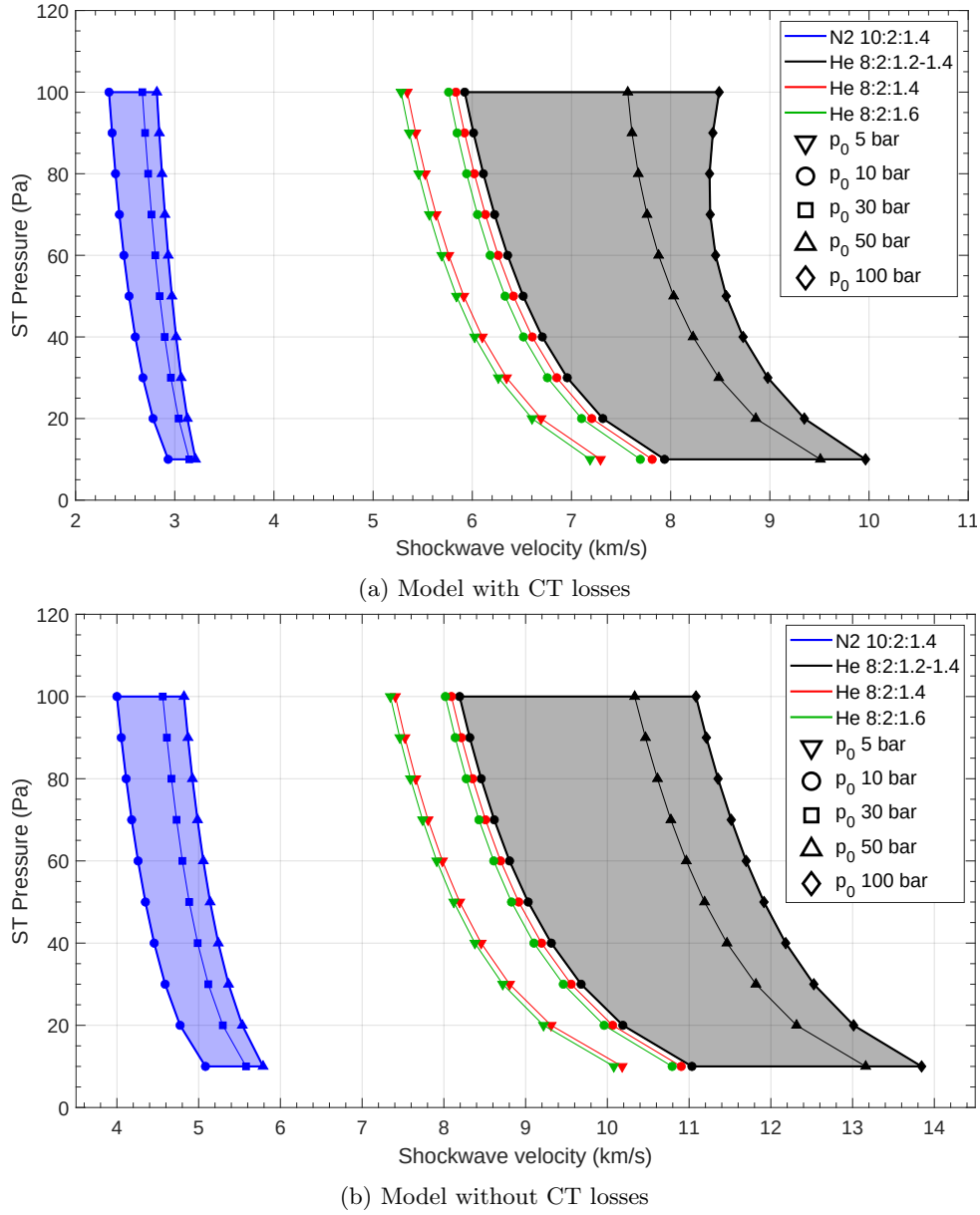


Figure 5.32: ESTHER performance range Earth ($\text{N}_2\text{-O}_2$), with and without CT Losses

Table 5.5: STAGG parameters for middle shock wave velocity region (5-9 km/s) testing. Helium in compression tube and Earth atmosphere ($\text{N}_2\text{-O}_2$) for shock tube.

Driver Temperature (K)	2,650				
Driver/CC filling pressure (bar)	5	10			
Driver post-combustion pressure (bar)	30	60			
Compression tube Losses	Yes	No			
Compression tube pressure (mbar)	20	50	100	500	1000
Shock tube pressure (Pa)	10	30	50	70	100

calibration values for the CT Losses may be refined to give a more trustworthy result. Oddly, the fixed conditions mode had a higher performance than the optimization mode for $\text{He:H}_2\text{:O}_2$ [8:2:1.6] $p_0=10$ bar driver mixture (5.6-7.3 vs. 5.3-7.3 and 5.9-8.0 vs. 5.8-7.8).

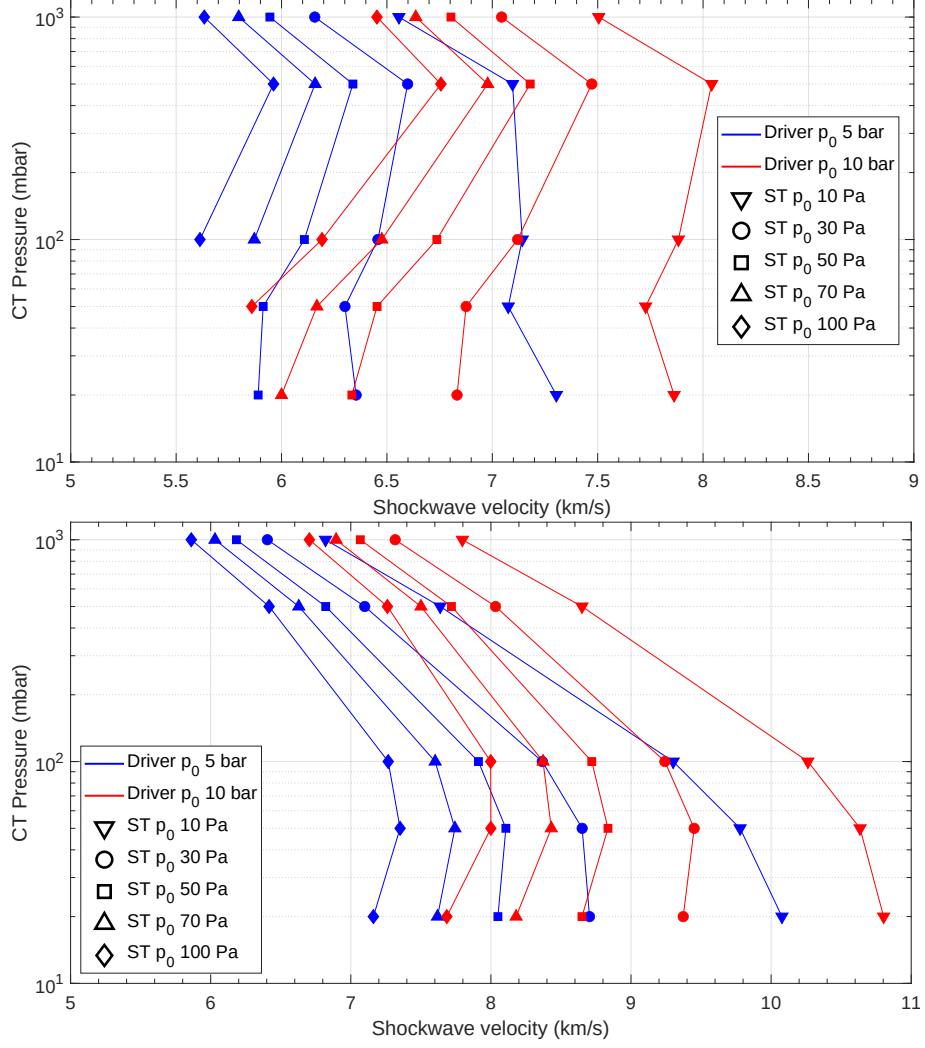


Figure 5.33: ESTHER STAGG simulation for fixed conditions, slow entry velocity Earth atmosphere (N_2 - O_2), with and without CT Losses. $p_0 = 5$ and 10 bar, $He:H_2:O_2$ 8:2:1.6

Table 5.6: STAGG middle region, helium driver fixed condition (top) and optimization mode (bottom) simulation runs results on Earth atmosphere (N_2 - O_2). Driver gas temperature fixed at 2,650 K.

Mixture Molar $He:H_2:O_2$	Driver pressure filling-peak (bar)	Shock Speed (km/s)	
		With CT Losses	No CT Losses
8:2:1.6	5 - 30	5.6 - 7.3	5.8 - 10.0
8:2:1.6	10 - 60	5.9 - 8.0	6.7 - 10.8
8:2:1.6	5 - 30	5.3 - 7.3	7.3 - 10.2
8:2:1.6	10 - 60	5.8 - 7.8	8.0 - 10.9

5.6.5 Updated Performance Map

The complete performance map of ESTHER is shown in Figure 5.34, and the details of each region are in Table 5.7. The lowest velocity region is achieved using the nitrogen diluted mixture, corresponding to velocities below 6 km/s. The middle section, between 6 and 11 km/s, is achieved using a non-optimized compression tube gas pressure. By tuning the compression tube pressure, one can adjust the shock wave at the testing region. Non-optimization used 20, 50, 100, 500 and 1000 mbar for compression tube

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region pressure. An extrapolation of the driver post-combustion parameters, at a filling pressure of 5 bar, was done to study a lower performance mixture. The maximum performance region, above 10 km/s, is obtained by optimizing the compression tube gas pressure and maximizing driver filling conditions. A small gap is still present in the performance map. To “close it”, two approaches may be taken. One is using the normal helium diluted mixture driver and replacing the helium in the compression tube with a heavier gas, such as nitrogen or argon. The other is using a hybrid He-N₂:H₂:O₂ combustible mixture. The driver performance should be in between the two regions. Nonetheless, an experimental performance map must be created to completely understand the driver operational input and output conditions.

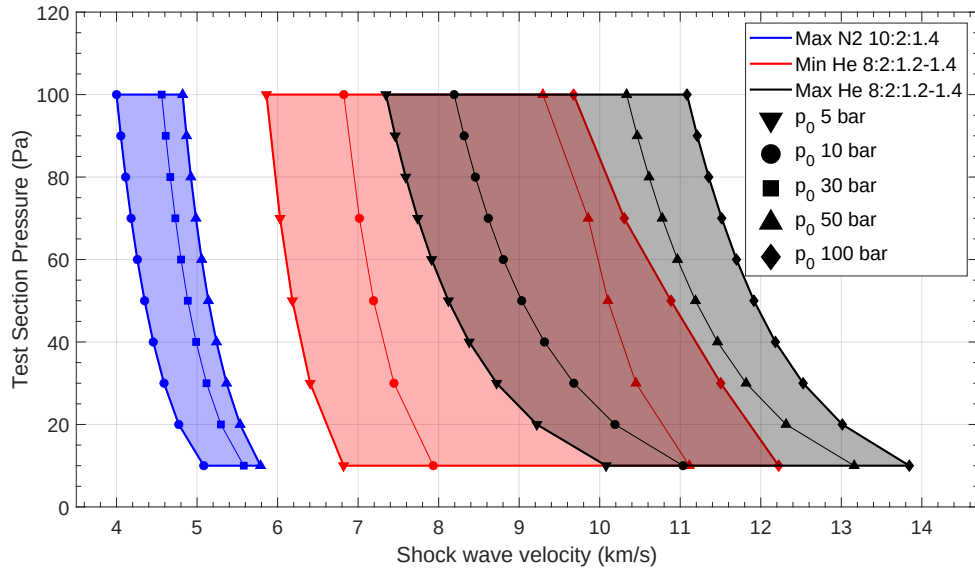


Figure 5.34: ESTHER Performance map for Earth atmosphere (N₂-O₂).

Table 5.7: STAGG parameters for Earth (N₂-O₂) performance map simulation. Driver input conditions and STAGG running mode (optimization of CT pressure or simple computation) without any CT losses.

Mixture molar ratios X:H ₂ :O ₂	Driver pressure filling-peak (bar)	Driver temperature (K)	STAGG mode	Shock wave velocity (km/s)
N ₂ 10:2:1.4	10 - 38	1,250	Optimization	4.0 - 5.1
N ₂ 10:2:1.4	30 - 114	1,250	Optimization	4.6 - 5.5
N ₂ 10:2:1.4	50 - 190	1,250	Optimization	4.8 - 5.8
He 8:2:1.6	5 - 30	2,650	Non-Optimization	5.8 - 10.0
He 8:2:1.6	10 - 60	2,650	Non-Optimization	6.7 - 10.8
He 8:2:1.6	5 - 30	2,650	Optimization	7.3 - 10.2
He 8:2:1.6	10 - 60	2,650	Optimization	8.0 - 10.9
He 8:2:1.2	10 - 61.5	2,650	Optimization	8.2 - 11.0
He 8:2:1.2	50 - 334	2,650	Optimization	10.3 - 13.2
He 8:2:1.4	100 - 660	2,650	Optimization	11.1 - 13.8

5.7 Outcomes

The ESTHER driver underwent a qualification campaign, where a series of full-scale combustion tests analyzed the combustion behaviour of high-pressure diluted hydrogen-oxygen mixtures in a closed-volume chamber. The parameters tested were filling pressure 10–100 bar, oxygen-hydrogen equivalence ratio 0.45–2.04 and inert gas dilutions of 54–79%, both with helium and nitrogen. The ignition system was a Nd:YAG high-power laser with a 100 mm bi-convex lens which could be unmounted at will. The effects of studied variables are summarized in Table 5.1. The filling pressure does not have a significant effect on compression ratio or induction time, in agreement with [313]. Nonetheless, it has a negative effect on flame velocity as a consequence of the high dilution factor, as in [324]. The acoustic wave formed inside the chamber is stronger at larger filling pressures, with a steep rise after 60 bar. Similarly, the mixture is more detonation prone at higher pressures in agreement with the finds of [365, 366].

Helium dilution lowers the compression ratio and flame speed, in concordance with [313]. Increasing dilution will also lower the acoustic wave amplitude and the detonation probability of the mixture. When nitrogen is used, the compression ratio, flame velocity and detonation probabilities are further reduced. The results are in agreement with [323]. However, our experiments show that the acoustic wave mean amplitude increases when nitrogen is used as a diluent.

The removal of the 100 mm focusing lens leads to an indetermination of the exact ignition point. Ignition may occur at multiple points along the chamber, which reduces the combustion length. These faster flames generate stronger acoustic waves and are more detonation prone than the focused ignition. It taking may be explain the flame front e ignition in the ignition channel also reduces the likelihood of detonation propagating into the main chamber. The effects of the input parameters are summarized in Table 5.1.

The data from the combustion experiments served as input in STAGG numerical code, which predicts the ESTHER shock wave velocity at different conditions. The result is ESTHER’s performance map, depicted in Figure 5.34. Medium and high velocity shots made use of the He:H₂:O₂ mixture in the driver. ESTHER is expected to achieve shock wave velocities in air close to 14 km/s using He:H₂:O₂ mixtures at filling pressures around 100 bar. Low velocity shots are to be performed with N₂:H₂:O₂ mixtures since these have a lower compression ratio, gas temperature, and heavier molar mixture when compared to He:H₂:O₂. In sum, ESTHER should be able to cover the 4 to 14 km/s range (in air) by tuning its driver and compression tube chemical composition and pressure. Table 5.7 summarizes the operational parameters needed to cover the shock velocity experimental range.

Chapter 6

ESTHER Vacuum Systems

“Beatings will continue until morale improves.”

-Unknown author

The current chapter presents a short overview of vacuum physics and technologies, focused on its application to ESTHER. An in-depth analysis of the ESTHER vacuum system is done, followed by a discussion on its design choices, shortcomings, testing, and how to improve it.

A shock tube facility has to deal with a wide range of pressures across it. The driver pressures can go above 1000 bar, whereas the shock tube pressure may fall down to around 10^{-6} mbar (12 orders of magnitude difference) for cleaning purposes. These different pressures must hold in different sections through the diaphragms. ESTHER pressure requirements for the different sections are summarized as:

- Combustion driver - capable of withstanding over 1000 bar as the result of detonations and transient pressure waves;
- Compression tube - moderate to low pressures, from 1 bar down to 10^{-2} mbar region;
- Shock tube - low to very low pressures, down to 10^{-6} mbar;
- Dump tank - moderate to low vacuum, below 10^{-2} mbar.

Additionally, one must consider the transient pressure caused by the moving shock wave and its reflections, and the tube overpressure at the end. They are not to exceed:

- Compression tube - 35 bar of transient pressure, 70 bar of maximum reflected wave pressure [300];
- Shock tube - 10 bar for passing shock, 20 bar for the reflected wave [300];
- Dump tank - 6 bar of overpressure, from hot gas equilibrium pressure [301].

The ultimate vacuum level, for each section, is determined by an equilibrium between the pumping speed of the vacuum pumps and the total leakage rate of the system. The latter is affected by various parameters, namely the seals, outgassing of the metallic walls, vessel leakage, back pressure and permeation, which will be discussed in more detail ahead.

6.1 Application to ESTHER

6.1.1 Gas contamination and cleaning process

As discussed before, ESTHER has a focus on cleanliness¹ to ensure a high-quality signal-to-noise ratio in the acquired data. The facility walls, with an emphasis on the test section, are to be clean of any contaminants. Carbon molecules are strong contaminants in radiation due to the broad IR emission spectra, which may hide other emission lines. The largest sources of contaminants are oils from human handling and machining. Oils and grease have a vapour pressure in the working range of our facility, thus neither being easily pumped out by the vacuum pumps nor remaining adsorbed in the walls. For reference, the vapour pressure of mineral oil is typically above 100 Pa at room temperature [387], whereas the vapour pressure for water is significantly higher, at around 2000 Pa. Cleaning is done by scrubbing the inner walls of the tube with a cotton cloth soaked in NCH NSL 01, a solvent degreaser for the food industry, which does not leave residues. This process is repeated until the cloth is no longer dirty or stained. Figures D.2 and D.3 show photographs of the cleaning equipment and a demonstration of the process on the compression tube.

The gas contamination degree is calculated as the pressure ratio between the vacuum pressure and test gas pressure before the start of the experiment. Typical experimental shots operate at a test gas pressure in the 10 to 100 Pa region. Between closing the gate valves and stopping the turbomolecular pumps about 5 minutes elapse, during which the test gases are injected into the shock tube. If a contamination factor of 1% is desired, for the lowest shock tube test gas pressure case of 10 Pa, the pressure before the gas injection should not exceed 0.1 Pa, or 10^{-3} mbar. Ideally, a contamination factor of 0.1% is desirable, which would imply the vessel to steadily hold a vacuum pressure of 10^{-4} mbar. Ultimately, the proper contamination may only be assessed on a case-by-case basis by comparing obtained spectra for identical test conditions, but with different contamination ratios for the test-gas.

6.1.2 Compression Tube

The ESTHER compression tube has a total of five access ports, with one for high-vacuum pumping near the connection to the combustion chamber. Since this opening would be subjected to very high pressures/forces due to being near the combustion chamber diaphragm and having a large diameter (63 mm), it was decided to seal off the port and manufacture a new port for the vacuum pumping and gas filling system. An ad-hoc solution was designed and manufactured, sealing the port with two Fluorine Kautschuk Material (FKM) O-rings, however, the very tight geometric tolerances caused some leakage. An additional custom-made large copper ring was added, allowing for the leakage to subside. Figure 6.1 depicts the Computer Aided Design (CAD) drawing of the compression tube high-vacuum port, and two photographs of the sealing copper ring, and sealed port.

A small vacuum pumping port for the compression tube was designed using one of the normal plug ports. A 5 mm diameter, 61.5 mm depth hole was drilled into the plug, and connected to a modified ISO KF-25 elbow connector, illustrated in Figure 6.2. The pump for the compression tube section is

¹evidenced by the laser ignition system and the surface roughness level

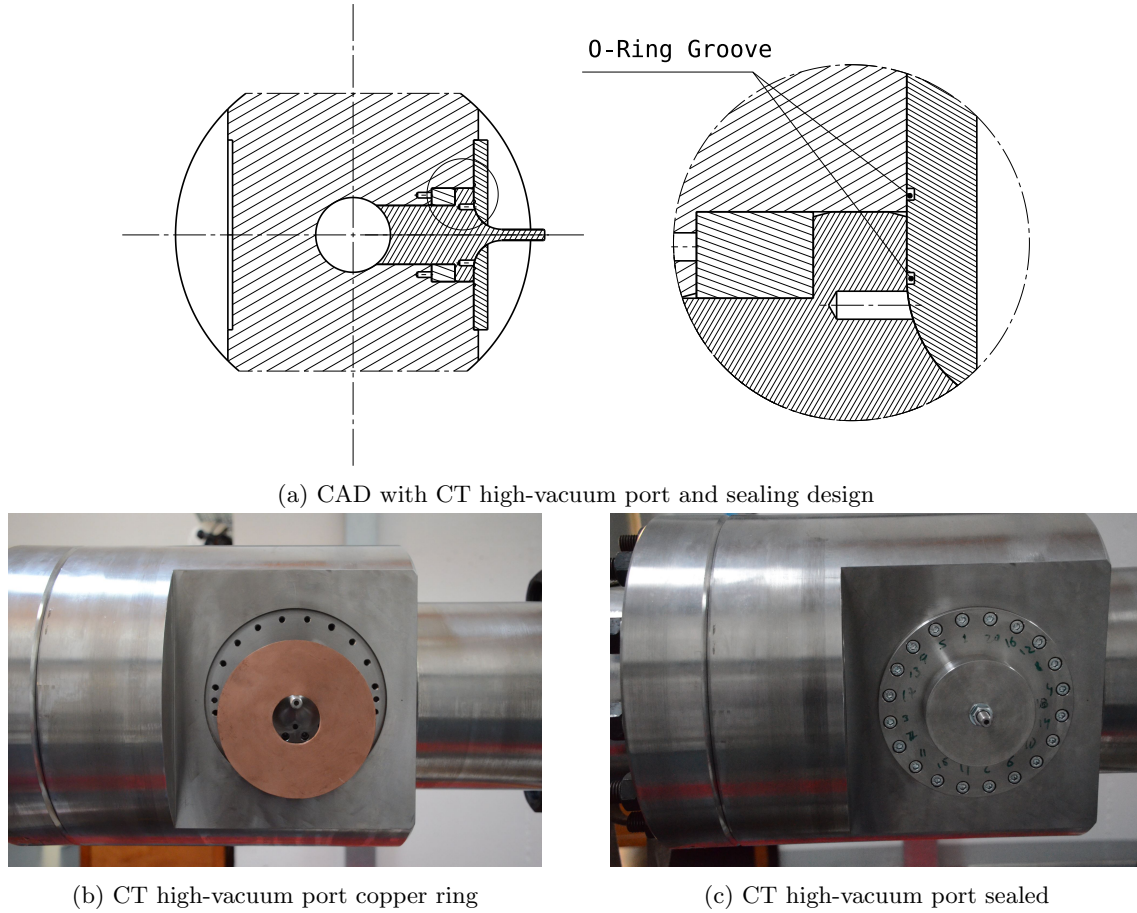


Figure 6.1: Compression tube high-vacuum port. CAD design (top), and photographs of the sealing (bottom).

an Edwards GX100L model capable of an ultimate pressure of 5×10^{-3} mbar and a pumping speed of 20 l s^{-1} . Assuming a final pressure of 1 mbar, and through Equations B.10 and B.11, one gets a conductance of 1.349 and $2.492 \times 10^{-1} \text{ l s}^{-1}$, for laminar and free molecular regime, respectively. Now, using Equation B.13 and B.12, the effective pumping speed is 1.289 and $2.470 \times 10^{-1} \text{ l s}^{-1}$, which yields a pumping time of 7.15 and 37.28 min, for the laminar and free molecular regimes, respectively.

The small pumping hole also has the advantage of not significantly interfering with the primary shock wave and, also damping any shock that might propagate into the pipes. The pressure in the compression tube is monitored with an Inficon BPG400 gauge covering the 5×10^{-10} to 10^{+3} mbar region, however with lower accuracy above 100 mbar. A piping and instrumentation diagram (PID) of the overall compression tube gas system is shown in Figure 6.3.

6.1.3 Shock Tube/Test section

The shock tube and test section have higher vacuum requirements than the compression tube or dump tank. The pumping system is composed of a primary pump Edwards GX600L connected to two Edwards STO603 turbomolecular pumps, one at each shock tube end. A primary vacuum is done down to 10^{-2} mbar, after which the turbomolecular pumps start and the pressure goes down to $10^{-5} - 10^{-6}$ mbar. The turbomolecular pumps have an ultimate pressure of 10^{-9} mbar, and a pumping speed of 650 l s^{-1} .

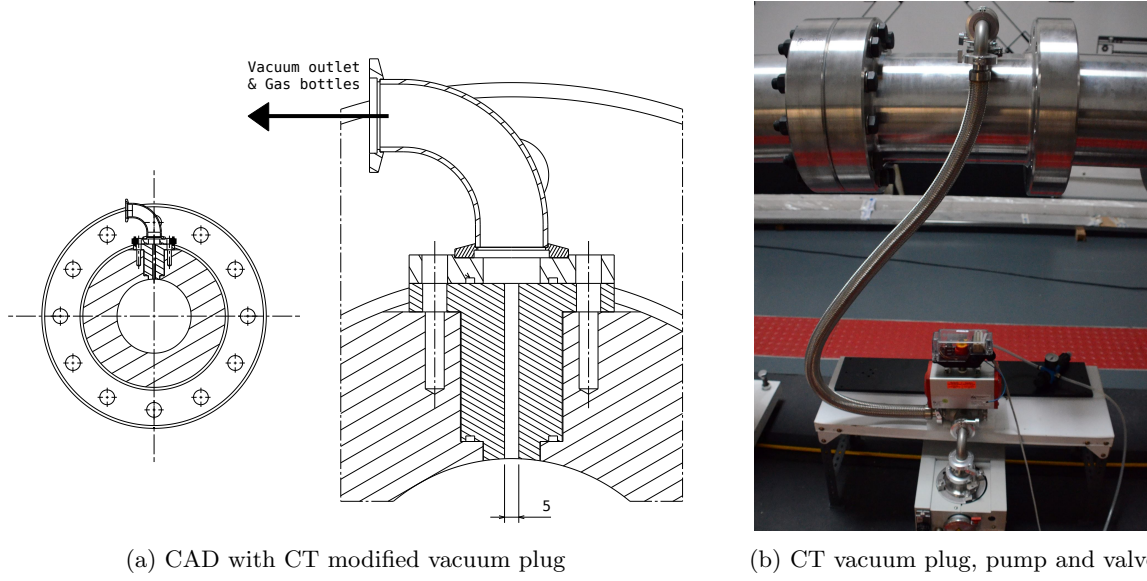


Figure 6.2: Compression tube vacuum plug and pump. CAD design (left), and photographs of pumping and valve system(right)

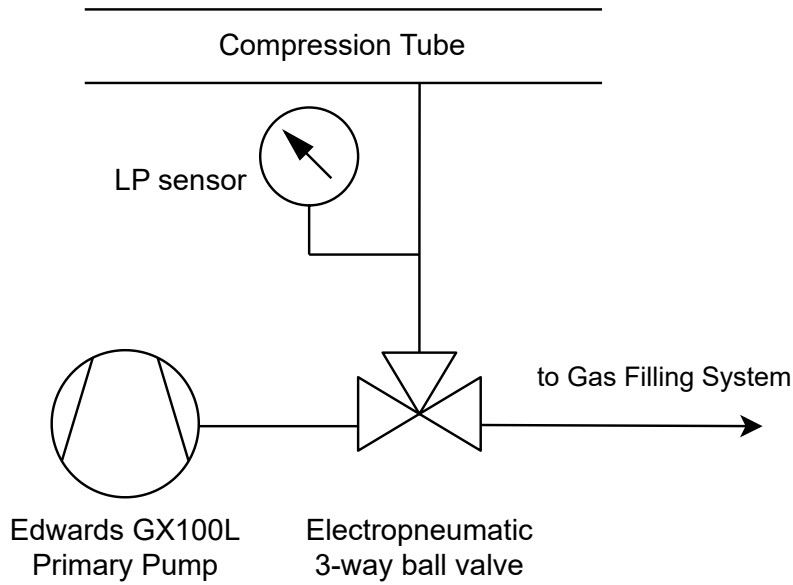


Figure 6.3: PID of compression tube vacuum system

To ensure maximum conductance, each pump is connected to the shock tube via a 63 mm diameter opening, actuated by an electromechanical arm. These arms are enclosed in and connected to the shock tube and turbomolecular pump via a tee fitting. Two 140 mm inner diameter gate valves insulate the shock tube from the pumping system. Two Edwards WRG-S pressure gauge monitor the pressure at both tee sections connected to both edges of the test section, just below the turbomolecular pumps. The working range is between 10^{-9} to 10^{+3} mbar, with low accuracy above 10^{+2} mbar. Figure 6.4 presents a photograph of the turbomolecular vacuum pumping system at the ST9 section, and Figure 6.5 presents the PID of the overall shock tube vacuum system.

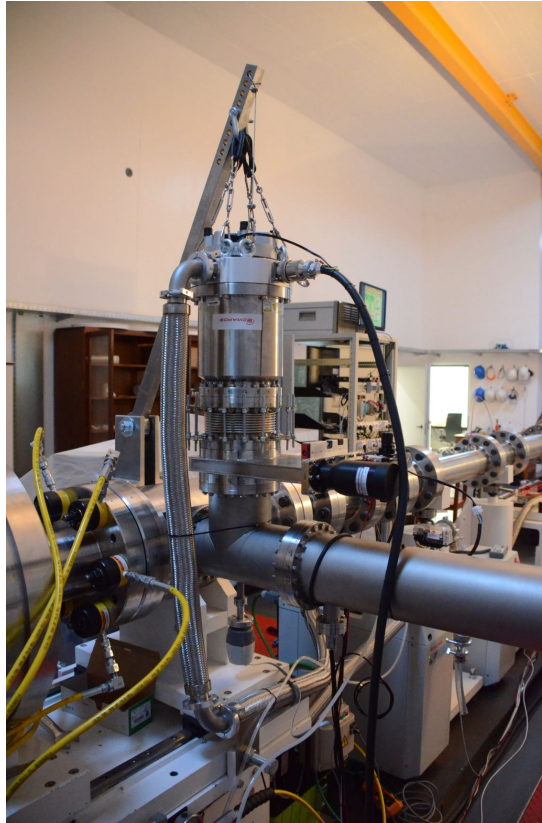


Figure 6.4: Photograph of the shock tube vacuum system, turbomolecular pump, gate valve and connection to shock tube.

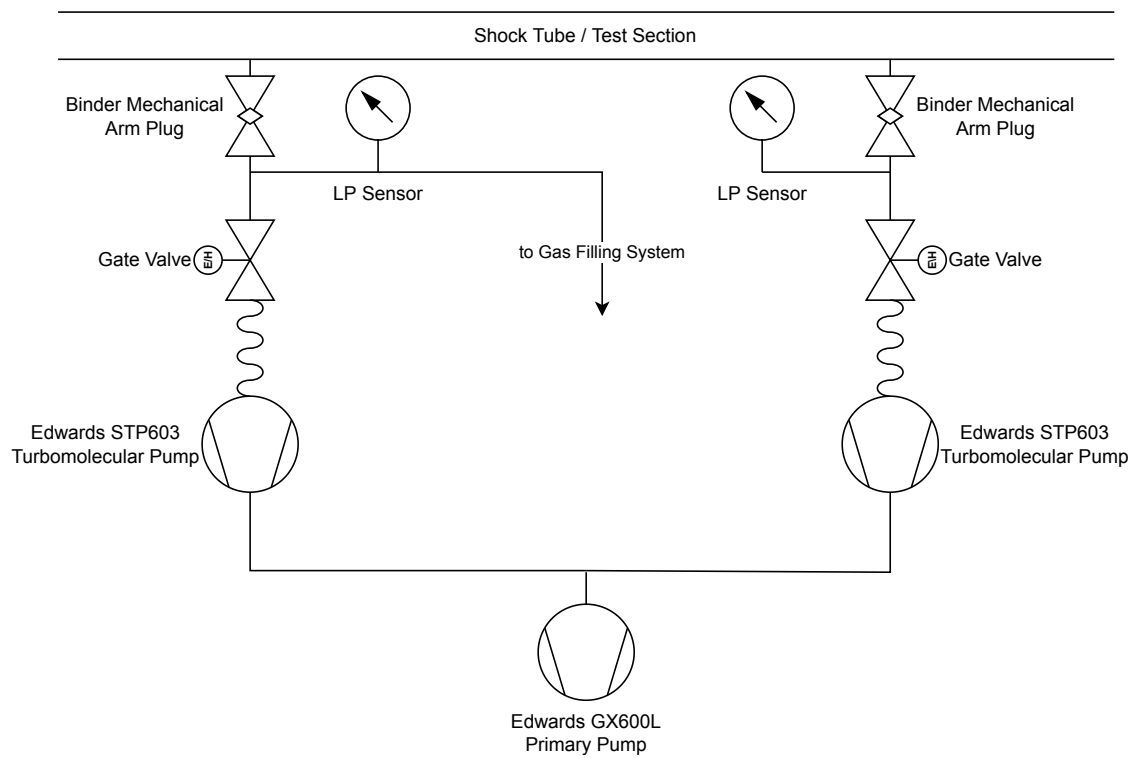


Figure 6.5: PID of shock tube vacuum system

Pump insulation

An unorthodox and complex assembly design was elected for the high vacuum turbomolecular pump system. The rationale for such a design stems from the need to ensure the highest conductance for the turbomolecular pumps. By maximizing the opening to the pump fan, equal to its diameter, and minimizing the distance from the tee to the port, one can optimize the pumping speed and the ultimate pressure of the vessel. The shorter mechanical arm length also helps reduce issues related to sagging, deflection, and misalignment of the plug with its port. This, in turn, brings a severe constraint of not clashing with the bench supporting the shock tube. A further difficulty is caused by the test section being free-standing and able to move alongside the facility recoil during a shot.

The solution to all these constraints was to mount the pump upside down, with the gate valve and bellows under it. A counter-weight balancing system was designed and mounted on the opposite side, alleviating some of the pump weight off the bellows, and tee section. Two independent flexible linkages, the first using steel cables, and the second using rubber rings and carabiners were chosen to connect the pump to its counterweight. These flexible connections allow it to better withstand the system's recoil and vibrations.

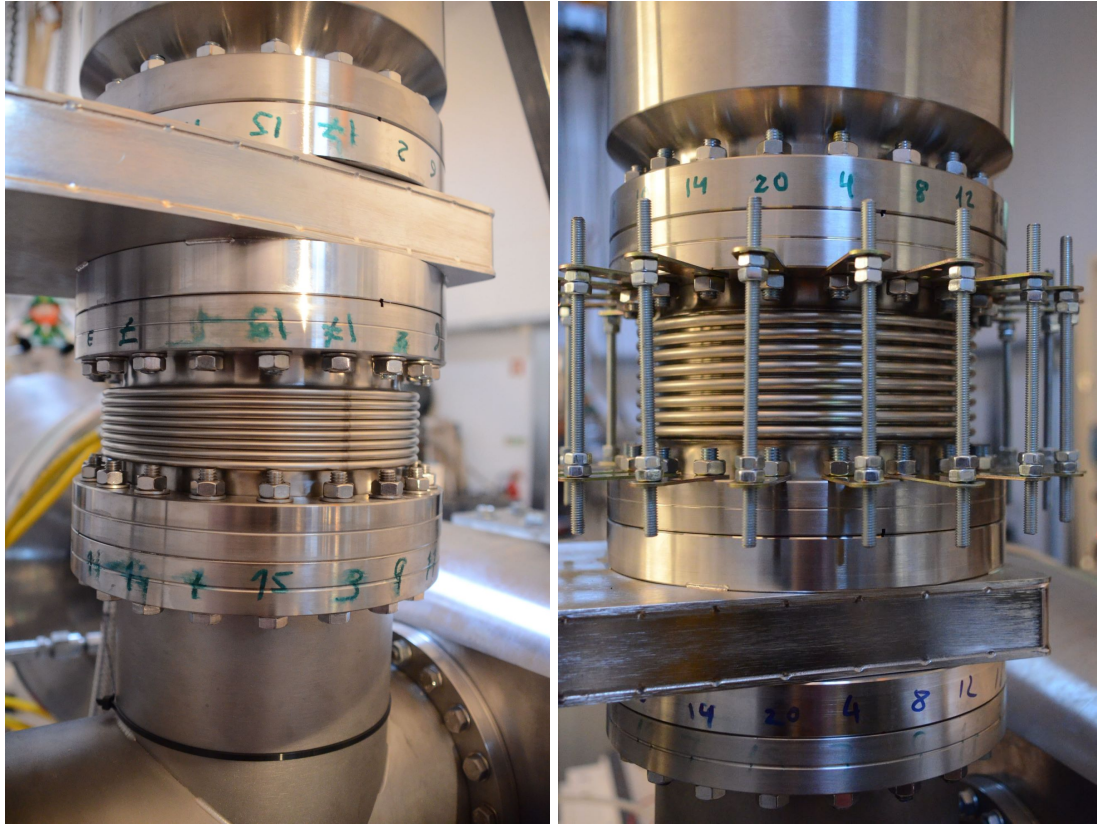
Another consequence of this need for high conductance is the strong force exerted on the vacuum bellows by the loss of atmospheric pressure inside the vacuum line. The 140 mm pump's internal diameter yields a cross-sectional area of 153 cm^2 , corresponding to around 1530 N of force. Figure 6.6 shows this effect, with the vacuum bellows fully compressed. Two major issues arise from the compressed bellows, the loss of flexibility may hamper the recoil damping, and the rubbing of the bellows may rupture/damage them causing a major vacuum leak. Two mitigation actions were carried out.

1. A limiter jig/cage was mounted around the bellows. This prevents the bellows from fully compressing. The system is still under stress and remains rigid, nonetheless, it prevents the rubbing/fatigue damage of the bellows;
2. The gate valve was moved from between the pump and bellows, to between the bellows and tee section (the gate valve is now upstream of bellows).

Once the final vacuum is reached, the gate valve is closed, and the pumping system shut down. The pressure downstream of the valve returns to atmospheric pressure, that in turn relieves the stress on the bellows, allowing them to move freely again. The gate valve is never mechanically insulated from the shocks during operation due to being linked with the shock tube through a rigid connection. In the previous configuration, the compressed bellows acted as a rigid piece, not damping the vibrations or recoil. However, with the newer configuration, the pump is mechanically insulated from the shocks by the closed gate valve and damped by the bellows. A comparison of the two assembly configurations is depicted in Figure 6.6.

Mechanical arm assembly

The mechanical arm assembly was designed to help insulate the pumping system from the high-energy shock wave and avoid having large open ports, like in CT. A plug attached to the mechanical arm can



(a) Old pumping setup with fully compressed bellows (b) New pumping setup with limiting compression jig

Figure 6.6: Shock tube turbomolecular vacuum pump, gate valve, and bellows. Fully compressed bellows during vacuum pumping (left) and new configuration with limiting jig assembly (right)

open/close the 63 mm port connecting the shock tube to the tee section. This is done by a linear electric motor. The arm retracts, opening the port to clean the vessel, vacuum it, and fill it with the test gases. The arm then extends, closing the port with the plug before the experimental shot.

A maximum shock wave overpressure of 10 to 20 bar is expected in the test section. It will exert a force, between 3200 and 6400 N, on the plug which might damage the mechanical arm. Leakage through the plug to the tee section will cause an overpressure of the system, as high as 6 bar in the exhaust phase. Again, damage to the arm mechanism might ensue. Some mitigation actions may be required to counteract the forces caused by the gas overpressure in the future. Figure 6.7 shows the CAD drawing of the tee and mechanical arm assembly.

Alignment issues: The tee section is devoid of any alignment dowels. This caused additional difficulties in the first alignment operations. Namely, the plug on the ST9 section got stuck and was severely scratched during operations. The plug and connection ring were refurbished through polishing. Figure 6.8 shows photographs of the damaged plug and respective connection ring before and after polishing the scratched surface.

To mitigate the aforementioned problem the mechanical arm is loosely bolted to the tee section. This should allow for a moderate movement, hence a self-alignment of the parts. The refurbishing polishing was done with alumina grit (Al_2O_3) to first recover the scratched surface and second, reduce

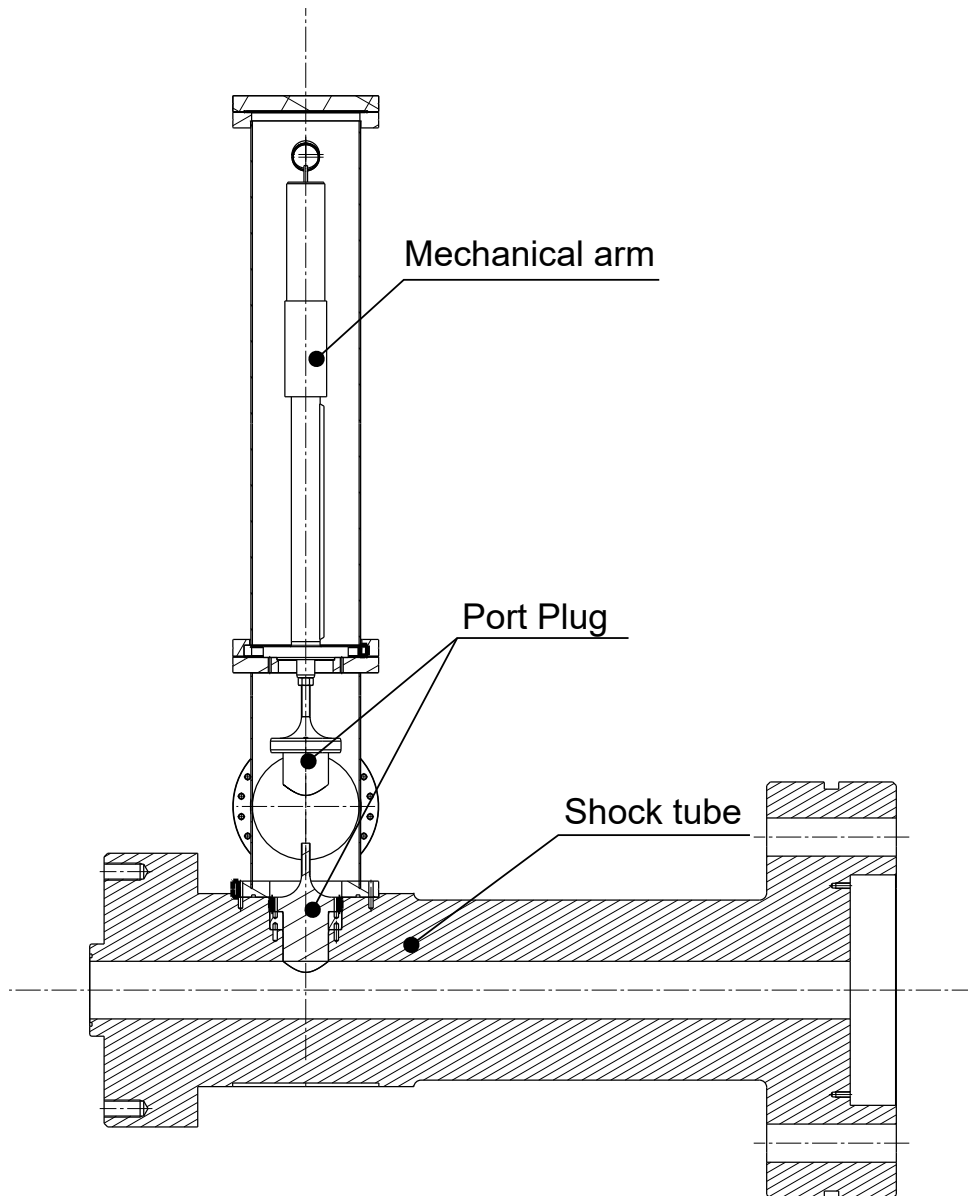


Figure 6.7: CAD drawing of part of the shock tube section, and the tee and mechanical arm assembly.

the surface roughness level. Lower surface roughness helps the sliding of one piece into another, lowering the possibility of the connecting part galling or being stuck. Alongside this, the centering rings (where the plug is scratched) are now bolted with only 2 of the 12 bolts, allowing for the part to move slightly and accommodate any alignment mismatches/deviations during the opening and closure procedures.

The final concern is having a self-locking nut on the plug-mechanical arm connection to ensure self-locking in the plug rotational axis. A nylon nut was initially considered since it is self-lubricating, however, the high outgassing and water affinity lead to the abandonment of this idea. A traditional steel nut was tightly bolted instead. If any issues arise, a different metallic (e.g. molybdenum) nut can be considered to replace it.

