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Title	Mitigation of Reentry Communication Blackout and Aerodynamic Heating through Gas Injection
Author(s)	宮下, 岳士
Degree Grantor	北海道大学
Degree Name	博士(工学)
Dissertation Number	甲第16840号
Issue Date	2026-03-25
DOI	https://doi.org/10.14943/doctoral.k16840
Doc URL	https://hdl.handle.net/2115/99685
Type	doctoral thesis
File Information	Takashi_Miyashita.pdf, 全文





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Mitigation of Reentry Communication Blackout and Aerodynamic Heating through Gas Injection

Doctoral Thesis

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Abstract

During atmospheric reentry, a spacecraft encounters extreme aerodynamic heating and the formation of a weakly ionized plasma sheath, which can interrupt radio-frequency (RF) communication with ground stations, a phenomenon known as communication blackout. These coupled thermal and communication hazards pose serious risks to mission safety and vehicle integrity. The present study investigates a unified mitigation strategy based on gas injection, in which a thin layer of electrically neutral gas forms near the vehicle surface. This gas film acts simultaneously as a thermal protection barrier against aerodynamic heating and as a plasma-free propagation corridor for RF waves.

To assess this strategy, the research combines experimental and numerical approaches across multiple scales. First, a multiphysics computational framework coupling nonequilibrium plasma flow and electromagnetic wave propagation was used to analyze the plasma-RF interaction. The simulations revealed that injected nitrogen gas can maintain a low-electron-number-density layer that enables wave transmission while reducing wall heat flux. Second, wind-tunnel experiments were conducted in two high-enthalpy facilities: the Institute of Space and Astronautical Science (ISAS) 1 MW arc-heated wind tunnel in Japan and the German Aerospace Center (DLR) L2K facility in Germany. These experiments confirmed the occurrence of blackout under plasma-flow and demonstrated that controlled gas injection restores RF signal strength and decreases surface temperature. Schlieren visualization and thermocouple measurements verified the presence of an insulating gas layer and its mitigating effect under steady plasma flow.

Finally, numerical extrapolation to real reentry conditions using the RAM C vehicle configuration showed that gas-film injection is effective at altitudes of 70 km and 80 km, where the electron number density typically exceeds the critical cutoff threshold for S- and C-band communication. Numerical simulations indicated that both nose and side injectors could reduce peak surface heat flux by up to 30 % and

open a partial RF transmission window in the 2.4 GHz communication band. These results provide the first integrated experimental and numerical evidence that a gas-film layer can concurrently mitigate aerodynamic heating and RF blackout during atmospheric entry.

The thesis thus establishes gas injection as a scalable approach for dual mitigation of plasma-induced hazards in hypersonic reentry. The combined framework of ground experiments and validated numerical modeling offers new design guidelines for future atmospheric entry spacecraft and next-generation communication systems operating in plasma environments.

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Chapter 1

Introduction

1.1 Background

Establishing robust, reliable atmospheric reentry technology is indispensable for the transport of supplies to space, crewed space exploration, and, ultimately, the realization of future space tourism. Atmospheric entry for exploration missions to other planets, such as Mars and Venus, likewise presents major challenges, and success in this area will advance planetary science and space engineering.

To return a spacecraft safely to Earth, advanced technologies are required in each phase of entry, descent, and landing (EDL). Throughout the EDL sequence, it is essential to continuously transmit information on the spacecraft's status and trajectory; doing so enables high-accuracy prediction of the touchdown or splashdown point and facilitates rapid location and recovery of the vehicle.

During EDL, telemetry/command links are maintained via direct-to-ground S-, C-, or X-band links or via relay networks (e.g., National Aeronautics and Space Administration (NASA) Tracking and Data Relay Satellite (TDRS)), while Global Navigation Satellite System (GNSS) (e.g., Global Positioning System (GPS)) supports onboard navigation. However, during the atmospheric reentry phase, the vehicle's velocity relative to the atmosphere is extremely high (typically greater than 7 km/s for Earth reentry), causing strong shock waves to form in front of the vehicle. Within these shock layers, adiabatic compression heats the gas to high temperatures, causing molecular dissociation and ionization; as a result, a plasma layer forms around the spacecraft. This high-temperature gas gives rise to two principal challenges during reentry: aerodynamic heating [1, 2, 3] and communication blackout [4, 5].

Aerodynamic heating imposes extremely high thermal loads on the spacecraft’s surface, necessitating the use of thermal protection systems (TPS) that incorporate heat-resistant materials and insulators. Simultaneously, the surrounding plasma layer can attenuate or entirely block RF signals, leading to communication blackout between the spacecraft and ground stations. The communication blackout poses significant risks to mission success and crew safety, as it impedes the transmission of critical telemetry data and commands during the most dynamic phase of the mission. This thesis focuses on mitigating communication blackout, with an emphasis on gas injection strategies and their coupled thermal protection benefits.

1.2 Thermal Protection Systems

To protect the spacecraft payload—both crew and cargo—from thermal damage, a TPS employing heat-resistant materials and insulation is required. The heat transfer mechanisms at the vehicle surface are a combination of convection and radiation, further complicated by chemical reactions and gas-dynamic effects, making the problem highly complex. These topics remain active areas of research, and the TPS must be tailored to the requirements of each space mission. Additionally, TPS design must be optimized to achieve the required thermal protection performance while avoiding excessive mass penalties and undue constraints on overall vehicle design.

A TPS incorporates various methods to reduce heat transfer to the vehicle, such as surface heat absorption, high-temperature structural materials, and reradiation from high-emissivity coating. Here, this thesis focuses on cooling methods employing mass transfer.

1.2.1 Ablation Cooling

Ablation cooling is a thermal protection method that utilizes phase changes such as melting and sublimation of surface materials [6, 7]. For non-reusable atmospheric entry spacecraft, such as Earth-orbiting return capsules and planetary atmospheric entry probes, ablation offers excellent performance, reliability, heat resistance, weight efficiency, and low cost. In the ablation method, the vehicle is coated with a resin called an ablator. When subjected to aerodynamic heating, the resin vaporizes and is ejected from the surface, blocking aerodynamic heating while also reducing internal temperature rise due to the resin’s poor thermal conductivity. The vehicle surface is eroded, and upon return to the ground, it becomes charred and cannot be reused.