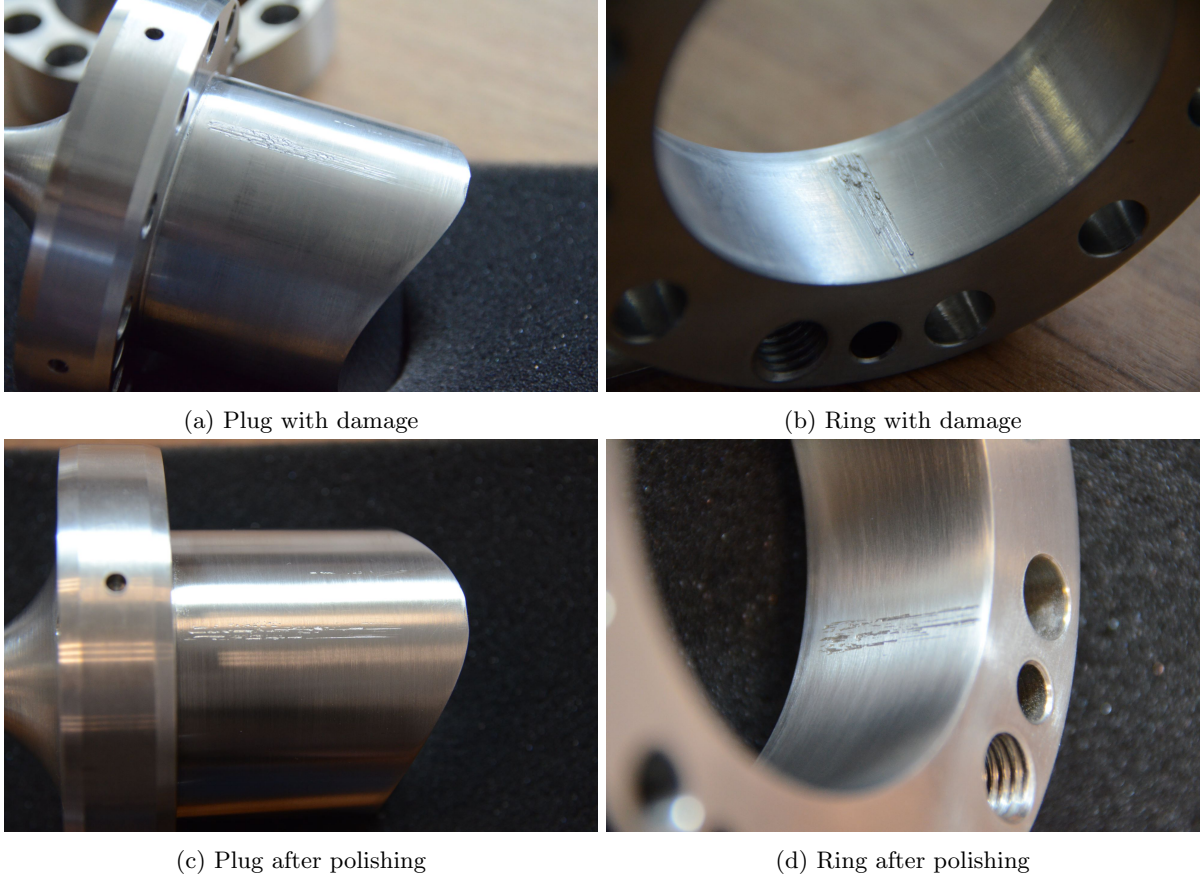


Figure 6.8: Damage to the shock tube vacuum insulating plugs and rings (top), and corrective actions (bottom).

6.1.4 Dump Tank

The dump tank's main purpose is to collect all the gases of the upstream sections so that after the shock the pressure is at an acceptable level. In the future, the dump tank may be used as an aerodynamic test facility by adding a nozzle and scale models. For this, a vacuum level of 10^{-2} mbar is required. The current vacuum pump is an Edwards GX600L, identical to the shock tube primary pump, with an ultimate pressure of 5×10^{-3} mbar and pumping speed of 29 l s^{-1} . Preliminary pressure measurements indicate a pressure of 2.5×10^{-2} mbar at the pump exit. Currently, the dump tank only uses it to collect the hot gases from the driver, compression and shock tubes. For this case, vacuum pressures down to 1 mbar are sufficient. As such, no particular concern is given to maintaining pressures below 1 mbar. Pressure is measured thanks to two sensors. For the sub-atmospheric region a Thyracont VSC43MA4 piezo-electric, with a working range between 1 and 1400 mbar, is used. For values above the atmospheric pressure a Honeywell MIPAM1XX050PAAAX is used, working range between 1 and 3.5 bar and capable of withstanding 6 bar of overpressure.

The pumping system is connected to the dump tank and exhaust port via a electropneumatic three-way ball valve. The three-way ball valve enables the system to switch between vacuum pumping and purging/exhaust lines equipped with a no-return valve. The valve is preset to open at 5 mbar, enabling an automatic purging of the dump tank. As hot gases reach the dump, the exhaust valve opens and starts purging

the gases from the system while they arrive at the dump tank. Purging the gases like this ensures that the system is in overpressure for the minimum possible time, and lowers the overpressure peak, hence reducing the stress in the components. Figure 6.9 depicts the PID scheme for the dump tank vacuum and exhaust systems.

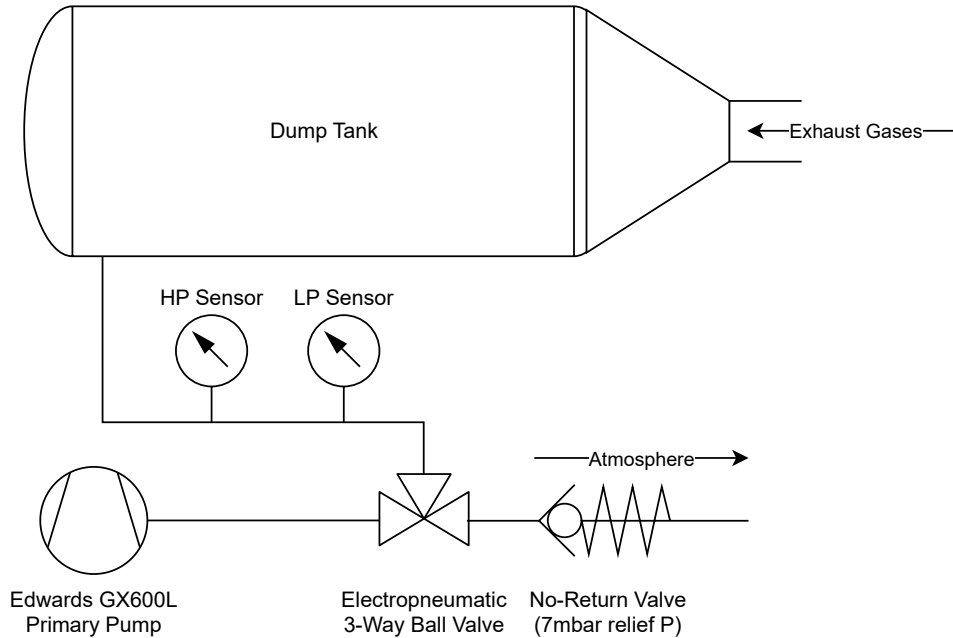


Figure 6.9: PID of dump tank vacuum system

In conclusion, two operational modes are considered:

- a) Vacuum pumping: The electro-pneumatic three-way ball valve opens the line between the vacuum pump and dump tank, pumping proceeds until a pressure below 1 mbar is reached. The valve switches positions, closing the connection between the dump tank and the pumping system and opening the connection of the dump tank to the exhaust line. The no-return valve acts as a closing valve holding the sub-atmospheric pressure inside the dump tank. The vacuum pump is closed and turned off.
- b) Exhaust/purging: Post-operation, the diaphragm separating the shock tube from the dump tank ruptures, and hot gases start to fill up the dump tank. When the pressure rises above 5 mbar, the no-return valve opens allowing the hot gases to vent out through the exhaust lines.

Two ranges of pressure sensors are required for this setup: One for measuring the pressure in the dump tank while it is vacuum pumped. This implies covering the range between 1 mbar to atmospheric pressure. The Thyracont VSC43MA4 fulfills this first purpose. Then, the post-test equilibrium pressure also need to be monitored. This in turn implies measuring pressures up to a maximum of 6 bar² down to atmospheric pressure during the hot gas exhaustion. This is fulfilled by the Honeywell MIPAM1XX050PAAAX gauge measuring between 1 and 3.5 bar, capable of handling 6 bar of overpressure. Both sensors are placed in

²worst case scenario of hot equilibrium gas

parallel right after the dump tank exit port. Figure 6.10 presents two photographs of the dump tank and vacuum/exhaust systems.



(a) Dump tank, gas port connection and pressure transducers



(b) Three way gate valve, exhaust line and vacuum pump

Figure 6.10: Dump tank vacuum system. Dump tank, connection port, and pressure transducers (left); three-way gate valve, exhaust lines and vacuum pump (right).

6.2 Testing and re-design

A series of different vacuum tests were performed to assess the vacuum quality of ESTHER, with a focus on the shock tube section to minimize the test gas contamination. Besides the ultimate pressure, the overall leakage rate of the system was also computed. It was done by fitting a first-order polynomial, $p = \dot{p} \times t + b$, to the pressure signal between the closing gate time and when the pressure reaches a steady pressure region. Here $\dot{p}$ is the pressure increase rate, and b is a constant to account for any shift in the starting pressure. The total leakage rate Q_{tot} is then computed by multiplying $\dot{p}$ by the vessel volume, 30 l in the case of the shock tube section.

6.2.1 Diaphragm materials

Different materials for the compression tube-shock tube (CT-ST), and shock tube-dump tank (ST-DT) diaphragms were tested, they were:

- 0.3 mm thick aluminium sheet
- 0.3 mm thick tin sheet

- 0.1 mm thick tin sheet

The aluminium sheet was selected owing to its low cost and good mechanical properties, whereas tin was chosen mainly for its high ductility, which may be beneficial to ensure a low leakage rate. Copper would be an ideal material in terms of ductility, however the cost per shot would be too high to keep it as a valid option. Nonetheless, a few vacuum tests have been carried out with copper diaphragms. Initial vacuum tests showed mediocre performance, with an ultimate vacuum (measured close to the turbomolecular pump) down to 10^{-5} mbar. After closing the gate valve the pressure quickly rose to 10^{-3} mbar, and 10^{-2} mbar after 20 min. The operational shots are carried out at a pressure of 10^{-1} mbar, and a vacuum of 10^{-2} mbar means a 10% pollution of the test gas. These results were not acceptable and required improvements.

Figure 6.11 shows the pressure evolution using a 0.3 mm aluminium sheet as a diaphragm. The dump tank pressure is 2.28 mbar and stays stable for the complete test duration. The compression tube pressure got down to 0.103 mbar, which is acceptable for its requirements, however, it also has a high leakage rate. The high leakage rate is evidenced by the fast rise in pressure after closing the ball valve at $t \approx 75$ min. In about 130 min from the ball valve closing ($t \approx 205$ min), the compression tube was at atmospheric pressure. As pointed out in Figure 6.5, the shock tube pressure is measured in two points, one at each of its ends. A fast drop in pressure observed until $t \approx 10$ min is caused by the primary pump, at $t \approx 35$ min the turbomolecular pumps start working. An ultimate pressure of around 10^{-4} mbar is reached, however, similar to the CT, once the gate valves are closed, at $t \approx 190$ min, a steep rise in pressure is observed. In 10 minutes the pressure rises to 1 mbar, this means that similar to the compression tube the sealing is not satisfactory.

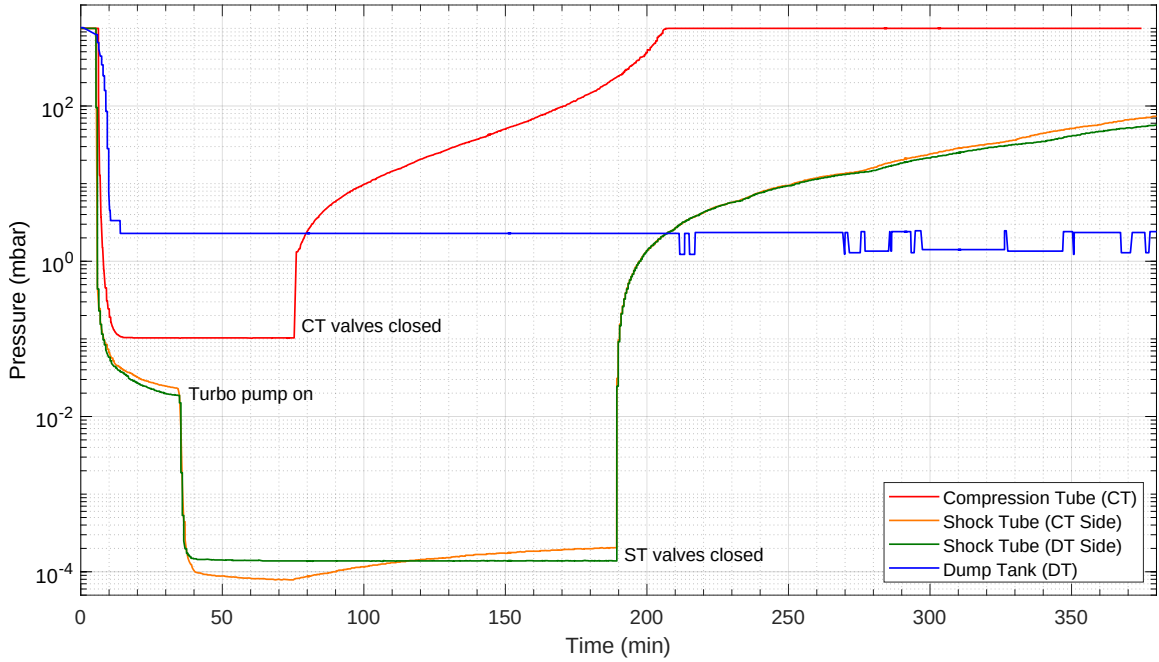


Figure 6.11: Pressure time evolution for vacuum tests with 0.3 mm thick aluminium blanks (3rd of February 2023).

Figure 6.12 illustrates the pressure evolution for a diaphragm set made of 0.1 mm tin sheet. The

compression tube showed a slightly higher vacuum level than when using an aluminium sheet. This is pointed out by the ultimate pressure being 0.086 mbar, whereas it was 0.103 mbar in the previous test. Alongside it, the vacuum in the dump tank improved from 2.28 mbar to 1.72 mbar, it could be explained by the outgassing of the walls. This latter test was done probably after a large quantity of water had been outgassed from the dump tank walls, leading to a better vacuum level. In regards to the shock tube, similar results and observations were taken when comparing the two cases.

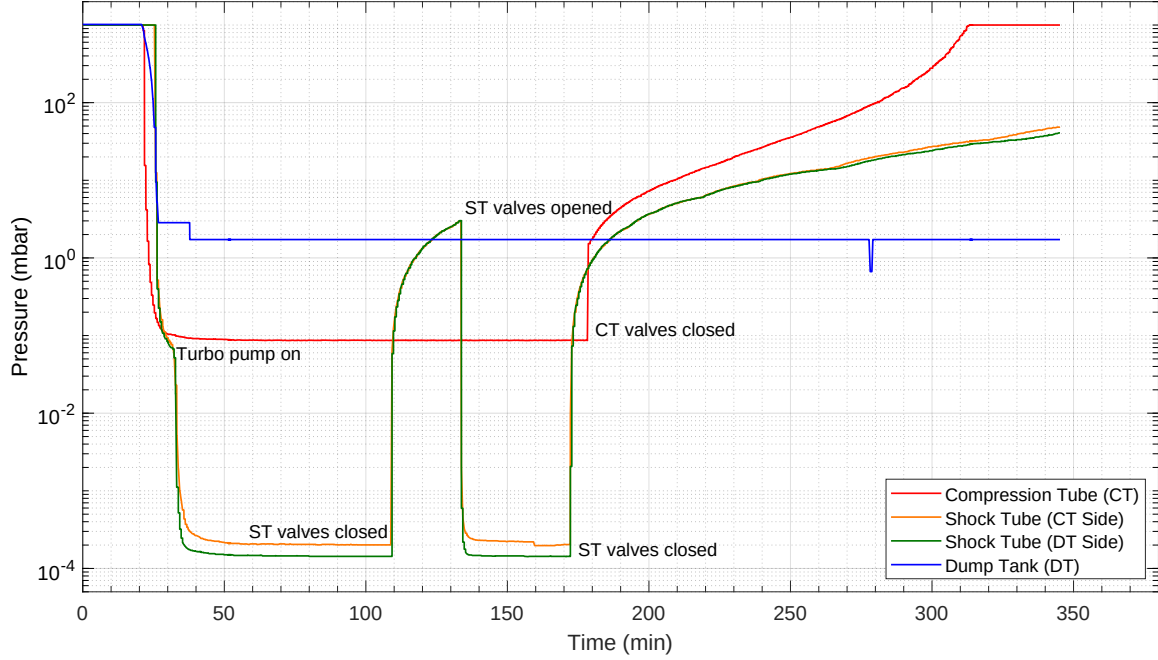


Figure 6.12: Pressure time evolution for vacuum tests with 0.1 mm thick tin blanks (10th of February 2023). $\dot{p}=2.01 \times 10^{-3}$ mbar/s, $Q_{tot} = 6.02 \times 10^{-2}$ mbar l s⁻¹

Neither aluminium nor tin diaphragms showed great results in providing adequate vacuum sealing of the shock tube. Some issues related to the mechanical behaviour of the diaphragms when under stress from the hydraulic bolts were identified. This was hypothesized at the time to be one of the causes of the high leakage rate. When subject to the hydraulic force compressing them, the diaphragms tend to shear and rupture near the sealing edge. Evidence of this is shown in Figure 6.13, where the two diaphragms on top are cut. Tin due to its higher ductility than aluminium could withstand compression without rupture, as seen in the bottom left example of Figure 6.13, yet it deformed and warped causing a leak. If a sufficiently low hydraulic pressure was used, even the 0.1 mm thick tin diaphragms could handle it without rupture. Nonetheless, they suffer buckling, indicated by the waving pattern near the outer border of the diaphragm, leading to a leakage in the system.

6.2.2 Diaphragm holder

It was hypothesized that the diaphragm holders stamping design could be a major cause for the large leakage rates in the facility. After closing the gate valves, the pressure rose very quickly in every section. Neither the level of hydraulic pressure nor the use of vacuum grease seemed to affect the vacuum quality. From this evidence and the previously discussed cuts on the diaphragms, we could infer that the



Figure 6.13: Test diaphragms for shock tube section. Tin sheet 0.1 mm (top left) shear cut and buckling; Aluminium sheet 0.3 mm (top right) shear cut; Tin sheet 0.3 mm (bottom left) plastic deformation warped; Tin sheet 0.1 mm (bottom right) plastic deformation buckling.

diaphragm holders stamping design could not be allowing proper sealing of the different sections. The stamping is done by a curved cavity and corresponding stamp. In theory, it would deform the metallic sheet and seal the vessel, similar to the con-flat connection of the vacuum pumps. The problem was considered to reside in the fact that the diaphragm holder has a blunt-curved edge, to avoid ripping at the edge when hit by the primary shock wave, whereas the con-flat has two sharp edges. Anyhow, sharp edges would be worn out over time by the shock waves, leading to future problems. Figure 6.14 depicts the CAD detail of the current diaphragm holders for the shock tube-dump tank connection.

The stamping method used in ESTHER is neither unique nor the only one possible to hold the diaphragm plate in place. Some hypersonic facilities around the globe use O-ring cavities to seal the diaphragm and vessel in a vacuum. Figure 6.15 shows the CAD designs of two different diaphragm holders. The first one, from VUT-1 shock tube, uses a similar method as ESTHER where a blunt edge clamps the diaphragm in place. While for the second, from X2 shock tube [180], the diaphragm is clamped in place with two O-rings, one at each end.

A test of this second configuration was done by manufacturing two custom blank steel diaphragms with one O-ring cavity and tightening them in place of the “normal” diaphragm holder. Figure 6.16 illustrates the custom blank diaphragm mounted in place. Since the custom blank diaphragms are bolted to the ST1 and ST9 parts, only the shock tube section can be tested.

Figure 6.17 depicts the pressure evolution in the shock tube section with the O-ring seals. A leak was identified near the dump tank side, as the pressure was significantly higher on that side than on

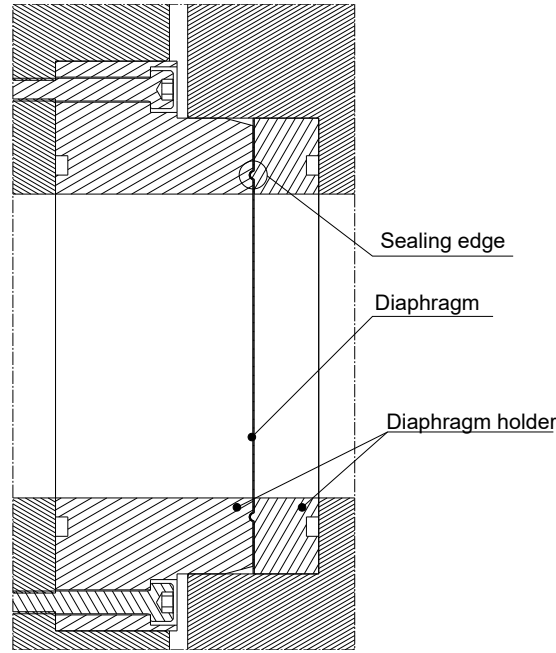
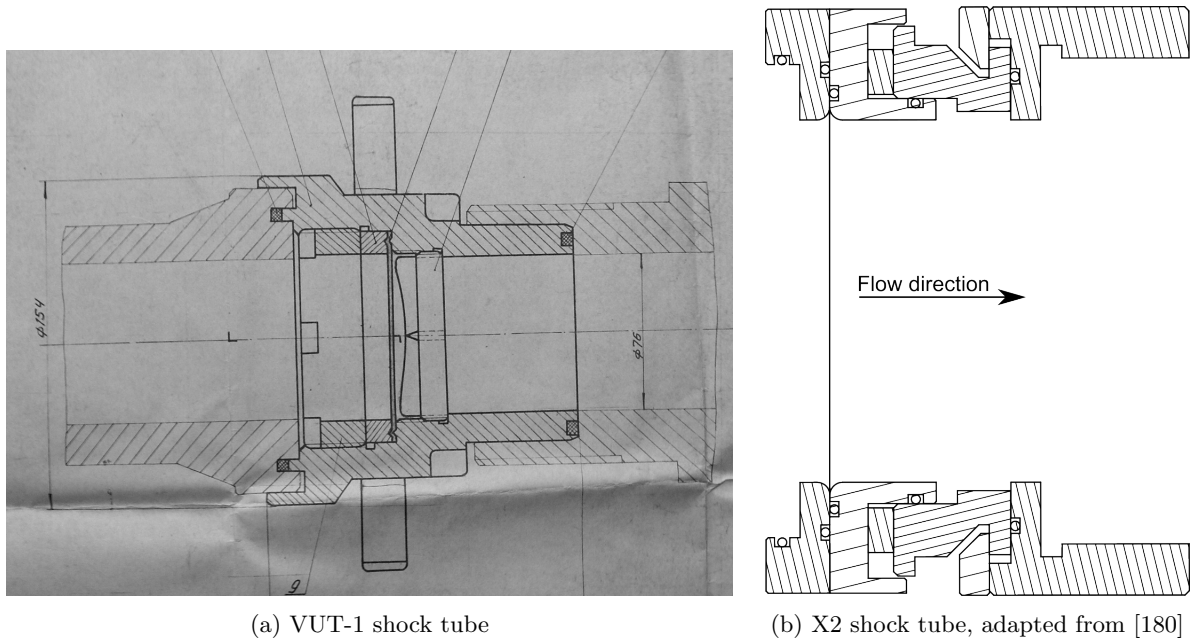


Figure 6.14: Current diaphragm holder design for the ST-DT section, similar to CT-ST section.



(a) VUT-1 shock tube

(b) X2 shock tube, adapted from [180]

Figure 6.15: Different diaphragm holder designs. Stamping method (left) and O-ring seal (right)

the compression tube side. The reliability of the pressure gauge data on the shock tube (DT side) was deemed not good enough because the values differ by two orders of magnitude when compared to the (CT side). Nonetheless, the vacuum quality improved when compared to the diaphragm which used the stamping method. Comparing Figure 6.12 to Figure 6.17, it is observable a decrease in the ultimate pressure from 2×10^{-4} to 3×10^{-5} mbar, and a decrease in the corresponding from $\dot{p} = 2.01 \times 10^{-3}$ mbar/s to $\dot{p} = 9.99 \times 10^{-5}$ mbar/s. Despite the change on the sealing method, the vacuum level was being yet not good enough to ensure a 1% contamination rate, and it was found that the original diaphragm holder could not be the major leak source. Instead, the testing section needed to be thoroughly checked for all

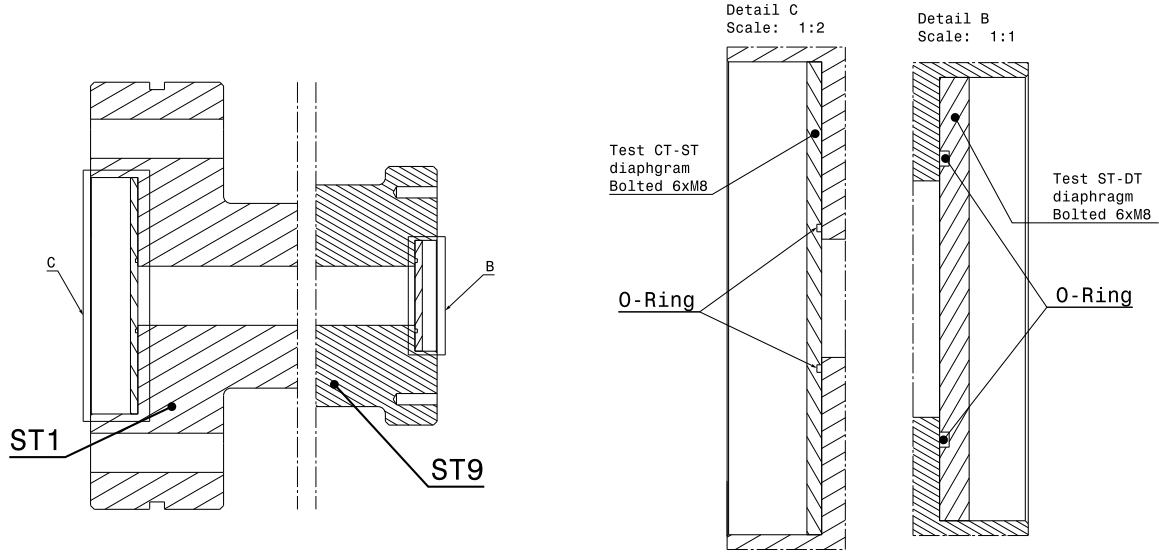


Figure 6.16: CAD of the blank and shock tube assembly for the vacuum test using O-ring sealing, based on University of Queensland design.

other possible leak sources. This will be discussed more ahead.

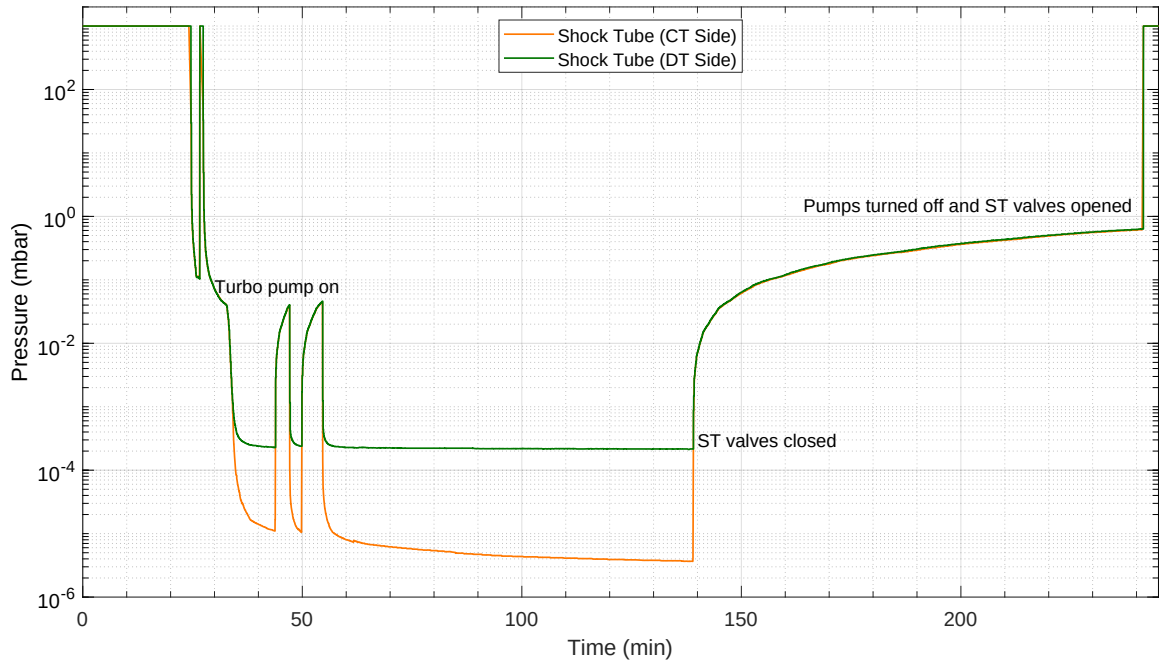


Figure 6.17: Pressure time evolution for vacuum tests using the custom blank O-ring sealing diaphragms (12th of May 2023). $\dot{p}=9.99 \times 10^{-5}$ mbar/s, $Q_{tot} = 3.00 \times 10^{-3}$ mbar l s⁻¹.

6.2.3 Leakage test

To quantify the vacuum leaks in each connection and identify the larger ones, a helium leak detector (“sniffer”) was used. It measures the flow of helium through a line parallel to the primary vacuum pump. The helium “sniffer” is comprised of a pumping system, a gas line connection KF-25, and a mass spectrometer. An external helium bottle provides the gas to the test. Helium is injected into the vicinity of the possible leakage sources of the vacuum vessel, like plugs, windows, hoses and bellows, and vessel

connections. Part of the helium that enters the vessel will pass through the “sniffer” and be measured by the mass spectrometer. Larger leaks will absorb larger amounts of helium into the vessel, which are then identified by a mass spectrometer. The leakage test setup is shown in Figure 6.18.

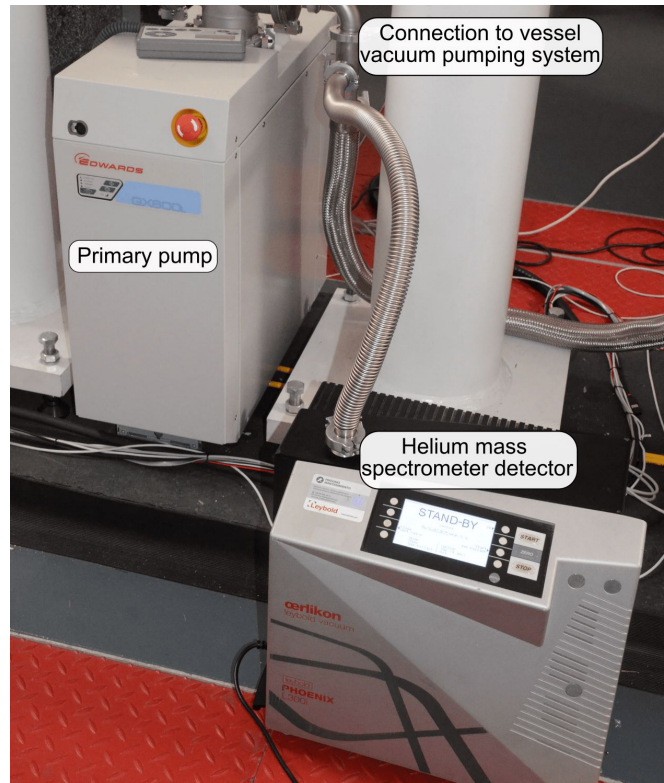


Figure 6.18: Helium leakage test setup

The system is connected as shown in Figure 6.18 and turned on when the vessel pressure reaches around $10^{-5} - 10^{-6}$ mbar. The mass spectrometer will have a reference value for the leakage rate, about 5×10^{-8} mbar l s^{-1} , any leakage rate below that cannot be identified. The flanges to be tested are firstly covered by a black plastic garbage bag to isolate the region at test from the rest of the vessel before injecting helium. The mass spectrometer will measure the helium that entered the vessel and passed through the “sniffer”.

The leakage rate of a connection is the maximum value identified by the mass spectrometer after the helium injection. The measurements take some time, as the helium atoms need to move from the injection region to the spectrometer. A waiting time is needed between measurements to allow the absorbed helium to diffuse out of the tube, so the leakage rate value can return to the reference one, or as close as possible. The next connection is then tested. Two good work practices were: to use the least possible amount of helium in each test; and test the least probable leaking connection first, leaving the most probable ones to the end. Reducing the amount of used helium reduces the possibility of it entering through a different point than the one tested conflicting the results, and reduces the waiting time between measurements. The number of connections and flanges in ESTHER shock tube section is summarized in Table 6.1. A total of 43 core connections are present in this section. Added to these, there are four upstream of the gate valves and one extra in each of the two machined plugs, making the final total of 49 possible leakage

points.

Table 6.1: ESTHER shock tube section flanges, connections and gaskets list.

Group	Location	Gasket type	Size (mm)	Quantity
Shock tube main body	ST main body	Helicoflex O-ring	$d_{int} = 80$	9
	Ports and plugs	FKM O-ring	$d_{int} = 26$	16
Tee section x2	Tee-ST9	Helicoflex O-ring	$d_{int} = 140$	1
	Tee flanges	Con-Flat	DN160CF	3
	Arm-binder	Con-Flat	DN25CF	1
	Tee-pressure gauge	Con-Flat	DN16CF	1
	Pressure gauge	ISO-KF O-ring	ISO-KF25	1
	Tee-pumping	Gate valve	—	1
Upstream of gate valve x2	Bellows	Con-Flat	DN160CF	2
Manufactured plugs	ST8 VUV optical	FKM O-ring	$d_{int} = 10$	1
	ST7-1 shock detection	Copper ring	$d_{ext} = 12$	1

As previously discussed, the lower vacuum level measured by the gauge at the dump tank side indicated the largest leaks should be on that side. Two different helium “sniffers” were used for leakage tests at different times. The first one ($\dagger$), was only used on the first test day, and had a lower reference value at $1.4 \times 10^{-9} \text{ mbar l s}^{-1}$, while the second one ($\ddagger$) could not go lower than $5 \times 10^{-8} \text{ mbar l s}^{-1}$ and was used for all other tests. Results from different equipments or even different test days cannot be directly compared with one another, thus the marked values are only indicative of the relative leak dimensions. A full illustration of the leakage rate observed during the first test is presented in Figures D.4 and D.5. Multiple leakage tests were performed over time, with the major leak sources identified and their leakage rates evolution described below (mbar l s^{-1}).

- ST7-1 plug for shock wave detection ‡ - $8.5 \times 10^{-6} \rightarrow 3.5 \times 10^{-6} \rightarrow 1.9 \times 10^{-7}$
- ST8-plug for VUV emission spectroscopy † - 3.7×10^{-9}
- Tee-section/Mechanical arm assembly DT side
 - Flange tee-section to arm binding † - $1.7 \times 10^{-7} \rightarrow 6 \times 10^{-9}$
 - Pressure gauge connection ‡ - $4 \times 10^{-7} \rightarrow 5.8 \times 10^{-8}$
 - Pressure gauge connection 2 ‡ - $1.5 \times 10^{-7} \rightarrow 5.9 \times 10^{-8}$
 - Flange to ST9 ‡ - $1.2 \times 10^{-6} \rightarrow 3.4 \times 10^{-7}$

Mitigation strategies were applied for each leak source to try to reduce its leakage rate. Some simple adjustments could be done on the fly during a test. These included things like applying extra torque to bolts or replacing small O-rings. The ST8 plug leak was small compared to the others, besides it, the VUV optics are held under vacuum and thus operationally should not be a large leak source.

The plug ST7-1 uses copper rings as vacuum seals. After inspection, these did not look plastically deformed indicating a bad sealing capacity. Extra torque was applied to the thread bolting piece in the assembly; the copper rings were further annealed on an oven at 450°C for 1h30min to increase their

ductility [388]; an O-ring was added between the copper ring and tightener piece; and both copper rings and steel pieces were re-polished with alumina sheet paper (400 grit) to provide better sealing. These modifications caused the leakage rate to improve as detailed above. Any further improvement might require an alteration of the plug design, such as a knife edge like a con-flat connection to more easily deform the copper rings, however we are confident that these mitigation actions are sufficient.

The auxiliary tee section on the dump tank side had multiple leaks. First, the connection to the pressure gauge had damaged O-rings, thus they were replaced and tightened again. The flange connection of the tee section to the extended arm containing the pneumatic arm was poorly tightened and re-tightened to better compress and deform the copper ring.

Results after the application of the above mitigation strategies are shown in Figure 6.19. During that time, data from the pressure gauge on the dump tank side was unavailable thus only the other pressure gauge values are plotted. An ultimate pressure of 9×10^{-7} mbar could be reached. The pressure increase rate was estimated as 3.02×10^{-6} mbar/s, and the leakage rate as 9.07×10^{-5} mbar l s⁻¹. Since the system was held under vacuum (about 1 mbar) for a long time, the turbomolecular pump could be turned on soon after turning the system on. The spike at $t \approx 540$ min is due to an anomalous closing of the gate valve.

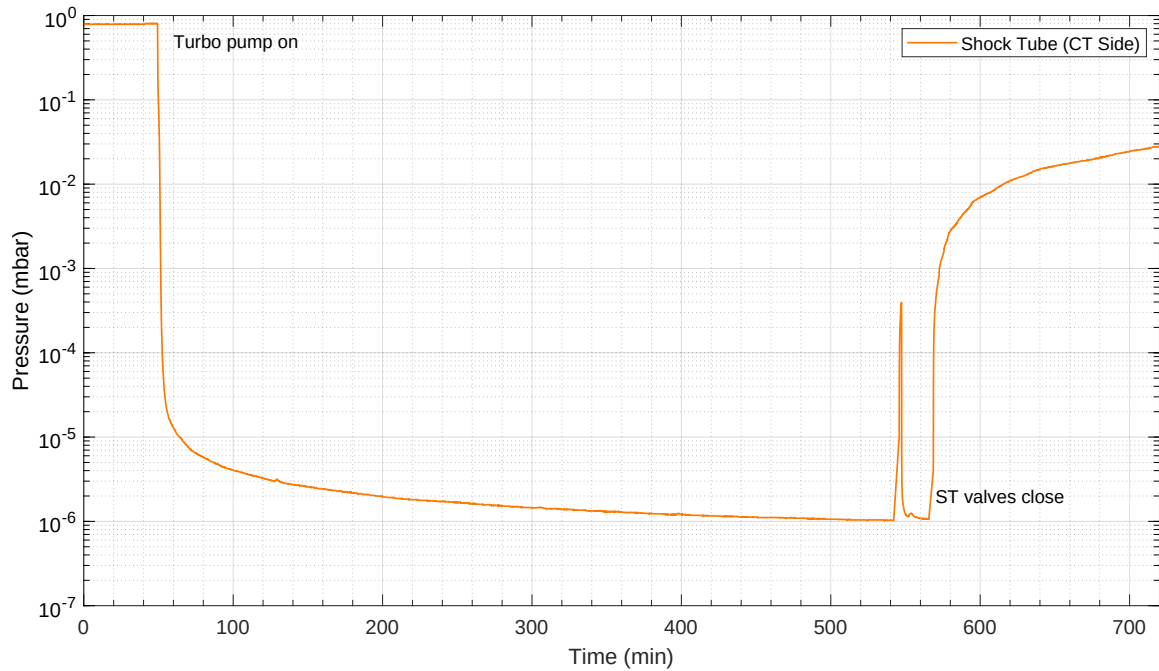


Figure 6.19: Pressure time evolution for vacuum tests after correction of small leak sources (03rd of July 2023). $\dot{p}=3.02 \times 10^{-6}$ mbar/s, $Q_{tot} = 9.07 \times 10^{-5}$ mbar l s⁻¹.

Lastly, the flange connecting the auxiliary tee section to the ST9 part of the shock tube was examined as it seemed to be significant. Initially, an added torque was applied to the securing bolts. It proved not enough to solve the leak. Thus, it was disassembled, cleaned and polished, before a new O-ring was put in place. The process was repeated a couple of times. After opening the connection, the metallic helicoflex O-ring had dirt marks consistent with plastic deformation. A design limitation, depicted in Figure 6.20, is that there is no physical barrier between the bolt holes and the O-ring groove. Metal debris/fillings

can fall over, or be pulled by the vacuum pump, to the O-ring groove or be caught by the O-ring causing leakage. All of the threaded holes were re-threaded, cleaned and an anti-seizing paste applied. In this way, the leftover metallic debris inside the thread are caught in it, and do not fall over to the O-ring groove.

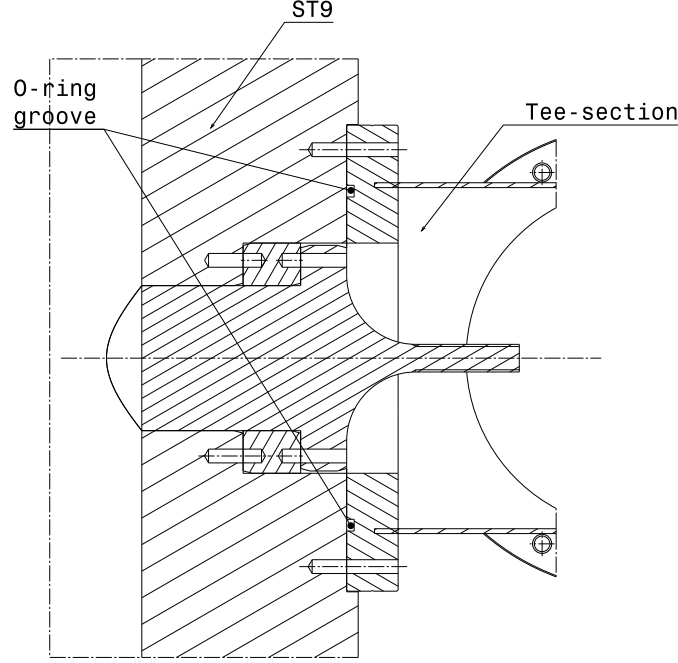


Figure 6.20: ST9 to Tee-section connection CAD.

Whenever large mitigation or correction operations took place that require opening the system for a long time, an outgassing period was required. During it, both water vapour from ambient air and cleaning products remains used to remove skin grease and oils were outgassed and pumped out. Only after a few days, the absolute vacuum level could be compared to the one before the mitigation/modification strategy was applied.

The helicoflex O-ring was temporally replaced by a FKM one to compare the sealing capacity of both materials. Figure 6.21 shows the pressure signal evolution after mounting the FKM O-ring. It shows both a higher ultimate pressure and leakage rate when compared to the one with a new helicoflex in Figure 6.22. The former, FKM O-ring, reached an ultimate pressure of 2×10^{-5} mbar and leakage rate of 1.24×10^{-4} mbar ls^{-1} , while the latter, new helicoflex, had an ultimate pressure of 9.8×10^{-7} mbar and a leakage rate of 3.94×10^{-5} mbar ls^{-1} . The pressure signal shown in Figure 6.22 also indicates that the pressure difference between the two gauges was reduced compared to Figure 6.17. This implies that the leakage rate is more homogeneous across the whole shock tube section. This is consistent with the elimination of the major leakage sources in the ST9 region, and on the overall test section. After applying all these corrections and mitigations strategies, ESTHER is expected to be capable of reaching the desired sub-1% contamination rate.

Preliminary results from the ongoing tests with the original diaphragm designs appear to indicate that these were not the major leak source as hypothesized. Current ultimate pressure values are in line with the ones observed on the 25th of July 2023. The aluminium diaphragm was not cut, otherwise the

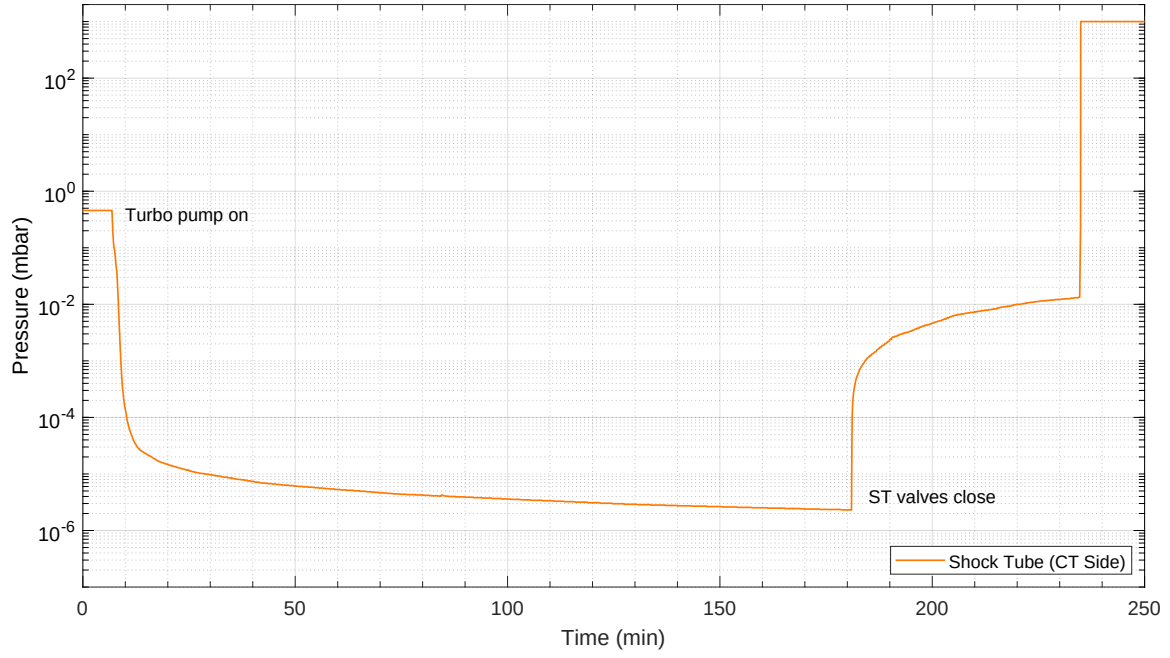


Figure 6.21: Pressure time evolution for vacuum tests after correction of leak sources in the tee section - FKM O-ring in tee-ST9 flange (20th of July 2023). $\dot{p} = 4.15 \times 10^{-6}$ mbar/s, $Q_{tot} = 1.24 \times 10^{-4}$ mbar ls⁻¹.

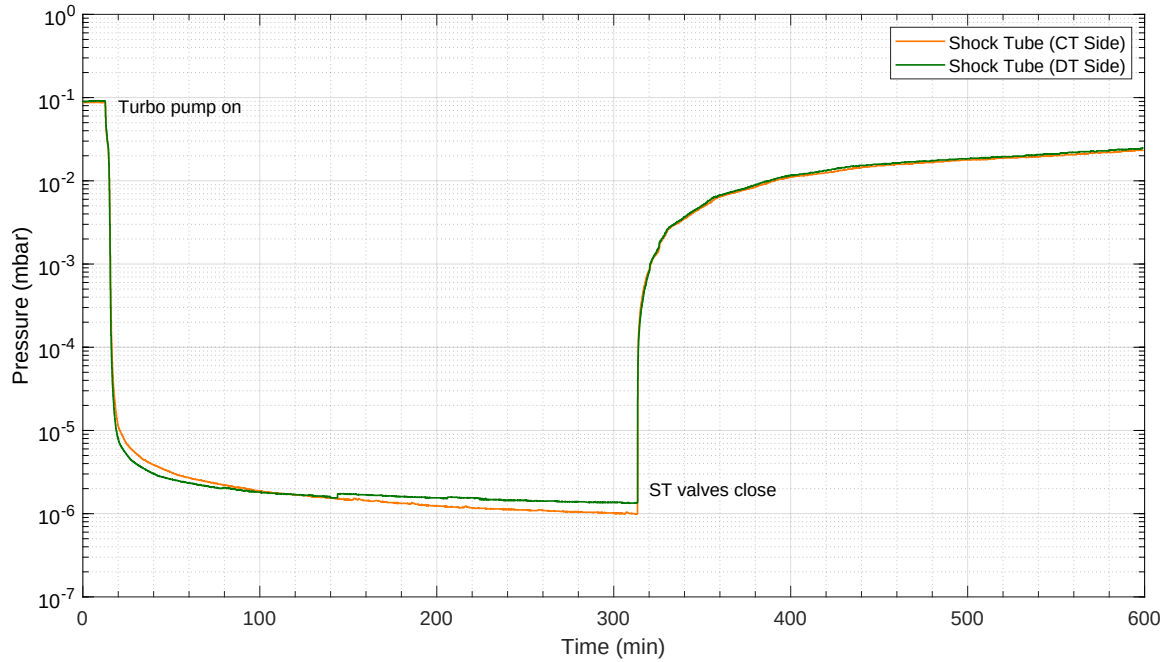


Figure 6.22: Pressure time evolution for vacuum tests after correction of leak sources in the tee section - helicox O-ring in tee-ST9 flange (25th of July 2023). $\dot{p} = 1.31 \times 10^{-6}$ mbar/s, $Q_{tot} = 3.94 \times 10^{-5}$ mbar ls⁻¹.

vacuum would not hold. The cutting on the aluminium diaphragm may be explained by an excessive hydraulic pressure. This pressure increase not only did not improve the vacuum, but increased the risk of rupturing leading to a vacuum vessel opening. The leakage level of the original diaphragm holders may be higher than the O-ring ones, nonetheless is still below other leak sources such as the ST7-1 plug or the mechanical arm assembly on DT side. As such, it was decided to retain the original design.

6.3 Outcomes

The main outcomes of the vacuum campaign for ESTHER can be summarized as follows:

Cleaning and outgassing The nature of a shock tube facility requires a continuous opening of its section for diaphragm replacement and maintenance. A cleaning setup was designed to scrub and clean the tube walls, removing contamination adsorbed in the walls. After opening the shock tube, an outgassing time is needed before a good-quality vacuum is achievable. The longer this time, the more the vacuum vessel is exposed to room air and other contaminants, most commonly water vapour and carbon deposits from carbon dioxide experiments.

Diaphragm holders using the original design wer found to not be a major source of leakage

The original design for the diaphragm holders between CT-ST and ST-DT sections presents a certain amount of challenges. It cuts the aluminium test diaphragms if hydraulic pressures are excessive, and buckles the tin ones, which may be causing leakage in the diaphragm. Two blank steel diaphragms with O-ring grooves were tested, but these only slightly improved the vacuum level. More testing, with different materials and different hydraulic pressures, was carried alongside with the operation of the helium leak detector. This task was recently completed, and preliminary results are very encouraging, implying the diaphragm holders design is adequate in terms of sealing.

Good surface state is key to ensure sealing All vacuum connections require a good surface state to ensure effective sealing. Most commonly on O-ring and groove flanges, a low roughness level is needed to ensure a good vacuum. Leaking flanges and connections surfaces, namely O-ring grooves, were polished after the initial vacuum tests to increase their sealing capacity.

Ad-hoc design with custom copper seals need careful assembly The shock wave detection plug ST7-1 was originally a major leak source. Its design uses copper rings and has sealants, however, the lack of a knife edge could explain the small plastic deformation of the copper ring. Polishing the surface, and annealing the copper rings lead to a significant improvement in sealing. An additional O-ring was also added to help seal the vacuum.

Con-Flat flanges are very effective When compared to other flange types, O-rings and grooves, or ISO-KF, Con-Flat flanges look to have fewer problems and lower leakage rates than other types. As expected, the plastic deformation on a knife edge yields a good seal. This was evident by the fact that any major leak with a Con-Flat flange was easily corrected, while others demanded more extensive work.

Chapter 7

Spectroscopy Diagnostics

“Like us, there’s more to them... than meets the eye.”

- Optimus Prime, Transformers

This Chapter deals with the optical emission spectroscopy diagnostics of ESTHER. It describes the specifications and requirements for diagnosing shock plasmas, as well as the selected equipment and connections to the test section. The results from the qualification campaign are also presented.

Atmospheric entry plasmas are very energetic and thus radiate very strongly in a broad range of wavelengths. Spectroscopy is, therefore, a key diagnostic for probing such plasmas, also taking into account that these diagnostics are non-intrusive in nature, hence not disturbing the flow. Measurements of time-dependent emission/absorption in specific wavelength ranges allow probing for specific atomic and molecular quantum transitions, and may indirectly provide information on the time evolution of species concentrations (relative or absolute depending on the setup calibration), as well as flow and species temperatures, indicating departures from Boltzmann equilibrium. A direct measurement of the overall time-dependent radiative fluxes (in W/cm^2) emitted by the shock wave provide important parameters for the TPS design, which needs to withstand the overall convective and radiative fluxes of the flow during the whole entry phase. Measurements carried out in the past were mostly restricted to optical emission spectroscopy from the near-UV to near-IR (NIR) range, which encompassed most of the spectral features of interest in this velocity region [389–391]. However, at higher velocities, the more prominent radiative features move towards the ultraviolet-vacuum ultraviolet (UV-VUV) region, while on the other side, Mars entries also have peculiar radiative heating characteristics with prominence in the mid-wave infrared (MWIR) [392]. Accordingly, two companion contracts were awarded to the consortium for the development of additional optical spectroscopy setups, one in the UV-VUV and another in the MWIR region.

7.1 Specifications & Requirements

Figure 7.1 depicts the radiation wavelength distribution of a Planck blackbody at different temperatures. As the temperature increases, so does the emitted radiation power with T^4 , in line with the law of

Stefan–Boltzmann. Besides this, the peak wavelength of the emitted radiation λ_{peak} moves to the shorter wavelengths following Wien’s law $\lambda_{peak} \propto 1/T$ [393].

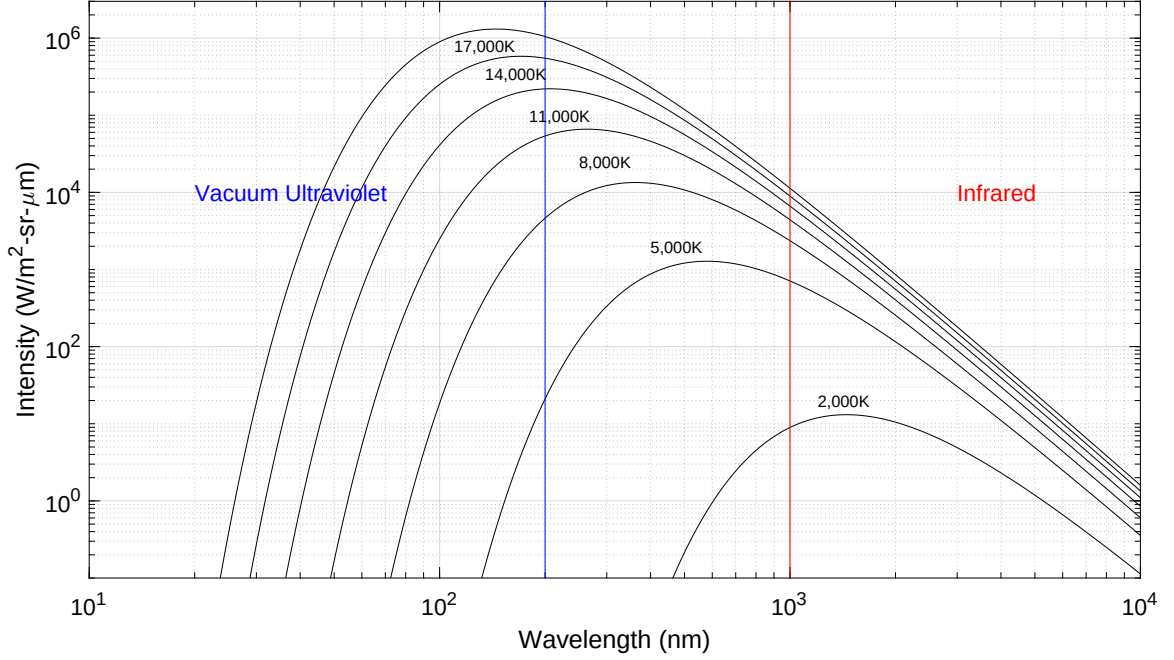


Figure 7.1: Planck blackbody emission wavelength distribution for various temperatures.

Typically, atmospheric entries occur at a gas pressure low enough that emitted radiation cannot be assumed in equilibrium and treated as a black-body, yet this assumption provides an upper limit for the emitted radiation over the whole spectral range. In general, the plasma is optically thin and the discrete radiation spectrum is in strong non-Boltzmann equilibrium state. The dominant spectral features will depend on the gas’s chemical composition and pressure, as well as the temperature (derived from the shock velocity). Entries faster than 7 km/s will usually ionize the gas and thus, emit radiation in the ultraviolet region. Slower entries can only excite the molecular internal vibration levels, which radiate in the infrared region. Figure 7.2 shows the most important emission regions for typical planetary entries, which is based on experimental data from previous shock tube campaigns by different teams all around the world. Data for Earth, Mars and Venus radiative transitions were taken from [390, 394], Bose et al. [151], and Cruden et al. [150], respectively. Data for Jupiter was adapted from the work of Cruden and Bogdanoff in [395]. Neptune data was taken from [396, 397]. Titan data was reported by Magin et al. [398].

As expected, the radiative features for these different classes of entry flows are quite rich, with a great deal of measured atomic transitions in the VUV region (120-200 nm), the Balmer series of H in the visible, and the O atomic lines in the near-IR region (at 777 nm and at 849 nm respectively). Molecular radiation features are also very rich, with many emitting systems from C₂, CH, CN and N₂ all over the visible range, and H₂, CO and NO emitting in the VUV range. For the IR, one typically observes emission from the different rovibrational bands of CO and CO₂ from 1.5 μm to over 5 μm. These spectral features may now guide us in selecting the appropriate measurement setup.

The spacecraft entry trajectory, as discussed in Chapter 1, provides us with the operational conditions,

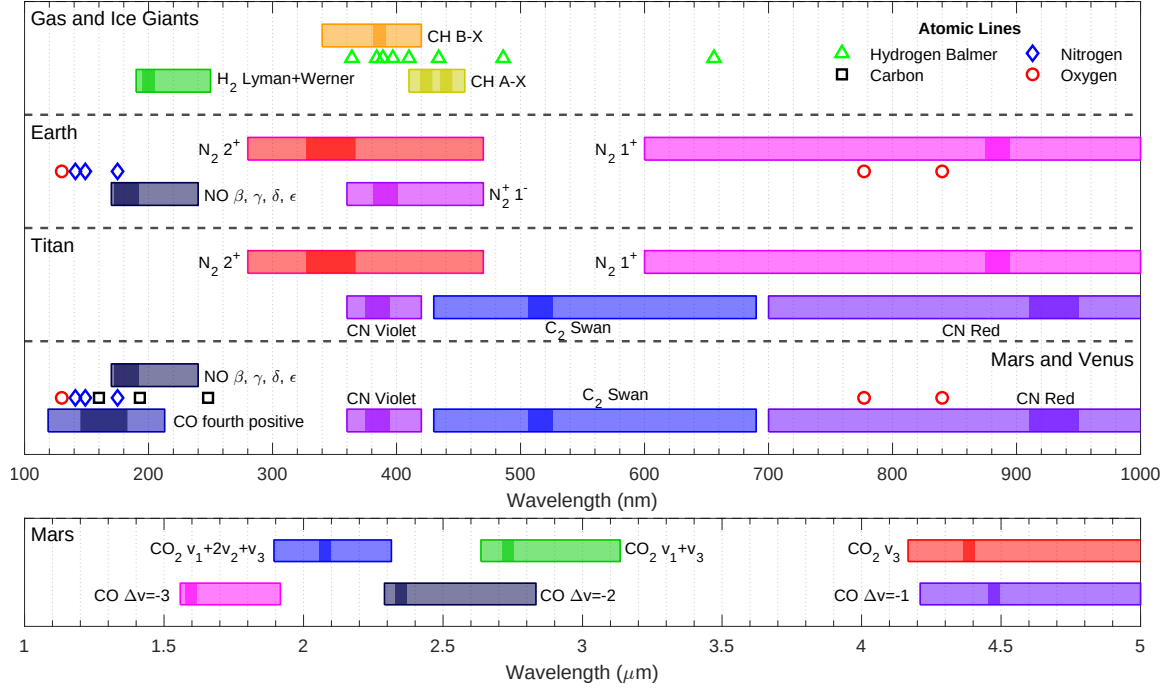


Figure 7.2: Emission region of different gas species in typical entry conditions for different planets. Observed atomic lines are explicitly presented for their measured wavelengths. Typically, molecular bands have strong bandhead peaks, with more diffuse tails in the neighbouring regions. To provide an estimate of the spectral regions where these bands may be emitting, we ran the in-house spectral line-by-line code SPARK [399, 400] with a representative temperature of $T = 5,000$ K. We represent the band heads with stronger colour and the tails with the same faded-out colour. The band heads/tail intensity ratio is taken as a factor of 100.

pre-shock pressure and target speed, for our setup, and more specifically Figure 7.2 provides us with the specific wavelengths of interest for the different gas mixtures considered in the testing. However, the wavelength alone is not sufficient for the specification of the equipment as one needs to consider as well the shock speeds. These will define the acquisition time, and in turn, the temporal/spatial resolution of the system. The design of the trigger system and the fast spectroscopy electronics is also influenced by the shock wave speed, the total time should be about $20 \mu\text{s}$ at worst (for maximum operational speeds).

Another two important parameters are the spectroscopic resolution and the sensitivity of the system. In terms of the spectroscopic resolution, typical spectroscopic systems which work in monochromator mode try to target the best possible resolution, as they are typically deployed in steady-state experiments where the wavelength can be slowly scanned and acquired (either via a photomultiplier or an intensified camera) can be tailored to be long enough that a good signal to noise ratio is achieved. An example of such a setup is described in [401]. In the case of a shock tube facility, we are collecting light trailing a moving shock wave for a few μs in an experiment that is typically one or few hours in the making. Not only a spectrograph setup (imaging the full spectral window in one sweep) is mandatory, but one needs to ensure that the maximum amount of light is collected during the passage of the shock wave. Therefore, there is the need to ensure that the setup collects the maximum possible amount of light, and the need to tailor the spectral window. On the latter, higher spectral resolutions yield lower spectral windows and vice-versa. The spectral resolution is determined by the gratings installed in the spectrograph, which can

be exchanged. The separation of different wavelengths is called the angular dispersion $\mathcal{D}$, and can be computed as the derivative of the reflection angle θ_m in respect to the wavelength λ ,

$$\mathcal{D} = \frac{d\theta_m}{d\lambda} = \frac{k}{d_G \cos \theta_m} \quad , \quad (7.1)$$

where d_G is the groove distance in the grating (inverse of the groove density). The reflection angles in Equation 7.1 can be calculated by,

$$d_G(\sin \theta_i + \sin \theta_m) = k\lambda \quad , \quad (7.2)$$

where k is the order of diffraction (1, 2, ...). For a set of d_G and $\theta_{i,m}$ multiple λ satisfy Equation 7.2. The lowest order solution, $k = 1$, corresponds to the longer wavelength λ_1 , with higher order solutions corresponding to $\lambda_k = \lambda_1/k$. The dispersion of the wavelength at the spectrograph focal plane, with focal distance F is computed through

$$\frac{dy}{d\lambda} = F\mathcal{D} = \frac{Fk}{d_G \cos \theta_m} \quad . \quad (7.3)$$

A first-order littrow blaze can be applied to the diffraction grating. It increases the intensity of the refraction order and wavelength of interest by tilting the grating surface to direct the light at a preferred angle. These governing equations are useful for defining which gratings will be best suited in terms of apparatus function and spectral window.

Broadly speaking, three different spectral regions (each with its own peculiar characteristics) are identified:

1. The Ultraviolet and Vacuum Ultraviolet regions where most of the radiation for high-speed entries should be emitted (see Figure 7.1). This region is bounded roughly between 120 nm (below which most windows become opaque) and 300 nm (where the visible region begins).
2. The near-UV to near-IR region in the 300–850 nm range, colloquially referred to as the visible (VIS) range, where many atomic and molecular systems are emissive for moderate entry speeds (see Figure 7.2).
3. The near-IR to mid-IR region (roughly in the 1–5 μm range) where rovibrational transitions between heteronuclear molecules such as CO_2 , CO and NO are strongly emissive. Radiation is observed in this spectral range for low-speed entries in planetary atmospheres with such gases in their composition (mostly Mars, for which the large impact of IR CO_2 radiation has been recently assessed [392, 402]).

Each of these three spectral regions requires its own setup. This means selecting an adequate spectrograph/camera combo. In terms of spectrographs and beyond the selection of appropriate gratings, one needs to account for the absorption of room air, which encompasses all the spectral ranges below roughly 200 nm, and specific bands in the IR (owing to the trace amounts of CO_2 and water vapour).

7. SPECTROSCOPY DIAGNOSTICS

This means that a VUV setup needs to be held in vacuum and an IR setup typically needs to be flushed in an inert non-IR absorbing gas such as Nitrogen. The camera itself needs to be sensitive to the spectral range of interest. Intensified high-speed cameras (iCCD) encompass all the spectral ranges of interest in this work, whereas streak cameras are limited to the UV-VUV and visible ranges. Streak cameras have an advantage over iCCD cameras in the sense that the temporal variations of light intensity at an imaged point are translated to a variation in image brightness along the streak direction. In contrast, iCCD cameras sample a line of points where the shock wave evolves over a given acquisition time, hence translating spatial points into time through the shock wave speed. These cameras are subject to motion blur whereas streak cameras are not. Nonetheless, the deployment of the latter for higher speeds is more arduous. Whereas IR is the only spectral region where streak cameras do not exist, the shock waves are slower and therefore the issue of motion blur is less critical.

The optimal setup selection is therefore schematized in Figure 7.3. The detailed specifications and requirements for these equipments, as defined by ESA, are presented in Table 7.1. The requirements were later related to the specification from suppliers, shown in Table 7.2, namely relating radiation intensity with signal-to-noise ratio, and the time for acquisition with the minimum integration time. The radiometric accuracy was related to the signal-to-noise ratio through $SNR = 1/\text{accuracy} = 1/0.05 = 20$. The minimum integration time is inversely proportional to the time for acquisition for the VUV setup. In the case of the IR setup, due to the nature of the fast iCCD camera, the minimum integration time is also related to the acceptable “smear”/blur of the image. A shock at 10 km/s crosses a 10 mm window in 1 μs , thus a sub- μs integration is needed. A more thorough discussion on this points is presented later in this Chapter,. Note that no specifications and requirements have been defined in the visible region, as the optical setup from the previous TCM2 shock tube is [389] reused.

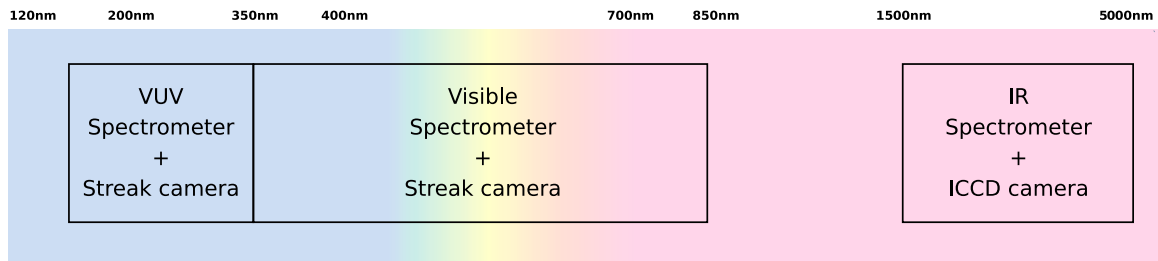


Figure 7.3: ESTHER instrumentation spectral range coverage

These specifications and requirements have been defined taking into consideration generic characteristics for spectroscopic setups in their spectral regions. These are understood to be adjustable for a shock tube configuration. In terms of spectral range, resolution and accuracy, most of the commercially available spectrograph equipment are compliant with the specified parameters. In turn, most of the streak and iCCD cameras are capable of achieving integration times lower than 1 μs . The specification of the signal-to-noise ratio will be more experience-dependent on the amount of radiation emitted by the shocked flow, which then will drive the requirements to minimize stray light and general noise from the acquisition system.

With this said, the assembly and deployment of a setup for detecting short bursts of light typical

Table 7.1: ESTHER spectroscopy specifications and requirements from ESA

UV-VUV Spectroscopy					
Equipment	Spectral range	Spectral Resolution	Spectral Accuracy	Time for Acquisition	Radiometric Accuracy
Spectrometer + Streak Camera	80–300 nm	0.1 nm @ 110 nm	0.01 nm	10 points in 10 μ s	< 5%
MWIR Spectroscopy					
Equipment	Spectral range	Spectral Resolution	Spectral Accuracy	Time for Acquisition	Radiometric Accuracy
Spectrometer + iCCD camera	1–6 μ m	0.5 nm @ 2.7 μ m	0.05 nm	10 points in 10 ms	< 5%

Table 7.2: ESTHER spectroscopy specifications and requirements updated with supplier information

UV-VUV Spectroscopy					
Equipment	Spectral range	Spectral Resolution	Accuracy	Integration time	S/N Ratio
Spectrometer + Streak Camera	80–350 nm	0.1 nm @ 110 nm	0.01 nm	$\leq 1 \mu$ s	>20
MWIR Spectroscopy					
Equipment	Spectral range	Spectral Resolution	Accuracy	Integration time	S/N Ratio
Spectrometer + iCCD camera	1–6 μ m	0.5 nm @ 2.7 μ m	0.05 nm	$\leq 1 \mu$ s	>20

of shock tube experiments entails a certain number of restrictions which immediately narrow down the range of compliance to the specifications and requirements of Table 7.2. Among others, one needs to account for window transmissivity; spectral response, range and sensitivity of the streak/iCCD camera photocathode/CMOS sensor, respectively; bit-rate for the high-speed electronics of the cameras; and availability of fibre-optics to connect the spectrograph to the shock tube optical windows. All these narrowed-down specifications and requirements are discussed in detail in section 7.2 (taking into account the spectral region, either UV-VUV or IR), except for the window transmissivity which is discussed here.

VUV light is easily absorbed by gas molecules and most optical materials such as quartz or glass. This mandates the VUV system to be held in vacuum to prevent the collected light from being absorbed by oxygen and nitrogen molecules. Using an optical fibre cable is also not possible, due to their strong attenuation below 300 nm. Alongside this, the optical window material should be as transparent as possible in the 100–300 nm range. Figure 7.4 and Table 7.3 show the optical transmissivity of different materials in the VUV region. Usual VUV windows are made of lithium fluoride (LiF) or magnesium fluoride (MgF_2), however, these materials cannot handle the force of the passing shock wave and will break after two or three experimental runs. For this reason, VUV-graded sapphire was the material chosen for the windows of the spectroscopy system. Regarding the NIR-MWIR spectral region, all the aforementioned optical window materials are essentially transparent up to 4–5 μ m, and there is the additional advantage that infrared optical fibres are available to "transport" the light signal from the

shock tube to the spectrometer. These have low attenuation in the infrared region [403] and do not require vacuum as oppose to the VUV wavelengths.

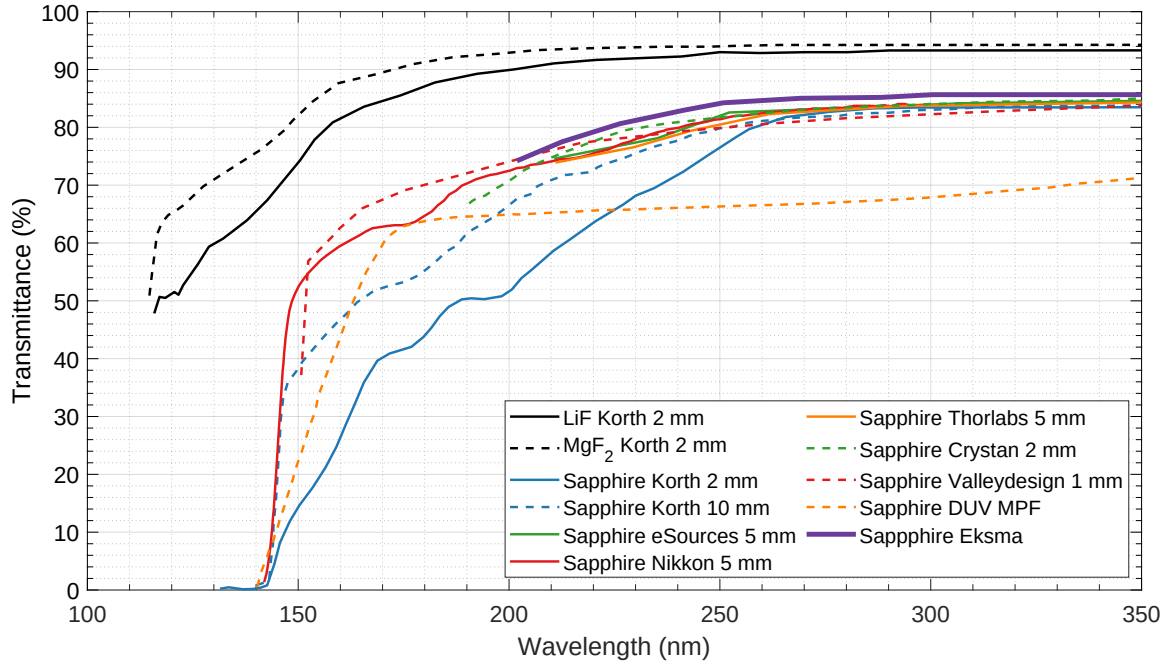


Figure 7.4: Optical transmissivity for common window materials, data adapted from [404–411]

Table 7.3: Comparison between different optical window materials for VUV spectroscopy.

Material	Knoop Hardness	Advantages	Disadvantages
Lithium Fluoride (LiF)	100 kg/mm ²	Large VUV-IR transmittance	Very brittle and expensive
Magnesium Fluoride (MgF ₂)	100 kg/mm ²	Large VUV-IR transmittance	Brittle and expensive
VUV-graded Sapphire)	1370 kg/mm ²	Cheap, resistant and some VUV capabilities	Limited transmittance window
Fused Quartz	741 kg/mm ²	Cheap and resistant	Limited transmittance window

7.2 Equipment

The specifications and requirements, as defined in the previous section, lead to the optical spectroscopy final setup for ESTHER, which is now discussed in detail:

- a UV-VUV spectroscopy setup covering the 120–350 nm range,
- a Visible spectroscopy setup covering the 350–850 nm range,
- a MWIR spectroscopy setup covering the 1–5 μm range,

where the spectral regions are bounded by the window transmissivity and grating efficiency of the UV-VUV setup, the streak camera input optics and photocathode sensitivity of the visible setup, and the

optical fibre transmissivity of the MWIR setup. For each of these, appropriate blazed gratings are selected, which ideally maximize throughput on a specific wavelength of interest while maintaining enough transmission efficiency over the whole spectral window. Lastly, finer/coarser gratings are selected to maximize spectral resolution/range over a single measurement, respectively.

7.2.1 UV-VUV Spectroscopy setup design and acceptance testing

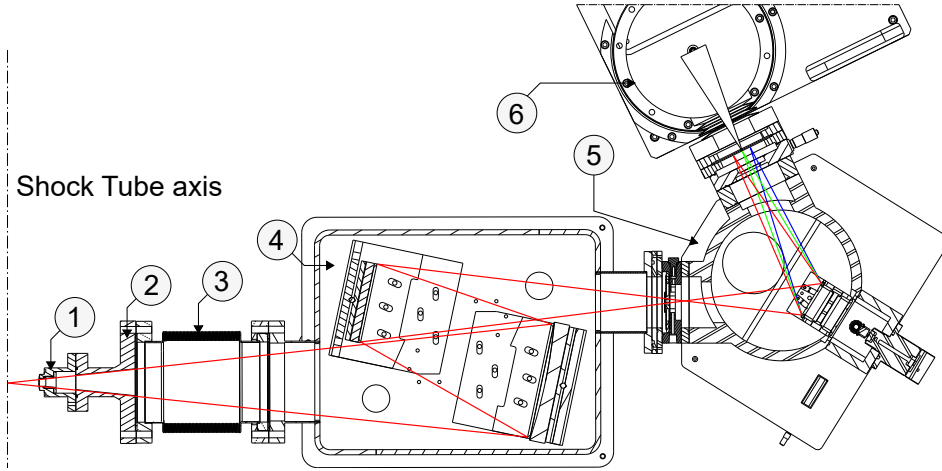
The selected equipment for UV-VUV spectroscopy setup is a McPherson Model 234/302 with an $f/4.5$ aperture and 200 mm focal length, coupled to a UV-VUV-customized Hamamatsu Streak Camera M10913-11, acceptance tested in November 2019. The use of a streak camera presents advantages over a simple iCCD camera. The most relevant is that the signal is time-resolved since the streak camera creates a time discrimination of the received light. However, increasing the number of elements on the optical path reduces the signal-to-noise ratio. Streak cameras are based on the photoelectric effect, thus they do not work with low-energy photons, namely in the infrared region. Therefore, any infrared spectroscopy instrumentation cannot make use of a streak camera.

Figure 7.5 shows the overall assembly of the UV-VUV spectroscopy system and a detailed view of the optical plug which connects the test section to the UV-VUV spectrometer. The design maximizes the collected light via a 12° light cone throughout the optical path. An optical plug with a VUV-graded sapphire window is held in place and vacuum sealed using one FKM O-ring and is connected to a collection optics box by flexible bellows¹. The optics box is comprised of two mirrors, which collect and focus the totality of the light cone into the spectrograph, hence maximizing the signal throughput. The spectrograph is in turn connected to the streak camera optical relay, which focuses the light into the photocathode that converts the photons into electrons and deflects them using a saw-tooth signal. The streak camera is coupled to an iCCD camera (model ORCA-Flash4.0 C13440-20CU), which records the final time- and wavelength-dependent signal. Figure 7.6 depicts a photograph of the UV-VUV spectroscopy setup.

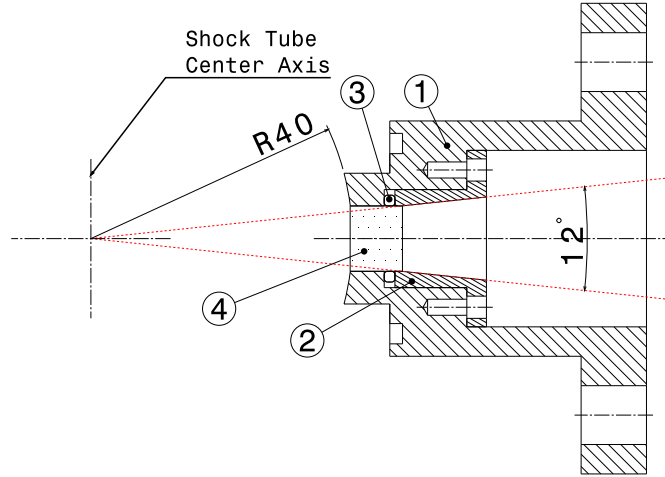
The spectrograph has been equipped with two different diffraction gratings of 600 and 1,200 grooves per millimeter (gr/mm) blazed at 150 nm, ensuring an adequate throughput in the 120–350 nm spectral range. Validation tests for the setup were conducted with a mercury vapour lamp and a laser impulse generator. First, the equipment was calibrated with the 1,200 gr/mm grating using the Hg 253.65 nm line. The centre pixel is located at number 628, with a total range of 782 pixels. Using the 1,200 gr/mm grating a spectral range of 10 nm can be observed by the streak camera. The spectral dispersion is $10 \text{ nm}/782 \text{ pixels} = 0.0128 \text{ nm/pixel}$. The line FWHM at three different points of the photocathode was computed directly by the streak camera computer program to determine the wavelength resolution. Using the coarse grating, the centre location was found to be pixel 618. The spectral range is now doubled to 20 nm with 803 pixels, thus giving a spectral dispersion of $20 \text{ nm}/803 \text{ pixel} = 0.0249 \text{ nm/pixel}$. The wavelength resolution values are presented in Table 7.4 and an example for the 1,200 gr/mm grating is depicted in Figure 7.7 (top).

The accuracy and reproducibility were tested by measuring the peak 10 times while sweeping the

¹which allows connecting the shock tube, standing over a seismic slab, to the optics table standing in the experimental hall floor



(a) 1-Shock tube plug; 2-Connection piece; 3-Adjustable Bellows; 4-Optics collection box; 5-Spectrograph with diffraction grating; 6-Connection to streak and CCD camera.



(b) 1-Plug; 2-Window tightener piece; 3-FKM O-ring; 4-Sapphire window.

Figure 7.5: ESTHER UV-VUV spectroscopic setup design schematic. General design (a) and optical plug detail (b)

camera's spectral centre position. The average centre position was found to be at 632.5 ± 4.5 pixels. Lastly, the signal-to-noise ratio was evaluated by the ratio between the peak intensity (513.79) to the noise standard deviation (0.15), yielding a signal-to-noise ratio of $513.79/0.15 \simeq 3400$. The test values for reproducibility and signal-to-noise are depicted in Figure 7.7 middle and bottom, respectively. The results from the UV-VUV acceptance campaign were in conformity with the requirements. However, the temporary unavailability of the vacuum pumps prevented testing the equipment with the deuterium line at 110 nm or the mercury line at 237.83 nm. The streak camera sampling rate was tested with a picosecond laser. Table 7.5 presents the outline of the UV-VUV acceptance campaign.

7.2.2 Visible Spectroscopy setup description

The visible spectroscopy setup used here was recovered from the previous TCM2 shock tube facility in Marseille, France and is only be briefly outlined here (see Figure 7.8). The setup has been used

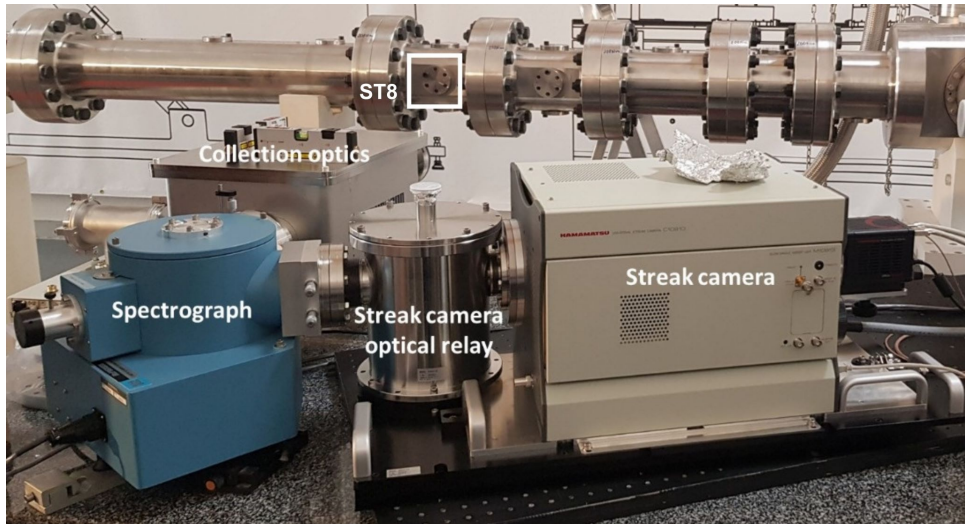


Figure 7.6: ESTHER UV-VUV spectroscopic setup during acceptance testing, ST8 optical port mark in photo .

Table 7.4: Calibration results for the 1,200 gr/mm and 600 gr/mm gratings, Hg 253.65 nm line, with FWHM in pixels.

1,200 gr/mm			600 gr/mm		
Position	Resolution (pixel)	Resolution (nm)	Position	Resolution (pixel)	Resolution (nm)
Left	28	0.36	Left	18.9	0.47
Centre	17.8	0.23	Centre	24.6	0.61
Right	25.1	0.32	Right	46.8	1.17

previously in a series of spectroscopic studies at the aforementioned facility [11, 13, 389, 412]. It is comprised of a 0.64 m Czerny-Turner Jobin-Yvon HR640 spectrograph of focal length $f/5.2$ using the following gratings: two low-resolution large-window 600 and 1,200 gr/mm gratings blazed at 500 nm, and two high-resolution narrow-window 2,400 and 3,000 gr/mm gratings blazed at 330 nm, and at the 250–550 nm region, respectively. The spectrograph is coupled to a visible-range Hamamatsu streak camera comprised of an M1953 slow-speed streak sweep unit and a universal temporal disperser C1587. The streak tube is the YD2369/N1643-01 with a spectral range 200–850 nm and input optics model A1975 with a spectral range of 350–850 nm. A CCD camera (Hamamatsu C4880) is used to record the spectral-time images.

7.2.3 MWIR Spectroscopy setup design

The infrared spectroscopy setup is currently in the late definition phase. The preselection of its main components is being carried out, namely an optical fibre cable transparent in the MWIR, a spectrograph

Table 7.5: ESTHER spectroscopy acceptance campaign results

–	Resolution 1,200 gr/mm	Resolution 600 gr/mm	Accuracy	S/N Ratio
Specs. & Req.	<0.25 nm	<0.50 nm	<5 pixels	>100
Test	0.23 nm @ 253.65 nm	0.47 nm @ 253.65 nm	4.5 pixels	3425

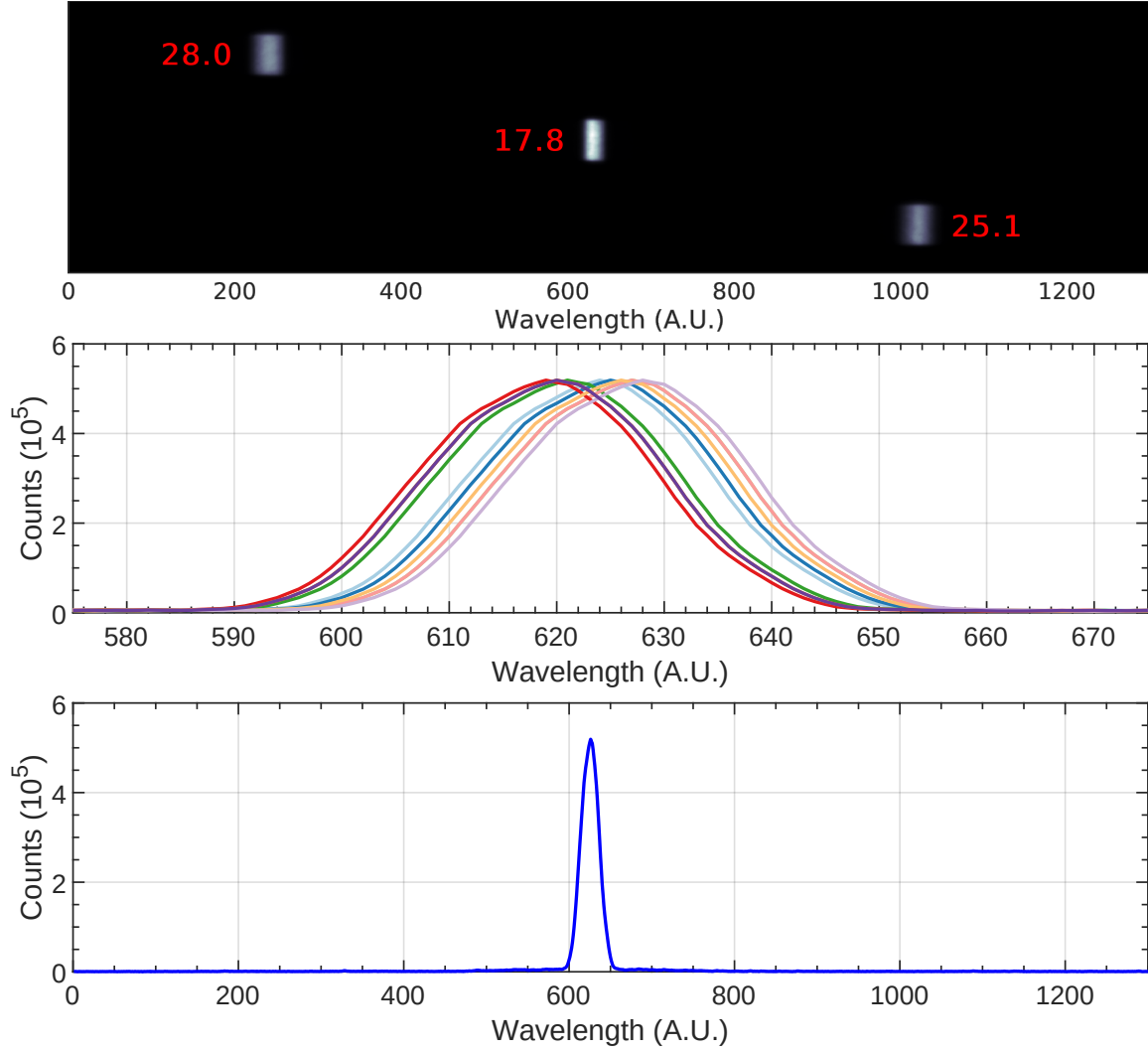


Figure 7.7: ESTHER UV-VUV spectroscopic setup acceptance campaign results. Wavelength resolution in pixels for the 1,200 grooves/mm grating (top); Repeatability and accuracy (middle); and Signal-to-Noise ratio (bottom).

tailored for this spectral region, and a fast IR iCCD camera. The possibility of using an optical fibre cable connected to the shock tube allows for significantly more flexibility in the setup, compared to the UV-VUV system, which requires a bulky, vacuum-purged physical connection between the shock tube and the spectroscopy setup. Figure 7.9 depicts the optical plug scheme of the aforementioned setup. The optical fibre (4) is tightened to a part (3) which compresses the copper rings and seals the optical window (2), and it is connected on the other end to the spectrometer and the high-speed camera. This design is inspired by the shock detection system of the X2 facility at the University of Queensland, where the optical fibre is connected to a photodetector [183].

Table 7.6 shows the pre-selected equipment for the infrared spectroscopy setup. Optical fibre cables can either be single or multimode. The former has a higher peak transmittance but a narrow window, while the latter has a lower peak transmittance but a broader transmission band. A large core-diameter fibre is also desirable to augment the collected light and provide an adequate signal-to-noise ratio. However, it may distort the signal over time due to possible differences in the travel distance between two



Figure 7.8: Visible range spectroscopy setup

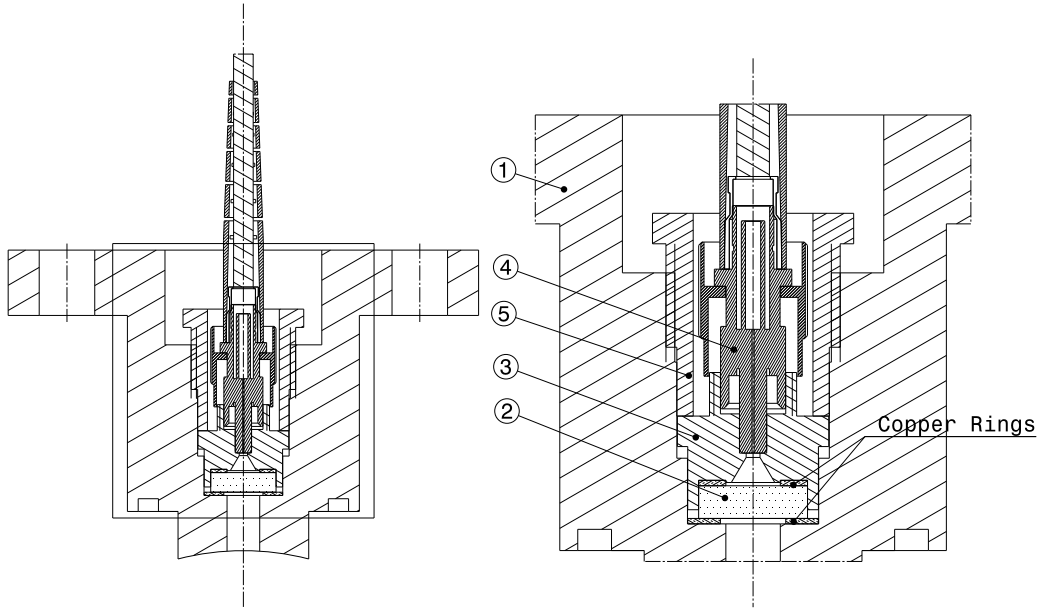


Figure 7.9: Optical plug for infrared spectroscopy diagnostics. 1-Plug; 2-Sapphire window; 3-Optical fibre connector; 4-Optical fibre cable; 5-Threaded screw tightening the window and optical fibre connector.

photons. All the fibre patches are to be mounted with a ferrule connector on both the spectrograph and shock tube sides. Due to our broad spectral window, the chosen fibre must be a multimode one. Another important parameter is the acceptance angle θ_a , which determines the maximum angle a light ray can have to the optical fibre axis to be accepted by it. This relates to the numerical aperture (NA) of the chosen fibre through Equation 7.4. The total acceptance angle 2θ is used as an “effective” field-of-view of the optical fibre.

$$\theta_a = \arcsin NA \quad (7.4)$$

For our case, Indium fluoride core fibres have a slightly broader transmission band when compared to zirconium fluoride (ZrF_4), despite the better transmissivity of the latter in the 2 to 3.6 μm region [403]. Both Thorlabs and Le Verre Fluore propose fibres made of Indium trifluoride (InF_3) which present good

7. SPECTROSCOPY DIAGNOSTICS

transmittance in the 1 to 5 μm region. The CIR fibre from Art photonics has a well in the transmittance near 4 μm , and thus the overall signal-to-noise ratio would be lower than for the InF_3 fibres. The selected optical fibre is thus a multimode indium fluorite (InF_3) with a 200 μm core and 2 meters of length.