1.2.2 Transpiration Cooling

Transpiration cooling is a method of achieving cooling effects by allowing a small amount of coolant gas to percolate through a porous surface [8]. The coolant gas is typically supplied from a high-pressure tank within the vehicle. The cooling gas forms a thin layer on the surface of the object, preventing direct contact between the high-temperature fluid and the object surface, thereby reducing heat transfer. This method is particularly effective in high-enthalpy flow environments, such as atmospheric reentry, where conventional cooling methods may be insufficient [9]. Transpiration cooling excels in reusability, but it faces challenges such as clogging of the porous material, limited off-design performance, and the complexity of the coolant gas ejection system.

1.2.3 Film Cooling

Film cooling is a technique similar to transpiration cooling that uses injected cooling gas to reduce heat transfer to the vehicle surface [10, 11, 12]. While transpiration cooling distributes the ejection of cooling gas over the entire surface, film cooling concentrates the ejection at several locations. To achieve cooling effects over a wider area, distributed film cooling, which employs multiple injection slots or holes, has also been proposed. Unlike transpiration cooling, which yields nearly uniform surface blowing, film cooling uses discrete jets that form a protective layer; effectiveness depends on injection momentum and coverage. Mass addition thickens the boundary layer and dilutes high-temperature freestream species, thereby reducing convective heat flux. When applied to spacecraft, it is important to optimize the shape and installation position of the gas injectors to minimize the total injected mass flow [13].

This thesis focuses on the gas-film layer generated by the film-cooling technique [14, 15, 16]. The thermal barrier effect created by the gas-film layer is expected to reduce aerodynamic heating during atmospheric reentry, and the electrically neutral gas film is also expected to mitigate communication blackout by providing a propagation path for electromagnetic waves. In the following sections, the fundamentals of communication blackout and prior mitigation strategies are reviewed to establish the context for the gas film approach.

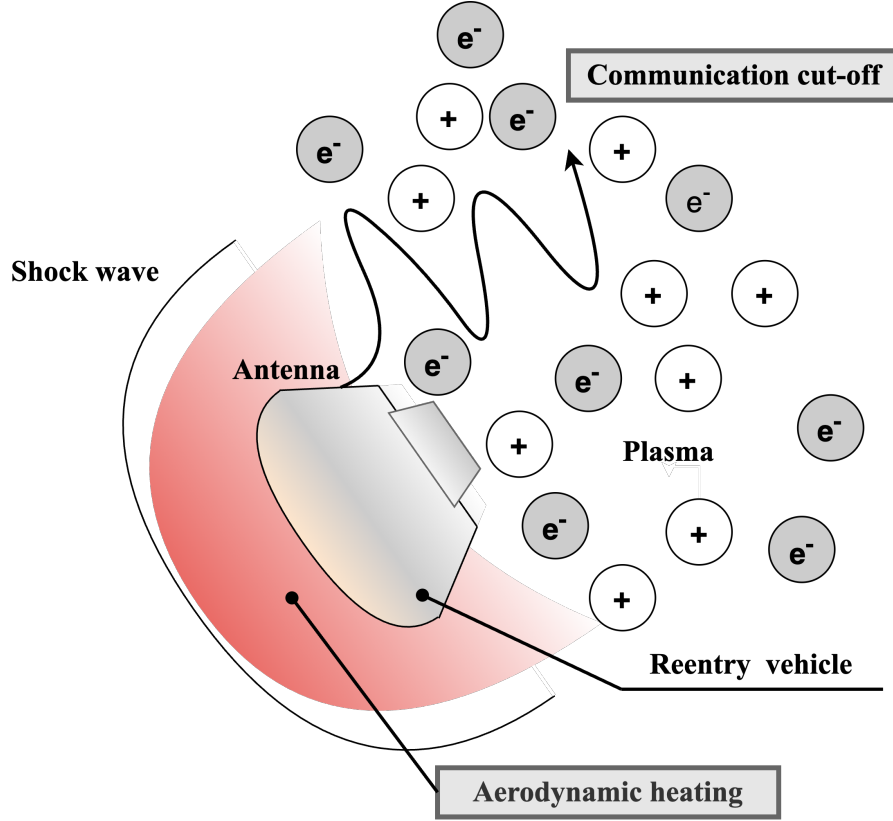


Figure 1.1: Plasma-induced blackout and aerodynamic heating during atmospheric reentry.

1.3 Fundamentals of Communication Blackout

Figure 1.1 depicts the hypersonic environment during atmospheric entry. The bow shock that stands ahead of the fore-body compresses and heats the freestream so drastically that the surrounding gas dissociates and becomes partially ionized. As a result, a plasma sheath forms around the spacecraft. While this plasma is responsible for the severe convective and radiative heating that drives thermal-protection design, it also interferes with radio-frequency (RF) links, producing the well-known communication blackout.

The plasma frequency is a governing parameter for communication blackout and is described as follows:

$$f_p = \frac{1}{2\pi} \sqrt{\frac{e^2 n_{\text{ele}}}{m_{\text{ele}} \varepsilon_0}}, \quad (1.1)$$

where e is the elementary charge, n_{ele} the electron number density, m_{ele} the electron mass, and ε_0 the vacuum permittivity. When f_p overtakes the RF link frequency f_{link} , electromagnetic waves cannot propagate and the signal is either reflected or

heavily attenuated [17],

$$f_p \geq f_{\text{link}}. \quad (1.2)$$

This expression assumes a cold, collisionless plasma with normal incidence of the RF wave.