Table 7.6: Infrared spectroscopy optical fibre pre-selection equipment

Optical fibre	Material	Spectral range (μm)	Core Diam. (μm)	$2\theta_a$ (deg)	Attenuation (dB/m)	Transmissivity
Thorlabs MF22L2	InF_3	0.310–5.5	200	30.14	<0.25 [2–4 μm]	88%
Thorlabs MZ22L2	ZrF_4	0.285–5.5	200	23.07	<0.20 [2–3.5 μm]	91%
LeVerreFluore IFG MM 200/260	InF_3	0.310–5.5	200	23.07	<0.01 @ 3.5 μm	$>81\%$
Guiding Photonics Mid-IR	Glass	2–16	200		4	45%
Art Photonics CIR250/300	Chalcogenide	1–5.5	250	34.92	0.3 [1–4 μm]	42%

Similarly to the UV-VUV and visible setups, an imaging spectrograph is needed. Table 7.7 presents popular equipments capable of working in the IR region. These are essentially spectrographs similar to the ones used in the visible range, except they have the possibility of being vacuumed or flushed with inert/dry gases, hence avoiding room air absorption from $\text{CO}_2/\text{H}_2\text{O}$ traces. Longer focal length spectrographs yield larger wavelength separations; however, the spectral window narrows down accordingly. Besides this, wavelength resolution, accuracy, and window will naturally also depend on the selected grating.

The selected imaging spectrograph is a McPherson 2035 equipped with two different diffraction gratings. A finer one with 300 grooves/mm blazed at 2.5 μm for higher resolution, and a coarse one with 17.5 μm blazed at 4.2 μm for a larger spectral window. The setup characteristics for coarse, broadband measurements are very close to the ones of the EAST shock tube at NASA Ames, which has provided good quality data for CO_2 shocked flows in this spectral region for Venus and Mars atmospheric entries [133]. For the high-resolution grating, the specifications were taken considering an optimized spectral region where CO and CO_2 bands may be observed with little interference. The 2–3 μm region is attractive for this purpose as shown by a spectral simulation for equilibrium radiation of CO and CO_2 at 3,000 K equilibrium temperature, with an apparatus function of 0.2 nm, carried out with the SPARK Line-by-Line code [399, 400]. Figure 7.10 shows the spectral features for these bands. CO rotational bands should be well-defined and easy to probe in the 2.3–2.6 μm region, whereas CO_2 bands would be prominent in the 2.65–2.9 μm region. The rotational features, allowing determining characteristic temperatures, are evident (see details in Figure 7.10).

Finally, a group of infrared cameras was also pre-selected for the setup. Table 7.8 lists the various cameras identified together with the corresponding specifications. A low minimum integrating time is desired to avoid excessive smearing of the image in the iCCD since, as previously discussed in section 7.1, temporal discrimination of the infrared signal cannot be performed with a streak camera. A short integration time results in minimal smearing of the moving radiation signal, however, short integration

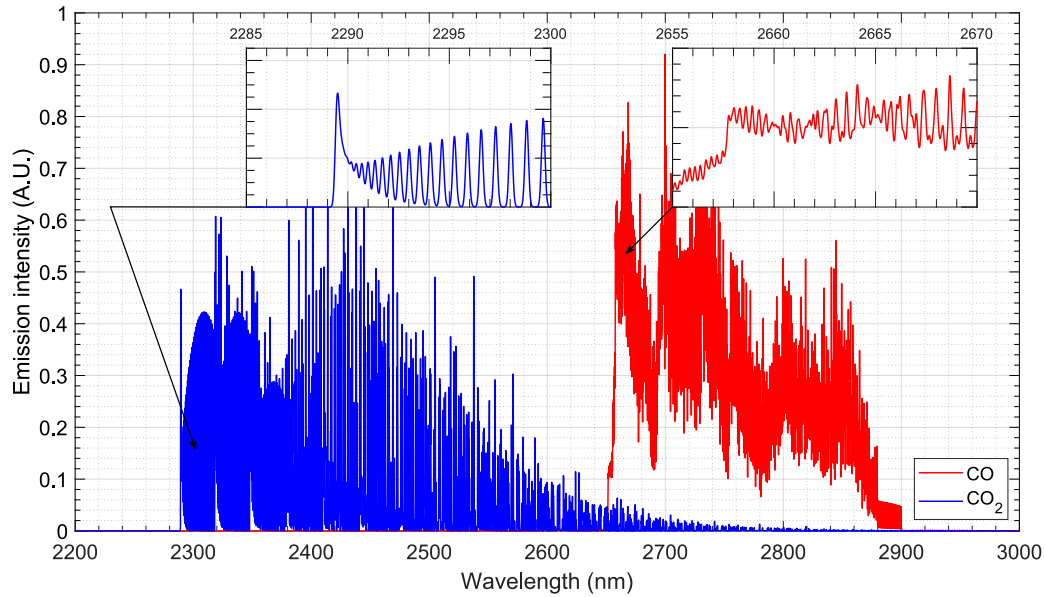


Figure 7.10: Synthetic spectrum for CO (blue) and CO₂ (red) radiation in the IR. The spectrum is normalized for both radiative systems and assumes an equilibrium temperature of 3,000 K and an apparatus function of 0.2 nm.

times decrease the signal-to-noise ratio. Due to the fast-moving shock wave (up to 6 km/s), a 1 μ s integration time is equivalent to a spatial smear of 6 mm in the worst-case scenario. Higher-resolution cameras have more pixels allowing for a higher spectral resolution without sacrificing the width of the spectral window. Similarly, a higher temporal/spatial resolution can be achieved by increasing the pixel density. A characteristic that is not relevant to our experiment is the acquisition speed/full frame rate. The full frame rate of a fast iCCD camera is limited by the bandwidth of the camera electronics, not by the CCD sensor itself. Faster frame rates may be achieved at the cost of trimming pixels of the camera sensor, thus taking pictures with lower resolution. However, due to the nature of the shock tube experiments, only the first frame is relevant, as it captures the whole useful run time up to the contact wave, and therefore this is not a concern for our application.

The spectral cameras of interest exhibit similar characteristics, with the cost-driven primarily by the capability of the equipment electronics to achieve high-resolution, high-frequency framerates. Since this is of no concern for our application, the final selection criteria will be likely driven by equipment cost.

7. SPECTROSCOPY DIAGNOSTICS

Table 7.7: Infrared spectroscopy spectrograph pre-selection equipment

Spectrograph	Focal Length	Aperture Number	Grating [Blazing]	Resolution (nm)
McPherson 2035	350 mm	$f/4.8$	300 gr/mm [2 μm]	0.2 @ 312.6 nm
			20 gr/mm [3.7 μm]	3.0 @ 312.6 nm
McPherson 207	670 mm	$f/4.7$	300 gr/mm [2 μm]	0.16 @ 312.6 nm
			20 gr/mm [3.7 μm]	2.04 @ 312.6 nm
McPherson 2061	1000 mm	$f/7$	300 gr/mm [2 μm]	0.07 @ 312.6 nm
			20 gr/mm [3.7 μm]	1.05 @ 312.6 nm
McPherson 209	1330 mm	$f/4.7$	300 gr/mm [2 μm]	0.04 @ 312.6 nm
			20 gr/mm [3.7 μm]	0.6 @ 312.6 nm
Princeton HRS-300	300 mm	$f/3.9$	300 gr/mm [2 μm]	0.4 @ 500 nm
			50 gr/mm [0.6 μm]	2.4 @ 500 nm
Princeton HRS-750	750 mm	$f/9.7$	300 gr/mm [2 μm]	0.16 @ 500 nm
			50 gr/mm [0.6 μm]	0.96 @ 500 nm

Table 7.8: Infrared spectroscopy fast iCCD cameras characteristics.

Fast Infrared Camera	Aperture Number	Resolution	Min. Integration Time (μs)	NETD* (mK)
Flir X8580	$f/2.5$	1280x1024	0.27	≤ 30
Flir X6980	$f/2.5$	640x512	0.27	≤ 30
Infratec Image IR 9400	$f/2.2$	640x512	0.10	< 30 @ 30°C
Telops FAST M1K	$f/2.5$	640x512	0.27	≤ 25
Telops FAST M200hd	$f/3$	1280x1024	0.50	≤ 20
Telops FAST M100hd	$f/3$	1280x1024	0.50	≤ 20
Tigris 640 InSb BB	$f/3$	640x512	NA	≤ 25

* The Noise Equivalent Temperature Different (NETD) is the parameter that regulates the signal photon resolution

Chapter 8

Microwave Diagnostics

“Nihil obstat, imprimi potest”

- Known Author

Here the design and manufacturing of a compact microwave reflectometer/interferometer diagnostic for electron density measurements is presented and discussed. The specifications and requirements for adapting this technique to atmospheric entry plasmas applications, and a review of past *in situ* electron density measurement missions and tests are presented. Lastly, the general architecture for a compact microwave diagnostic and the initial proof-of-concept test that was carried out are reported and discussed.

8.1 Introduction

Electron density is one of the keys for the comprehension and understanding of every kind of plasma, and this includes atmospheric entry plasmas. Although these have typically low ionization degrees (around 10^{-4} – 10^{-2}), electrons still play an important role in the kinetic and radiative processes of these high-speed, reactive flows, namely through electron-impact excitation reactions. Historically, electrostatic (Langmuir) probes are relatively simple diagnostics that have been successfully deployed for the measurement of such electron densities and temperatures [265]. However they suffer from a few drawbacks. The most important being that these are intrusive diagnostics that perturb the flow in the measurement region. An alternative is to use non-intrusive techniques such as microwave interferometry/reflectometry.

The electron density also affects communications blackout, and therefore, influences the positioning of communication antennas. Finally, knowledge of the spatial distribution of electron densities is relevant for the validation of aerothermodynamic codes deployed for the prediction of the aerodynamic forces and thermal heat fluxes sustained by an entry spacecraft. Information about the heavy species excitation states is also important for the design of TPS [413], as heavy species will heat the wall through convective and radiative heat transfers, possibly endangering the spacecraft if such fluxes exceed the engineering limits [147]. The electron density may be inferred either through spectroscopic methods (Stark broadening techniques [157, 414]) or through microwave interferometry techniques. Although these are, to date, not as developed as the formerly discussed optical spectroscopy diagnostics, several proofs of concept for such

diagnostics have been established, and one may expect such techniques to be more vigorously deployed in the near future [415].

Microwave reflectometry is a radar-like technique mostly based on the dispersive properties of the medium where electromagnetic waves propagate. The technique was originally employed to measure the ionospheric structure [416, 417] and found a large range of applications in fusion plasma diagnostics following a rapid development in the mid-1980s [418–420]. Reflectometry is well suited for measurements of both equilibrium and fluctuating components of several plasma parameters, such as electron density. A review of several microwave-based techniques for plasma diagnostics can be found in [421]. A comprehensive description of reflectometry fundamentals can be found in [265] while a review of the technique can be found in [422]. An overview of present-day diagnostic systems and capabilities is found in [423]. In particular, the electron density profile can be reconstructed if electromagnetic (EM) waves are reflected in the plasma where a cutoff condition is met. Note that relativistic effects may appear for electron temperatures above 1 keV [424], however, this is not the case for re-entry plasmas where the electrons are thermalized in the absence of external electromagnetic fields. In microwave interferometry, an EM wave probes the plasma gaining a phase delay when compared to the vacuum propagation. This phase delay is related to the plasma refractive index, that in turn, is a function of the plasma properties, namely its electron density and magnetic field.

8.2 Theory and Methods

A probing signal $S_{src} = A_{src} \cos(\omega t)$ is sent to the plasma via a waveguide/antenna, and it propagates until a layer with refraction index $\mathcal{N}(\mathbf{x})$ equal zero is found, where it reflects. The received signal $S_{rcv} = A_{rcv} \cos(\omega t + \phi)$ differs with respect to S_{src} in both amplitude and phase ϕ . The phase is decomposed as $\phi = \phi_u + \phi_0 + \phi_p$, which represents the propagation in the waveguide, vacuum and plasma, respectively. The waveguide phase can be known through calibration, the vacuum phase is easily estimated, and finally, plasma information can be inquired from the last term.

The refraction index can be related to the electron density $n_e(\mathbf{x})$ through the Altar-Appleton equation [425]. Particular relevance for the hypersonic atmospheric re-entries is the so-called O-mode, where a wave's electric field is parallel to the exterior/imposed magnetic field or when the latter is non-existent. For O-mode propagation, ϕ_p is a function of the wave's vacuum frequency f and of the refractive index $\mathcal{N}(\mathbf{x})$, where the latter depends only on the electron density, and collisions between electrons and heavy particles are neglected, thus $\phi_p[f, \mathcal{N}(n_e)]$. In practical terms, two configurations may be used to probe the electron density of a plasma: Reflectometer and interferometer.

The plasma cutoff frequency f_{pe} is the minimum value at which an electromagnetic wave can propagate in a plasma. As stated, in the O-mode it simply relates to the critical electron density $n_{e,p}$ via Equation 8.1 [265], where ε_0 , e and m_e are the vacuum permittivity, the electron charge and mass, respectively:

$$f_{pe} = \frac{\omega_{pe}}{2\pi} = \frac{1}{2\pi} \sqrt{\frac{n_{e,p} e^2}{m_e \varepsilon_0}} \quad . \quad (8.1)$$

Where in the case of a reflectometer, the probing frequencies should be around f_{pe} , in an interferometer these must completely cross, thus $f > f_{pe}$ the plasma. The phase shift gain by crossing the plasma is given by Equation 8.2, where D is the plasma thickness, and ω_{pe} and ω are the plasma oscillation and probing angular wave frequencies (from [265]), respectively:

$$\Delta\phi = \frac{2\pi}{\lambda} \int_0^D (1 - \mathcal{N}) dl = \frac{2\pi}{\lambda} \int_0^D \left(1 - \sqrt{1 - \omega_{pe}^2/\omega^2}\right) dl \quad . \quad (8.2)$$

Using Equation 8.1, $\lambda = c/f$, and ($\omega \gg \omega_{pe}$), Equation 8.2 can be simplified into

$$\Delta\phi \simeq \frac{e^2}{4\pi cm_e \epsilon_0 f} \int_0^D n_e dl = \frac{e^2}{4\pi cm_e \epsilon_0} \frac{\bar{n}_e D}{f} \quad , \quad (8.3)$$

where $\bar{n}_e$ is the average electron density over the plasma path. This integrated value is a very good approximation of shock tube measurements, as the plasma can be approximated by a disk whose properties change only in the longitudinal direction. Optimally, the working frequency should be high enough to transverse the plasma and guarantee $D/\lambda > 3$. However, if it is too high (small λ), the phase gained may be too small and too difficult to measure. Alongside working as an interferometer, the equipment base should be convertible to a reflectometer to be mounted on a small spacecraft.

Figure 8.1 shows the differences between the two methods. In reflectometry, the EM wave reflects when encountering a region whose electron density is above a critical value n_e . By performing a frequency sweep covering the relevant range of critical electron density values, the spatial electron density profile can be drawn out. The selected probing frequencies must be around the cutoff frequency for the expected peak electron density. In interferometry, an EM wave with a frequency higher than the plasma cutoff frequency passes through the plasma gaining a phase delay compared to the vacuum, from which an average electron density can be calculated. Since this is an integrated signal, it must be post-processed to create a real plasma profile, via Abel-inversion or “onion-peel” techniques. The chosen frequencies should be well above the critical n_e to prevent unwanted reflections. Both techniques can be used in mono- or bistatic configurations, in the first configuration one antenna is used for both emitting and receiving the EM waves, in the latter, emission and reception are performed by a dedicated antenna. In the case of monostatic interferometry, the probe signal reflects at the wall, so the wave passes through the plasma twice, and these phase delays must be taken into account. In bistatic reflectometry, the emitted EM wave “hits” the plasma at an angle, so a second antenna is required for the reception. This phenomenon may also occur due to the plasma profile refractive index, which may not reflect the EM wave back to the emitting antenna.

The complex plasma-wave interactions in turbulent media have driven the continuous development of both analytical theory and sophisticated full-wave numerical codes to support the interpretation of reflectometry data. Typical analytical solutions require simplifications and approximations of the plasma parameters. Figure 8.2 compares a laminar to a turbulent plasma. Turbulence can originate effects such as: multi-scattering; local resonances; secondary cutoffs; beam splitting and 5-wave trapping. These will scatter the emitted EM waves, lowering the received power and complicating the signal interpretation. A full-wave numerical code may be required to properly design and validate microwave diagnostics, namely

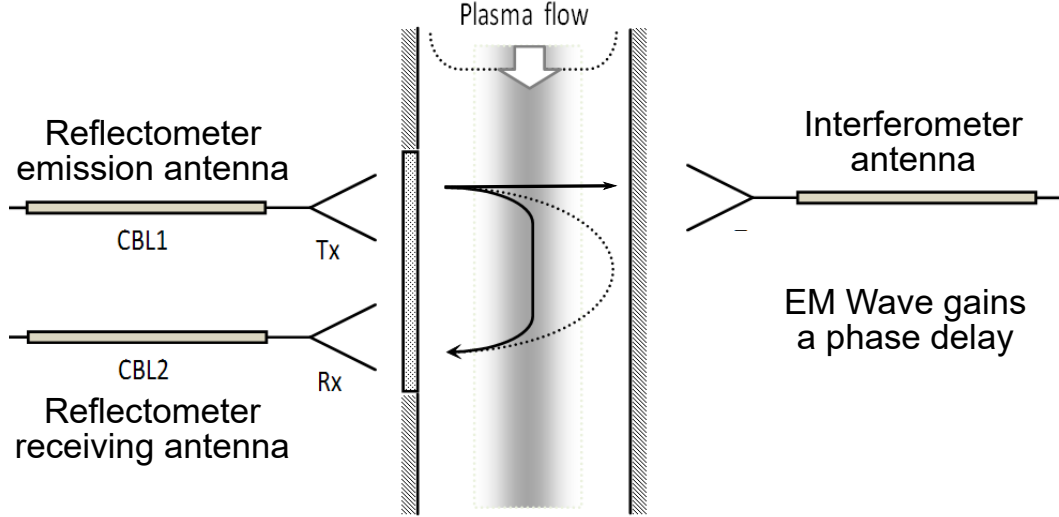


Figure 8.1: Differences between the reflectometer and interferometer methodology to probe electron density of a plasma.

a reflectometer. To overcome many shortcomings of the analytical descriptions while modelling any real-sized geometry, the two-dimensional finite-difference time-domain (FDTD) full-wave code REFMUL [426] is used to simulate wave propagation while retaining fundamental physics.

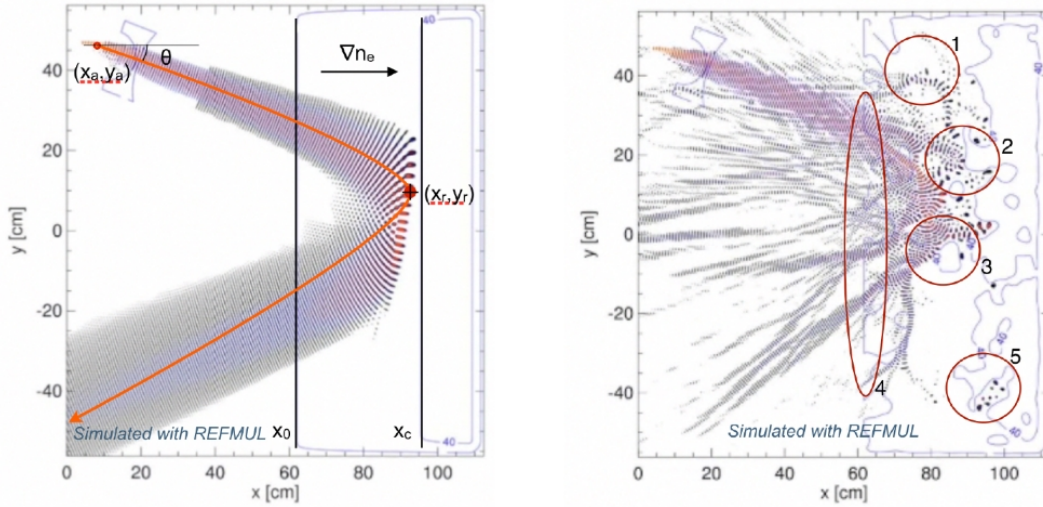


Figure 8.2: Simulation with REFMUL for a non-turbulent plasma (left) and a turbulent plasma (right), taken from F. Silva in [427].

8.2.1 Specific challenges for entry applications

It is important to properly address the specific features of entry plasmas, compared to larger-scale plasmas, such as in fusion applications. The usual electron density profile of a fusion plasma has a positive gradient towards the centre, a radial-like profile. This means that the plasma has a low electron density near the reactor walls and antenna position, and high electron density at the centre. Such a trend helps create a density profile, since as the probing signal frequency increases, so does the reflection point

distance. A density vs. distance profile may be easily drafted from the reflectometer data, as the device sweeps the plasma at different frequencies and collects the reflected signal.

This is not so for atmospheric entry plasmas that typically showcase a relatively steep gradient at the boundary layer, with a decrease of density away from the spacecraft wall. As an illustration, we present in Figure 8.3 a simulation of the RAM-C II flight experiment, launched in 1968, which has been performed within our group in the recent past [428]. In the stagnation-line region, we have a shock wave lying very close to the spacecraft surface (1 cm), with a peak electron density in the shock-layer, and a steep decrease in the boundary layer, owing to catalytic wall recombination reactions. In the flow wake, the shock-standoff distance is up to 15 cm, with much lower densities in the post-shock region where flow velocities increase to supersonic conditions. This leads to a higher global density gradient towards the boundary layer, and we may even witness an increase in electron densities near the wall, despite losses from catalytic recombination. Such density profile accordingly becomes “counter-intuitive” to typical reflectometer applications where the antenna is closer to the lower density side. This limits the capability for tracing the electron density profile beyond the first peak since a full profile cannot be obtained, as the reflectometer is essentially “blind” beyond the higher electron density peak. A more detailed discussion on the RAM-C II test case will be presented in section 8.3.1.

A further problem arises from the typical dimensions of atmospheric entry plasmas. Most vehicles have dimensions which are lower than those of larger tokamaks, namely the vehicles for robotic exploration which may be less than 1 m in diameter. Only larger man-rated vehicles such as the Space shuttle or larger capsules have sizes significantly larger than most tokamaks. Additionally, the plasma flow is severely constricted between the bow shock wave and the spacecraft surface, with a thin shock and boundary layer lying very close to the wall, and a larger plume in the wake. This means that typical shock standoff lengths may be in the order of the cm, compared with the characteristic lengths of the larger tokamaks, which may exceed 1 m. Such small scales yield a few problems of their own: The probe signal is reflected very shortly after the antenna exit, which hinders a reliable measurement, and the plasma length scale is similar to the reflection layer, meaning an accurate reflection position cannot be inferred. The smaller spacecraft scales further induce a few other practical issues: reflectometer needs to be light and compact since weight and volume are a premium in space missions, and commonly used horn antennas may also be too large to be easily integrated into a spacecraft and there is the further risk of destruction due to direct contact with the plasma [429].

Last but not least, typical electron densities differ among both applications. Typical electron densities range between 10^{17} – 10^{21} el./m³ in tokamaks, and 10^{16} – 10^{22} el./m³ in atmospheric entry flows. This means that different frequency bands will be relevant for each application, and the spatial accuracy will also differ (since it depends on a first approach on the wavelength of the corresponding frequency bands).

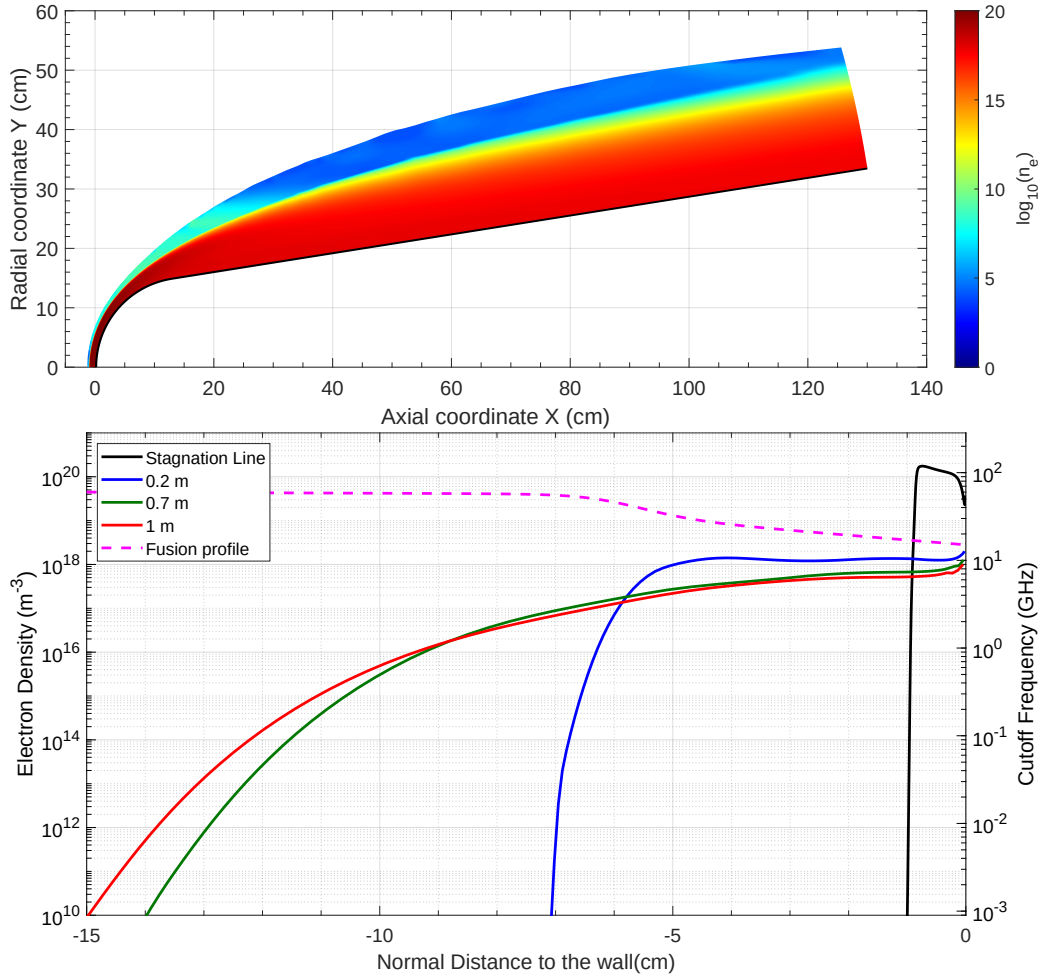


Figure 8.3: Top: Navier-Stokes simulation for RAM-C II, electron density at 61 km altitude. Bottom: Electron density profiles normal to the wall at the antenna’s location and stagnation line. A typical electron density profile from a tokamak fusion plasma is added for comparison purposes, from [430]. The RAM-C II simulations data is taken from [428].

8.3 State-of-the-art

8.3.1 Inflight experiments

To date, a total of five in-flight experiments with *in situ* measurements of the electron density/temperature were designed over the last 60 years. These include, in chronological order, the RAM program in the 60’s and MRIS in 1989 by NASA, OREX and HYFLEX in 1994 and 1996 by JAXA and ARD in 1998 by ESA. The RAM program ran between 1960 and 1970, and MRIS was halted during the engineering phase.

RAM-C II

The “Radio Attenuation Measurements” (RAM) program was designed by the NASA Langley Research Center and the U.S. Department of Defence. The motivation of this program was to increase the understanding of blackout phenomena and find ways to minimize resulting in-flight problems [431].

Since calculations for the aft (rear) plasma profile require knowledge of the gas history as well as local chemistry, from the shock entry point to the aft point, the RAM program was also designed to validate and help improve computational models and the understanding of plasma formation in both nose and aft body regions. The program was comprised of a theoretical and an experimental part, the latter with tests in ground facilities followed by in-flight experiments (RAM C). Three flight experiments were performed with a hemisphere-cone body at a velocity of 7620 m/s to measure plasma parameters and test radio-attenuation alleviation techniques. Only the second flight (RAM C-II in 1968) was equipped with a reflectometer.

Reflectometer Equipment

Four-stage solid-fuel Scout vehicles were used to launch the RAM C-II body up to an apogee greater than 220.5 km, then propelling it back into Earth's atmosphere. The spacecraft was spin-stabilized prior to the motor ignition reentering with a nominal flight-angle path of -15° , reaching a maximum velocity of 7,678 m/s at 67.0 km of altitude. The payload geometry was 6-in. (approximately 15 cm) radius hemispherical nose followed by a 9° half-angle cone and had an overall length of 51-in. (about 130 cm). The payload had a Teflon afterbody and a beryllium heat sink nose cap, with a mass before the launch of 121.8 kg. The beryllium cap and Teflon body were found not to release significant amounts of alkaline impurities, fulfilling its purpose of avoiding contamination so the measurements could be compared with pure-air models. All RAM C flights had negatively biased Langmuir probes, which will not be discussed here.

A four-frequency microwave reflectometer was put into the RAM C-II payload to infer the peak electron density time and altitude history. The antennas are positioned along the body as shown in Figure 8.4. The radiofrequency of the NATO L-, S-, X- and K_a -bands correspond to 1,116, 3,344, 10,044 and 35,000 MHz, respectively.

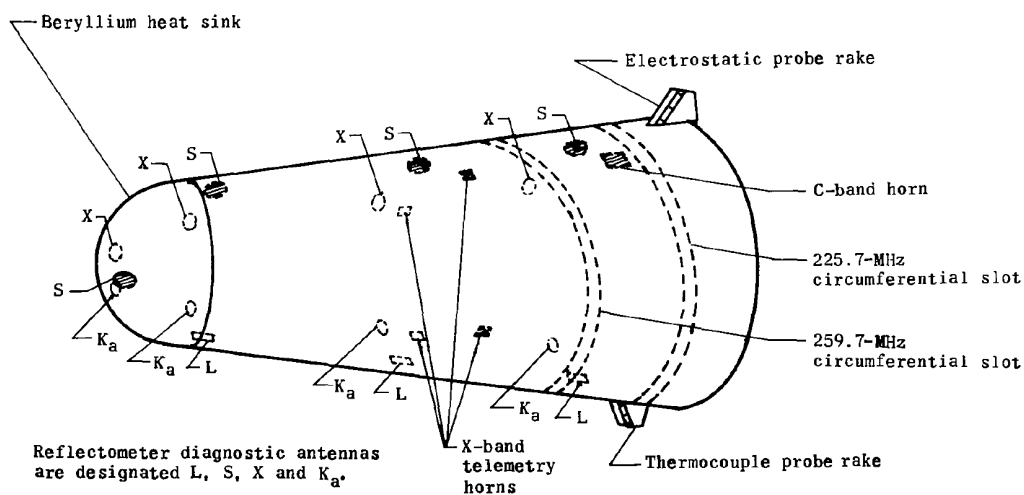


Figure 8.4: RAM C-II reflectometer antennas positions, adapted from [431].

Each reflectometer system was composed of a microwave signal source, an isolator, and a four-way power divider (three-way for L-band) to split the signal into the various antennas, bidirectional couplers,

four electric field probes, and antennas. Open-end waveguides were used as antennas for the nose region, where the plasma reflective layer distance to the surface was small. On the aft region X- and K_a -bands used high gain horn-antennas and L- and S-bands used open waveguides.

Mission Outcomes

The electron density measurements from the reflectometer antennas had good agreement with the ones from Langmuir probes. A strong density gradient was observed between the payload surface and the flow for the first 1-2 cm. After that, the density value is stable for several centimetres. The plasma density showed a strong correlation with the longitudinal position [431].

The numerical simulations were done using a clean-air assumption. This was considered valid down to 56.4 km, whereas in the case of RAM C-I flight, the flow was by then contaminated by ablation products [431]. Reflectometer data was more accurate than electric probes data as the latter were influenced by the temperature around them. For a valid interpretation of the fixed-bias electric probes, an accurate knowledge of the leading-edge temperature is required. Above 1,366 K (measured by the thermocouple) the insulating properties of the dielectric were degraded. Thus, the validity of the data interpretation was questionable. For lower altitudes, the comparison between experimental reflectometer data and computation calculations is invalid, as pointed out in [432]. The reasons are the higher neutral-electron collision frequency and the ablation of microwave antennas. This ablation changes short-circuit-phase reference and introduces a small voltage standing wave ratio which complicates data interpretation. The sharp rise in reflection coefficient is not as sharp due to neutral-electron collision frequency and thus a clean signal of the reflective layer is no longer visible.

The plasma at the aft body region, measured over the 7 cm deep electrostatic probe rake, was larger and less dense than expected [432]. A reasonably good agreement between the experiment and computational model was found for the electron density gradient in the nose region. This agreement was not observed for altitudes higher than 70 km. The electron density was consistently higher in the computational model, this over-prediction means that at the aft region, the electron density will also be over-predicted. The author also reports that free electrons are originated from ionization in the nose region, and then move to the aft body. A possible reason for the deviation between computational and experimental data at the rear may be the lack of implementation of ambipolar transport in the numerical code. Over the years, the RAM-C experiment has become a popular CFD (Computational Fluid Dynamics) validation test-case experiment, with many authors (including ones from IST [428]) using its data to validate and improve the kinetic models of numerical codes [428, 433–435], study the catalicity effects on the electron density [434, 436] or the ambipolar diffusion effects of ions [437]. As attested in Figure 8.5, good reproduction of these past experimental results is nowadays achieved by contemporary CFD models in this lower speed orbital entry regime. More details on our CFD model [428] will be presented in section 8.4.1.

OREX and HYFLEX

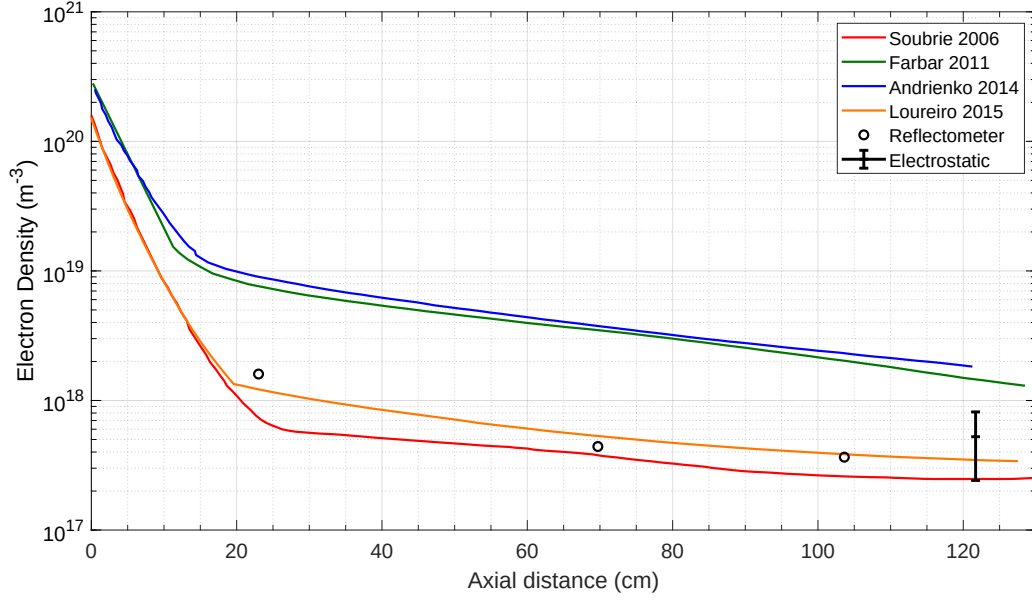


Figure 8.5: RAM C-II electron wall density. Comparisons between measurements and numerical simulations from different authors.

The Japanese space agency (JAXA) performed studies and experiments during the development of the H-II Transfer Vehicle (HTV), [438]. To study the re-entry conditions of said vehicle, two flight experiments were designed and launched, the Orbital Re-entry Experiment (OREX) and the Hypersonic Flight Experiment (HYFLEX).

OREX was a sphere-cone, with a 50° half-cone angle, capsule equipped with Langmuir probes to evaluate electron density and TPS in a flight environment [439, 440]. The flight took place in February 1994, with results showing good agreement with CFD simulations, see Gupta et al. [440].

The HYFLEX vehicle had a slender lifting-body design, more complex than the OREX capsule. Its objectives included the validation of the aerodynamic design, data acquisition, and the evaluation of the aerothermodynamic properties of the vehicle. For the latter objective, HYFLEX was equipped with temperature and pressure sensors, as well as two reflectometers [438, 441]. The maximum electron density predicted for the re-entry was around 10^{17} el./m³. The reflectometers centre frequency was set to around 1 GHz to account for the plasma layer being thinner than the wavelength. The selected frequencies were 902.85 and 1,676.5 MHz, with the antennas installed 960 mm aft from the nose of the ship and 180 mm off-centre. These were rectangular waveguides measuring 135x267 mm and 74x145 mm, respectively. At their locations, the aluminium skin of the ship was replaced with a Kevlar panel and thermal protection tiles were attached to it. The panel and tile effect on the reflectometer waves was found to be negligible, as reported in [441].

The reflectometer system was comprised of an oscillator, circulator, detector, amplifier, and a standing wave ratio (SWR) section for the 902.85 MHz only. The radio wave generated from the oscillator travelled through the circulator into the antenna. The EM wave was then reflected at the plasma and entered via the same antenna to the circulator. It was lastly received at the detector, where the reflected wave intensity was measured. The SWR section function was to measure the phase of the reflected wave, which was useful for inferring the distance from the antenna to the plasma layer. Unfortunately, the data

quality was not enough to be useful.

Flight data from the reflectometers was analysed via the power reflection coefficient. The expected reflection coefficient was estimated using a numerical simulation, and then compared to a ground test using a metallic plate. Good agreement was observed between the 3D simulation and the ground test. The flight reflectometry data showed agreement in shape and peak values with the CFD simulations, nonetheless, the peak occurred sooner than expected. The high-frequency signal (1676.5 MHz) showed a power reflection coefficient peak of 0.2, whereas for the low frequency (902.85 MHz) a peak coefficient of 0.3 was found. This dependence is reasonable since the lower frequency wave should be reflected more easily than the higher one. The blackout time and duration are consistent with the observed Very High Frequency (VHF) telemetry data from the ground station. The obtained results found a peak electron density of 10^{17} el./m³, for a plasma thickness of about 1 cm. Using the VHF telemetry attenuation signal, the plasma density around the antenna was compared to the CFD simulation with good agreement of peak plasma density of 10^{15} el./m³ and thickness of 50 cm.

Atmospheric Reentry Demonstrator - ARD

The Atmospheric Reentry Demonstrator (ARD) was an ESA flight mission with multiple objectives, among these, the testing and validation of re-entry technologies and flight-control algorithms, qualification of design and TPS, the study of radio communications and black-out, and validation of aerothermodynamic predictions during re-entry [442]. The ARD shape was a 70% scale Apollo capsule, with a diameter of 2.8 m and weight of 2.8 tonne. Two reflectometer antennas were located 45° off the centre axis on the leeward side of the heat shield, and the working frequencies were 2.203 and 2.218 GHz respectively [443]. The reflectometers were comprised of a power unit and a waveguide and were calibrated against a metallic target pre-flight, and then again post-flight. Results differ between the two calibrations and are in agreement with each other only at altitudes below 45 km. Above 97 km, the post-flight calibration gives slightly higher peak electron densities around 7.75×10^{16} el./m³ vs. 1.24×10^{16} el./m³ at 45 km. However, between the 90–45 km altitude and using the post-flight calibration values, the electron density could not converge to a valid solution. The pre-flight calibration gives a peak electron density of 1.52×10^{17} el./m³. The authors of [443] argue that the reason for the different calibration results may be related to the plasma formation and the corresponding metallic plate erosion in the antenna region.

Other radio attenuation measurements

Several works have examined the attenuation of VHF and UHF (Ultra High Frequency) telemetry radio waves during spacecraft entries to assess the electron densities in the flow. Typical approaches imply simulating the CFD flowfield (and particularly the plasma density profile) at different reentry times and overlaying an electromagnetic solver to determine the signal attenuation for specific paths over the plasma. Then a comparison of the real attenuation (signal telemetry before/during reentry) is carried out. While this does not allow for fine estimations of the flow density, it is sufficient for a coarse estimation of the CFD reconstruction accuracy, namely comparing the rate of change for the electron

densities along the different trajectory points. An illustration of this is shown in Figure 8.6. Examples of such works may be found in [444, 445] for ESA's Inflatable Re-Entry Technologies (IRDT) and [446] on ARD.

ESA's IRDT mission communication operated in the UHF band with a frequency of 219 MHz, and its antenna mounted in the nose of the capsule. During re-entry communications blackout only lasted 45 seconds instead of the expected 80 seconds. Reynier et al. [444, 445] reconstructed the electron density profile, then used an EM solver to simulate the observed attenuation at different frequencies. Their results predicted a signal attenuation ranging from -6 to -15 dB at different frequencies. The initial 1D engineering estimation using the plasma critical density overestimated the blackout time as observed in the flight.

Takahashi et al. [446] used a similar methodology to study ARD's blackout of the Tracking and Data Relay Satellite (TDRS) communication at 2.267 GHz. The attenuation results at the rear were low enough to permit communications, except at 70 km altitude. However, Tran [443] reports that there was never a full blackout at the TDRS band, despite a strong attenuation at 70 km. On the front side of ARD, the blackout was complete.

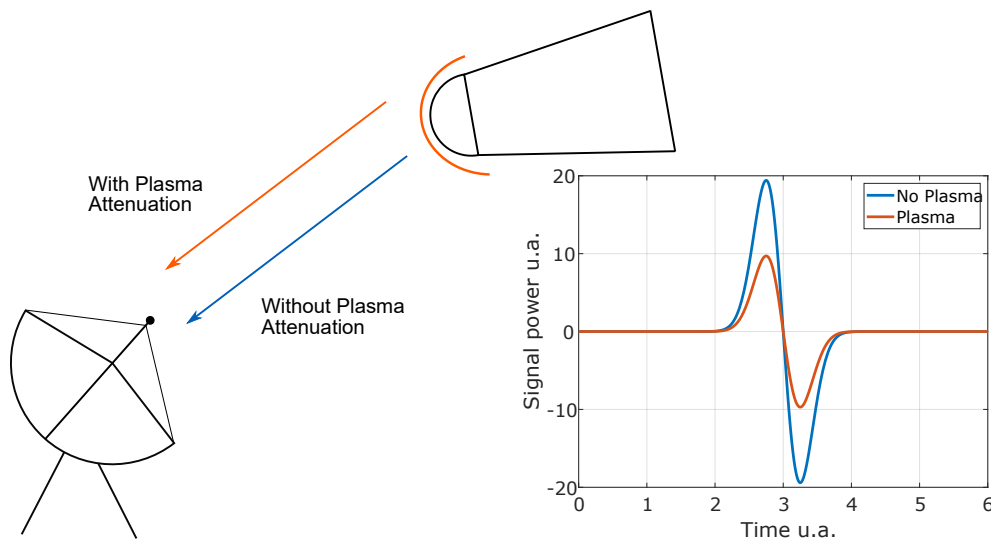


Figure 8.6: Illustration of communications attenuation during re-entry

Cancelled flight experiments

NASA's Microwave Reflectometer Ionization Sensor (MRIS) program, fully detailed in [447], began in 1989 to develop a millimetre wave reflectometer to evaluate the electron density during a hypersonic re-entry. The program was halted in 1991, with the design phase completed and the engineering model near completion. The instrument design was composed of 4 antennas operating at 20, 44, 95 and 140 GHz. The equipment is divided into two parts, a detector and an emitter/receiver part. The 20 GHz frequency antenna works in a monostatic configuration, while the others in a bistatic configuration. The lower frequency limits were chosen due to the antenna size limitations, the upper limit was limited by device reliability and receiver sensitivity. The frequency sweep was chosen to have a bandwidth of 4 GHz using

a conventional stepped-FMCW (Frequency Modulation Continuous Wave) linear approach. Vector Error Correction (VEC) was identified as a powerful tool to improve the performance of the equipment.

MRIS equipment radiates modulated millimetre wave (mmW) signal into the plasma field and then coherently detects the phase and amplitude of the reflected signal received in the antennas. This information is used to reconstruct the electron density, the amount of reflected power, and the distance up to the plasma reflective layer. This density is specific for each mmW, and the bands are narrow enough so that the signal delay time can be used to estimate the critical density distance.

The instrument specifications could be met at all frequency bands for targets further than two inches away without the need for any vector error correction. Nonetheless, the use of VEC improved the accuracy and was required in Band-C and Band-D to meet the requirements for target distances of less than two inches. A more sophisticated detection algorithm was used in Band-C where a simple algorithm was insufficient.

8.3.2 Ground test experiments

Shock tubes and Plasma Wind Tunnels are used to reproduce typical conditions for entry plasmas for aerothermodynamics experiments. In such ground test facilities, reflectometry and interferometry equipments are used alongside spectroscopy diagnostics. These have the advantage of measuring electron density directly. A series of experimental programs have been developed over the different decades for probing electron densities and temperatures, but have mostly been restricted to Langmuir probe techniques or indirect spectroscopic measurements, such as Stark-broadening measurements of the H- α atomic lines. Examples like these are too many and out of the scope of this work, and we will only review applications of the reflectometry/interferometry principle.

Shock Tube interferometry measurements

An experimental campaign under the auspices of the European Space Agency (ESA) [65, 247, 448, 449] has been carried out in 2007-2008 at Moscow Institute of Physics and Technology (MIPT) shock tube. The campaign objective was to validate the existing CFD tools employed in the design of the EXOMARS mission. A two-channel 94 GHz microwave interferometer was used to determine the electron density. Such interferometer works by combining two microwaves whose phases are $\pi/2$ shifted. The microwave phase increases proportionally to the electron density as it propagates through the plasma. The working frequency is selected to ensure that it does not reflect in the expected plasma conditions. Nonetheless, a following spectral analysis can confirm the results, and if the interferometer wave is reflected at the wall or mid-away in the plasma. Figures 8.7 and 8.8 depict a schematic of said interferometer and the measured electron density for a sample experimental shot in MIPT.

The interferometry principle can also be applied to combustion or shock tube experiments. A 94 GHz interferometer was used in a combustion shock tube [450] to measure the electron density of thermally ionized argon and krypton gases. Schneider et al. [451] used a microwave interferometer to measure

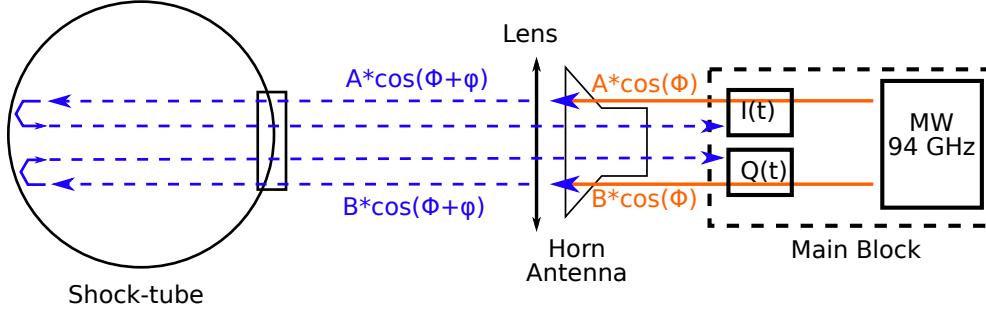


Figure 8.7: MIPT shock tube 94 GHz microwave 2-Channel Interferometer

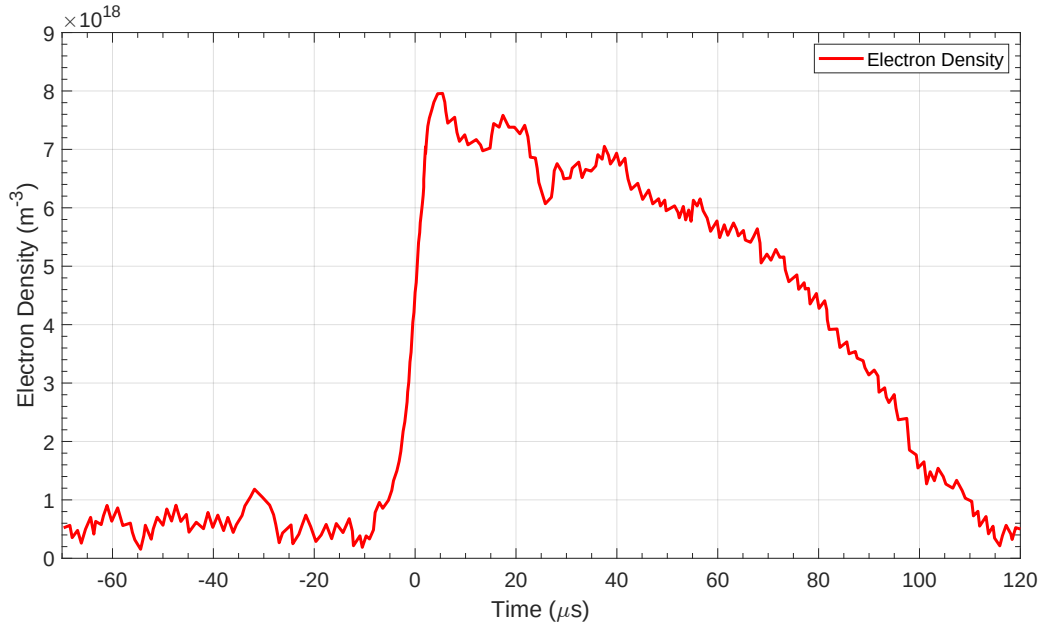


Figure 8.8: Measured electron density for a shock wave speed 3470 m/s, filling pressure 6.2 Torr (826.5 Pa), gas mixture CO_2 .

the ionization rates of NaCl-contaminated argon flames. Other authors [452–454] conducted similar experiments which will not be detailed here.

Plasma plume reflectometry measurements

Interferometry/reflectometry are useful techniques for non-intrusive probing of the electron density of a plasma plume. Its main advantage is being non-intrusive contrary to the usual Langmuir probes [455, 456], however, the acquired signal is a line integration over the density profile. To create the 2D or 3D density profile an inversion method to the line integrated signal must be applied. These can vary from a “onion-peel” or Abel-inversion to more complex techniques such as Tikhonov deconvolution, described in [457].

Several works have been done in characterising a plasma plume with microwave diagnostics. The electron number density of a closed drift SPT-100 Xenon Hall thruster was mapped by Ohler et al. [458, 459], with a discharge voltage and current of 300 V and 4.5 A. Using a 17 GHz microwave interferometer

via differential phase measurements the thruster plume profile could be obtained. The same group also characterized a 1 kW hydrogen plume in [460].

Capelli et al. [461, 462] used a 90 GHz interferometer to probe the electron density of a 200 W Hall thruster. Using an Abel-inversion, they could reconstruct the electron density profile of said Hall thruster. Kuwabara et al. [463] studied the plasma density fluctuations of a 200 W class magnetic layer type Hall thruster. They used a 76 GHz interferometer to probe the electric field fluctuations. In 2021 Kuwabara et al. [464] measured the electron density using the same 76 GHz interferometer.

In 2008 a swept Frequency Modulated Continuous Wave reflectometer for space plasmas was first designed to measure electron density in a space environment. The reflectometer swept frequencies in the range of 2-18 GHz, which corresponds to critical electron densities in the order of 4.96×10^{16} to 4.02×10^{18} el./m³. A full sweep could be performed in less than 100 microseconds and the signal acquired for up to 10 milliseconds at a time, or up to 100 sweeps. Spatial resolution was determined to be less than 1.5 cm using 16 Mega samples per second and a spectral length of 1024 samples. The system could operate in both monostatic or bistatic configurations. A complete description of the equipment and the test can be found in [465]. A typical schematic for a reflectometer in monostatic configuration is depicted in Figure 8.9.

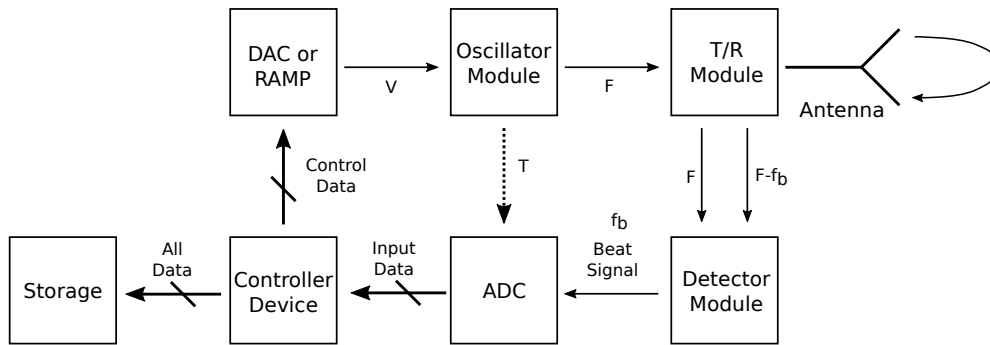


Figure 8.9: Schematic of the reflectometer

In 2010, the equipment was tested in the Von-Karman Institute 1200 KW Induction Plasmatron. A shift in the wall reflection frequency was observed when the plasma was turned on, from 2.2 to 1.8 GHz. The FFT amplitude signal can be observed in Figure 8.10. The shift represents the difference in the time of flight of the wave. The shift is towards a lower frequency, which corresponds to a slower wave, as it propagates in the plasma the EM wave is slowed down in comparison to the vacuum propagation. Conclusions from the test were positive as the system operated as anticipated and the results were in agreement with the other Plasmatron diagnostics. A thorough report of this test is presented in [465].

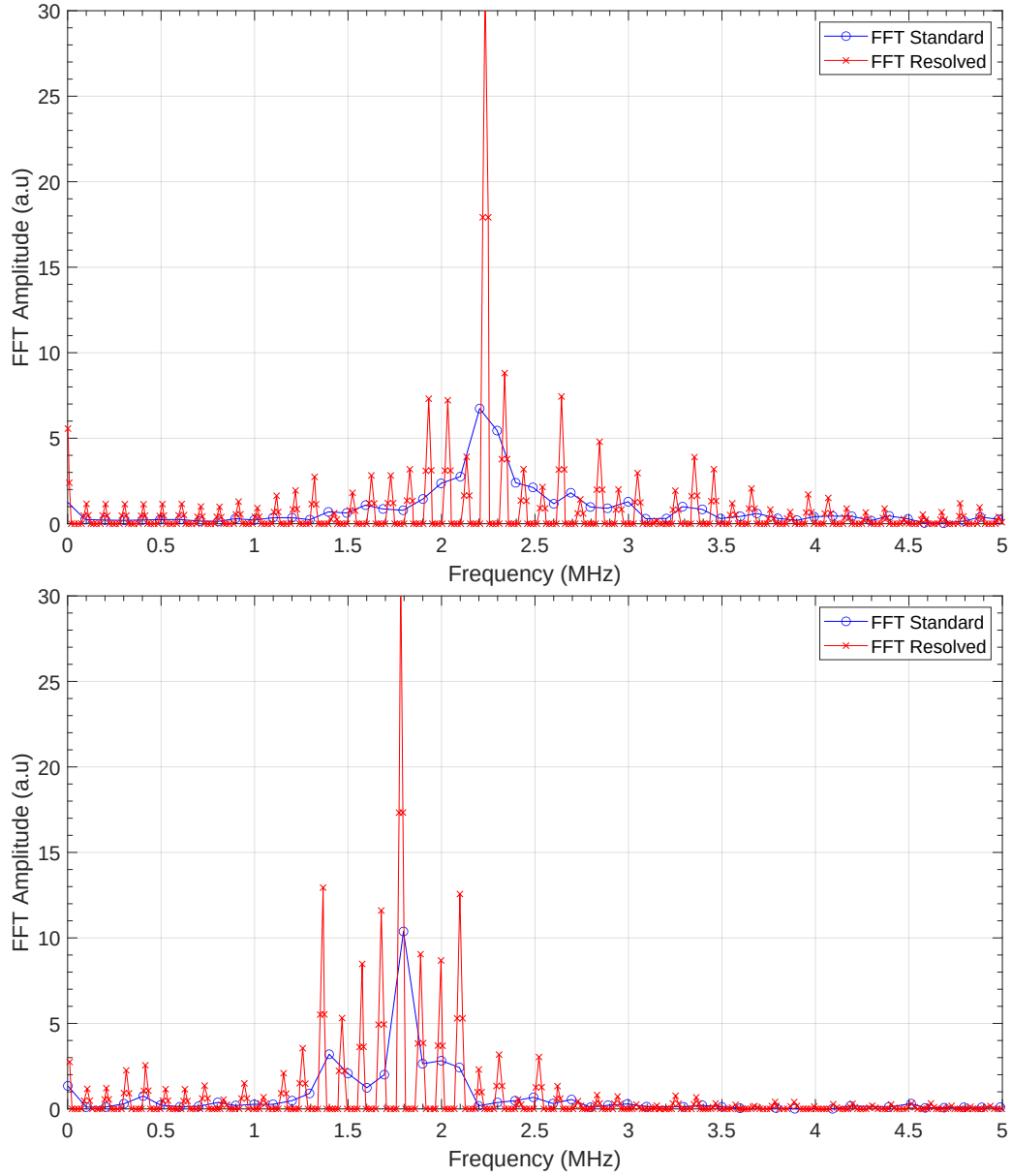


Figure 8.10: Wall reflection frequency testing at VKI plasmatron. Plasma turned off (top), plasma turned on (bottom), adapted from [465]

8.4 Designing a Compact Reflectometer/Interferometer for Entry Applications

Owing to the extensive legacy of in-flight experiments and ground testing results, it is somewhat surprising that such a diagnostic is not more widespread than it currently is within the hypersonics research community with no deployed instrumentation of this kind existing to the author's knowledge. Despite being a very complete diagnostic, reflectometry has a number of challenges to overcome to be correctly implemented. First, it requires the equipment antennas to be directly exposed to hypersonic flow. These will be damaged during the re-entry and alter the received EM signal. Another limitation of this technique is the difficulty in interpreting the signal and relating it to the electron density value. The reason is the already mentioned steep electron profile and a small distance between the antenna and the

plasma reflective layer. Besides it, the high electron density at the boundary layer further complicates the data interpretation. A pre-flight calibration of the reference levels can be used to help interpret the reflected signal. For example, the microwave reflection coefficient was used by NASA in the RAM-C II [432] to identify the critical electron density by a steep rise in the reflected power. Nonetheless, this is a non-trivial technique and the used approximation may not be always valid. Communication blackout is no longer a prominent problem in spacecraft and lastly, these diagnostics are relatively heavy and cannot be simply added to an existing mission.

This is somewhat even more surprising as the knowledge of time/space resolved electron densities in atmospheric entry plasmas can enable a reliable quantitative assessment of key processes in such high-speed flows, as discussed in section 8.1 (physical-chemical processes, flow history, blackout characteristics, and plasma radiation¹).

Specifications and Requirements

A reflectometer tailored for in-flight and ground test facilities has a certain amount of limitations when compared to diagnostics tailored for fusion activities, see for example [466]. Mostly these are identical to other embarked diagnostics where one needs to ensure

- compactness
- low weight
- low power consumption and good hibernation characteristics
- adequacy to withstand launch conditions (shocks, vibrations, fast depressurization)
- adequacy to the Space environment
 - vacuum
 - low temperature
 - cosmic radiation

For the last bullet point, we may distinguish between flight experiments which target Earth re-entries, where the spacecraft remains inside Earth’s magnetosphere from launch to touchdown (and where the overall mission may last hours instead of years), and other planetary entries, with a mission profile of several years where the spacecraft needs to withstand the cosmic environment. Even for some Earth re-entries, radiation may need to be accounted for if the spacecraft passes the van Allen belt. In these two latter cases, one needs to define radiation-hardened or fault-tolerant hardware, just like in fusion applications (owing to the severe radiation environment found in tokamaks), however, this discussion is beyond the scope of this manuscript, and the reader may refer to [467] for examples of radiation-tolerant hardware.

Naturally, these architecture requirements derive from the functional requirements of the setup. These include:

¹which is highly influenced by the plasma electron density

- available frequency bands
- frequency sweep characteristics
- high-speed remote control and acquisition
- data storage and transmission

For the selection of appropriate frequency bands, one may first refer to the bibliographical review presented in Section 8.3. The operational frequencies for the RAM-C II experiment ranged between 1 and 35 GHz, whereas the HYFLEX and ARD experiments had reflectometers with frequencies in the 1–2 GHz range. These were tuned for measuring sparser electron densities, characteristic of the back-body plumes for these flight experiments. Freestream flow experiments in supersonic plasma wind tunnels, in the absence of an obstacle, will typically reproduce the conditions of these afterbody plasma plumes, and it is reasonable to consider similar frequencies. Hence a good candidate bandwidth would be in the 1–50 GHz to account for most practical situations.

In contrast, electron densities in the stagnation region are predicted to be significantly higher, and although reflectometry experiments have limited viability in the stagnation streamline region, owing to the small shock standoff, one may nevertheless carry out interferometry experiments in shock tube facilities, such as the ones carried out in the VUT-1 shock tube. Here, the higher electron densities will mandate higher frequencies, above the plasma cutoff, and even for slower shocked flows around 3–4 km/s, a 94 GHz frequency was considered for the VUT-1 reflectometer. Since not much experimental data is available for direct electron density measurements, a set of CFD simulations was performed to estimate the required range for the diagnostics equipment. Shock wave conditions were estimated through the chemically reactive CFD code Software Package for Aerothermodynamics, Radiation and Kinetics (SPARK) [468], designed for hypersonic reentry flows. It uses object-oriented programming to implement different physical models and numerical methods in a unified framework, which allows for greater flexibility and maintainability than other available numerical tools [468]. The code itself is capable of performing 0D (temporal relaxation), 1D (post-shock relaxation) or 2D (planar or axisymmetric) simulations using Navier-Stokes compressible flow formulations for nonequilibrium, multicomponent chemically reacting gas mixture. The code employs a cell-centred finite volume formulation, whereas, for the temporal discretization both Roe and Harten-Yee (predictor-corrector) explicit second order schemes are available. The nonequilibrium flow description accounts for separate global or individual vibrational temperatures, using a Millikan & White model coupled to a Park nonequilibrium chemistry model [469].

The code is capable of computing the chemical composition behind the shock wave and its respective electron density and emission radiation. A total of six simulation runs were carried out in 1D (post-shock relaxation) conditions. Table 8.1 shows the CFD simulations' initial conditions and references for the chemical-kinetic reaction schemes, as well as for the chosen velocity and pressure conditions. The representative cases are: a sample return mission to Earth, the ExoMars, Huygens and Galileo missions to Mars, Titan and Jupiter, respectively. Simulation conditions for Neptune and Venus were taken from trajectory calculations. The electron densities for these cases are depicted in Figure 8.11, where the

electron density can reach values of 4×10^{23} electrons/m³ for the case of a Venusian entry. The typical electron density profile has a sharp rise right after the shock front, followed by a slower decay until chemical equilibrium is achieved.

Table 8.1: CFD simulation conditions and parameters. All simulations are 1D post-shock relaxation with initial gas temperature of 300 K.

Planetary object	Chemical mixture	Pressure (Pa)	Velocity v_∞ (km/s)	Chemical Model	Reference
Earth	N ₂ -O ₂ (79-21%)	26.66	10.29	[470]	[29, 471]
Mars	CO ₂ -N ₂ (95-5%)	57	2.6	[470]	[472]
Venus	CO ₂ -N ₂ (96.5-3.5%)	447	8.9	[470]	[473]
Venus	CO ₂ -N ₂ (96.5-3.5%)	37	10.6	[470]	[473]
Titan	N ₂ -CH ₄ (95-5%)	13.3	5.15	[474]	[473]
Neptune	H ₂ -He-CH ₄ (79.75-18.7-1.54%)	892	18.3	[396]	[396]
Jupiter	H ₂ -He (89-11%)	27.5	46.7	[475]	[396]

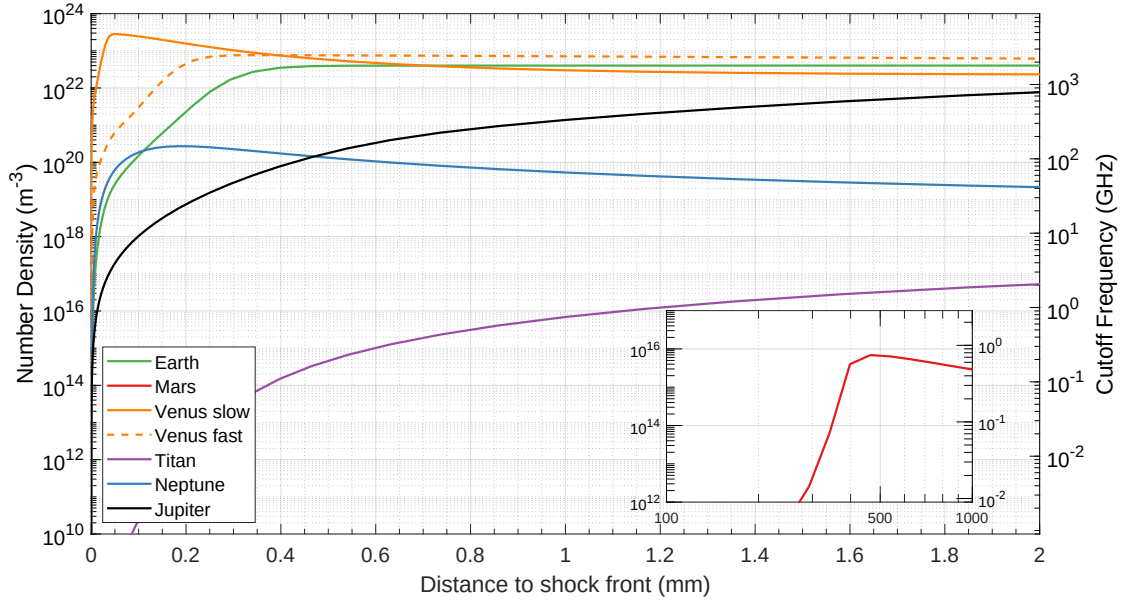


Figure 8.11: Electron density in shock tube for typical entry conditions in different atmospheres.

8.4.1 Application to the RAM-C II test case

To further assess the behaviour of a compact reflectometer in the conditions of an atmospheric entry the RAM-C II flight experiment was modelled using the SPARK code, coupling it to a wave propagation code (REFMUL). This allows for achieving a better understanding of the propagation dynamics of the electromagnetic waves in an environment characteristic of an atmospheric entry, which in turn helps better tailor the position of the reflectometer antennas, allowing precise measurements of the electron density profiles of the plasma.

The starting point is given by the critical conditions of the flight experiment (peak heating and peak dynamic pressure). These have been extensively discussed in the literature and will not be expounded here. The relevant flight points are outlined in Table 8.2.

Table 8.2: RAM-C II flight data, from [432], and peak electron density at stagnation line, from [428]. Shock stand-off distance ~ 2 mm for all cases.

Time (s)	Altitude (km)	Velocity (km/s)	n_e (m^{-3})	f_{pe}
392.3	81	7.65	1.189×10^{14}	97.9 MHz
396.9	71	7.65	5.326×10^{19}	65.53 GHz
402.3	61	7.65	1.746×10^{20}	118.64 GHz

Here we have chosen the 61 km altitude point and carried out a CFD simulation, followed by an overlay of the wave propagation code over the simulated plasma profiles. The following paragraphs provide an abridged description of the codes and models used in this simulation, followed by the simulation results.

CFD Code

Simulations for the 61 km altitude have been carried out assuming a simplified 2D axisymmetric geometry, with an 80x160 mesh refined in the shock and boundary layer regions. Thermal and chemical nonequilibrium was assumed, for the following freestream conditions: $v_\infty = 7.65$ km/s, $T_\infty = 244$ K, and $\rho_\infty = 2.73 \times 10^{-4}$ kg/m³. The 11 species (N_2 , O_2 , NO , N , O , N_2^+ , O_2^+ , NO^+ , N^+ , O^+ , e^-) 2001 Park chemistry model [476] was considered, and multicomponent transport was treated either using the Wilke [477] or the Gupta-Yos [478] model. A non-catalytic/super catalytic 1,200 K wall was considered in two sets of simulations. Grid convergence was checked through a simulation using a mesh with double the size (160x320), which yielded similar wall fluxes results in the boundary layer. As seen in Figure 8.12, the transport model has a slight influence on the electron properties at the wall, which is not surprising as the Wilke and Gupta-Yos models yield similar results for the lower temperatures associated with orbital entry velocities such as the one from RAM-C II [428]. Further, and as expected, the wall boundary conditions have a more marked influence on the simulated electron densities, with a super catalytic boundary condition providing the best agreement with experimental data. This is a reasonable assumption, owing to the surface materials utilized in the RAM-C II experiment, see section 8.3.1.

Wave Propagation Code

Full-wave codes are required in modelling wave propagation to retain the physics of several wave effects. This is the case for instance when the radiated fields of a specific antenna need to be simulated and/or small distances are considered which do not allow to work with far-field assumptions. The REFMUL* codes family allows the simulation of reflectometry diagnostics coupling wave propagation, a time-dependent plasma, antenna positions and waveguides. The REFMUL* family is a finite differences in time-domain (FDTD) unconditional stable algorithm using the Yee-scheme to discretize the spatial coordinates. The system of equations solved by REFMUL* codes is the curl equations from Maxwell's electromagnetism laws, coupled with a linear current density model. Currently, there are four REFMUL* codes, REFMUL, REFMULX, REFMULF and REFMUL3. The first three codes are already proven in simulating 2D waves, they simulate the O-mode, the X-mode and a generic wave polarization [426, 479–484], respectively. The REFMUL3 code is in the advanced development phase and is capable of simulating

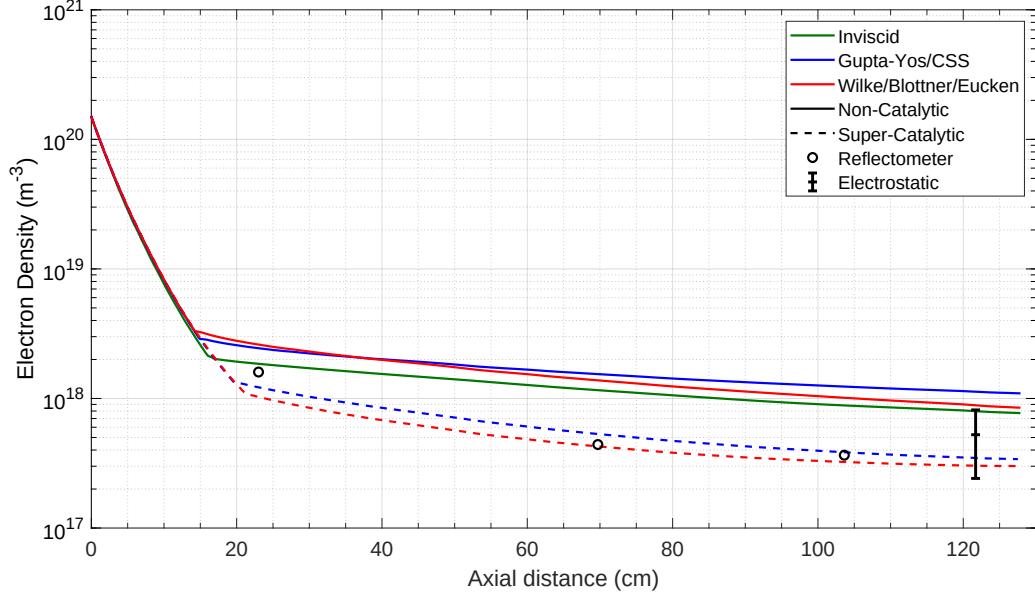


Figure 8.12: RAM C-II electron wall density. Comparisons between measurements and numerical simulations with different transport models.

a 3D full-wave. REFMUL* system of equations to solve is the following:

$$\begin{cases} -\varepsilon_0 \frac{\partial \vec{E}}{\partial t} + \vec{\nabla} \times \vec{H} = \vec{J} \quad , \\ +\mu_0 \frac{\partial \vec{H}}{\partial t} + \vec{\nabla} \times \vec{E} = 0 \quad , \\ \frac{\partial \vec{J}}{\partial t} = \varepsilon_0 \omega_{pe}^2 \vec{E} + \omega_c \frac{\vec{B}}{|\vec{B}|} \times \vec{J} \quad , \end{cases} \quad (8.4)$$

where $\vec{E}$ is the electric field, $\vec{H}$ and $\vec{B}$ are the magnetic field and induction field, respectively, and $\vec{J}$ the current density. The plasma parameters are inserted in the equations through the plasma frequency ω_{pe} , and the cyclotron frequency ω_c accounts for the external magnetic field, these are given by,

$$\begin{cases} \omega_{pe} = \sqrt{e^2 n_e(\mathbf{x}) / m_e \varepsilon_0} \\ \omega_c = |eB(\mathbf{x}) / m_e| \end{cases} \quad (8.5)$$

The response of O-mode reflectometry was studied using a monostatic design, i.e. one antenna for both emission and reception. A unidirectional transparent source was used for the injection of the signal in a waveguide structure allowing the separating of the emitted probing wave from any returning waves [426]. A PML (Perfectly Matched Layer) approach was used for boundary conditions [485]. The spatial resolution of the Cartesian grid was $\Delta x = \Delta y = \lambda(40 \text{ GHz})/20 = 3.75 \times 10^{-4} \text{ m}$, where $\lambda(40 \text{ GHz})$ is the vacuum wavelength of a wave with frequency in vacuum $f = 40 \text{ GHz}$ which is an upper limit of the frequency range employed here. The time resolution of the REFMUL simulations was $\Delta t = \tau(40 \text{ GHz})/40 = 6.25 \times 10^{-13} \text{ s}$, the period of a wave at a frequency of 40 GHz.

Results

The numerical simulations used antennas in positions similar to the RAM-C II experiment. Figure 8.13 depicts the antenna positions used in the numerical simulations. A first simulation of reflectometry with a K_a band wave (35 GHz) in the RAM-C II nose region, position 1 in Figure 8.13. The results are depicted in Figure 8.14. The spacecraft's outer wall is represented by the black line, the shock wave by the red line, and the electric field in blue-purple colours. Preliminary simulations for the nose position showed that the emitted wave was reflected shortly after emission, as expected. The result shows a good integration between the SPARK CFD code and the REFMUL EM wave code.

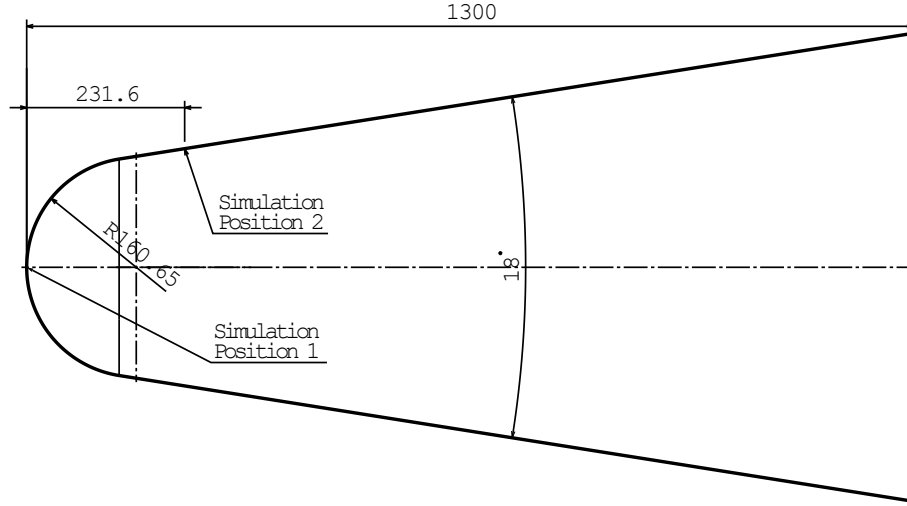


Figure 8.13: REFMUL simulation antenna positions, dimensions in mm

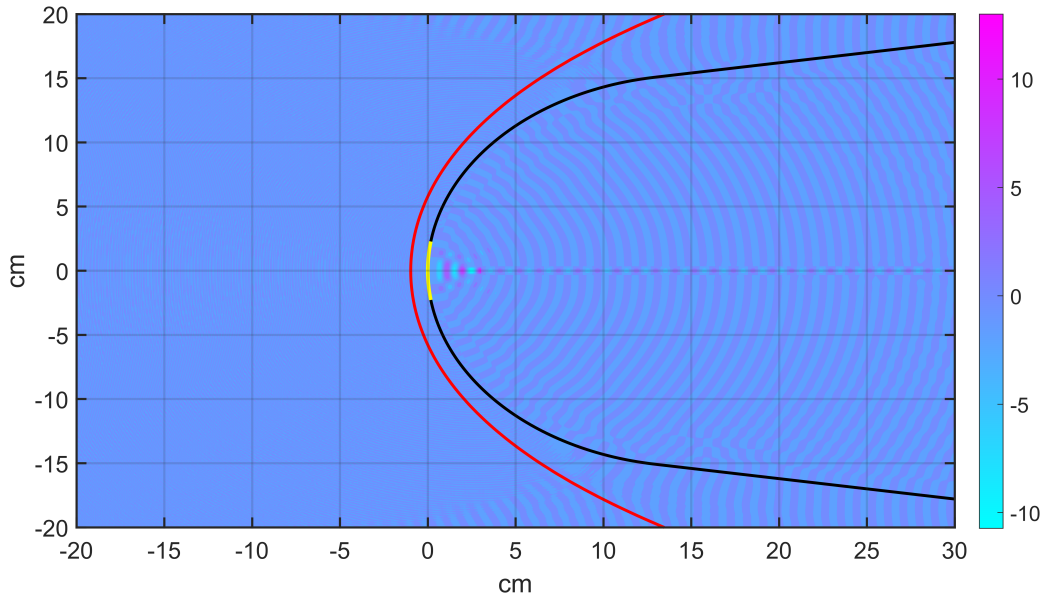


Figure 8.14: REFMUL simulation in RAM-C II conditions, wave frequency 35 GHz - nose region. Spacecraft in black and shock wave in red. EM wave electric-field represented by the colour bar.

Another simulation was performed using a similar setup, but with the reflectometer antenna in the half-cone region, position 2 in Figure 8.13, with results shown in Figure 8.15. The EM wave propagates towards the shock wave, where a partial reflection of the wave occurs towards the rear occurs. Part of the emitted wave is deflected and passes the shock wave, and another is reflected back to the emitting

antenna. In this position, the density profile peaks at a lower density and is further away from the outer wall. This improves the capability of reflectometry to provide a reliable electron density profile measurement in standard microwave frequency ranges.

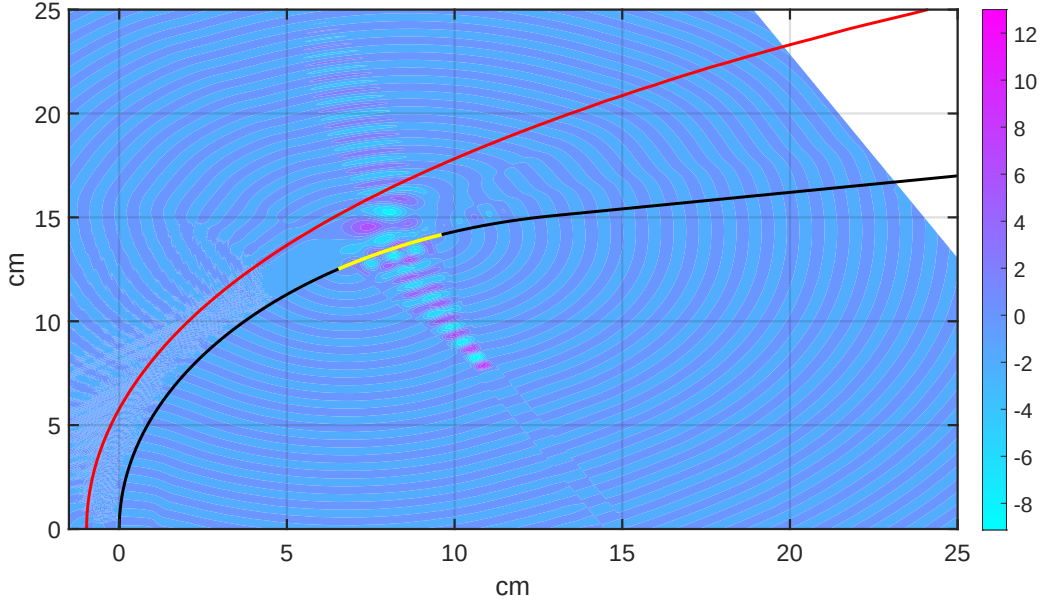


Figure 8.15: REFMUL simulation for 35 GHz wave propagation in RAM-C II conditions - shoulder region. Spacecraft in black and shock wave in red. EM wave electric-field represented by the colour bar.

These kinds of simulations are useful for determining the propagation dynamics of the emitted electromagnetic waves, and the variability that stems from the emitted wave frequencies, but also from the electron density profiles in the flow. This allows better tailoring flight experiments, where one may consider developing standard reflectometry techniques, based on the time-of-flight delay measurements (suited for afterbody flows with larger plasma volumes), or more simple “transmissometer” techniques, where the plasma cutoff density along the line of sight may be determined by checking the limit frequency where electromagnetic waves start/stop being reflected back to the antenna. This technique is more suited for probing the shock layer in the spacecraft forebody, where the typically small shock stand-off distance disallows the determination of a plasma profile, and only the maximum electron density may be determined.

8.4.2 Equipment design

As discussed in section 8.2 and shown in Figure 8.11, the working frequency of a microwave diagnostic will depend on the electron densities of interest. These can fall into the microwave (3–30 GHz), the millimetre-wave (30–300 GHz), or the sub-millimetre-wave (>300 GHz) region. Accordingly, an interferometry system needs to be flexible to encompass several frequency-band segments and switch among them to scan a specific range, or alternatively, it may consist of a stack-up of simpler instruments running in parallel. This latter approach allows the development of very compact instruments and does not limit future extensions of the bands to cover. A piece of bespoke equipment [415], depicted in Figure 8.16, was developed according to this design philosophy, which is composed of two sections: a back-end signal

generator and a front-end frequency multiplier and antenna assembly.

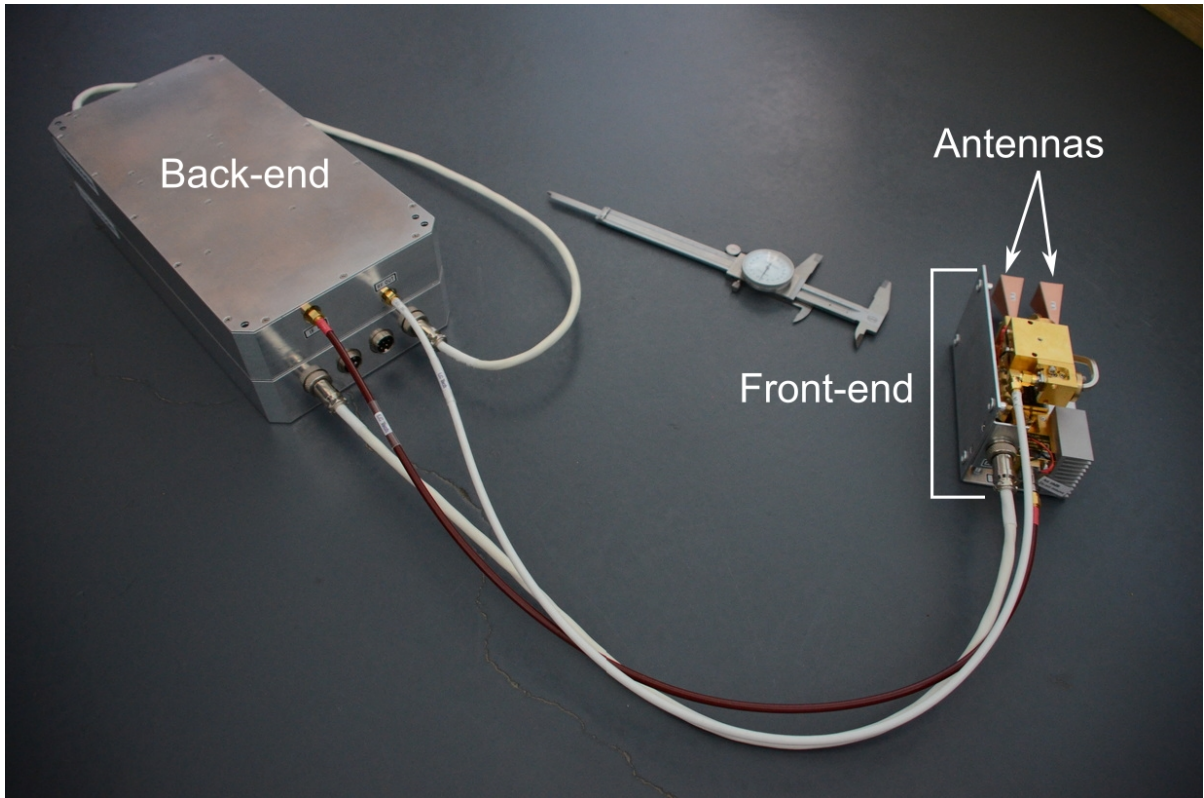


Figure 8.16: Compact microwave (70.8–112.8 GHz) interferometer.

The back-end is a complete, digitally controlled, signal-swept generator operating at a convenient microwave frequency range, such as 8–12 GHz or 12–18 GHz (or 10–20 GHz). The fast-swept signal is generated inside the back-end by ramping the control voltage of a VCO (voltage-controlled oscillator). There are several VCOs available that cover a variety of frequency ranges, but none of them, without exception, exhibit a linear relation of control voltage versus frequency. Therefore, the proper sweep of a specific frequency range requires pre-distorted voltage ramps to obtain a linearized frequency sweep at the output. This is achieved by a digital arbitrary waveform generator, which is integrated with the back-end. Additionally, to allow the easy control of several units, each back-end unit is controlled/configured over a TCP/IP connection.

The front-end comprises an active frequency multiplier, which produces the actual frequency range that probes the plasma, the antenna and signal detection device, which interface the plasma, and either a mixer or a single-end detector. We chose to multiply the back-end frequencies by 6 since this brings us to the 70.8–112.8 GHz frequencies (NATO W bands). The choice for this specific front-end was driven by the legacy of the successful application of this sampling technique in the VUT-1 shock tube [415] for low-ionization shocked flows. Selecting higher frequencies might be more appropriate for typical atmospheric entry conditions (see Figure 8.11), as the 70.8–112.8 GHz band is likely to be below the cutoff frequency of most shocked flows of interest. On the other hand, front-end developments for higher frequencies incur steeply increasing costs. Moreover, these high-frequency front-ends are more difficult to test with steady-state plasma sources since very-high-density plasmas are required. The development of higher-frequency

front-ends is therefore contingent on the results obtained using the selected frequency band. We note that this equipment could also be considered for probing integrated electronic densities in steady-state plasma wind tunnels. Here, the issue lies in our probing frequencies being significantly above the plasma cutoff frequency, for which the measurements of the phase shift (see Equation 8.2) will be less accurate (with phase shifts of a few degrees at most). To offset this issue, a lower-frequency front-end working in the 4–18 GHz range (essentially with no frequency multiplication) is under development.

With this architectural choice, a standard compact back-end that may connect to different front-end units will enable quick instrument re-configuration to probe different plasma scenarios. Ultimately, several back-end units, along with corresponding front-ends, may be run in parallel for probing vaster plasma density regions, simultaneously. This type of modular design shares the hardware components with a reflectometer and can be modified into one if needed.

8.5 Proof-of-Concept Application to a Steady-State Plasma Source

8.5.1 Experimental setup

A fluorescent lamp was used as a plasma source for demonstrating the interferometry concept. The lamp was placed between the emitter/receptor antennas and a metallic plate, where the electromagnetic waves are reflected. With the lamp in the off position, the received signal has a given phase corresponding to the path that the electromagnetic wave travels over the air and inside the lamp, then back inside the lamp and the air upon bouncing on the metallic plate. Once the lamp is turned on, the electromagnetic wave is further delayed, owing to the presence of free electrons inside the lamp, leading to a phase shift in the signal, according to Equation 8.2. The setup is shown in Figure 8.17. Data acquisition is performed using a digital oscilloscope.

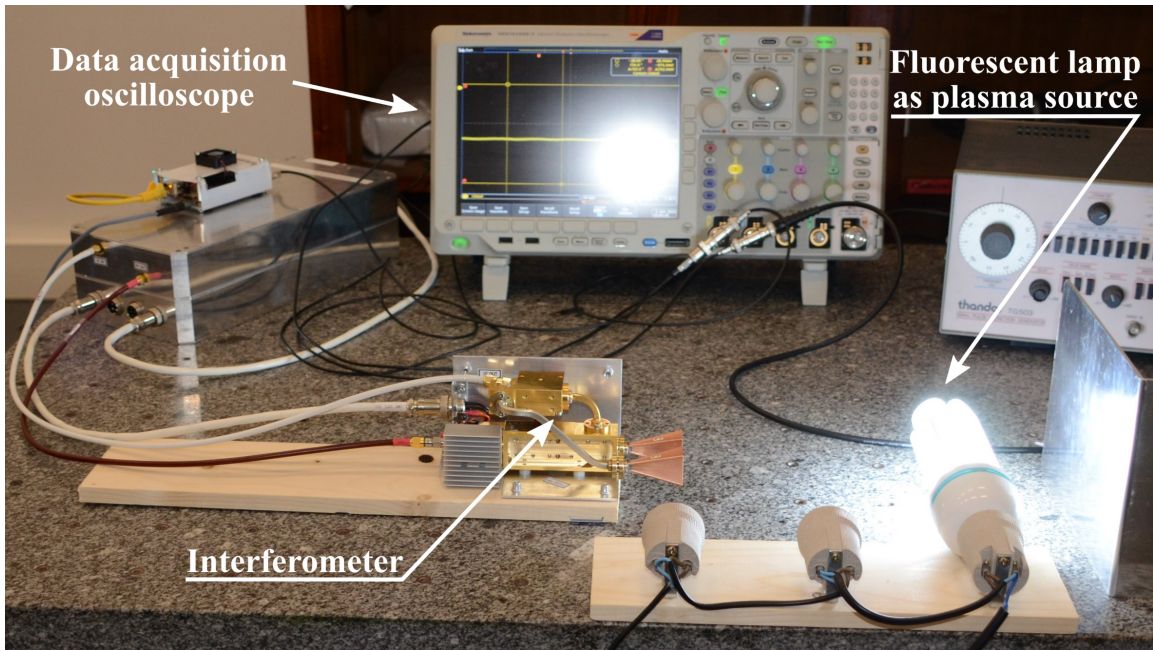


Figure 8.17: Interferometry setup for proof-of-concept experiment.

The signal frequency was set to 70.8 GHz, corresponding to a wavelength of 4.24 mm. The plasma thickness D inside the lamp tube was calculated, taking the tube diameter D_{tube} and subtracting its thickness d_{tube} . Since the microwave crosses N lamp tubes, the result was multiplied by the $2 \times N$ to account for the reflection at the metallic wall and round trip as shown in Equation 8.6:

$$D = 2N (D_{tube} - 2 \times d_{tube}) = 2 \times 4 \times (11.4 - 2 \times 1.4) = 68.8 \text{ mm.} \quad (8.6)$$

8.5.2 Results

The signal collected by the interferometer during the experiment is depicted in Figure 8.18. At $t \approx -0.02$ s, the lamp is turned on, leading to a sharp rise in the voltage signal, and turned off at $t \approx 0.42$ s. Once the lamp is turned on, the plasma density starts to increase until a steady state is reached at $t \approx 0.1$ s. During lamp operation, a 100 Hz oscillation of the signal is present. This corresponds to the rectified sinusoidal signal of the electric grid outlet at 50 Hz.