Equivalently, one can define a critical electron number density,

$$n_{\text{ele,crit}} = \frac{4\pi^2 \varepsilon_0 m_{\text{ele}}}{e^2} f_{\text{link}}^2, \quad (1.3)$$

and state that blackout occurs when $n_{\text{ele}} \geq n_{\text{ele,crit}}$. Avoiding the blackout therefore requires either raising f_{link} or depressing n_{ele} beneath this threshold. Practical systems can combine both tactics.

This thesis focuses on $f_{\text{link}} = 2.4 \text{ GHz}$, a band widely adopted for reentry telemetry and relay communications, because it represents a practical proving ground for mitigation concepts. This frequency also matches the hardware available in the arc-heated experiments discussed later. Substituting this frequency into Equation 1.3 yields $n_{\text{ele,crit}} = 7.1 \times 10^{16} / \text{m}^3$, which establishes the electron-density target that any mitigation strategy must satisfy.

Table 1.1 compiles representative communication frequencies and their corresponding critical electron number densities, including the 2.4 GHz band used in this study. By comparing the historically referenced RAM C VHF/C/X bands with higher-frequency links of future interest, the table provides an intuitive sense of the plasma rarefaction required.

In addition to such fundamental considerations based on the local plasma frequency and critical electron number density, communication blackout has been analyzed using coupled flowfield and electromagnetic simulations for realistic reentry configurations. Ramjatan et al. performed a detailed blackout analysis for Mars entry missions by combining high-fidelity flowfield solutions with radio-wave propagation calculations, highlighting the strong sensitivity of the blackout duration to the trajectory and atmospheric environment [18]. Similarly, Takahashi et al. investigated radio-frequency blackout for a blunt-body capsule in Earth atmospheric reentry, demonstrating how the spatial distribution of electron number density around the capsule controls the onset and termination of blackout [19]. These studies underscore the importance of consistently treating both the plasma flowfield and wave propagation when assessing blackout characteristics in practical missions.

Table 1.1: Representative RF bands and their critical electron number densities.

Band (context)	f_{link} [GHz]	$n_{\text{ele,crit}}$ [$1/\text{m}^3$]
VHF (RAM C telemetry)	0.260	8.4×10^{14}
S-band (Apollo missions)	2.30	6.6×10^{16}
2.4 GHz (Wi-Fi / this work)	2.40	7.1×10^{16}
5.0 GHz (Wi-Fi)	5.00	3.1×10^{17}
C-band (RAM C telemetry)	5.70	4.0×10^{17}
X-band (RAM C telemetry)	9.21	1.1×10^{18}
Ku-band (TDRS links)	12	2.3×10^{18}
Ka-band (deep-space links)	26.5	8.7×10^{18}
Terahertz (Liu et al.[20])	6,000	$4.5 \times 10^{23*}$

*Indicative value for terahertz-band proposals; these frequencies are numerically assessed for blackout mitigation but remain outside practical flight implementations.

1.4 Communication Blackout in Reentry Phase

1.4.1 Early Crewed Reentry Missions

In the 1960s, the National Aeronautics and Space Administration (NASA) conducted a sequence of pioneering crewed missions that revealed the operational impact of blackout. Programs such as Mercury, Gemini, and Apollo encountered communication losses during peak heating, when reentry velocities and plasma densities were greatest. The Mercury and Gemini programs encountered communication interruptions during the reentry phase [21, 22]. Mercury considered implementing an X-band (8.4 GHz) telemetry system as a mitigation measure; nevertheless blackout durations of approximately four minutes persisted. Flight GT-3 tested the injection of approximately 7 kg of water into the plasma sheath. The GT-3 experiment restored ultra-high-frequency (UHF) signals at 230.4 MHz and 296.8 MHz during high-flow injection and partially recovered C-band (5,960 MHz) communication, demonstrating the feasibility of temporary RF windows under operational conditions.

In contrast, Apollo lunar returns experienced blackouts lasting four to ten minutes at S-band (2.3 GHz) [23]. The higher entry velocity (approximately 11 km/s) relative to low Earth orbit missions intensified the plasma environment and broadened the affected frequency bands. These early missions established blackout as a tangible operational hazard that demanded sustained research into mitigation technologies.

1.4.2 Subsequent Reentry Flight Projects

Experience from operational missions motivated targeted flight experiments such as the Radio Attenuation Measurement (RAM) and Flight Investigation of Reentry Environment (FIRE) programs [34]. RAM C-I (1967) measured electron number density during a 7.6 km/s entry using an electrostatic probe and demonstrated that water injection reduced signal attenuation between 79 km and 41 km altitude [24, 25]. RAM C-II (1968) and C-III (1970) extended the campaign by injecting electrophilic liquids (perfluorooctane) and collecting ion density data, providing benchmark measurements for subsequent computational fluid dynamics (CFD) and RF propagation models.

The FIRE program concentrated on high-enthalpy lunar-return conditions [26]. FIRE II (1965) recorded total and radiative heating through optical diagnostics and documented a blackout interval of roughly 30 s, illustrating the coupling between plasma production, heating, and communications. Together, RAM and FIRE shifted the research emphasis from observational accounts during crewed missions to controlled experiments capable of quantifying plasma parameters and assessing mitigation concepts.

1.4.3 Other Flight Experiments

The European Space Agency’s Atmospheric Reentry Demonstrator (ARD) capsule, approximately 70 % of the Apollo geometry, flew in 1998 with a reentry velocity of 7.45 km/s [28]. This mission targeted comprehensive measurements of reentry aerodynamics over a capsule-scale geometry. RF blackout was observed in the L-band GPS signal (1.58 GHz) and in aircraft relay links (2.203/2.218 GHz); meanwhile, the Ku-band TDRS link at 2.27 GHz avoided complete blackout but showed attenuation. Significant plasma-induced attenuation was observed between 86 km and 44 km altitude, supplying valuable data for validating CFD and RF analysis tools [19].

In Japan, the Orbital Reentry Experiment (OREX) conducted by the National Space Development Agency (now JAXA) in 1994 reported a two-minute outage covering both VHF and C-band links between 96 km and 60 km altitude [27]. Despite the communication blackout, onboard data recorders preserved telemetry for later downlink, delivering one of the earliest empirical datasets on plasma-induced RF attenuation in Japan. These international experiments underscore that blackout is a persistent, global challenge for high-speed atmospheric entry.