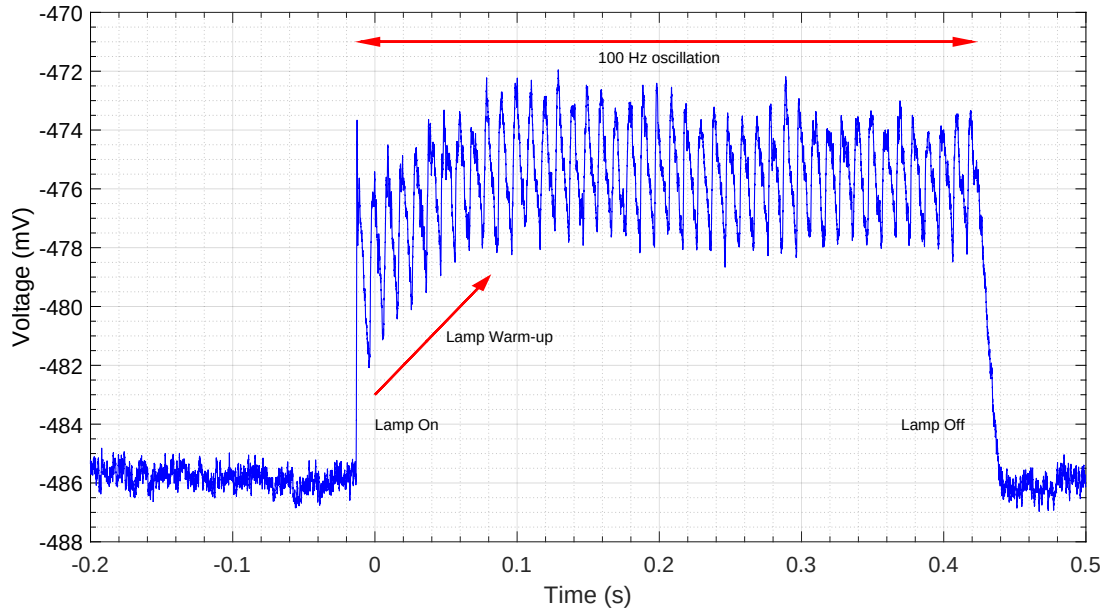


Figure 8.18: Interferometer fluorescent lamp test collected signal.

The collected signal needs to be converted to an equivalent phase in radians before the electron density can be computed. The interferometer signal is proportional to $\sin \phi$, where ϕ is the signal phase. To extract the phase, one first needs to know the full signal amplitude $1/A$ and the shift in signal B . By running the equipment in frequency sweeping, where the probing frequency changes over time, using the maximum available range one gets the raw signal limits, which correspond to $-\pi/2$ and $\pi/2$, respectively. An illustration of the calibration process is depicted and discussed in appendix C. These voltage limits are then used to fit the voltage signal V to a normalized function Y , ($-1 < Y < +1$), from where the phase ϕ can be extracted. The system of equations needed to calibrate the signal is,

$$Y = A \times V + B \quad (8.7a)$$

$$A = \frac{2}{V_{max} - V_{min}} \quad (8.7b)$$

$$B = -A \frac{(V_{max} + V_{min})}{2} \quad (8.7c)$$

where V_{max} and V_{min} are the maximum and minimum signal voltage. From there an arcsin function can be used to extract the signal phase. The calibration parameters are presented in Table 8.3.

Table 8.3: Interferometer calibration values.

Parameter	Value	Units
V_{max}	0.0280 ± 0.002	V
V_{min}	-0.674 ± 0.002	V
A	2.8490 ± 0.016	V^{-1}
B	0.9202 ± 0.011	–

Applying Equations 8.7 and 8.3, since we verify the condition $D/\lambda > 3$, we obtain an average electron density of $(4.0 \pm 2.4) \times 10^{16}$ el./m³. The results are within one order of magnitude as the ones observed by Liu in [486], where the author performed a similar experiment to measure the influence of electric frequency on the electron density of the fluorescent lamps.

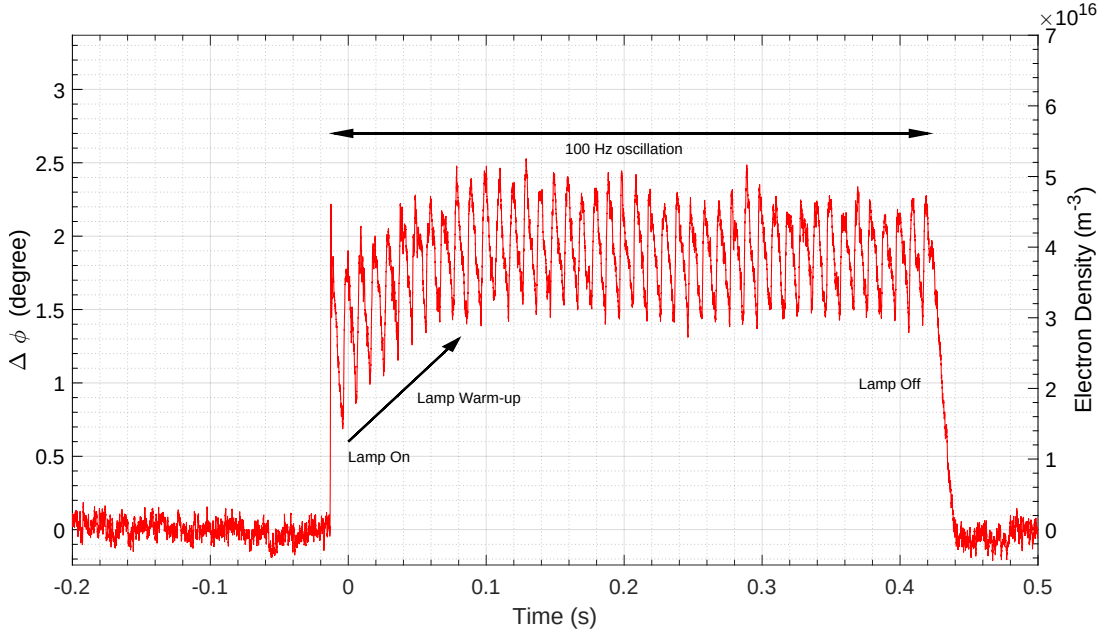


Figure 8.19: Interferometer phase difference and electron density for the fluorescent lamp plasma source setup.

This simplified experiment highlights the simplicity and flexibility of this non-intrusive technique for measuring plasma electron densities. The tested configuration with the emitting and receiving antenna placed side by side allows for seamless use of the equipment in any arbitrary plasma source, provided

it has two facing windows with the front-end placed in front of one and a metallic plate placed in front of the other. This requires that a careful alignment is performed to ensure that the electromagnetic waves are properly reflected in the metallic plate and not elsewhere. An alternative would be to decouple the receiving antenna, which would be placed facing the emitting one; however, this requires a bespoke configuration adapted for each plasma source (a specific configuration of this type will be used for measurements in ESTHER). Another limitation is that a plasma source needs to have a size of the same order of magnitude or bigger than the horn antennas to ensure that most of the emitted electromagnetic waves effectively cross the plasma. Lastly, the range of frequencies preferentially needs to be “compatible” with the application as discussed in section 8.2. The frequency should be higher than the plasma cutoff frequency, as otherwise, the wave will not propagate, yet it should not be so high that the phase shift becomes as small as the background noise. With this said this small experiment shows that reasonable measurements of plasma densities may still be achieved with just a few degrees of phase shift.

Microwave electron density measurements may be cross-checked with other techniques, specifically Stark broadening, which is the only other diagnostic which may be straightforwardly applied to shock tubes. To properly assess the applicability range of Stark-broadening techniques, two 1D shock wave simulations were ran, yielding the chemical conditions behind the shock wave for a high-ionization case (Jupiter entry) and a low-ionization case (Mars entry). The broadening of the H- α line was then computed from the post-shock conditions for peak electron density. Table 8.4 presents the expected broadening of the H- α spectral line caused by different broadening mechanisms, computed with the code SPARK Line-by-Line (SPARK-LbL). A more detailed description of these is found in [3, 399, 400]. The total spectral broadening may be modelled as a convolution of the Lorentzian and the Doppler broadening. For the Jupiter entry case, the high entry velocity leads to a post-shock temperature in the vicinity of 42,000 K, and a corresponding electron density of about 1.40×10^{21} el./m³. In the case of a Mars entry, the peak electron density is 1.74×10^{17} el./m³ at a temperature of around 3,200 K. In the former case, the Lorentz component of the broadening is dominated by the Stark effect, whereas in the latter, the collisional term is dominant. As expected, for lower ionization flows, the Stark line broadening effects are not dominant, meaning interferometry measurements are more advantageous. For highly ionized flows, where the plasma cutoff frequencies require THz-rated diagnostics, Stark-broadening techniques might be more straightforward.

Table 8.4: Discrimination of the different spectral broadening effects for shock tube experiments on the H- α line (656.46 nm). Data computed from the simulations in Table 8.1 and using SPARK-LbL code [399].

Case	FWHM Broadening (cm ⁻¹)					
	Collisional	van der Waals	Resonance	Stark	Lorentz (All)	Doppler
Jupiter	0.056	0.011	0.173	0.522	0.762	2.228
Mars *	0.551	0.068	0.006	0.004	0.630	0.713

* Mars conditions adapted from VUT-1 shock tube test case [247]: pure CO₂, $v_\infty = 3.4$ km/s, $p_\infty = 826.5$ Pa.

Chapter 9

Final Remarks

“Just leave me alone, I know what to do!”

- Kimi Räikkönen, Abu Dhabi F1 Grand Prix 2012

This Chapter summarizes the main conclusions and contributions of the present work. Alongside it, suggestions for the next steps in the operation of ESTHER are discussed.

9.1 Outcomes

After a decade into the development, design and re-design, scale-testing, manufacturing, assembly, qualification of multiple sub-systems and their integration, equipment selection, mounting, and back and forth in all these steps ESTHER is ready to fire its first real shot. The thesis was centred around the qualification and preparation of its driver, vacuum system and main diagnostics. Nonetheless, it aims to also report the development procedure and history of the project at large. A lot of work was done “behind the curtains” to progress this work and project, thus increasing the deliverables of this work from a very specific one on spectroscopy to a more general work on ESTHER. Among these operational and technical works, Chapter 4 summarizes ESTHER’s overall design and its sub-systems, as well as some engineering concerns that need to be accounted for.

The ESTHER qualification campaign is complete and the first experimental shot may soon take place. During this multidisciplinary campaign experimental, tests were performed on high-pressure combustion for the driver qualification, detailed in Chapter 5, and on the vacuum system to better optimise it and correct its inefficiencies, as discussed in Chapter 6. Alongside it, the VUV-UV optical emission spectroscopy system was qualified, verifying it met the specifications & requirements set by ESA, detailed in Chapter 7. A pre-selection of equipment for a mid-IR optical emission spectroscopy setup was also done. Lastly on the topic of diagnostics, a compact microwave reflectometer/interferometer was designed and a prototype built. Simulation of its working principle on a Space mission, as well as prototype testing, was undertaken with success, as explained in Chapter 8. The major conclusions and outcomes this work could draw from each main ESTHER section are as follows.

9.1.1 Combustion driver

The driver qualification campaign runs full-scale combustion experiments to understand and qualify ESTHER’s combustion driver. This result data later served as input in STAGG numerical code, which predicts the ESTHER shock wave velocity at different driver conditions. The experiments studied combustion of high-pressure diluted hydrogen-oxygen pre-mixtures. These had a filling pressure range between 10-100 bar, oxygen-hydrogen equivalence ratio in 0.45-2.04 region, and inert gas dilutions of 54-79%, both with helium and nitrogen. A Nd:YAG high-power laser with a 100 mm bi-convex lens, which could be unmounted at will, was used to ignited the gas mixture. A simplified summary of the effects of the variables is shown in Table 9.1.

Table 9.1: Lookup table, influence of experimental parameters on compression ratio, flame speed, induction time and propensity to detonation.

	Compression ratio	Flame speed	Induction time	Acoustic wave	Detonation ease
Po ↑	≈	↓	≈	↑	↑
X_{He} ↑	↓	↓	↑	↓	↓
He → N₂	↓	↓	↑	↑	↓
No Focus lens	↑	↑	≈	↑	↑

Driver filling is the dominant parameter in combustion Compression ratio remains approximately constant in the 10–100 bar filling pressure range, as does the ignition induction time. These observations are in agreement with [313]. Increased pressure reduces the laminar flame velocity as pointed out by [324]. The acoustic wave amplitude, formed by the pressure signal bouncing back and forth in the chamber, is larger at higher filling pressures, especially beyond 60 bar. Besides it, the mixtures tend to detonate or transit to a detonation at these higher filling pressures [365, 366]. This mandates the adjustment of the driver molar mixture [He:H₂:O₂] from 8:2:1.2 to 8:2:1.4 at $p_0 > 70$ bar for operational purposes.

Higher dilution yields weaker combustion The dilution by an inert gas, either helium or nitrogen, lowers the compression ratio, flame speed [313], alongside the acoustic wave amplitude and detonation probability of the mixture [323]. Nitrogen is on a first-approach a “stronger” diluent than helium as it further lowers compression ratio, flame velocity and detonation probability [323]. Despite this, our results indicate that the acoustic wave mean amplitude is larger when nitrogen is used as a diluent. The use of nitrogen-diluted mixtures is needed for slow-speed shocks because these combustion processes have lower temperature, pressure and heavier molecular mass, which in turn, mean a lower driver performance.

Lean mixtures are more stable than rich ones Mixtures with excess oxygen produce lower oscillations and are less prone to detonate when compared to mixtures with excess hydrogen. Lean mixtures also have a slower flame velocity, explained by hydrogen having higher diffusivity than oxygen [313, 316, 322, 323, 326]. Nonetheless, varying the air:fuel equivalence ratio shows the typical U-curve behaviour with a maximum near stoichiometric ratio, for compression ratio, flame velocity, acoustic wave

9. FINAL REMARKS

amplitude and detonation. The characteristic curve for compression ratio and flame velocity can be seen in Figure 9.1.

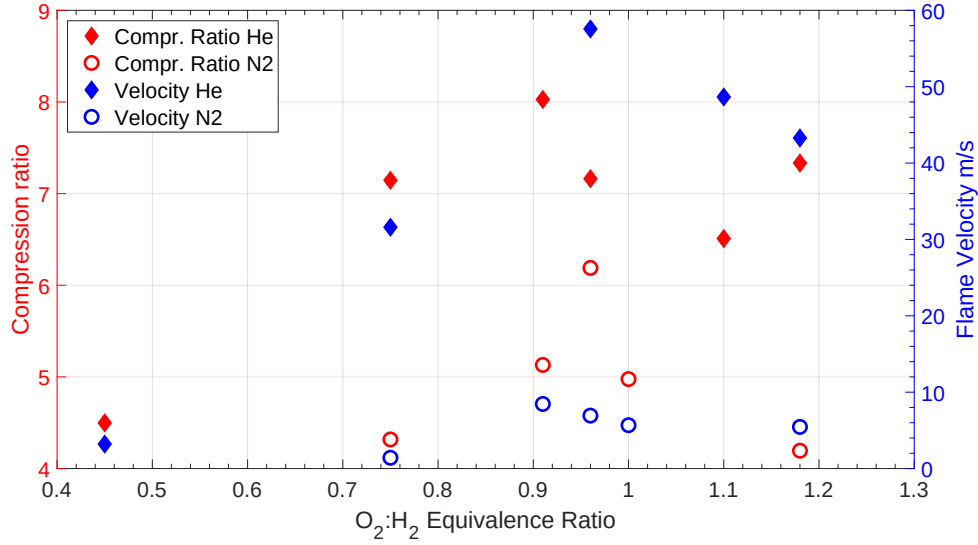


Figure 9.1: Compression ratio function of O₂:H₂ equivalence ratio (left axis), average flame velocity (right axis). $p_0 = 20$ bar, $\chi_{He/N_2} = 71\%$, lens focal distance 100 mm.

Ignition without focusing lens is very turbulent If one removes the 100 mm focusing lens the exact ignition point location is indeterminate. Possibly, ignition occurs at multiple points, which reduces the “effective” chamber length. The increased turbulence caused by multiple ignition points may also contribute to faster flames, which generate stronger acoustic waves and may easily transit to a detonation. Figure 9.2 compares the pressure signal of two identical shots with and without the use of a focusing lens. Operationally the shots should use the lens to minimize detonations and unwanted diaphragm bursts. Besides, the lens is mandatory for filling pressures below 50 bar otherwise ignition is not guaranteed.

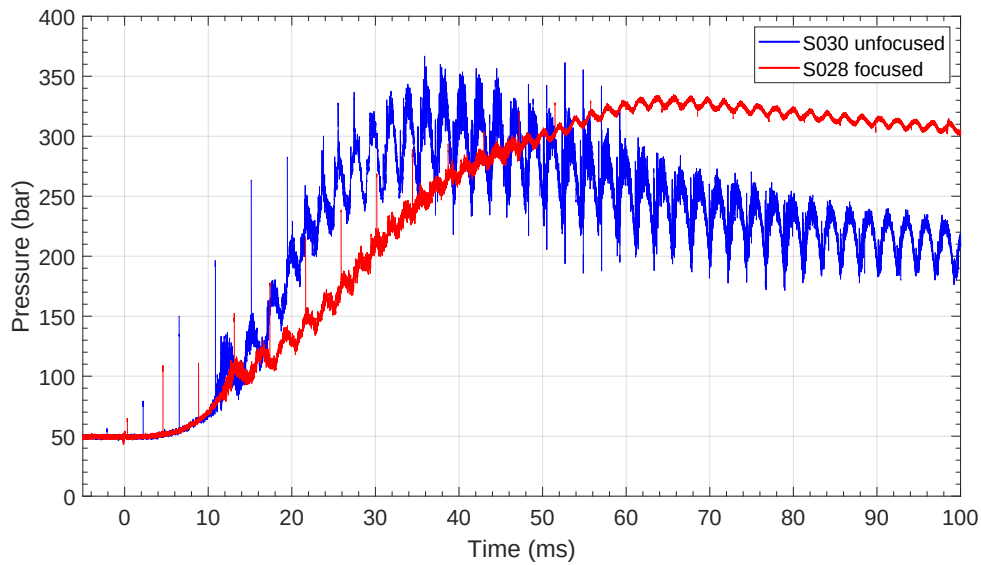


Figure 9.2: S028 and S030 pressure signal comparison. He:H₂:O₂ mixture, $p_0 = 50$ bar, $\lambda = 1.17$ and $\chi_{He} = 71\%$.

ESTHER performance map shows it is capable of achieving shocks between 4 and 14 km/s in air The performance map was computed with the STAGG code. Depending on the gas filling pressure and chemical composition of the different sections one may tune the shock wave speed. The performance map is reproduced in Figure 9.3, with the corresponding driver filling pressure in Table 9.2. Low velocity (< 6 km/s) shocks use the nitrogen mixture. Due to its high acoustic oscillation, filling pressures for nitrogen diluted mixtures cannot go above 50 bar. The helium mixture is used for medium (6–10 km/s) and high-speed (> 10 km/s) shots. By adjusting the compression tube filling pressure, one may tune ESTHER's shock wave at the test section.

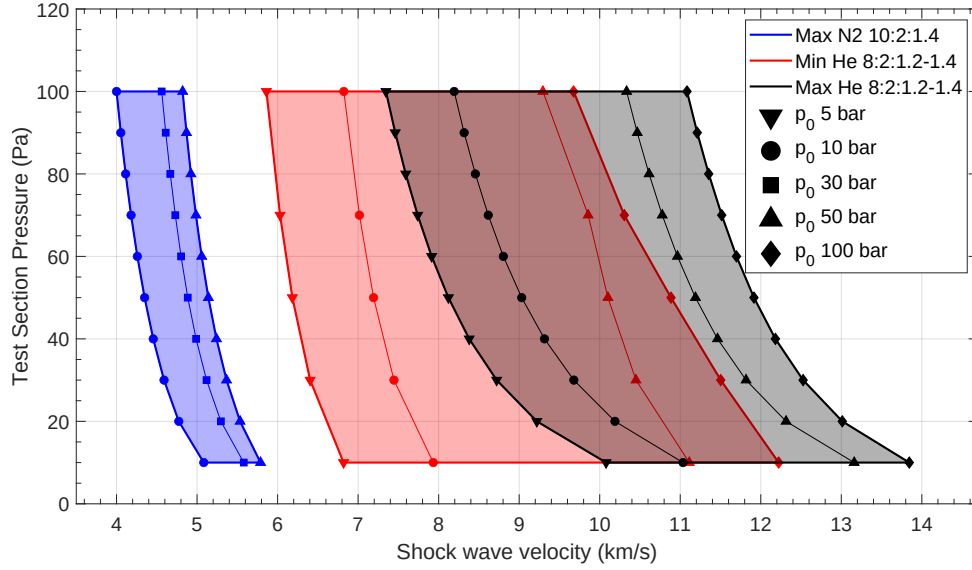


Figure 9.3: ESTHER Performance map for Earth atmosphere.

Table 9.2: STAGG parameters for Earth ($\text{N}_2\text{-O}_2$) performance map simulation. Driver input conditions and STAGG running mode (optimization of CT pressure or simple computation) without any CT losses.

Mixture molar ratios $\text{X:H}_2\text{:O}_2$	Driver pressure filling-peak (bar)	Driver temperature (K)	STAGG mode	Shock wave velocity (km/s)
N_2 10:2:1.4	10 - 38	1,250	Optimization	4.0 - 5.1
N_2 10:2:1.4	30 - 114	1,250	Optimization	4.6 - 5.5
N_2 10:2:1.4	50 - 190	1,250	Optimization	4.8 - 5.8
He 8:2:1.6	5 - 30	2,650	Non-Optimization	5.8 - 10.0
He 8:2:1.6	10 - 60	2,650	Non-Optimization	6.7 - 10.8
He 8:2:1.6	5 - 30	2,650	Optimization	7.3 - 10.2
He 8:2:1.6	10 - 60	2,650	Optimization	8.0 - 10.9
He 8:2:1.2	10 - 61.5	2,650	Optimization	8.2 - 11.0
He 8:2:1.2	50 - 334	2,650	Optimization	10.3 - 13.2
He 8:2:1.4	100 - 660	2,650	Optimization	11.1 - 13.8

9.1.2 Compression tube and test sections

Both the compression tube and shock tube/test section were in a constant state of improvement throughout this work. Starting with the surface state, which was measured and polished to enable them

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to have a low roughness level $R_a < 0.8$ and $R_a < 0.5 \mu\text{m}$, for the CT and ST sections, respectively. Figure 9.4 shows an example of a measurement of the roughness level of an O-ring cavity. The vacuum system and vacuum sealants were a particular example of this, as they suffered changes, adaptations and corrections constantly through the course of this work. Such modifications included addressing the position of the turbomolecular pumps, eliminating leakage sources or designing parts for the gas delivery system. Initially, the turbomolecular pumps were upstream of the gate valve and vacuum bellows, making the bellows compressed under load, possibly damaging them. The gate valve was moved downstream of the bellows and a cage-like jig was added to avoid full compression, as illustrated in Figure 9.5.

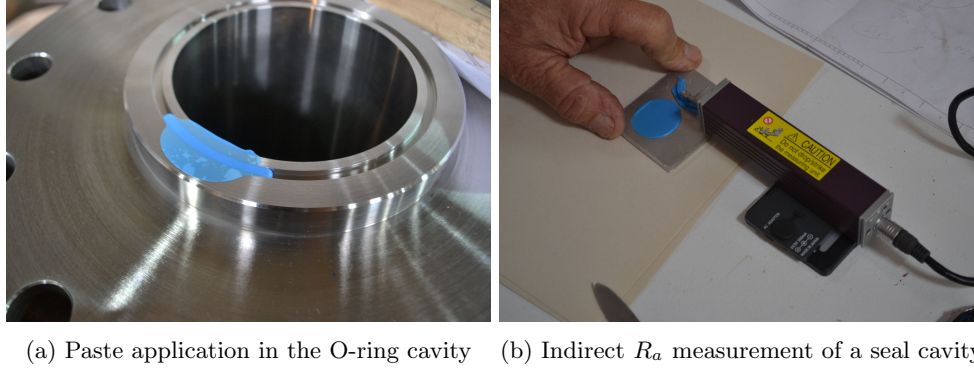
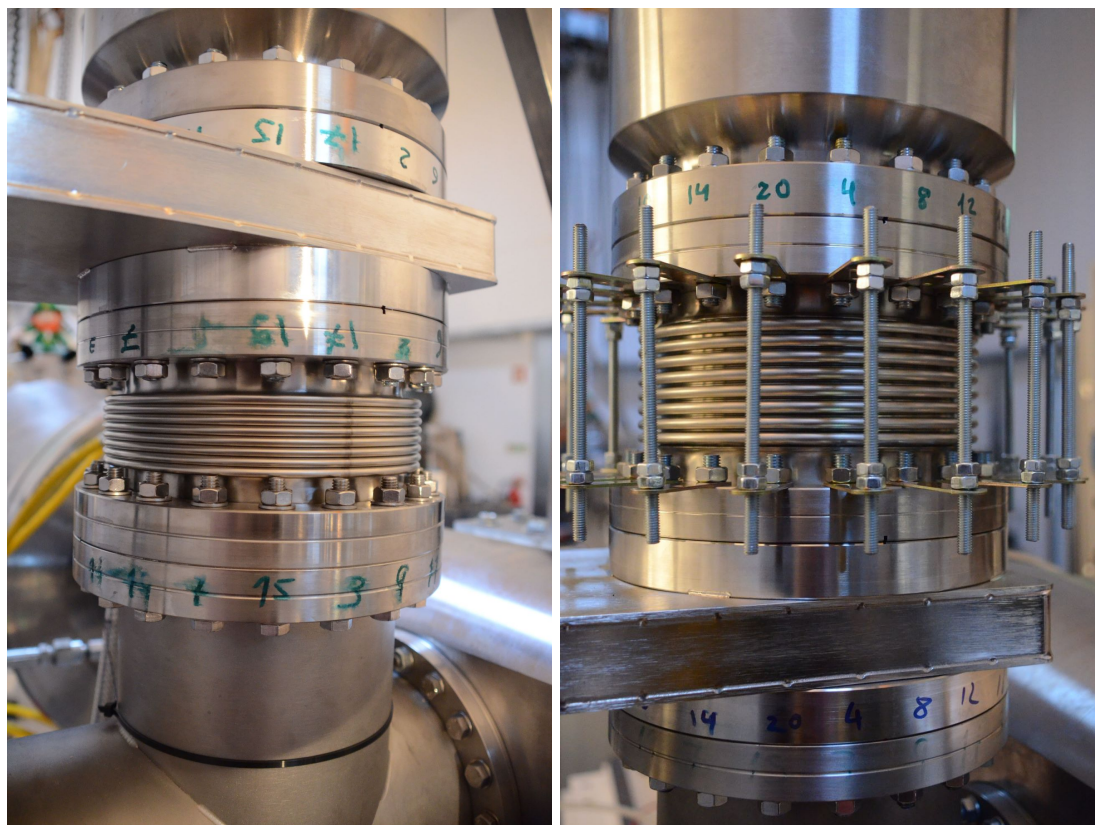


Figure 9.4: Indirect roughness measurements, images from the ST8 section.

The test-section can achieve an ultimate vacuum pressure of around 1×10^{-6} mbar and a leakage rate of 3.94×10^{-5} mbar l s^{-1} By correcting the leakage points and some parts designs, the test section can reach an ultimate pressure near 1×10^{-6} mbar and a leakage rate of 3.94×10^{-5} mbar l s^{-1} . Major leak sources were identified in the auxiliary tee-shock tube connections and the shock detection plug. Contamination from the residue of manufacturing/machining operations (small steel fragments/dust) was found in the sealant cavities and helicoflex O-rings. A careful re-cleaning of the offending threaded cavities near the O-ring, followed by the cleaning of the O-ring cavity and the replacement of the helicoflex O-ring, solved the problem and brought leakage rates of this specific seal to an acceptable level. In the case of the shock detection plug, the sealing copper rings were annealed and polished for better ductility and plastic deformation. An overall contamination rate below 1% is thus achievable.

Diaphragm holders of CT-ST and ST-DT sections seem to be appropriate Initially, the hydraulic system sealing the diaphragms by stamping them in place was found to buckle or rupture them. The thin diaphragm disk may not have enough material to deform before being ripped or suffering buckling. Copper could be used but its cost are prohibitive, and even so may still fail. An alternative, deployed in other impulse facilities, is to have O-rings sealing the diaphragm as exemplified in Figure 9.6. Tests using blank disks with an O-ring revealed that this design would perform well in vacuum. New vacuum tests using this configuration saw improved overall leakage rates from 6.02×10^{-2} to 3.00×10^{-3} mbar l s^{-1} , a result compounded of the sealing of leaks discussed in the previous paragraph, and the good behavior of the end taps sealed with FKM O-rings. Since then, these end taps have been removed



(a) Old pumping setup with fully compressed bellows (b) New pumping setup with limiting compression jig

Figure 9.5: Shock tube turbomolecular vacuum pump, gate valve, and bellows. Fully compressed bellows during vacuum pumping (left) and new configuration with limiting jig assembly (right)

and the original diaphragm holders have been put back in place. A new series of vacuum tests have been carried out, and the ultimate vacuum and the corresponding leakage rates have been assessed. The hydraulic force applied to the diaphragms has been tailored to pressure values around 50 bbar. The aluminium diaphragms have been selected in favor of the tin ones, since the former were found to buckle less in our clamping configuration. First results show that the ultimate pressure remains around the current values of $\sim 10^{-6}$ mbar, showing promise for still achieving sub-1% contamination rates without the need of additional FKM O-ring seals. Those FKM seals could be a potential liability in case parts of the hot shocked flow would leak into the seal cavity, leading to erosion of the seal and contamination of the flow.

Surface cleaning and leaving the system under vacuum over time improves vacuum level

By cleaning the wall surface and letting the system under vacuum the materials had time to outgas and vaporize carbon-based molecules. In this way, the system could reach lower ultimate pressure in a shorter time. Besides enabling a lower vacuum pressure, and thus contamination level, cleaning and outgassing reduces the presence of carbon molecules that typically radiate a lot and can easily mask the spectroscopy data. It is clear that outgassing is something that needs to be carefully monitored in the test section (besides leakage) during the first operational campaigns. An example of a typical vacuum test is shown in Figure 9.7.

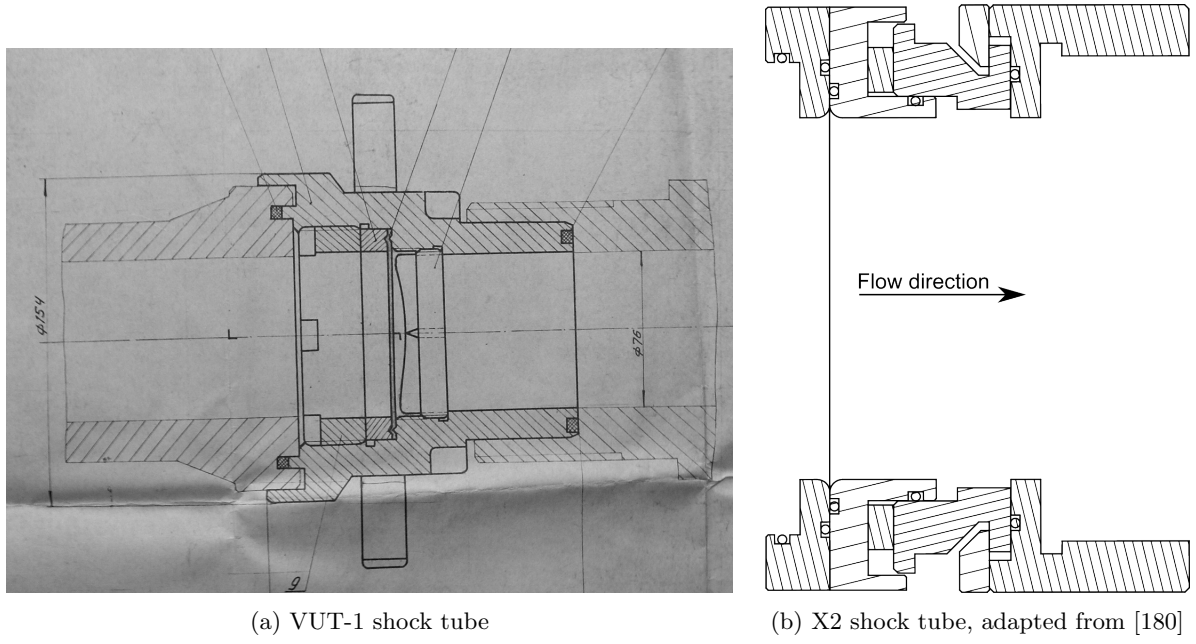


Figure 9.6: Different diaphragm holder designs. Stamping method (left) and O-ring seal (right)

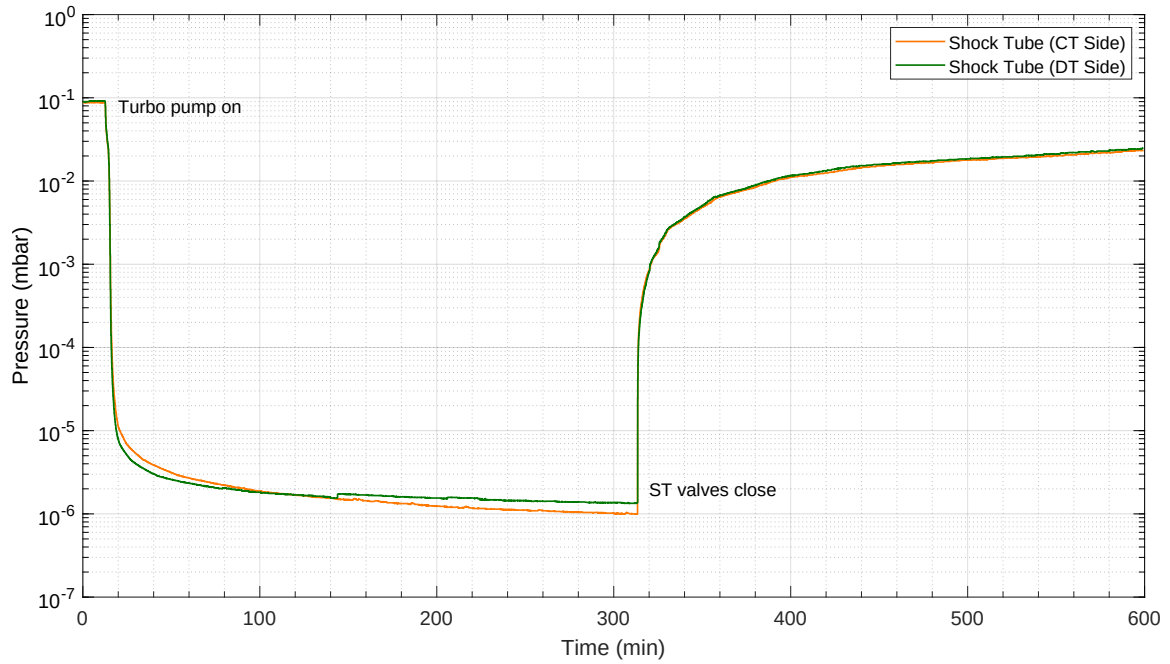


Figure 9.7: Pressure time evolution for vacuum tests on 25th of July 2023. $\dot{p} = 1.31 \times 10^{-6}$ mbar/s, $Q_{tot} = 3.94 \times 10^{-5}$ mbar $\cdot$ ls $^{-1}$.

Since these tests, a simple heating system, comprised of electrical heating strips wrapped around the surface of the test section, has been added to the facility. These should improve the outgassing characteristics of the test section and ultimately decrease surface contamination, improving cleanliness.

9.1.3 Diagnostics

Plugs for shock detection were designed to be interchangeable Shock detection on ESTHER can be done in two ways, by observing the rise in pressure by the passing shock, or through the light

it emits. In the first case, a piezo-electric pressure transducer sensor is used, while the second uses a photodetector, each with its own advantages and disadvantages. Along the shock tube axis, multiple stations will identify the shock wave so a computer can compute the shock speed and trigger the diagnostic in time. The compression tube section will only work with pressure transducers, however, it should be possible to switch between Kistler piezo-electric sensors 603C and 6215 with ease. In the shock tube section, the multiple ports are to function with either Kistler model 603C or by attaching an optical fibre coupled to a photodetector, based on [183]. Figures 9.8 and 9.9 show the designs for both these systems. The plug is manufactured to mount the sensor with the largest dimensions, while an adapter is manufactured to mount the smaller one.

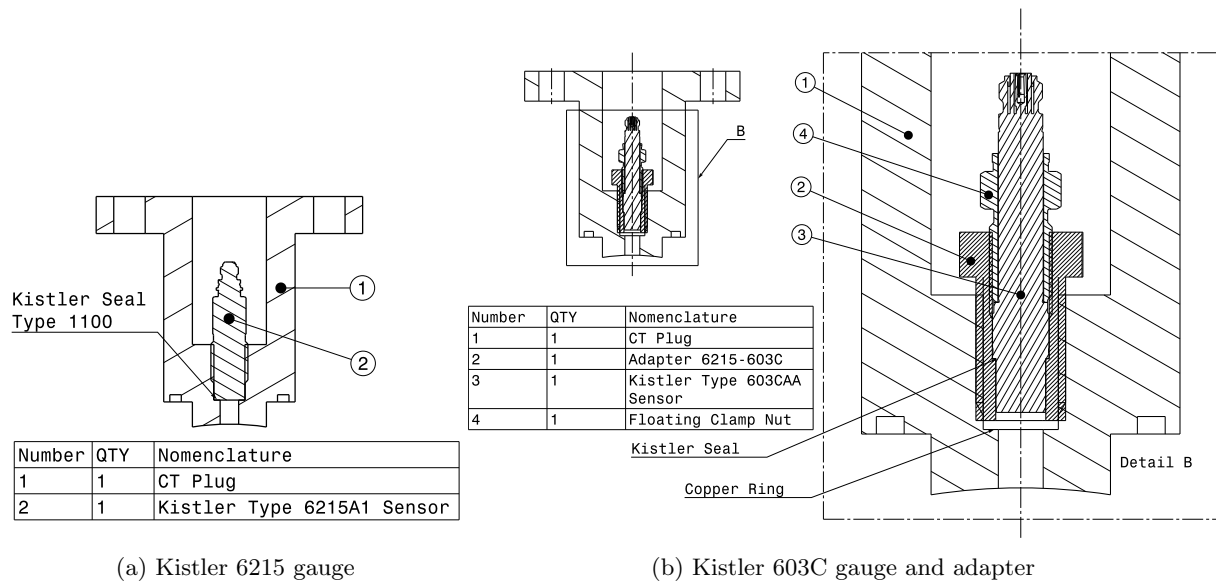


Figure 9.8: CAD drawing of ESTHER shock wave detection plug and different gauge options for the compression tube section.

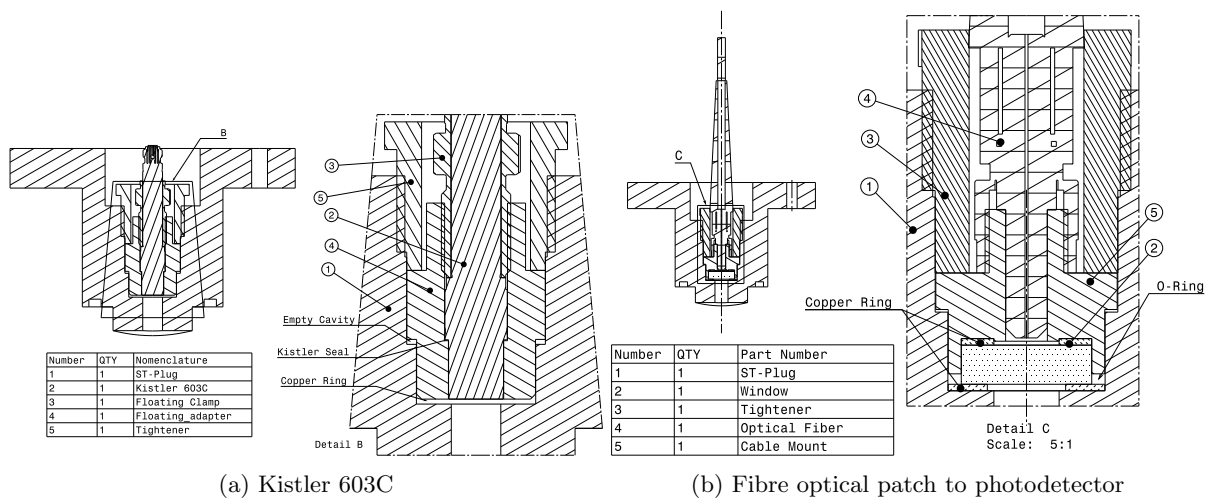


Figure 9.9: CAD drawing of ESTHER shock wave detection plug and different options, piezo-electric or photodetector, for the shock tube section.

9. FINAL REMARKS

Qualification of the VUV-UV optical emission spectroscopy equipment was successful The setup verifies the specifications and requirements set by ESA. Tests used a mercury vapour lamp and two diffraction gratings 1,200 and 600 gr/mm, a resolution of 0.5 nm @ 253.6 nm with an accuracy of 5 pixels was achieved with the finer grating. An optical plug was designed to mount onto the test section, maximizing the amount of light collected during the experiment. The overall VUV-UV setup is depicted in Figure 9.10

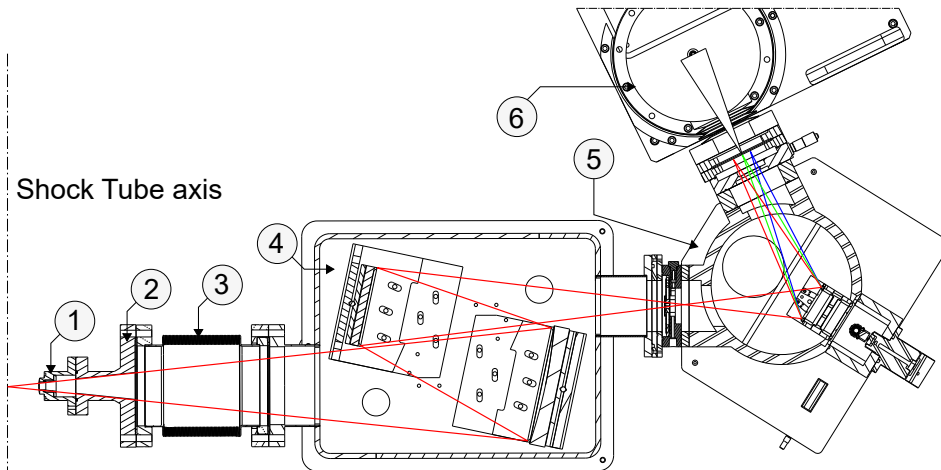


Figure 9.10: ESTHER UV-VUV spectroscopic setup design schematic. 1-Shock tube plug; 2-Connection piece; 3-Adjustable Bellows; 4-Optics collection box; 5-Spectrograph with diffraction grating; 6-Connection to streak and CCD camera.

A pre-selection for mid-IR spectroscopy setup parts was done Due to the longer wavelengths involved, IR spectroscopy cannot use a streak camera setup, owing to the lower photon energy, not sufficient to eject an electron from the photocathode. A fast infrared camera is used instead. A low groove density diffraction grating is used to have a broader spectral range (1–5 μm). A fibre optic cable can be used to transport the light signal from the test section to the spectrometer. The corresponding plug to mount on the test section was designed to bolt the fibre optic cable to it, connecting it then to the spectrometer.

A prototype compact microwave reflectometer/interferometer was designed, built and tested

A modular architecture was chosen, where the same back-end can connect to different front-ends suited for different plasmas. The back-end is a complete, digitally controlled, signal-swept generator operating at a microwave frequency range, of 8–12 GHz or 12–18 GHz. The front-end assembly has active frequency multipliers, the antenna and signal detection device, which interface the plasma, and either a mixer or a single-end detector. To study this technique, a computational rebuild of the RAM-C II flight mission [431] done through the SPARK code [428, 469] was implemented into REFMUL code simulating the on-board reflectometer signal. The codes showed good integration as REFMUL could recreate the expected behaviour of RAM-C II reflectometers as exemplified in Figure 9.11.

Currently, a frequency multiplier of 6 (six) brings the probing frequency to the 70.8–112.8 GHz range

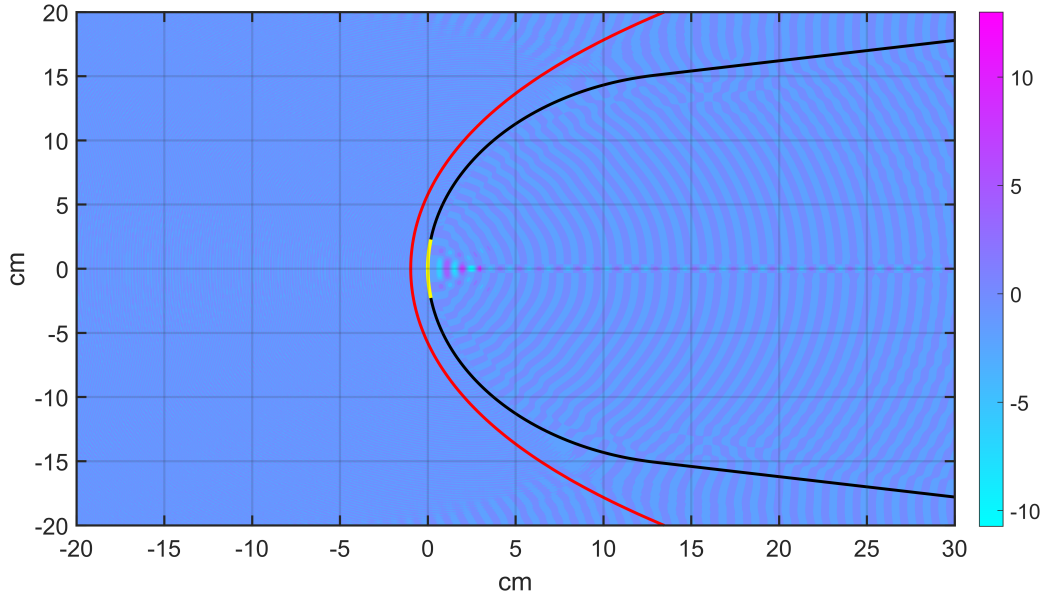


Figure 9.11: REFMUL simulation in RAM-C II conditions, wave frequency 35 GHz. Spacecraft in black and shockwave in red. EM wave electric field represented by the colour bar.

(NATO W bands). A proof-of-concept test was designed and performed to estimate the electron density of a fluorescent lamp. An average electron density of $(4.0 \pm 2.4) \times 10^{16} \text{ m}^{-3}$ was obtained, which is within one order of magnitude of the results of [486]. Figure 9.12 depicts the temporal evolution of the electron density in the test, where one can even identify the power grid 50 Hz oscillation. The test deemed successful.

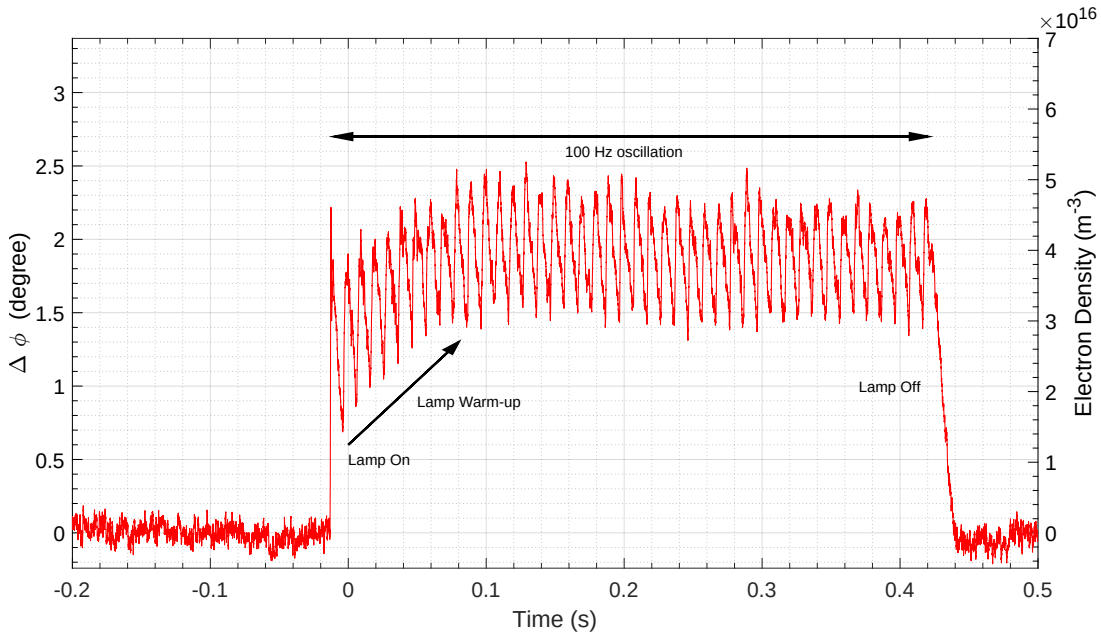


Figure 9.12: Interferometer phase difference and electron density for the fluorescent lamp plasma source setup.

9.2 Next steps and suggestions

Since ESTHER qualification is complete, the next step is to prepare and perform an experimental shot. Nonetheless, some challenges can in the meanwhile be addressed:

Intensity calibration of the spectroscopy system needs to be done before any quantitative results are drawn. Currently, only the temporal calibration is concluded. The spectral window is miss-aligned and still requires some adjustment of the optics, however the intensity calibration requires a known radiation source to be used.

Concerning the microwave interferometer, its current configuration has the emission and receiving antennas side-by-side, which is not optimal. A second waveguide is to be designed so the antennas can be mounted face-to-face on the ESTHER test section. Alongside this, the test ports for these antennas are also to be manufactured. Different front-end assemblies are also to be selected and manufactured, so this diagnostic equipment can cover a broader electron density range.

Regarding the driver section, the STAGG code will need a small re-calibration with the new experimental data. It was initially calibrated using data from the VUT-1 shock tube and can be fine-tuned for ESTHER utilizing its driver and shock speed data. The driver section also presents the opportunity to study high-pressure combustion and ignition. In the future, different focusing lens could be investigated for ignition, or the chamber could be equipped with a spectrometer or a Schlieren imaging method.

Overall, ESTHER is basically ready to operate, yet it is a living facility. Procurement of the infrared spectroscopy equipment and calibration sources still needs to be finished. An extensive calibration procedure, including spectral, spatially/temporal and intensity-wise, must follow before absolute emission measurements can be done. Improvements to its diagnostics equipment and analysis methodologies should always be sought. Its large number of diagnostic ports (16) provides opportunities for further research. Lastly, the large dump tank section may in the future be upgraded to test small-scale spacecraft models and serve if needed as an hypersonic aerodynamic facility.

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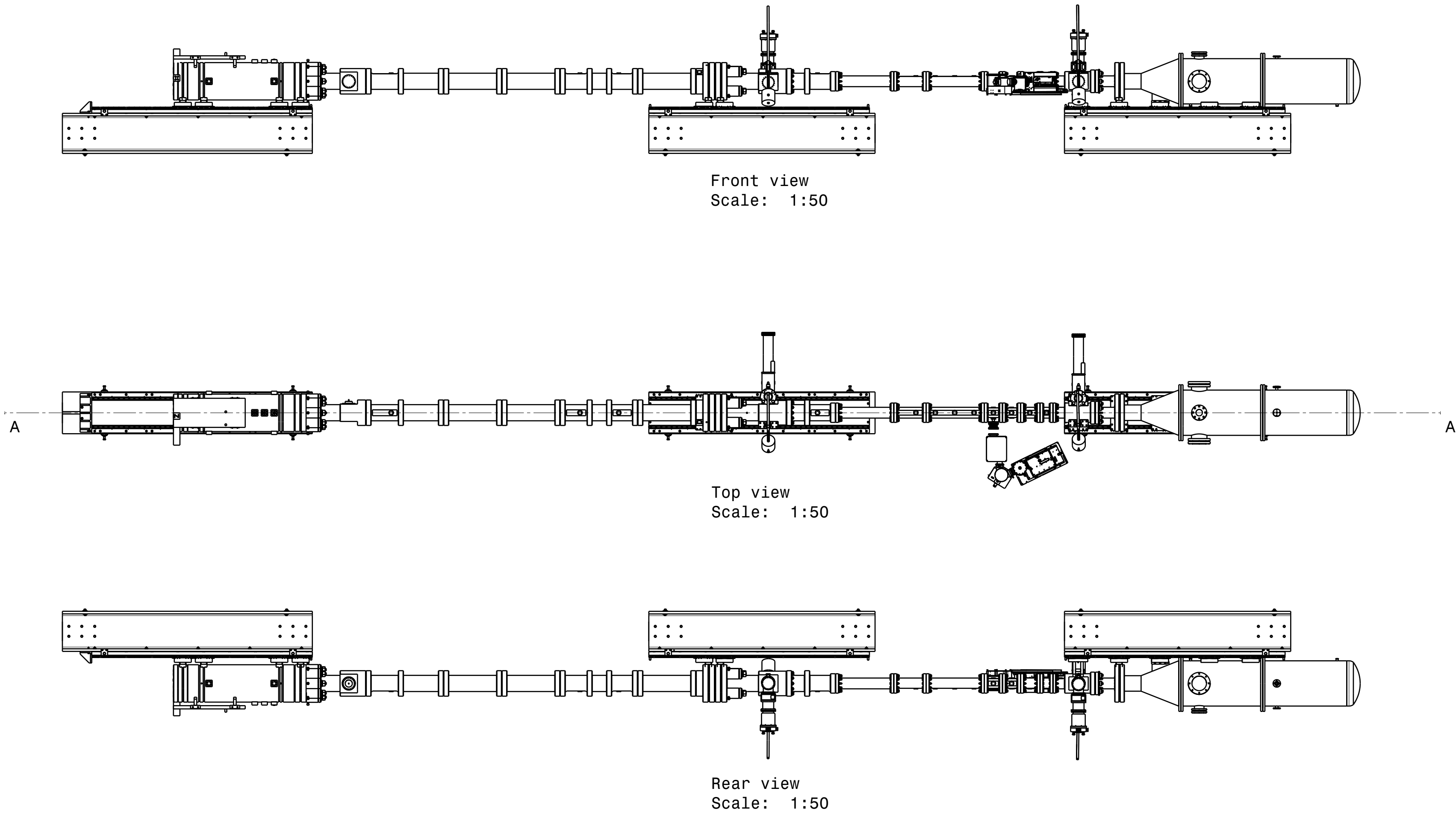
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Appendix A

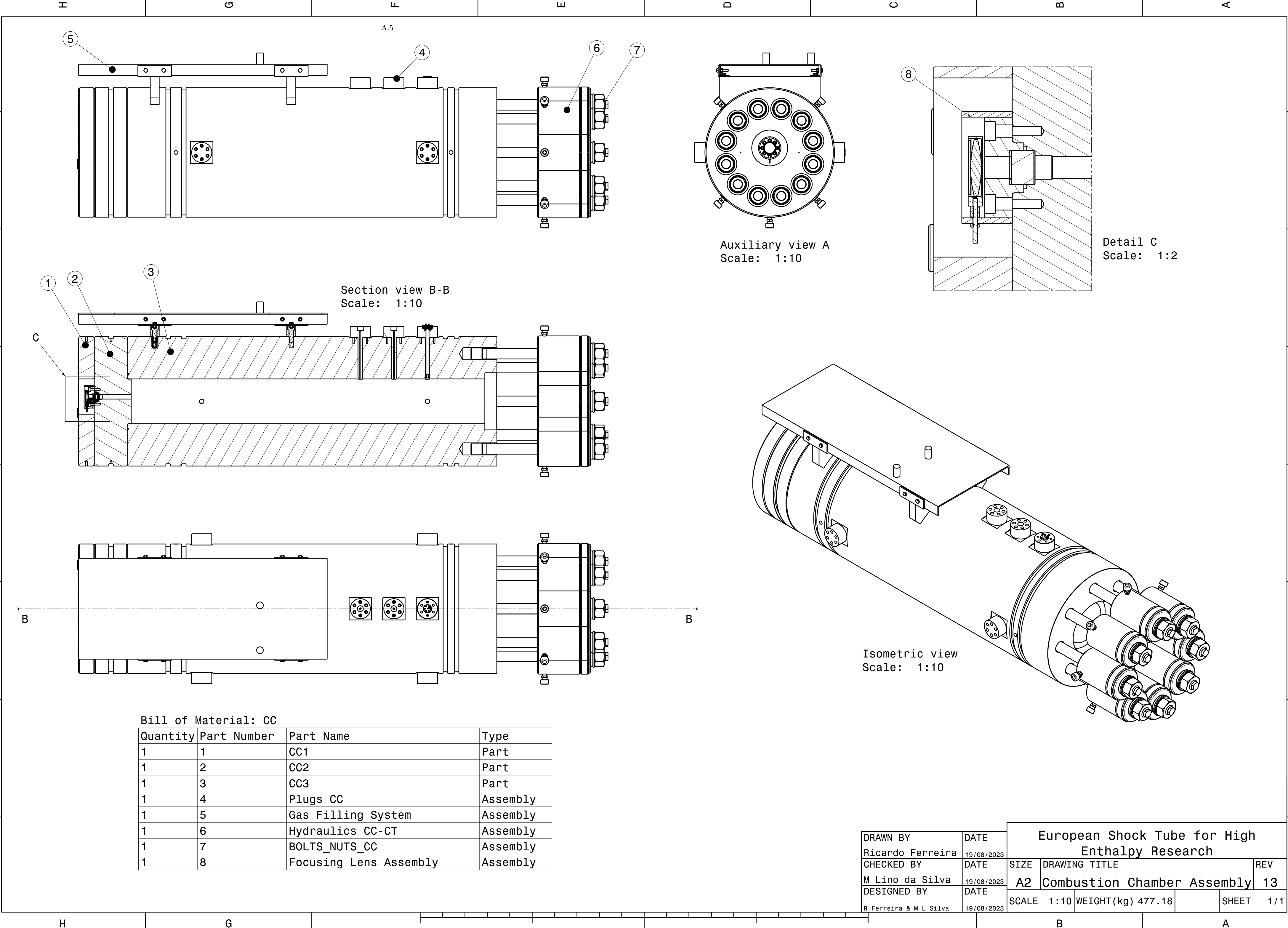
Computer Aided Design - Drawings

A selection of CAD drawings from the ESTHER project is shown in this appendix. The list is not an extensive one, as it only covers the main body sections. The list of selected drawings encompasses:

- ESTHER overall assembly
- Driver/Combustion chamber assembly
- Compression tube assembly
- Shock tube/test section assembly



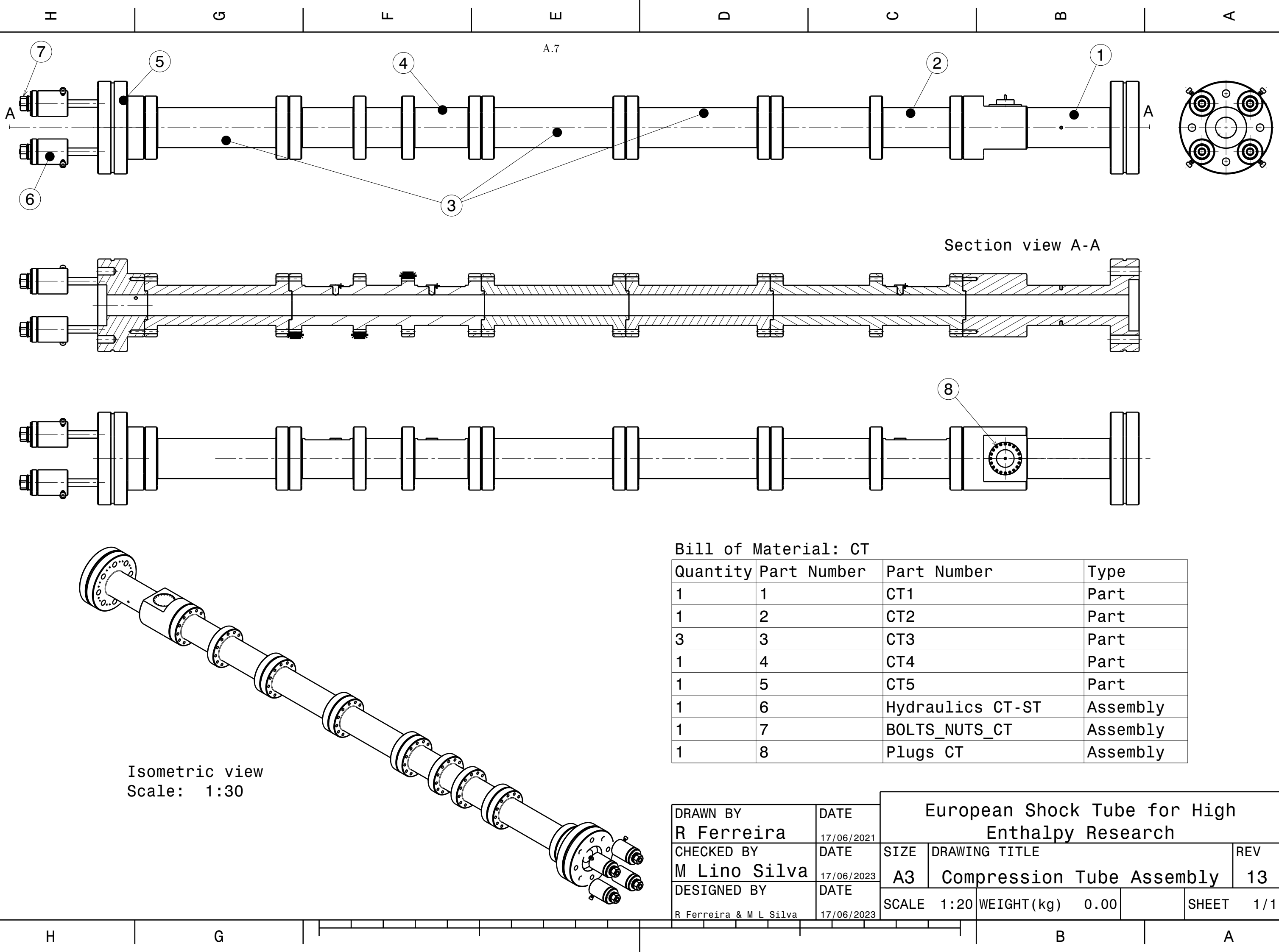
DRAWN BY		DATE		European Shock Tube for High Enthalpy Research			
Ricardo Ferreira		18/08/2023		SIZE		DRAWING TITLE	
CHECKED BY		DATE		A2		ESTHER	
M Lino Silva		18/08/2023		SCALE		1:30	
DESIGNED BY		DATE		WEIGHT(ton)		SHEET	
R Ferreira & M Lino Silva		18/08/2023				1/1	



Bill of Material: CC

Quantity	Part Number	Part Name	Type
1	1	CC1	Part
1	2	CC2	Part
1	3	CC3	Part
1	4	Plugs CC	Assembly
1	5	Gas Filling System	Assembly
1	6	Hydraulics CC-CT	Assembly
1	7	BOLTS_NUTS_CC	Assembly
1	8	Focusing Lens Assembly	Assembly

		European Shock Tube for High Enthalpy Research				
DRAWN BY	DATE					
Ricardo Ferreira	19/08/2023					
CHECKED BY	DATE	SIZE	DRAWING TITLE			REV
M Lino da Silva	19/08/2023	A2	Combustion Chamber Assembly			13
DESIGNED BY	DATE					
R Ferreira & M L Silva	19/08/2023	SCALE	1:10	WEIGHT(kg)	477.18	SHEET 1/1

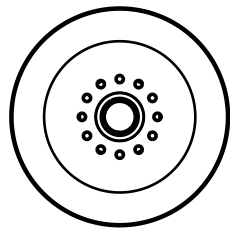


Bill of Material: CT

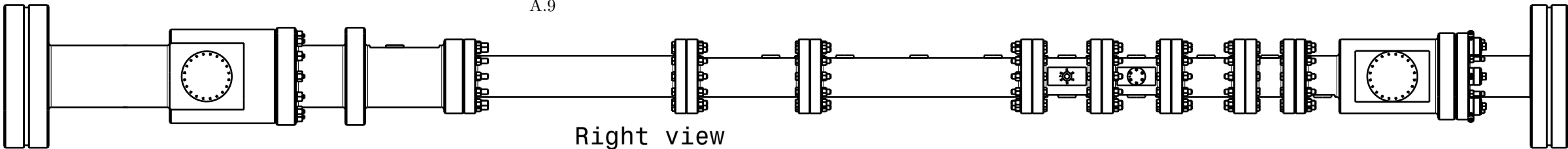
Quantity	Part Number	Part Number	Type
1	1	CT1	Part
1	2	CT2	Part
3	3	CT3	Part
1	4	CT4	Part
1	5	CT5	Part
1	6	Hydraulics CT-ST	Assembly
1	7	BOLTS_NUTS_CT	Assembly
1	8	Plugs CT	Assembly

Isometric view
Scale: 1:30

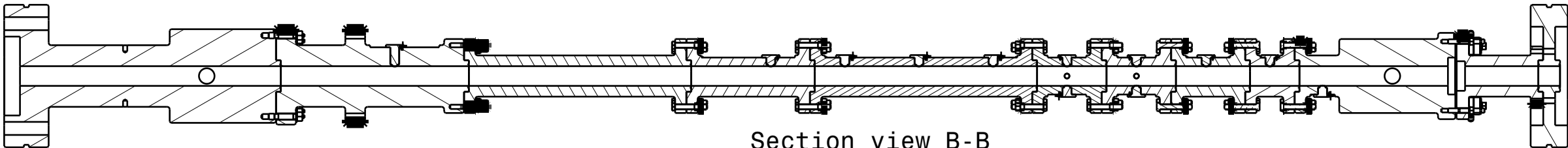
DRAWN BY R Ferreira		DATE 17/06/2021		European Shock Tube for High Enthalpy Research			
CHECKED BY M Lino Silva		DATE 17/06/2023		SIZE A3	DRAWING TITLE Compression Tube Assembly		REV 13
DESIGNED BY R Ferreira & M L Silva		DATE 17/06/2023		SCALE 1:20	WEIGHT (kg) 0.00		SHEET 1/1



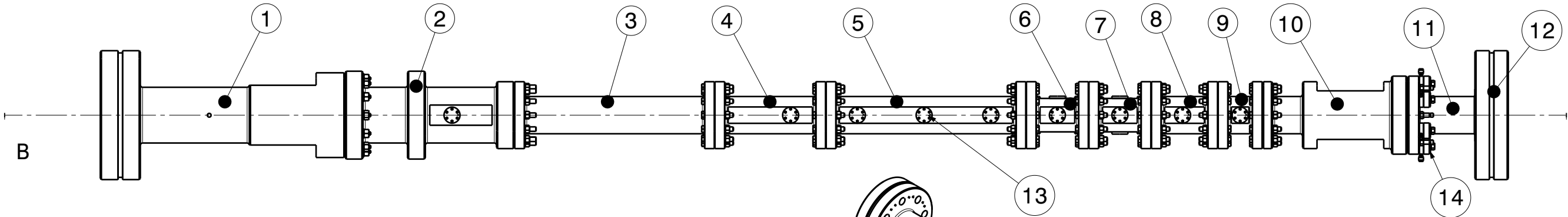
Rear view
Scale: 1:20



Right view
Scale: 1:20



Section view B-B
Scale: 1:20



Isometric view
Scale: 1:30

Bill of Material: ST

Quantity	Part Number	Part Name	Type
1	1	ST1	Part
1	2	ST2	Part
1	3	ST4	Part
1	4	ST3	Part
1	5	ST7	Part
1	6	ST8	Part
1	7	ST8B	Part
1	8	ST5	Part
1	9	ST6	Part
1	10	ST9	Part
1	11	ST10	Part
1	12	FL1	Part
1	13	Plugs ST	Assembly
1	14	Hydraulics ST-DT	Assembly
1	15	BOLTS_NUTS_ST	Assembly

DRAWN BY R Ferreira	DATE 19/08/2023
CHECKED BY M Lino Silva	DATE 19/08/2023
DESIGNED BY R Ferreira & M L Silva	DATE 19/08/2023

European Shock Tube for High Enthalpy Research			
SIZE A3	DRAWING NUMBER Shock Tube Assembly		REV 13
SCALE 1:20	WEIGHT (kg) 305.85	SHEET 1/1	

Appendix B

Vacuum theory and technologies

B.1 Theory

Here we will present a small theoretical framework on vacuum and vacuum generation physical models. For an ideal gas we have the well-known equation of state [393, 487],

$$pV = Nk_B T \quad , \quad (\text{B.1})$$

where the product of pressure p and volume V is proportional to the number of gas particles N and the absolute temperature T by the Boltzmann constant k_B . One may define the pumping rate S , as the rate at which one decreases the volume,

$$S = \frac{dV}{dt} \quad . \quad (\text{B.2})$$

If we definite the gas quantity $q \equiv pV$ ([487, 488]), and combine with Equation B.1, one gets the gas throughput Q [in SI units Pa·m³/s],

$$Q = \frac{dq}{dt} = p \frac{dV}{dt} = pS \quad . \quad (\text{B.3})$$

Gas flowing through a piping system encounters a natural resistance to flow. It is mostly caused by the interaction between the walls and molecules, viscosity μ . The conductance, C , between two cross-sections is defined as,

$$C = \frac{Q}{\Delta p} \quad , \quad (\text{B.4})$$

with Δp the difference in pressure between two points. One may equate a pipping system to an electric circuit, with electric resistors, where $R = 1/C$. For a set of pipes in serial arrangement, the equivalent conductance C_{eqv} is given by [487, 488],

$$C_{eqv} = \sum_{i=1}^n \frac{1}{C_i} \quad (\text{serial}), \quad (\text{B.5})$$

with C_i the conductance of each pipe. For the case of a parallel pipeline arrangement C_{eqv} follows,

$$C_{eqv} = \sum_{i=1}^n C_i \quad (\text{parallel}), \quad (\text{B.6})$$

The conductance of a pipe will differ for different flow regimes (free molecular or continuum/viscous flow). These can be identified by the dimensionless Knudsen number (Kn), as illustrated in Figure B.1. A flow is said to be in a free molecular regime if $\text{Kn} > 3$, while in continuum/viscous flow if $\text{Kn} < 10^{-2}$. For continuum/viscous flow, the conductance is proportional to the mean pressure $\bar{p}$ of the pipe, while in free molecular it becomes pressure independent. The Knudsen number is defined as,

$$\text{Kn} = \frac{\bar{l}}{L}, \quad (\text{B.7})$$

where $\bar{l}$ is the mean free path, and L is the representative length scale (typically the tube diameter for vacuum piping). For a gas, comprised of identical particles, $\bar{l}$ can be estimated in a hard sphere model as,

$$\bar{l} = \frac{k_B T}{\sqrt{2} p d_{mol}^2}, \quad (\text{B.8})$$

where d_{mol} is the average molecule diameter.

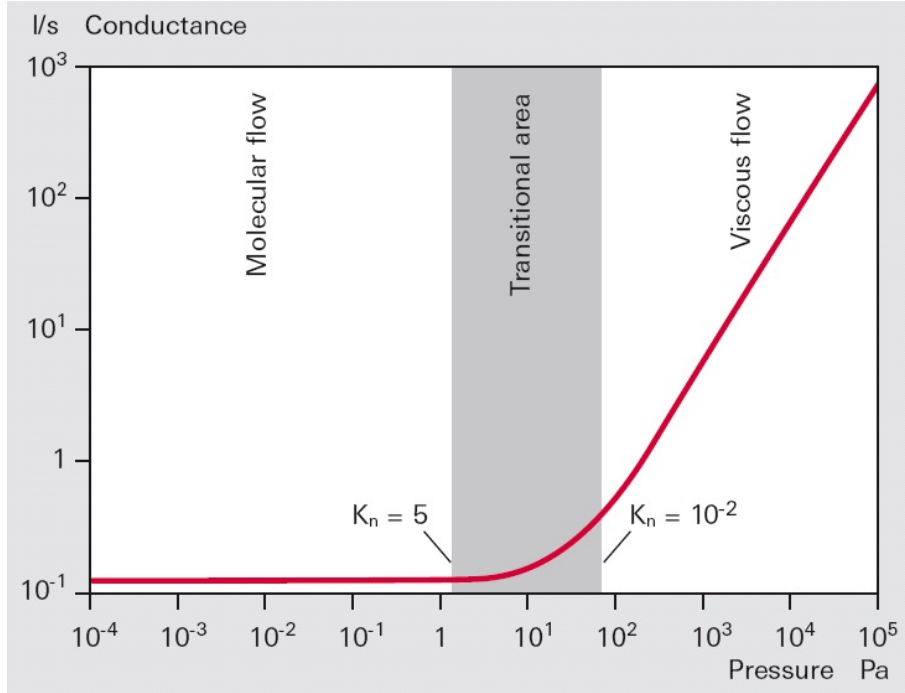


Figure B.1: Piping system conductance function of Knudsen number, from [488].

B.1.1 Conductance

The conductance of an orifice with cross-section area A in free molecular regime, $C_{or,mol}$, may be estimated from its thermal velocity v_{th} and mean gas mass $\bar{m}$,

$$C_{or,mol} = A \frac{v_{th}}{4} = \frac{A}{4} \sqrt{\frac{8k_B T}{\pi m_0}} = A \sqrt{\frac{k_B T}{2\pi \bar{m}}} \quad . \quad (B.9)$$

The conductance for continuum/viscous flow is not as straightforward to estimate, thus we will not present it. For a pipe with diameter d and length L , in continuum/viscous flow its conductance $C_{pipe,lam}$, is proportional to the mean pressure through,

$$C_{pipe,lam} = \frac{\pi d^4}{256\mu L} (p_1 + p_2) = \frac{\pi d^4}{128\mu L} \bar{p} \quad , \quad (B.10)$$

where p_1 and p_2 are the pressures the pipe ends, and μ the viscosity. In free molecular regime, the conductance of a pipe $C_{pipe,mol}$ is proportional to the probability of a molecule passing it, $P_{pipe,mol}$, and the orifice, thus,

$$C_{pipe,mol} = C_{or,mol} \times P_{pipe,mol} = \frac{A}{4} v_{th} \times \frac{3}{4} \frac{d}{L} = \frac{d^3}{6L} \sqrt{\frac{2\pi k_B T}{\bar{m}}} \quad , \quad (B.11)$$

The pumping time t for a system of volume V may be estimated knowing the initial and final pressures, as well as the effective pumping speed S_{eff} , [488]. This later needs to be computed from the vacuum pump nominal pumping speed S_{nom} and the system total conductance S_{total} , [488]. Thus, it follows,

$$t = \frac{V}{S_{eff}} \ln \left(\frac{p_{initial}}{p_{final}} \right) \quad , \quad (B.12)$$

$$\frac{1}{S_{eff}} = \frac{1}{C_{total}} + \frac{1}{S_{nom}} \quad . \quad (B.13)$$

B.1.2 Ultimate Pressure

Ultimate pressure, p_u , is the limiting pressure of the vacuum vessel below which more time spent vacuuming does not lead to a significant pressure reduction [487]. Higher pumping speed and lower pump ultimate pressure will contribute to a higher vacuum however, some gas influx will occur until pressure reaches an equilibrium. The gas influx Q_{in} is mostly caused by:

- a) Q_{leak} - Leakage into the vessel from the surrounding atmosphere;
- b) Q_{des} - Outgassing is the release of any material or component from the inner walls. It can be condensed vapour, adsorbed chemicals, etc;
- c) Q_{diff} - Back-diffusion is the entering of gas through the pump system port. Some gas will flow from the high-pressure side to the vacuum side of the pump.
- d) $Q_{back-stream}$ - Back-streaming from the pump is caused by fluid or lubricant fluids that scatter towards the vacuum vessel. Added to this, is also the evaporation of said lubricant from the pumping system towards the vacuum vessel.
- e) Q_{perm} - Permeation is the penetration of the seals and vessel walls by small gas molecules, such as helium.

All these parameters are dependent on pressure, and temperature, and will vary over time. A straightforward calculation of the ultimate pressure, and/or pressure evolution of a vacuum system are therefore hard to obtain.

For ESTHER a more empirical approach was mostly taken owing to the nature of the experiments carried out in a shock tube. Since a shock tube is a non-steady state facility and requires opening/closing for diaphragm replacement, some leakage may occur through the seal/seal cavities. The outgassing of ESTHER walls therefore cannot achieve the low levels seen in steady high vacuum facilities. The reason is twofold: firstly, ESTHER vacuum operates for short periods (~ 1 hour), secondly, after each run both the compression tube (CT) and more importantly the shock tube (ST), are freshly contaminated with water and test gas products. As previously explained in Chapter 5, the hydrogen combustion and laser ignition system help with keeping carbon deposits out of the system. However, any experiment with CO_2 gas will pollute the shock tube with carbon.

Outgassing

The primary outgassed species for an unbaked steel vacuum vessel is water, with H_2 , CH_4 , CO , and CO_2 making around 10% of the total outgassed species [301]. The desorption¹ time, t_{des} , can be estimated by (adapted from [488]),

$$t_{des} = q_{des} \cdot A \frac{t_0}{Q} = q_{des} \cdot A \frac{t_0}{p_b S_{eff}} \quad , \quad (\text{B.14})$$

where t_0 is the typical desorption time constant, about 1 hour for metal surfaces, q_{des} the outgassing rate constant and A the vessel area. The desired cleaning pressure, p_b , should be at least one order of magnitude above the pump's ultimate pressure. Moshey in [489] compiled data of outgassing rate constants for multiple materials. The outgassing rate of unbaked S304 steel would depend on its surface treatment, some examples in $\text{Pa m}^3/(\text{s m}^2)$ are:

- beadblasted $\approx 2.0 \times 10^{-6}$
- sandblasted $\approx 8.9 \times 10^{-7}$
- degreased, after 10 hours, $\approx 2.0 \times 10^{-7}$
- degreased, after 40 hours, $\approx 1.1 \times 10^{-7}$

Baked materials have q_{des} at least one order of magnitude lower than unbaked ones. These values are highly variable and require experimental data/evidence to be fitted or estimated for different facilities.

Li and Dylla in [490] provide experimental desorption rates for vented vacuum chambers. They concluded that water is desorbed from the surface oxide layer, and its rate is highly dependent on the amount of adsorbed water. Venting in a dry atmosphere was highly beneficial. The authors also speculate that the surface finishing is important for the desorption rate for an unbaked surface. After 10 minutes of pumping, a trend for the desorption rate, Q_{des} , was identified, what is called the regular mode,

¹desorption is the physical process where a surface bounded substance is released. Its opposite process is called adsorption

$$Q_{des} = Q_{r0}/t^a \quad , \quad (B.15)$$

where Q_{r0} is the outgassing rate at the regular mode onset time. The values for a ranged from 0.65 to 1.3, with Q_{r0} ranging from 6.8×10^{-5} to 1.1, in $\text{Pa m}^3/(\text{s m}^2)$, in dry to humid venting, respectively. Rozanov in [491] shows that electropolishing can be effective in removing the initial carbon contamination. The author also shows that the desorption rate will converge to the regular mode.

Leakage

For a continuously pumped vessel an equilibrium pressure p_{eq} will be established balancing the leakage throughput rate Q_L and pumping rate S , as

$$Q_L = p_{eq} S \quad . \quad (B.16)$$

Typically, a system is said to be correctly tight if its equilibrium pressure is lower than 10% of its working pressure. Stainless steel vessel can achieve $Q_L < 10^{-9} \text{Pa m}^3/\text{s}$. From Equation B.16, one may estimate the equilibrium pressure on the shock tube section, knowing $S = 100 \text{ m}^3/\text{s}$, one gets,

$$p_{eq} = \frac{10^{-6}}{100} \approx 10^{-9} \text{ Pa} = 10^{-7} \text{ mbar} \quad , \quad (B.17)$$

which is below the desired vacuum level of 10^{-6} mbar [301], thus should not be a problem providing all the leakage sources are under control.

Permeation and seals

Since this process is not a function of time, it results in a sustained increase in the desired ultimate pressure. The permeation gas flow is proportional to the pressure gradient across the wall thickness and a material-dependent permeation constant [488]. The permeation gas flow is proportional to the pressure gradient over the wall thickness d_{wall} and a material-dependent constant k_{perm} ,

$$Q_{perm} = k_{perm} \cdot A \frac{p_{atm}}{d_{wall}} \quad . \quad (B.18)$$

Permeation starts to be significant for pressures below 10^{-6} Pa .

The high temperatures and cleanliness of ESTHER exclude elastomeric seals and mandate metallic seals for the shock tube section². Helicoflex seals are used in all shock tube joints, excluding frequently opened joints, while con-flat (CF) flanges are used on the pump connections [301].

Since seals are all metallic, their outgassing characteristics are identical to the vessel walls. The leakage rate from seals is therefore included in the outgassing term of the pressure equilibrium Equation B.19,

$$Q_{des}(t) + Q_{diff}(t) + Q_{perm} + Q_{leak}(t) + Q_{back-stream}(t) = p(t) \cdot S_{eff} \quad . \quad (B.19)$$

²FKM seals are still retained for the various plugs for cost-effectiveness, and also owing to their smaller diameters

Since Q_{perm} and Q_{diff} are much smaller than Q_{leak} and Q_{des} , and $Q_{back-stream} = 0$ because ESTHER pumps are dry, we can simplify Equation B.19 to better fit our system,

$$Q_{des}(t) + Q_{leak}(t) = p(t) \cdot S_{eff} \quad . \quad (B.20)$$

B.2 Vacuum technology

B.2.1 Materials

The requirements for materials selection in a vacuum vessel and its accessory systems will depend on its application, ambient condition and vacuum level. It is of the most importance that the chosen materials allow for a preset vacuum level to be attainable. The materials list includes not only the vacuum vessel but the gasket and seals, as well as any lubricants or greases. The desired vacuum level imposes the greatest material constraints, namely for ultra high-vacuum conditions the choice of materials is very limited [488, 492]. Typically the list of relevant requirements is:

- Mechanical strength: The vessel must be able to withstand the force of the atmospheric air while guaranteeing its functionality.
- High gas tightness: The material must not be permeable to gas in the relevant working conditions, namely ultimate pressure and temperature range. Typical FKM (fluoroelastomer) have a permeation rate for atmospheric air of $4 \times 10^{-7} \text{ Pa m}^3 \text{ s}^{-1}$, precluding its use below $1 \times 10^{-8} \text{ mbar}$.
- Low intrinsic vapour pressure, high melting and boiling point: Oils and grease with high vapour pressure will limit the vessel's ultimate pressure, as it cannot be pumped out. Additionally, the intrinsic vapour pressure of metals and alloys materials must be considered.
- Clean surfaces, low content of foreign gases, easy degassing: Clean surfaces are a prerequisite, however, any surface exposed to the ambient air has an adsorbed layer. This must be cleaned and materials with a low desorption rate must be used to achieve a high vacuum level.
- Good thermal shock resistance, adjusted dilatation behaviour: High-temperature variations can cause degradation of the seals, and dilatation limits must match otherwise connections will fail.
- Corrosion resistance, chemical resistance: Thin-walled components are particularly susceptible to corrosion by chemically active gases inside the vessel. These can cause damage to both the vessel in the seals.
- Radiation resistance: Radiation exposure can lead to sealant failure, e.g. elastomers hardening, or vessel damage, e.g. viewport glasses darkening.

Metals and metallic alloys are, in general, good materials to use in vacuum vessels. They have high stress, are easy to clean and manufacture, are temperature resistant and more resilient to damage than glass. The most common metallic alloys are those of stainless steel and aluminium.

Mild steel: is usable down to 10^{-3} mbar of pressure, however is liable to rust. It is typically cheaper than stainless steel.

Stainless steel: is the preferred material for the construction of chambers or components in vacuum technology. It is sufficiently strong for flange connections and can be welded if desired. Its surface is sufficiently well-passivated and can withstand corrosion. Welding can cause some distortions. Reference [488] has a detailed analysis and common use for common stainless steel alloys.

Aluminium: is mostly used as an alloy in low and high vacuum. It can be used for metallic seals in the form of profile seals or wires. Its large thermal expansion and conductivity make it difficult to weld, risking the formation of pores and cracks. Aluminium flange connections are not the best suited for UHV as their hardness is often too low. Nonetheless, it is lightweight and easy to machine while maintaining high corrosion resistance.

Nickel: excellent corrosion resistance and strength at high temperatures. Difficult to machine and not easily available making it expensive.

Copper: oxygen-free high conductivity (OFHC) has good corrosion resistance and is excellent for vacuum systems, namely seals. Can be expensive in large quantities.

Brass: can be suited for specific applications, due to its good corrosion resistance. Brass has a high zinc portion that starts to evaporate at temperatures above 100°C .

Titanium: light and very clean material, usually used for its gettering properties like in ion pumps cathodes, and getter pump filaments.

Plastics: typically desorb large quantities of gas and have higher permeability rates than metals [493]. In general, these are kept to a minimum, nonetheless, some are well-suited for seals and will be discussed in detail in the next section.

Glass and ceramic materials are also well-suited for vacuum vessel components. Ceramics can withstand higher temperatures (up to 1500°C) than most metals, but are brittle in comparison. Some can also be brazed to suitable metals through a molybdenum-manganese fire coating [493]. Glass, mostly commonly borosilicate, can be used to manufacture the vacuum vessel, has high corrosion resistance, yet its permeability depends on the temperature. Optical glasses can be used to fabricate viewing windows into the vessel, some like VUV-graded sapphire, even for ultraviolet light. Figure B.2 depicts typical outgassing rates for different materials.

B.2.2 Connections, gaskets, and seals

Different sections of the complete vacuum system must be connected through either non-removable or detachable connections. The former is achieved by welding, brazing or fusing, metalizing or sintering, with subsequent brazing. Recently, vacuum-resistant adhesives have surged in use but cannot be used in systems that require a UHV level. Since ESTHER has next to no non-detachable connections these will not be discussed in further detail, and one may refer to [488] instead.

Detachable connections required the use of a gasket or seal to be vacuum tight. Some connections and seals, like CF, mandate that the seal be replaced each time the connection is open, whereas others like an O-ring can be re-used. The most used connections are:

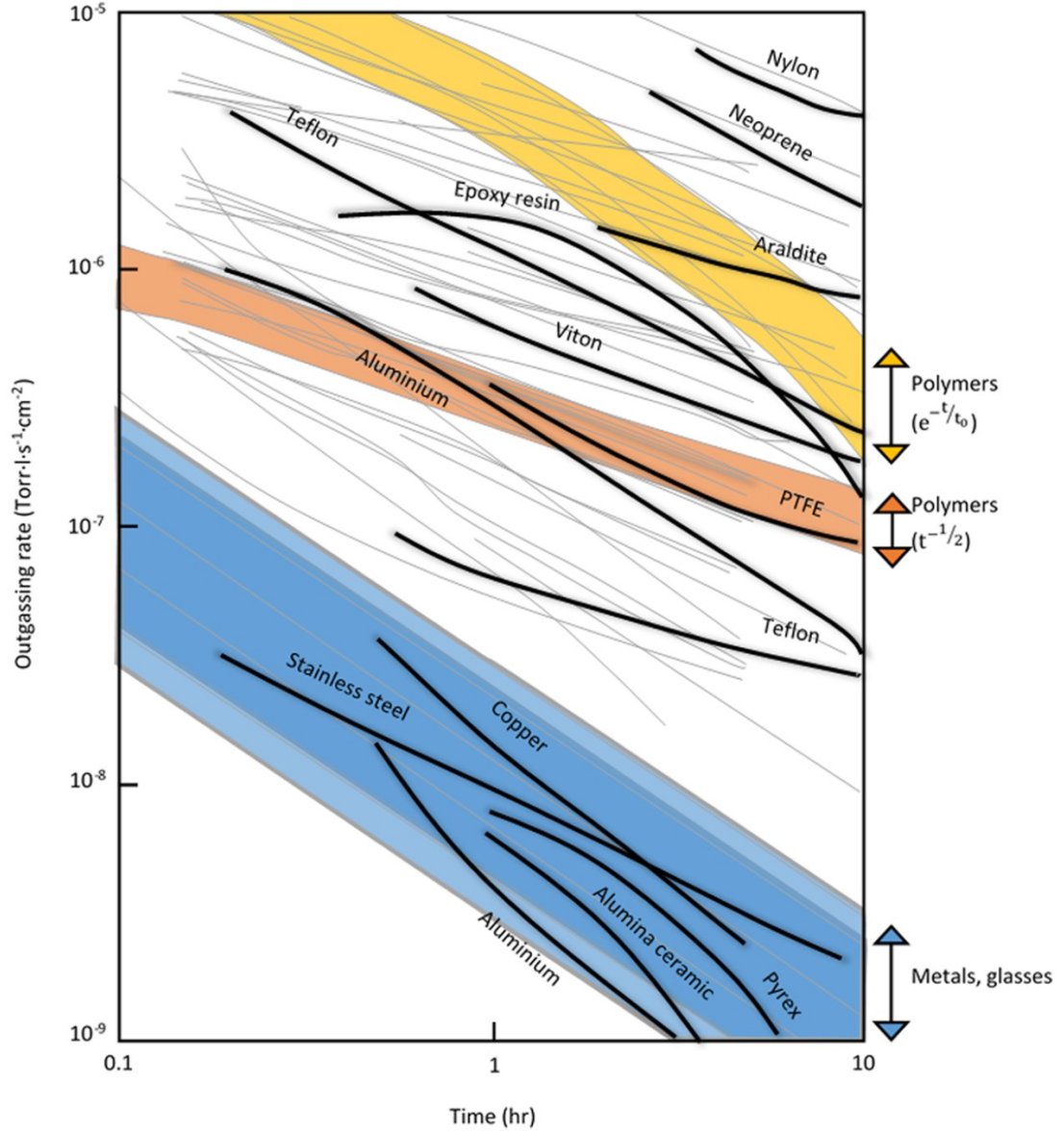


Figure B.2: Surface area-related gas emissions (outgassing flow rates) j , adapted from [494]. Typical outgassing rates: metals $jQ_{des} \propto t_0/t$; Plastics $Q_{des} \propto \sqrt{t_0/t}$ or $Q_{des} \propto \exp(-t/t_0)$.

- O-ring and groove;
- ISO-Kleinflansch (KF) and similar;
- ConFlat (CF);

O-ring and groove O-rings are the most frequently used seals. Typically made of elastomer material, other options also exist. The O-ring will deform and adapt to the unevenness of the mating surfaces. When compressed, the O-ring must not be “bitten” by the surface’s edges, otherwise, a leak would occur [493]. The degree of deformation should be big enough to ensure good contact between the O-ring and sealing surfaces. Usually, the O-ring is placed in a groove of a flange and pressed against a flat flange. The groove and O-ring dimensions are carefully selected to guarantee adequate sealing. Figure B.3 shows some

O-ring and groove parings geometries, and Table B.1 outlines the common properties of usual elastomer O-rings. Besides elastomer O-rings, metallic spring O-rings, called helicoflex, also exist [492]. These have low outgassing rates, high temperature and corrosion resistance and provide very high vacuum tightness. However, they cannot be reused as many times as an elastomer, and their price can be prohibitive to use in many applications or connections.

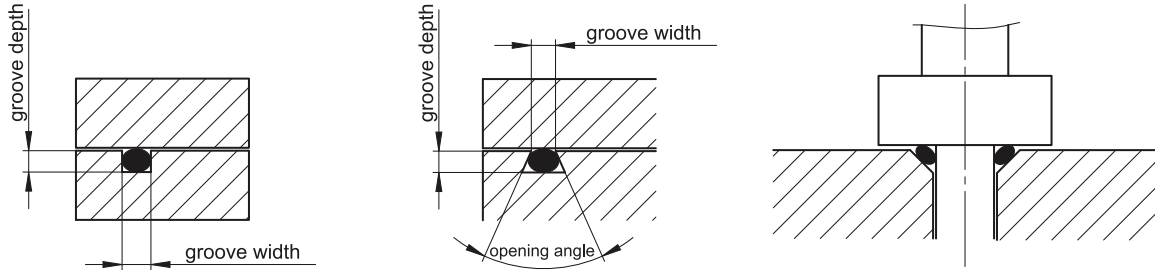


Figure B.3: Different O-ring grooves, adapted from [488].

Table B.1: Elastomer material properties of typical O-rings, adapted from [488],

Brief name	Base elastomer	Temperature range (°C)	Properties
FKM	Fluoroelastomer	-15 to 200	•Used for most vacuum applications; •Low gas permeability; •Excellent resistance to temperature and aging; •Chemical resistant to common chemicals; •Operating pressure down to 10^{-8} mbar.
NBR	Nitrile butadiene	-25 to 120	•Good mechanical properties; •High abrasion resistance; •Resistant to oils and lubricant; •Good gas tightness; •Operating pressure down to 10^{-7} mbar.
CR	Chloroprene rubber	-5 to 120	Similar properties to NBR
EPDM	Ethylene propylenediene-monomer rubber	-50 to 130	•Resistant to hot water and steam; •Suited for cold and oxidizing environments; •Non-resistance to o aliphatic and aromatic, hydrocarbons nor mineral oils; •Operating pressure down to 10^{-7} mbar.
VMQ	Silicone rubber	-50 to 200	•Excellent temperature resistance, but not with hot water or steam •Resistance to cold; •Relatively high gas permeability •Operating pressure down to 10^{-6} mbar.