A post-flight analysis of Mars Pathfinder’s 1997 direct-to-Earth X-band teleme-

try reported a 30 s signal outage, largely attributed to charged particles in the shock layer generated at the probe’s 7.5 km/s entry speed [29].

In contrast, the Mars Exploration Rovers entered at roughly 5.5 km/s; both pre-flight predictions and post-flight data confirmed that blackout was negligible on their X-band links [29]. The Phoenix Mars Lander (2008) adopted a multi-link strategy: its UHF carrier was captured by three orbiting relays and by the 100 m antenna dish of the National Radio Astronomy Observatory (NRAO) at Green Bank, enabling a detailed reconstruction of the modest signal degradation encountered during descent [30, 31, 35].

The subsequent Mars Science Laboratory (MSL) entry exhibited a closer alignment between prediction and observation: the UHF relay links to Mars Reconnaissance Orbiter and Mars Express experienced roughly 70 s of combined brownout and blackout beginning about 30 s after atmospheric interface and persisting until 95 s, matching the pre-flight degradation forecasts [32]. This agreement between analysis and telemetry underscores how modern missions incorporate blackout mitigation directly into link design.

ESA’s ExoMars Atmospheric Mars Entry and Landing Investigations and Analysis (AMELIA) campaign similarly anticipated a blackout lasting several minutes on the UHF link, motivating coordinated relay coverage and entry-science planning [33].

As summarized in Table 1.2, these records span Earth, Mars, and international programs, and map the evolution from ad-hoc countermeasures toward deliberate mitigation strategies.

1.5 Overview of Blackout Mitigation Strategies

Numerous techniques have been proposed to alleviate RF blackout and improve communication reliability during atmospheric entry [36, 37]. These approaches can be broadly classified into two categories: (i) techniques that primarily modify the communication system itself, for example, by changing the carrier frequency or using relay architectures; and (ii) techniques that directly modify the plasma environment or the vehicle configuration to reduce the effective electron number density or otherwise alter the electromagnetic properties around the antenna. This section provides a brief overview of these concepts and introduces the rationale for focusing on surface gas injection in the present study.

1.5.1 High-Frequency Transmission Technique

One direct approach is to employ higher-frequency RF signals (e.g., X-band, Ka-band, or even optical links) whose frequencies remain above the local plasma frequency throughout the blackout-prone portion of the trajectory [38, 39, 20]. In principle, such high-frequency links can propagate through the plasma sheath without requiring any modification of the surrounding flow, and they have been successfully demonstrated in several missions.

However, this approach comes with important trade-offs. Higher-frequency systems typically require more stringent pointing accuracy, larger or more precise antenna and optical assemblies, and are more sensitive to path losses and scattering by the atmosphere. Moreover, under extreme plasma conditions, even high-frequency signals can suffer substantial attenuation or reflection, so that complete elimination of blackout is not always guaranteed. From a mission-integration standpoint, these techniques tend to require a substantial redesign of the communication system and supporting avionics, while leaving the aerothermal environment itself unchanged.

1.5.2 Electron Density Reduction Technique

A second class of mitigation strategies attempts to lower the local electron number density or to tailor the plasma response so that conventional RF frequencies can penetrate. Representative approaches include magnetic confinement, catalytic recombination, trajectory shaping, and active injection of neutral species.

In practice, these concepts must balance mitigation benefits against added system mass, power, and operational complexity. Among them, active gas injection stands out for its ability to respond in real time and to couple RF transmission improvements with thermal protection enhancements, motivating the focus of this thesis.

Magnetic Field-Assisted Propagation

By applying a strong magnetic field, the motion of free electrons can be restricted, altering the plasma's dielectric properties and reducing RF attenuation [40, 41]. Field strengths on the order of several tesla are typically required, implying substantial power supply and structural penalties for flight systems.

More recently, magnetohydrodynamic concepts have regained attention, with superconducting coils employed to impose strong magnetic fields that steer or thin the plasma layer and, in turn, mitigate heating and blackout. Laur et al. showed via ray-tracing that tailored magnetic fields can open propagation corridors for RF

signals, while the MEESST program demonstrated the feasibility of integrating such superconducting systems into future entry vehicles [42, 43].

Catalytic Surface Recombination

Catalytic materials on the heat shield surface can promote recombination of dissociated species, leading to lower ionization near the surface [44, 45]. Its effectiveness has been demonstrated numerically in realistic reentry environments [46]. Although this passive method does not require additional power and controlling systems, the heat generated by recombination reactions can increase thermal loads on the vehicle.

High-Altitude Deceleration Strategies

This concept is mainly applicable to blunt capsules with very low ballistic coefficients, which naturally decelerate above 70 km altitude where ionization levels are reduced [47]. This passive strategy requires substantial design compromises and is not adaptable in real time.

Vehicle Geometry Optimization

Altering the vehicle’s shape can modify shock standoff distance and flow characteristics, potentially reducing peak near-wall electron number densities; separately, remote antenna assemblies increase standoff distance between radiating elements and the plasma sheath. Such remote-antenna assemblies extend the standoff distance between radiating elements and the plasma sheath [48]. Flared fore-body concepts thicken the plasma layer and lower near-wall densities, yet these modifications must be reconciled with mass, aerodynamic performance, and TPS constraints [49].

Gas Injection (Present Study)

Active injection of neutral gas into the boundary layer has emerged as another attractive option for blackout mitigation. By injecting gas from the vehicle surface, a low-temperature gas layer (or gas film) can be formed between the antenna region and the surrounding high-enthalpy plasma flow [50]. If this gas layer is sufficiently thick and continuous, the electron number density along the RF propagation path can be reduced, creating a localized “communication window” within an otherwise opaque plasma sheath. Recent studies have extended this concept from small-scale laboratory experiments to high-enthalpy facilities and have suggested the possibility of simultaneously reducing RF attenuation and surface heat flux [15, 51, 52].