In some situations, the use of greases or oils might be required. This is most common whenever the system has a moving part, e.g. a power shaft. The O-ring can be coated with a thin film of a low vapour pressure grease (silicon grease, mineral oil-based or perfluoropolyether-based grease), depending on the application [488]. The vacuum grease smooths out microscopic irregularities on the O-ring contact surfaces improving the vacuum seal. Particular caution is to the grease vapour pressure, an adequate grease must have a vapour pressure well below the working pressure, otherwise, it will outgas to the working system. Besides it, greases have hydrocarbons which may not be suitable for the application in

question, namely its cleanliness [492].

ISO Kleinflansch (KF) and similar ISO-KF small flange components are suitable for pressures down to 10^{-8} mbar and overpressure of 1.50 bar when using an elastomer gasket. It consists of two symmetrical flanges and one O-ring seal, which is positioned and supported with an internal or external centering ring. The sealing force is exerted by a clamping ring placed over the conical tightening area and is tightened with a wing screw. These types of flanges are suited for fast, efficient assembly and disassembly without requiring any tools [488]. KF flanges can be mounted in a base plate by using a claw or bulkhead clamp and bolts. Metal gaskets can be used instead of elastomers, a different centering ring is required, however, lower pressures of 10^{-9} mbar can be achieved. Figure B.4 shows two mounting of KF flanges, one with a clamping ring, and another with a bulkhead clamp. ISO-KF flange dimensions are between 10 and 50 mm.

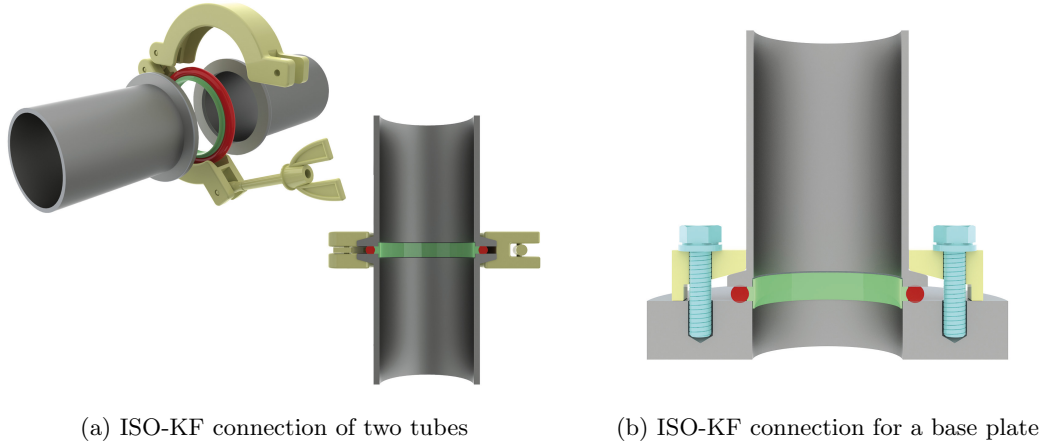


Figure B.4: ISO-KF connection illustration, adapted from [488].

The ISO-K and ISO-F flanges also have a centre ring and sealant, similar to the ISO-KF. The major differences are related to the maximum diameter and the clamping method. ISO-K flange range from 10 to 630 mm, whereas ISO-F goes up to 1000 mm [488]. A circumferential outer ridge on the back of the ISO-K flanges prevents slippage of the clamp screws. It can also be mounted on a base plate with either a centering ring or an O-ring. The ISO-F flange uses fixed bolt holes instead of clamps to secure and tighten the gasket in place. It can also be mounted on a base plate, or even with a ISO-K flange using a bolt ring. Figure B.5 shows two pipe connections, one with ISO-K and another with ISO-F flanges. Similarly to ISO-KF, if a metallic seal is used the ultimate pressure can drop to 10^{-9} mbar.

CF flanges CF flanges are the most commonly used for UHV applications [493]. They were designed for UHV applications, are bakeable up to 450°C and are suitable for pressures below 10^{-12} mbar. These are typically made of low-carbon stainless steel and use an OFHC copper ring as the seal. CF connections consist of two symmetric flanges with two knife edges in shallow grooves, where the copper ring sits, and the necessary bolt holes [488]. When the bolts are tightened, the copper ring is plastically deformed both axially and radially, sealing the vacuum connection. Applications with lower vacuum levels can use FKM rings instead of copper, which are reusable. A small radial recess on the flange is used for leak detection,

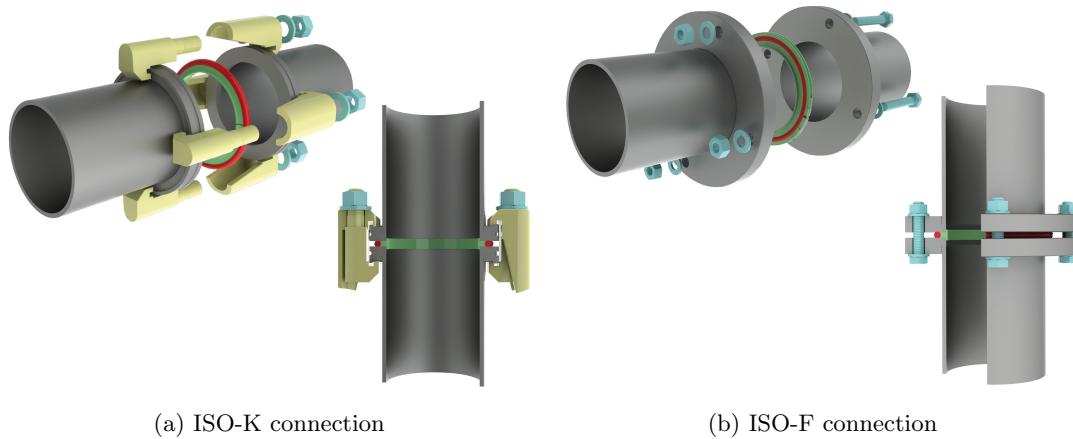


Figure B.5: ISO-K and ISO-F connection illustrations, adapted from [488].

as the helium can be sprayed directly onto the sealing surface. CF flanges can have fixed flanges or a rotating collar flange to align the flanges at will. The flanges' internal diameter sizes range from 10 to 400 mm. Figure B.6 depict an illustration of CF flanges with a copper ring as a gasket. Other flanges formats exist, such as COF flanges using a welded wire ring, however, they are not as commonly used as the ones previously described [488].

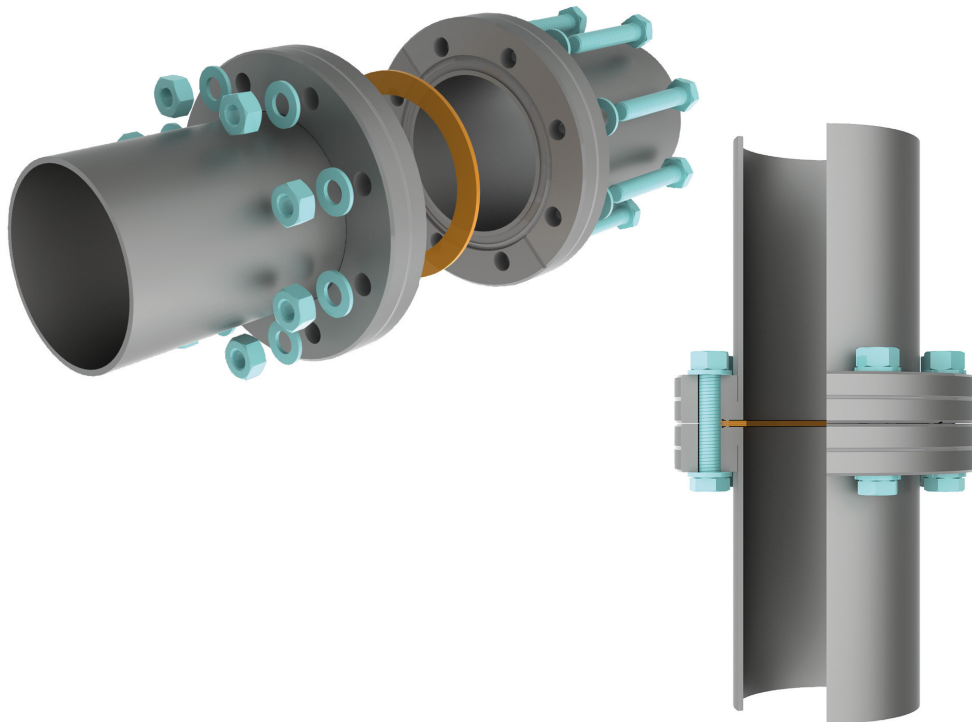


Figure B.6: CF flange connection, adapted from [488].

Hoses and bellows In some vacuum systems, it is necessary to have flexible connections. The simplest form is a plastic hose, however, plastic outgasses in the vacuum and is not suitable for high vacuum levels [488]. For pressures below 10^{-4} mbar metal hoses are needed. These are cold-formed from thin-walled

tubes and welded to the flange connections to seal them. Under vacuum, corrugated hoses shrink axial and exert an axial force onto the flanges. Large nominal diameter hoses create large forces that must be accounted for when designing the system.

When one needs to consider axial compression and vibration, vacuum bellows are normally a better solution than hoses. Two forms of metallic bellows are used in vacuum systems: convoluted (or spring), and edge welded (or membrane) bellows [488, 492]. Convoluted bellows are made through hydraulic forming, being stiff and having a high spring rate, making them hard to bent. Edge-welded bellows are made from individual metal discs welded together. By having a lower spring rate they allow some displacement and can act as vibrations insulators. Figure B.7 depicts the section for the two forms of bellows.

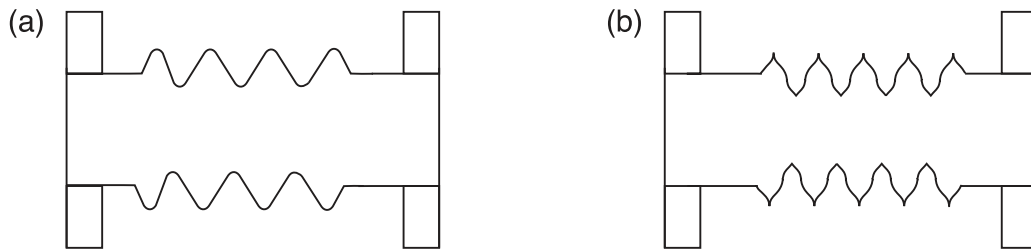


Figure B.7: Vacuum bellows schematic, convoluted/spring (a), and edge welded/membrane (b), adapted from [492].

B.2.3 Vacuum Pumps

A vacuum pump is designed to remove gas from a vessel to lower its pressure. Different applications require different vacuum levels, and a single pump is not enough to cover the range from 10^3 to 10^{-10} mbar. It is therefore useful to separate vacuum pumps into different categories by their gas extraction method [492].

Positive (or Gas) displacement pumps move the gas through mechanical movements of driven parts and synchronized valve actions, from the inlet to the outlet. Small amounts of gas are extracted with each cycle but at a high repetition rate. Rotary vane pumps are one example.

Momentum transfer (or Kinetic) pumps interact with the gas through a high-velocity fluid stream or fast-moving solid surface which continuously re-direct the gas molecules to the outlet. Turbo-molecular pumps operate in this category.

Capture (or gas biding) pumps remove the gas by trapping the molecules through condensation or adsorption processes. Cryogenic pumps are one example of this type of pump. In contrast to displacement and transfer pumps, which compress and exhaust gas in the gaseous phase, capture pumps do not have an outlet as the gas is stored inside the pump in a condensed state.

Positive displacement pumps typically operate from atmospheric pressure to 10^{-3} mbar, where gas has a fluid state. Most vacuum applications need higher vacuum levels than a positive displacement pump is capable of. Therefore, the primary pump, which exhausts the gas into the atmosphere, works in conjunction with a secondary pump to achieve the required vacuum level. The primary will pump from atmospheric pressure to 10^{-1} mbar, equalling 99.99% of the total air, after which the secondary pump

starts to operate. Secondary pumps cannot exhaust to atmospheric pressure and so, they must operate with a primary that ultimately compresses and exhausts the gas to the atmosphere. Normally the pumps are connected in such a way that the primary pump works in conjunction with the secondary pump. Where the secondary pump outlet port is connected to the inlet of the primary pump. Figure B.8 shows a diagram with different types of vacuum pumps.

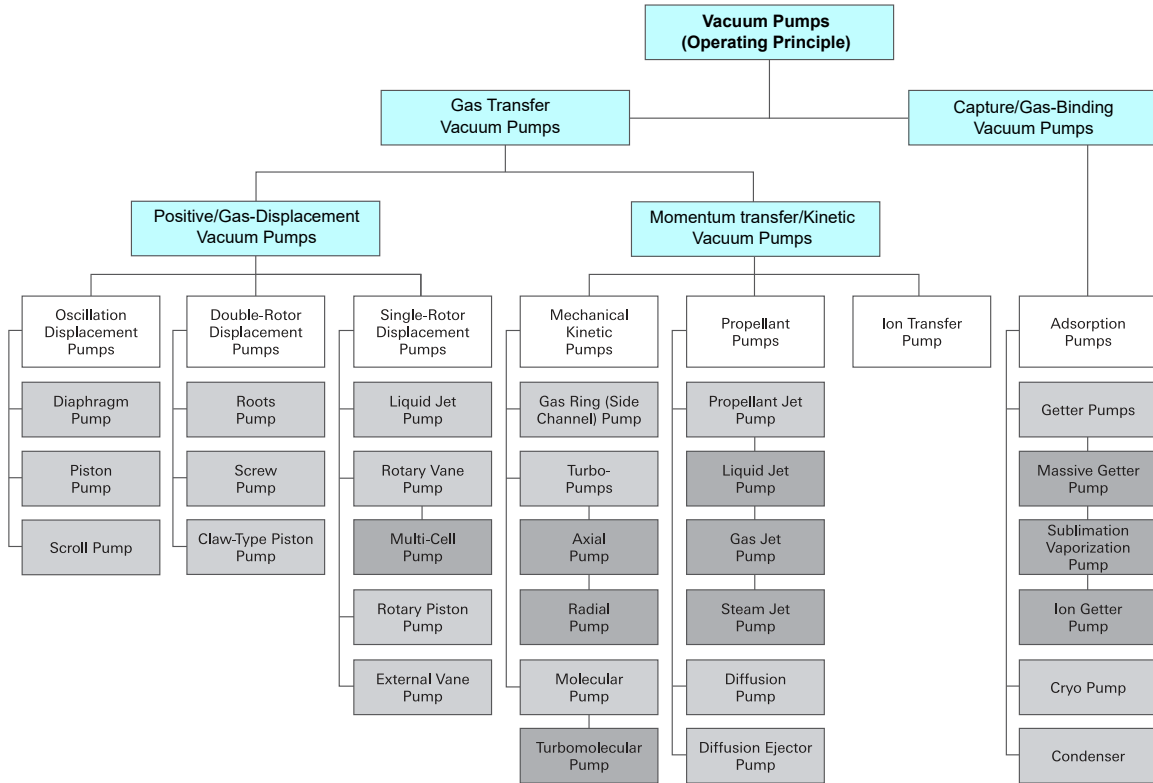


Figure B.8: Vacuum pumps operation categories diagram, adapted from [488].

Dry screw pumps Screw vacuum pumps were developed to have a single-stage pump capable of operating from atmospheric to 1 Pa pressures [495]. Figure B.9 illustrates a screw pump. It comprises two parallel intermeshing screw rotors (2 and 9) with small spatial clearances between them and the pump housing. Shaft-lip seals (11) isolate the pump housing from the supporting and centering bearing on both ends of the shafts. The shaft-lip seals isolate the lubricated bearing and timing gear-wheel oils from the pumping interior. With this arrangement, we get an oil-free system.

The two screws rotate in opposite directions, one directly via the motor (5) and the second via the timing gear wheels (7). The gas collected at the inlet port (1) is trapped by the screw rotation between the screw pitch valleys and is cooled when pushed against the water-cooled wall (3 and 10). It is transported axially towards the discharge port (8) and expelled into the atmosphere. Rectangular thread or variable pitch profiles can also be used in rotor screws. Reducing the screw pitch will compress the gas in each pitch increasing the overall pump efficiency. Water-cooled walls will help dissipate the compression heat [495]. The pumping speed is proportional to the geometrical size of the working volume at the gas inlet and the screw rotors' revolutions. The intermeshing geometrical shape and pitch-screw thread of the rotors will also affect the pumping speed. The pump's ultimate pressure is affected by all these

parameters and the pump's geometrical configuration. Screw pumps typically operate between 3400 and 4520 RPM, however, some can run at 10000 RPM [492, 495]. Typical dry screw pumps have ultimate pressures of 10^{-4} mbar.

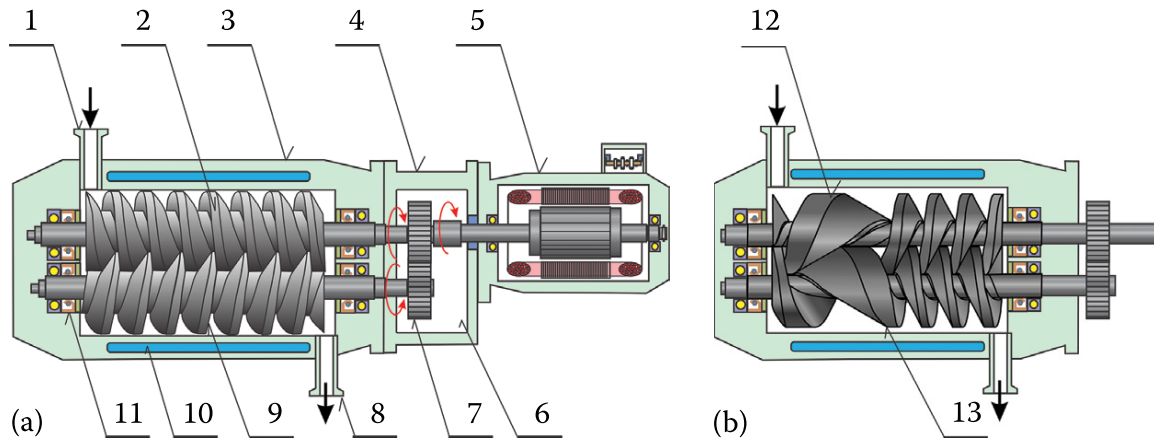


Figure B.9: Screw vacuum pump illustration, adapted from [495]. (a) concept of a screw pump: 1-inlet port; 2-screw rotor directly driven by a motor; 3-cooled pump housing; 4-gear box with driving timing gears and oil lubricant; 5-motor; 6-oil; 7-timing gear; 8-outlet port; 9-screw rotor driven via timing gear 7; 10-water cooling channel; 11-bearing. (b) screw rotors with variable thread pitch: 12-right-turn thread screw; 13-rotor with left-turn thread screw.

Turbomolecular pumps The turbomolecular pump design is similar to that of a turbine. A multi-stage, turbine-like rotor with bladed disks rotates inside a housing. The blade tips rotate at velocities of the order of the molecular thermal velocity [492, 495]. Turbomolecular pumps hence operate in the molecular free regime. Each rotor blade is paired with a similar set of stationary stator blades that project inwards from the wall of the cylindrical pump housing. The ultimate pressure of a turbomolecular pump is around 10^{-8} – 10^{-9} mbar.

A turbomolecular pump working principle is that the slanting blades by intercept more molecules (when moving at high speed) compared to than if they were stationary. When a rotor blade “hits” a molecule, the molecule is pushed, moving along the pump axis. This effect is duplicated by the stator blades which are oriented in the opposite direction [492]. A diagram of this working principle is shown in Figure B.10.

Turbomolecular pumps have been designed with both horizontal and vertical constructions. Both have the same working principle, however, horizontal pumps have the inlet perpendicular to the rotor shaft. Vertical pump construction allows direct entering of the gas into the turbomolecular unit. Since this construction allows for a higher pumping speed per unit mass, it is the most common configuration [495]. Figure B.11 shows an illustration of a vertical turbomolecular vacuum pump. It depicts the rotor disks (3) and annular stator rings (8), the bearings (2 and 5), the gas inlet (1) and outlet ports (4), as well as the venting port (7). The rotor blades are driven by the motor (6) with typical rotational speeds of around 60000 RPM. A rotor blade tip velocity of a 10 cm radius blade is 628 m/s. Such velocities allow for pumping speeds of several hundred litres per second and compression ratios of 10^8 , depending on the gas.

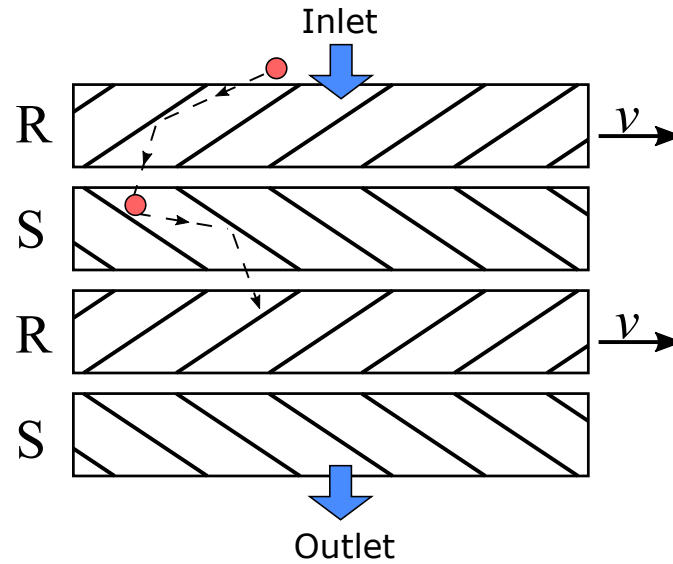


Figure B.10: Turbomolecular pump working principle diagram, adapted from [495]. Rotors R and stators S with blades are in sequential order. Rotors moving with velocity v pushes gas into stator channels from top to bottom direction.

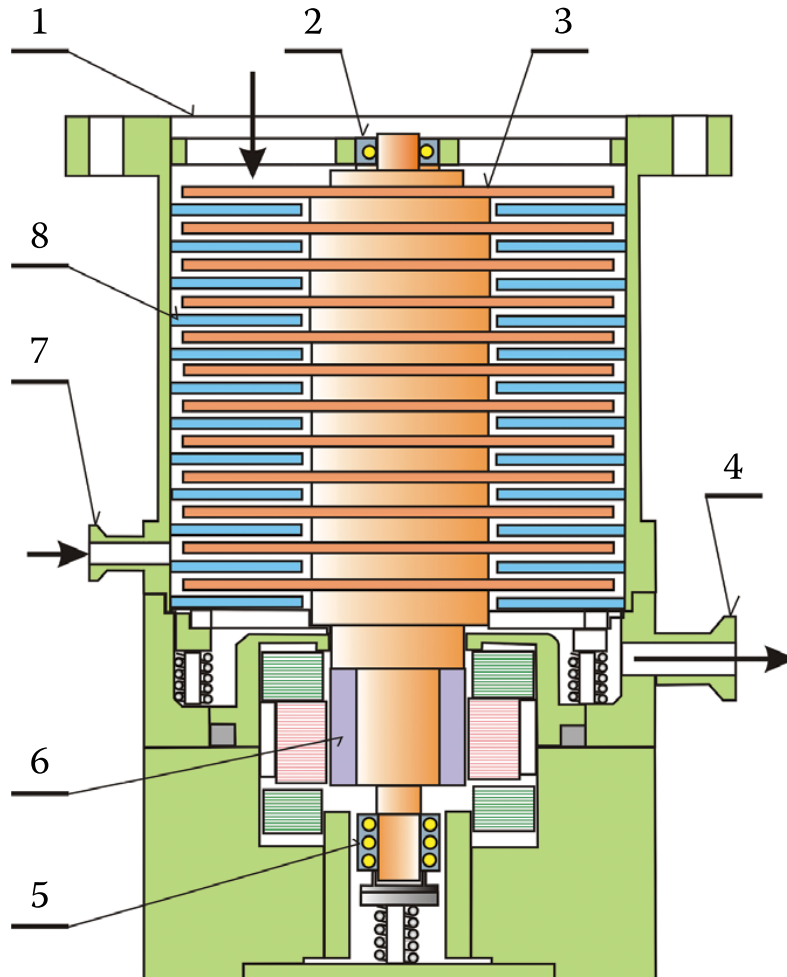


Figure B.11: Turbomolecular vacuum pump illustration, adapted from [495]. A turbomolecular pump with vertical construction: 1-inlet port; 2-top bearing; 3-rotor disks with blades; 4-outlet port; 5-bottom bearing; 6-motor; 7-venting port; 8-stator annular ring with blades.

Bearings are a critical components of a turbomolecular pump as they centre the rotor shaft, in relation to the stator, while it rotates at 30000 to 90000 RPM [495]. The rotational velocity is adapted to the pump size, with larger pumps having lower rotational velocities. The large pumps use mechanical ball bearings on both the rotor sides, whereas the smaller pumps use a mechanical bearing on the fore-vacuum side and magnetic bearing suspension on the high-vacuum side. Ceramic bearings enable higher revolutions by reducing friction and wear when compared to steel bearings. The higher revolutions cause heat that needs to be dissipated, either by water circulation on larger pumps, or forced gas flow through fans in smaller pumps. Lubricant fluids can be used to reduce wear on the mechanical bearing. Essential, synthetic lubricants with low viscosity and low vapour pressures are used. The bearing house is insulated from the pumping area thus providing an oil free, thus clean, environment.

Pumps bearings can be fully magnetic [493]. They use a combination of the fields of permanent magnets and electromagnets to levitate the rotor assembly and control its motion. An active control system is needed to continuously balance the rotor position and avoid clashing [488]. Magnetic levitation allows the system to rotate faster and be wear free since there is no physical contact between the parts. This also lowers the vibration produced by the pump and insulates the system from external vibrations. Pumps with magnetic levitation bearings are free from contamination concerns, and are hence ultraclean, although relatively expensive [492]. During their venting procedure, extra care must be exerted not to switch off the levitating facility until the rotation has completely stopped. In the event of a power failure, the rotational energy of the pump provides energy to the magnetic bearings. This allows for several minutes to re-enable the power in the system. An integrated safety bearing halts the pump rotation by slowly braking it, in the case of either a longer power failure or system malfunction, preventing damage to the pump [488].

Since our application only uses a dry screw and turbomolecular vacuum pumps, we will not further discuss other types of pumps. For more information, the reader can refer to [487, 488, 492, 493, 496].

Appendix C

Interferometer Operation notes

C.1 Functional requirements and challenges

For an effective use of the designed microwave diagnostic some requisites and non-trivial know-how on the operation of the equipment is needed. We are going to focus on interferometer mode as it is the simplest one, and was already tested. As previously stated an interferometer has a few functional requirements, these can be summarized as:

- One optical window
- Fast acquisition system, such as an oscilloscope
- “À priori” estimation of the n_e/f_{pe} range

Despite the possibility for working with a single optical window, the system works best in a face-to-face double window setup with one antenna on each side. By minimizing the number of refractions and reflections of the emitted wave, one can maximize the signal-to-noise ratio of the diagnostic. Furthermore, if one uses the tube wall as the reflection surface some attention to its curvature must be exerted, as some of the signal will be deviated, further reducing the signal-to-noise ratio. The selected probing frequencies should be well adjusted to the expected plasma profile, to have a large enough phase shift, while still ensuring unwanted reflection does not occur mid-way into the plasma. The aforementioned concerns are summarized as follows:

- Large enough optical window to fit the antennas
- Face-to-face windows work better than reflecting at the tube wall
- High-speed shocks mandate very high n_e/f_{pe} , that may need a different frequency range (THz)
- Probing frequencies need to be well suited
 - Too low may cause unwanted reflection
 - Too high will have a small phase deviation

Figure C.1 shows an illustration of the challenges and requirements for operating the compact microwave inteferometer in its current form.

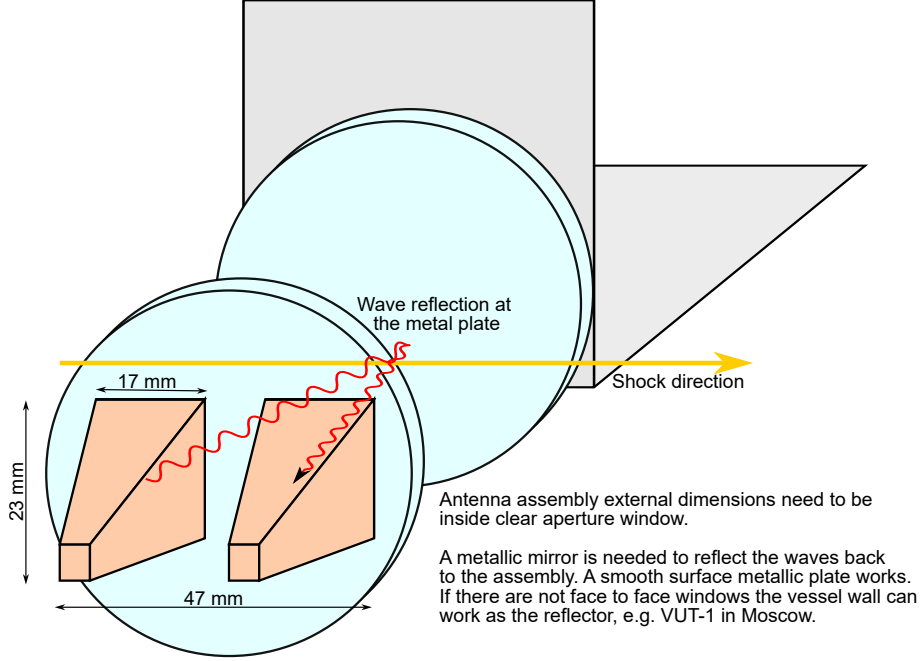


Figure C.1: Interferometer functional requirements

C.2 Working principle

The working principle of a microwave interferometer is based on the fact that an EM wave moves at a speed lower than c_{air} in a plasma, and thus gains a phase delay to the reference value in non-ionized air. The interferometer computes the amplitude corresponding to the beating of the received signal and a sample from the emission antenna. It has the form of $Y = A \sin \phi$. This value must then be converted to an angular phase, from which the shift can be computed. Figure C.2 shows an illustration of the interferometer working principle and of its signal output. The equation to be solved is,

$$\Delta\phi = \arcsin Y_{on} - \arcsin Y_{off} \quad .$$

The correct relation between phase shift and electron density of the plasma will differ from case to case, namely if a magnetic field exists in the system. The presence of a magnetic field cause the propagation mode to change from the simpler O-mode to the more complicated X-mode. Besides this, the plasma geometry must also be considered. These considerations may hinder some approximations, such as the one used in Chapter 8, no longer valid. A careful assessment of the expected plasma properties, and thus of the corresponding dispersion relation needs to be done to then compute the electron density. The reader can refer to [265, 497] to find more information on this topic.

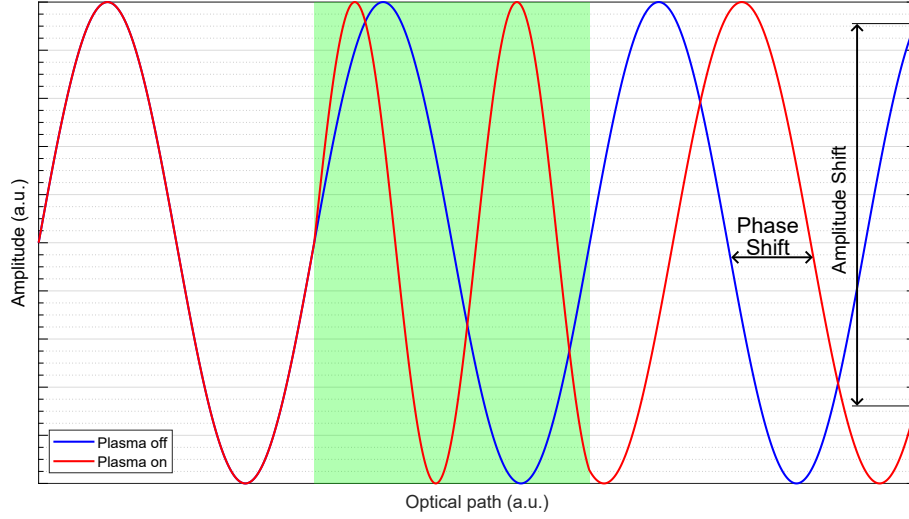


Figure C.2: Interferometer working principle diagram, green box corresponding to the plasma region.

C.3 Calibration

The full angular amplitude of the interferometer signal is required to correctly calibrate it. The system is thus ran in sweeping frequency mode, where a multitude of probing frequencies are sent in succession. In this way, the output signal, which corresponds to the beating of the emitted and received wave, will cover a multitude of points i each with the form $Y = A \sin \phi_i$, with ϕ_i the corresponding angular phase. The larger the number of points, the closer is to a continuum thus, covering the full spectra $-\pi/2 \leq \phi \leq \pi/2$. This behaviour is illustrated in figure C.3. An example of a calibration ran is depicted in figure C.4.

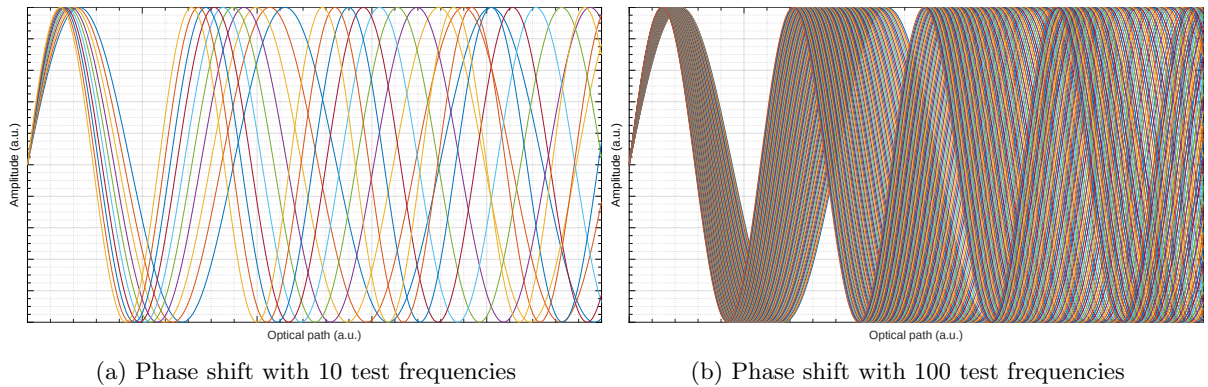


Figure C.3: Sinusoidal wave amplitude with sweeping frequency illustration.

Having the calibration run signal, one can then solve the following system of equations, to fit it to a normalized sinusoidal function:

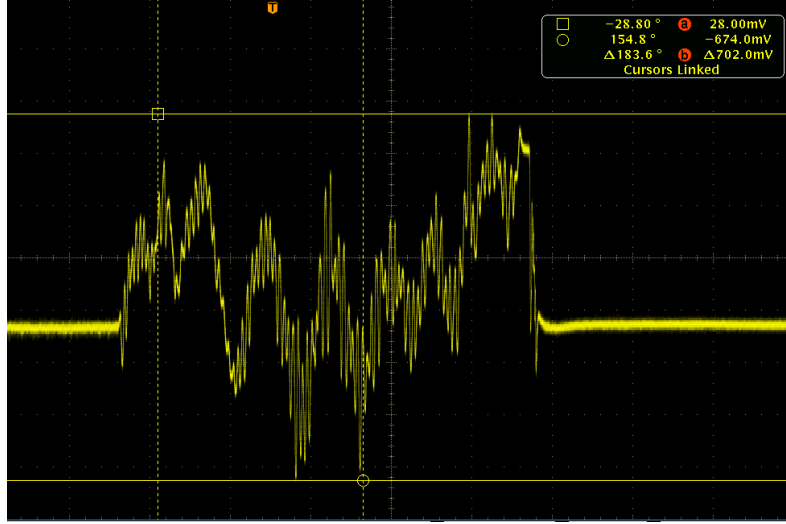


Figure C.4: Interferometer calibration signal.

$$Y = A \times V + B$$

$$A = \frac{2}{V_{max} - V_{min}}$$

$$B = -\frac{A}{2}(V_{max} + V_{min})$$

Here, V_{max} and V_{min} are the calibration signal maximum and minimum value. Having a normalized signal, the angular phase can then be extracted by a simple arcsin function. Another thing to take into account is that the signal may be shifted over 2π radians. In this case, the interferometer output signals of a phase shift ϕ and $2\pi \times N + \phi$ will be identical.

Appendix D

Miscellaneous

D.1 Manufacturing and design

Table D.1 compares different high-tensile strength steels which were pre-selected for the manufacturing of ESTHER. With exception of 316 LN and XM-19, all entries are duplex and super-duplex stainless steel alloys. The options are centred on duplex stainless steel alloys, a two phase type of steel composed of an austenitic and a ferritic phase, due to their stronger corrosion resistance and higher strength than the more common austenitic stainless steels. As discussed in Chapter 4, ESTHER is mostly composed of F51 and F55 stainless steel. The parameters analysis are the chemical composition by weight, tensile strength (R_m), yielding point ($R_{p0.2}$), uniform elongation and Pitting resistance equivalent number (PREN). Both R_m and $R_{p0.2}$ measure the mechanical strength of the alloy, with the elongation measuring its ductility. Lastly, PREN is a predictive measurement of a stainless steel's resistance to localized pitting corrosion based on its chemical composition, where higher number indicate a higher tolerance to pitting corrosion. It is calculated by $PREN = (\%Cr + 3.3 \times (\%Mo + 0.5 \times \%W) + 16 \times \%N)$.

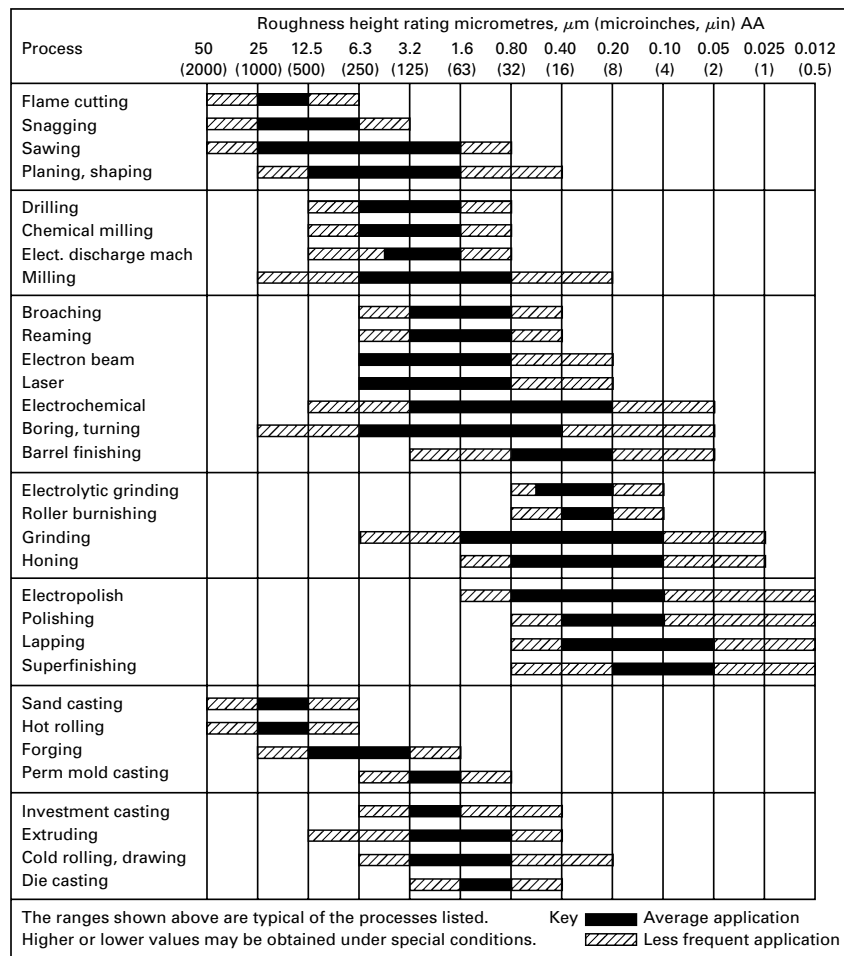
The surface finishing state of a manufactured part would depend on its manufacturing method. Different methods can be used in conjunction to first manufacture the piece in a rough state, and after that finish it. By doing it so, this may allow for the a cheaper/simpler process to undertake the majority of the work, and then finish the surface state of the piece with the more expensive/complex method. However, some manufacturing methods can change the material surface (or even bulk) properties and may be prohibitive for certain applications. Figure D.1 shows a compilation of the typical roughness heights of different manufacturing methods.

D.2 Cleaning procedure

The compression and shock tube sections of ESTHER require a thorough cleaning of its surface to achieve a low contamination level in the results. Cleaning the walls remove adsorbed materials which affect the contamination in two ways. The first is that these materials can outgas when vacuum pumps are turned on, which causes the vacuum level to not be as high as desired, and the second, is that they

Table D.1: Properties of stainless steel alloys.

Alloy name EU norm name		F51 1.4462	F53 1.441	F55 1.4501	U52N+ 1.4507	U47N+ 1.4410	XM-19 1.3964	316 LN 1.4406
Composition (% weight)	C	0.03	0.03	0.03	0.03	0.03	0.05	0.03
	Mn	2.0	1.2	1.0	2.0	1.2	4-6	2
	P	0.03	0.035	0.03	0.035	0.025	0.04	0.045
	S	0.02	0.02	0.01	0.015	0.015	0.03	0.03
	Si	1.0	0.8	1.0	0.7	0.8	0.75	1
	Ni	4.5-6.5	6.0-8.0	6.0-8.0	6.5	7	11.5-13.5	11-14
	Cr	21-23	24-26	24-26	25	25	20.5-23.5	16-18
	Mo	2.5-3.5	3.0-5.0	3.0-4.0	3.5	4	1.5-3.0	2-3
	N	0.08-0.20	0.24-0.32	0.20-0.30	0.25	0.3	0.2-0.4	
	Cu			1.0	>1.5			
	Co						0.1-0.3	
	V						0.1-0.3	
Resistance (MPa)	R_m	650	800	730	770	770	690	515
	$R_{p0.2}$	448	550	530	550	550	380	205
Elongation (%)		25	15	25	25	25	35	35
PREN		34.3	42.52	40.55	39.75	41.04	31.025	25.25



Extracted from General Motors Drafting Standards, June 1973 revision

Figure D.1: Outline of the surface finish obtained using manufacturing methods, adapted from [498].

D. MISCELLANEOUS

can radiate when excited by the shock wave and pollute the spectroscopy signal. Both of these problems are commonly caused by carbon-based molecules, such as mineral and human skin oils.

Cleaning of these two sections is done using a cloth soaked in a cleaning solution. Due to the tube geometry, a contraption was designed to effectively clean the inside of the tube. Two bottles are connected face-to-face with a rope (a second one is there for safety). The cleaning procedure follows these steps:

1. Tube sections are open and separated from one-another
2. The first bottle is put inside the tube and sent over the end using a compressed air hose
3. The second bottle's cap is un-tightened and removed
4. The cleaning cloth put over the bottle's neck and the cap re-tightened
5. A tight cloth is needed so it can scrub the tube walls
6. Using the rope, the bottle with the cloth is pulled towards the other
7. The cloth is removed and the bottle sent back
8. Repeat steps 3-7 until the cloth is no longer stained (it may require replacing the cloth pieces midway)

Figure D.2 shows a photograph of the cleaning setup equipment for the shock tube section, and figure D.3 shows a photograph of a cleaning demonstration on the compression tube.



Figure D.2: ESTHER surface cleaning equipment for the shock tube section, two bottles connected by a rope, cotton cloth and cleaning solution. A larger set of bottles is used in the compression tube.

Figures D.4 and D.5 show diagrams of the measured leakage rates for ESTHER's shock tube section using a helium leak detector. The first figure corresponds to the main body of the shock tube section, while the second to the two auxiliary arms and tees located on the side off the shock tube main body.

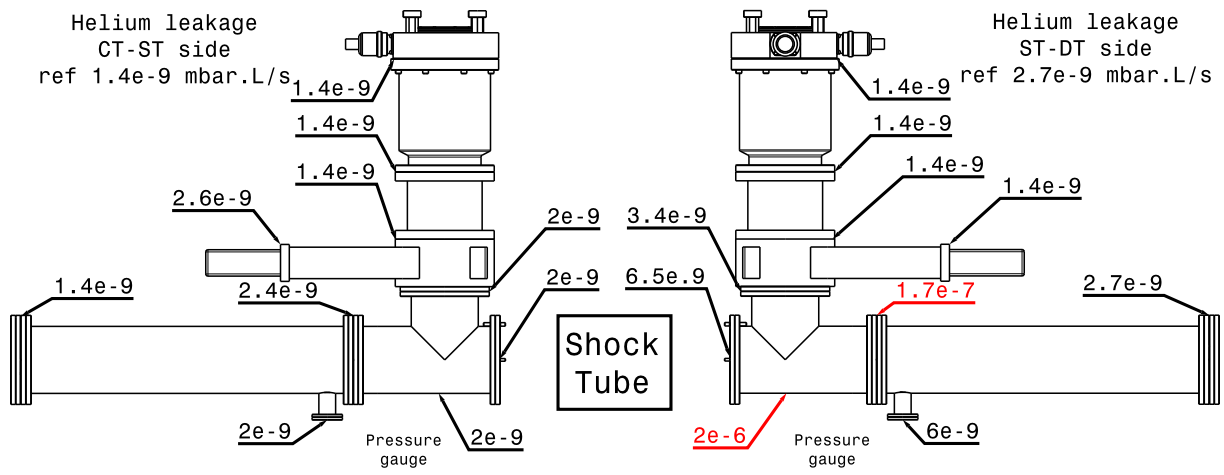


Figure D.5: Helium leakage rate test for ESTHER auxiliary arms and tees on shock tube section. Baseline value $1.4 \times 10^{-9} \text{ mbar l s}^{-1}$, $\dot{p} = 2.8 \times 10^{-4} \text{ mbar/s}$, $Q = 8.43 \times 10^{-3} \text{ mbar l s}^{-1}$.