Surface gas injection offers several distinctive advantages compared with the other techniques outlined above. First, it directly modifies the local plasma and thermal boundary layers in front of the antenna, and therefore can be targeted to specific regions of the vehicle without a complete redesign of the global communication architecture. Second, the injected gas layer is inherently cooler than the freestream plasma, so that it can function as a film-cooling layer, reducing convective heat transfer to the surface while also diluting the plasma. This dual potential—mitigation of RF blackout and reduction of aerodynamic heating with a single physical mechanism—is particularly appealing for reentry vehicles where both constraints are severe. Finally, the required hardware, such as gas reservoirs, valves, and injectors, can, in principle, be integrated into existing thermal protection systems and propulsion subsystems, making the concept compatible with typical spacecraft architectures.

On the other hand, the effectiveness of surface gas injection still depends on complex, coupled phenomena: three-dimensional mixing of the injected gas with the plasma, recombination and ionization kinetics, shock-layer structure, and the detailed geometry of the vehicle and antenna. Experimental demonstrations at high-enthalpy and under realistic RF conditions are scarce, and existing numerical studies often treat the flowfield and wave propagation separately or at reduced fidelity. As a result, key questions remain regarding how thick and stable the gas layer must be, how much the electron number density is actually reduced along the antenna line-of-sight, and how these effects scale from ground facilities to flight environments.

In this thesis, attention is focused on a blackout-mitigation concept based on surface gas injection. The basic idea is to inject gas from the vehicle surface in such a way that a gas layer is formed along the surface, and to use the interior of this layer as the propagation path for the radio signal. Because the injected gas is much colder than the surrounding high-enthalpy flow, the gas layer is also expected to act as a thermal insulation layer, providing a film-cooling effect that reduces the heat flux from the hot freestream to the surface. Thus, the same gas layer has the potential to address simultaneously the two major challenges of atmospheric reentry: severe aerodynamic heating and RF communication blackout.

Gas injection for blackout mitigation has previously been considered mainly from the standpoint of promoting plasma recombination by injecting cold gas and of reducing the electron number density by injecting electronegative gases that capture free electrons [50]. In contrast, the present study emphasizes the formation of a gas layer itself as the key mechanism. Once a sufficiently thick gas layer is established, the mitigation effect is expected to be largely independent of the specific gas

species and, in principle, can be combined with other concepts such as electronegative seeding to further enhance the reduction of electron number density. Despite this conceptual advantage, the effectiveness of the gas layer approach has not yet been systematically verified, particularly under high-enthalpy, flight-relevant conditions and with simultaneous consideration of blackout and heating. This gap in understanding provides the primary motivation for the gas-injection-based investigations carried out in this thesis.

1.6 Research Objectives and Scope

The review above highlights that, although gas injection has shown promise in laboratory-scale studies, its effectiveness under flight-relevant enthalpy, geometry, and RF-link conditions remains insufficiently quantified. Furthermore, existing computational tools seldom capture the coupled plasma chemistry, RF propagation, and aerothermal phenomena required to design mitigation systems.

Building on the mitigation landscape reviewed in section 1.5, this thesis pursues the following objectives: (i) experimentally characterize how gas-film injection modifies plasma properties, RF attenuation, and surface heat flux in high-power arc-heated facilities representative of hypersonic reentry; (ii) develop and validate a multiphysics numerical framework that couples nonequilibrium plasma flow, gas injection, and RF wave propagation across relevant frequency bands; and (iii) assess the feasibility of gas-film blackout mitigation for flight systems and identify key design parameters.

Achieving these goals enables a unified assessment of gas-film mitigation, bridging previously disjoint experimental and modeling efforts and establishing the context for the chapters that follow.

1.7 Publications Underpinning the Thesis

Early work by Good and colleagues demonstrated that injecting a cold neutral jet into the shock layer reduced local electron temperature but concluded that blackout mitigation was infeasible because the cooling effect was insufficient for suppressing ionization and dissociation reactions [50]. Their approach focused on thermal dilution; the present research instead hypothesizes that the advected injection gas can form a partially neutral corridor that behaves as an electromagnetic waveguide. As illustrated in Figure 1.2, the injected gas is electrically neutral, which keeps the plasma away from the vehicle; electromagnetic waves can therefore propagate

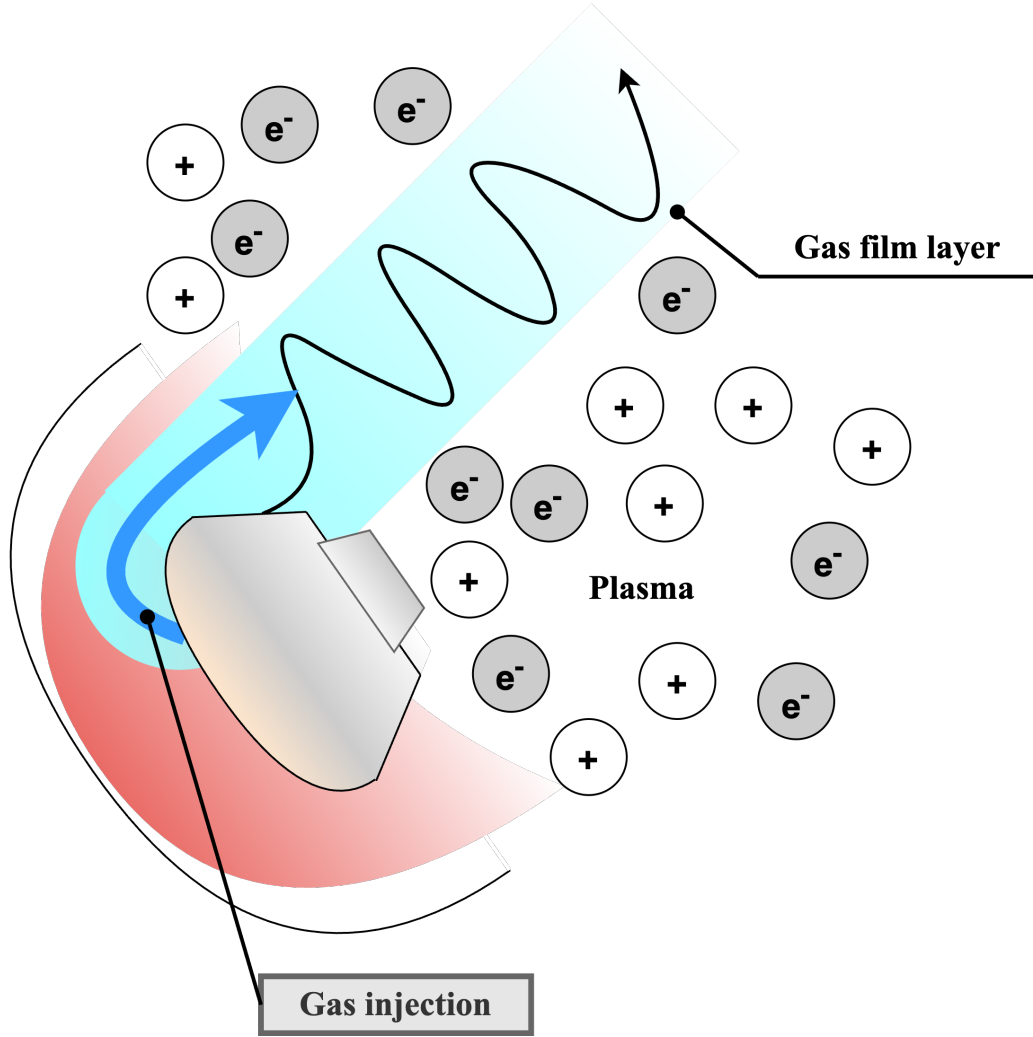


Figure 1.2: Mitigation mechanisms for RF blackout and aerodynamic heating via gas injection.

through it, enabling communication with the exterior.

To test this hypothesis, the underlying studies progressed from numerical feasibility analysis to laboratory-scale validation, followed by reentry-relevant modeling and, finally, large-scale arc-heated facility experiments.

The thesis builds on a four-stage research program:

- **Numerical feasibility assessment:** “Computational Study on Air Film Approach in Reentry Blackout Mitigation” [14], AIAA Journal, 2024; establishes the coupled plasma-flow/RF framework and numerically confirms, via high-fidelity simulations, that an advected gas film can open a propagation path while moderating surface heat flux.
- **Laboratory-scale validation:** “Mitigation of reentry blackout via gas injection in arc-heated facility” [15], Journal of Physics D: Applied Physics,

2024; reports the 1 MW ISAS wind-tunnel experiments and companion CFD analyses that first verify blackout-mitigation effectiveness via electron-density reduction and end-to-end RF transmission recovery with gas-film injection.

- **Reentry-environment evaluation:** “Communication blackout and aerodynamic heating reduction via air film for hypersonic spacecraft” [16], Journal of Applied Physics, 2025; extends the modeling framework to reentry freestream, quantifying simultaneous communication and thermal benefits in flight-representative environments.
- **Large-scale facility extension :** “Dual mitigation of aerodynamic heating and radio-frequency blackout through gas injection in large-scale plasma wind tunnel”, [53]; utilizes the large-scale DLR L2K facility and model to examine the simultaneous reductions in RF attenuation and surface heating in detail; repeated runs confirm reproducibility, providing an experimental verification of the concept at scale.

Accordingly, the thesis synthesizes the aforementioned publications and manuscripts, organizing them according to the progression of the research program. In the chapters that follow, these works are reorganized and supplemented with additional analysis to ensure consistent notation, shared assumptions, and cross-comparison of results.

1.8 Thesis Organization

This thesis provides a comprehensive assessment of gas injection as a dual-purpose mitigation strategy that simultaneously addresses reentry communication blackout and aerodynamic heating. The chapters are organized to move from motivation and methods, through laboratory validation and scaling, to flight-relevant application:

- **chapter 2** motivates arc-heated wind tunnels for multi-second RF studies, framing the facility capabilities and constraints that underpin objective (i).
- **chapter 3** develops the multiphysics computational framework for gas injection mitigation, nonseilibrium plasma CFD, and cold-plasma electromagnetic analysis—addressing objective (ii).
- **chapter 4** reports 1 MW ISAS arc-heated experiments that first achieved experimental verification of the mitigation effect—end-to-end RF transmission recovery with gas injection—with Schlieren imaging and CFD reconciling the jet/film structure; this chapter provides the experimental core of objective (i).

- **chapter 5** presents large-model tests in the German Aerospace Center (DLR) L2K facility, examining in detail the simultaneous reductions in RF attenuation and surface heating; repeated runs establish reproducibility while extending objective (i) to higher enthalpy.
- **chapter 6** translates the validated models to the RAM C flight configuration, evaluating injector placement, blowing parameters, and scalability to realistic reentry; the findings fulfill objective (iii).
- **chapter 7** synthesizes the numerical and experimental results, articulates the overarching conclusions, and outlines future research directions and mission integration pathways.

Together, these chapters provide an end-to-end evaluation of gas-film injection as a comprehensive solution for simultaneous blackout and heating mitigation, while the conclusion highlights remaining challenges and opportunities for follow-on investigation.

Table 1.2: Key reentry missions exhibiting communication blackout and mitigation attempts.

Mission	Year	Blackout signature	Mitigation / notes
Mercury	1962	≈ 4 min loss	X-band telemetry proposed; not flown [21]
Gemini GT-3	1965	Partial loss	7 kg water injection reopened UHF/C [22]
Apollo 11 return	1969	4–10 min loss	No active mitigation [23]
RAM C-I	1967	Attenuation 79–41 km	Water film reduced attenuation [24]
RAM C-II	1968	Ionization profiles	Perfluorooctane injection [25]
RAM C-III	1970	Ionization profiles	Perfluorooctane injection [25]
FIRE II	1965	≈ 30 s blackout	Heating/RF correlation [26]
OREX	1994	≈ 2 min (96–60 km)	Data recorder preserved telemetry [27]
ARD	1998	GPS loss; Ku attenuation	TDRS Ku link degraded [28]
Mars Pathfinder	1997	≈ 30 s blackout	Post-flight plasma analysis [29]
Mars Exploration Rovers	2004	None detected	Low enthalpy predicted no loss [30]
Phoenix Mars Lander	2008	Mild attenuation	Multi-link reception [31]
Mars Science Laboratory	2012	≈ 70 s brownout/blackout (30–95 s)	Predictions matched telemetry [32]
ExoMars (AMELIA)	2016	Several minutes anticipated	Coordinated relay coverage planned [33]

Chapter 2

Experimental Facilities

2.1 Generating High-Enthalpy Flows

A wind tunnel is the most common facility for reproducing aerodynamic flows. High-speed tunnels accelerate the working gas by allowing it to expand quasi-isentropically through a nozzle. In the conventional setup, high-pressure, high-temperature gas is stored in a reservoir, and the nozzle converts this stagnation energy into directed kinetic energy in the test section. Assuming the gas is an ideal gas with ratio of specific heats γ and the flow is quasi-one-dimensional and isentropic, the Mach number in the test section M_{test} is related to the area ratio between the test section and the nozzle throat A_{test}/A^* :

$$\left(\frac{A_{\text{test}}}{A^*}\right) = \frac{1}{M_{\text{test}}} \left[\frac{2}{\gamma + 1} \left(1 + \frac{\gamma - 1}{2} M_{\text{test}}^2 \right) \right]^{\frac{\gamma + 1}{2(\gamma - 1)}} \quad (2.1)$$

The stagnation temperature T_0 and pressure p_0 in the reservoir are obtained from the test-section Mach number and static temperature T_{test} :

$$\begin{aligned} T_0 &= \left(1 + \frac{\gamma - 1}{2} M_{\text{test}}^2 \right) T_{\text{test}}, \\ p_0 &= \left(1 + \frac{\gamma - 1}{2} M_{\text{test}}^2 \right)^{\frac{\gamma}{\gamma - 1}} p_{\text{test}}. \end{aligned} \quad (2.2)$$

Therefore, once the desired Mach number and test-section conditions are specified, the area ratio, reservoir temperature, and reservoir pressure should simply be set to the values computed from Equation 2.1 and Equation 2.2. In representative reentry experiments such as the Hayabusa capsule return, the stagnation-point

surface temperature and static pressure were approximately 3,400 K and 50 kPa, respectively [2, 54]. Generating a supersonic flow that reproduces these conditions demands reservoir temperatures and pressures several times higher. Even if such extreme conditions could be produced, there are no practical structural materials capable of storing them safely.

Furthermore, the above discussion of the isentropic nozzle-flow model assumes an ideal gas. In reality, high-temperature gas undergoes dissociation and ionization. Even with more modest stagnation conditions, the reservoir contains a mixture of partially dissociated and ionized species. As the gas expands through the nozzle, the flow cools rapidly. Because chemical reactions require finite time, the species distributions cannot equilibrate during the expansion, leading to a “frozen” flow. The test section therefore contains a nonequilibrium mixture that differs significantly from ambient gas.

Finally, as the required stagnation enthalpy increases, the feasible run time of a conventional high-enthalpy wind tunnel diminishes sharply. Facilities that deliver long-duration flow typically operate at lower stagnation temperatures and thus cannot reproduce strong real-gas effects.

Representative high-enthalpy facilities include, in addition to arc-heated tunnels, shock tunnels, free-piston-driven shock tunnels, and ballistic ranges. Shock tunnels and free-piston types can reproduce extremely high total temperatures and pressures with extremely short pulses of several milliseconds; however, they require considerable preparation for operation and offer only moderate reproducibility. Ballistic ranges can accelerate projectiles to observe shock wave structures, but test durations are limited to milliseconds or less, and measurement instruments cannot be directly inserted into the flow path. In contrast, arc-heated tunnels provide steady-state flows lasting seconds to minutes, facilitating diagnostics (such as Langmuir probes, microwave interferometers, emission spectroscopy, and Schlieren imaging) required for measuring local electron density and verifying continuous instrument operation. Hence, the following sections focus on arc-heated tunnels.

Each class has its own benefits and limitations, and the choice of facility must be tailored to the specific physical processes under investigation. Subsequent sections motivate the selection of arc-heated wind tunnels for simultaneous study of communication blackout and aerothermal heating.

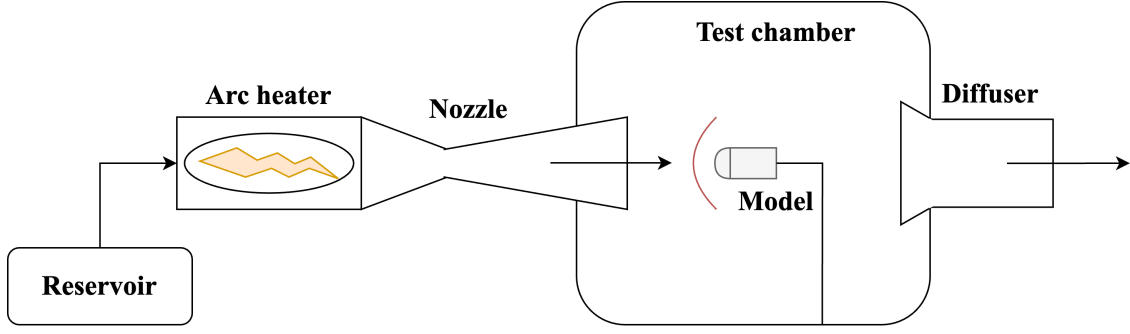


Figure 2.1: Schematic of an arc-heated wind tunnel system.

2.2 Arc-Heated Wind Tunnel

Arc-heated wind tunnels are used not only as experimental facilities for high-speed, high-temperature plasma flows but also as aerodynamic heating environment simulators for evaluating heat-absorbing materials. They use a nozzle to expand high-temperature plasma generated by arc discharge, creating the test freestream. There are several types, including segmented-type (e.g., ISAS 1 MW arc-heated wind tunnel [55], DLR L3K [56, 57]), Huels-type (e.g., DLR L2K [56, 57]), and constrictor-type (e.g., University of Stuttgart Plasmawindkanal (PWK) [58]).

A schematic of an arc-heated wind tunnel system is shown in Figure 2.1. The arc-heated wind tunnel system primarily consists of an air reservoir, arc heater, hypersonic nozzle, low-pressure test chamber, and diffuser. The reservoir stores the working gas at high pressure, which is then fed into the arc heater. The arc heater consists of a cathode and an anode, between which a high-voltage discharge is generated. The gas passing through the arc heater is heated to a high temperature by the arc discharge, creating a high-enthalpy plasma flow. The plasma then expands through a nozzle to form a hypersonic freestream in the test chamber. Finally, the flow is decelerated to subsonic speeds in the diffuser and exhausted by a vacuum pump. The working gas is typically air, nitrogen, or argon, but other gases can be used depending on the experimental requirements. Compared to other high-enthalpy facilities, arc-heated wind tunnels can provide relatively long run times (up to several minutes) and continuous operation, making them suitable for a wide range of aerodynamic and aerothermal studies.

2.3 RF Communication Blackout Experiments

Ground-based facilities that generate high-enthalpy flows are essential for studying communication blackout during atmospheric entry. Although they cannot repro-

duce all entry-flow characteristics at once, they support frequent cost-effective experiments compared to actual flight tests. Because measurements of free electron number density or communication signal strength usually require multi-second test durations, arc-heated wind tunnels—rather than shock tunnels or ballistic ranges—are typically used for demonstration experiments. Accordingly, this section reviews two references that recreate blackout conditions in the plasma freestream of an arc-heated wind tunnel.

2.3.1 DLR L2K Arc-Heated Wind Tunnel

Steffens et al. systematically characterized weakly ionized argon freestreams in the L2K test leg to support RF blackout mitigation studies [59]. Their work establishes operating maps over arc power and mass flow and documents the resulting thermodynamic and plasma states in the test section. Key properties relevant to RF propagation—such as electron number density, gas temperature, and their axial/radial distributions—were quantified to assess how facility settings affect the flow. L2K is a test leg of DLR’s LBK arc-heated facility. Its principal component is a Huels-type arc heater rated at 1.4 MW, which enables cold-wall heat fluxes in air up to 4 MW/m^2 at stagnation pressures up to 250 hPa [60, 61, 62].

Using a combination of non-intrusive optical diagnostics and in situ probes, the study evaluated spatial uniformity of the core jet and its sensitivity to operating conditions. In general, increasing arc power elevates gas temperature and ionization level, whereas increasing mass flow dilutes ionization; thus, the electron number density and degree of ionization can be tuned over a practical range for controlled RF experiments. The measurements provide reference conditions and scaling trends for designing and interpreting blackout/mitigation experiments in L2K and motivate argon as a stable working gas for generating repeatable, weakly ionized flows with long test durations.

2.3.2 ISAS 1 MW Arc-Heated Wind Tunnel

The ISAS 1 MW segmented-type arc-heated wind tunnel is a ground facility developed to generate high-enthalpy plasmas for aerothermal and reentry-relevant investigations [55]. Its segmented-arc design allows independent control of arc power and mass flow distribution, enabling reproducible generation of high-temperature, partially ionized freestreams over a range of stagnation enthalpies and pressures. Typical operating envelopes span arc powers on the order of 1 MW with working gases such as air, nitrogen, or argon; these conditions produce stagnation temperatures

of several thousand kelvin and electron number densities relevant to RF blackout studies in the test chamber.

Takasawa et al. employed experimental models equipped with communication transceivers and attempted transmission across the arc-heated freestream [63]. Two types of models were used: one with a copper nose and one with an alumina nose. Copper’s catalytic surface promoted electron-ion recombination, which increased the received signal strength; this was demonstrated experimentally. Computational fluid dynamics (CFD) analyses were also performed to investigate the tunnel flowfield in detail, confirming that surface catalytic effects promote recombination reactions and thus significantly reduce the electron number density around the communication devices.

2.4 Role of Arc-Heated Experiments

Building on the facility characteristics reviewed above, arc-heated wind tunnels uniquely enable experimental investigation of RF communication blackout under controlled, repeatable conditions. Their ability to deliver steady, multi-second-to-minute plasma flows with tunable enthalpy and electron number density allows direct measurement of electromagnetic wave attenuation and dispersion in conditions relevant to atmospheric entry while accommodating optical diagnostics and in situ probes.

To quantify the net effectiveness of mitigation strategies, this study adopts an end-to-end approach: fully functional communication hardware is installed in models placed within the freestream, and the transmitted signal is measured across the plasma column. This approach is therefore well suited for verifying whether a candidate mitigation increases usable link margin under representative plasma conditions.

For physical interpretation and extrapolation beyond the facility envelope, high-fidelity computational fluid dynamics (CFD) plays a complementary role. Reproducing the arc-heated flow with nonequilibrium chemistry and transport yields spatial distributions of key plasma parameters—such as electron number density and effective collision frequency—that govern RF propagation through the cold-plasma permittivity. Coupling these fields to electromagnetic models clarifies the mechanisms responsible for blackout and mitigation and enables sensitivity analyses difficult to realize experimentally.

Accordingly, the wind-tunnel experiments in this work establish baseline blackout behavior and quantify mitigation gains via in situ RF transmission measurements, while CFD-based reconstruction explains the underlying mechanisms and extends

the findings to flight-relevant conditions. This combined framework positions the experiments as both a validation bed for mitigation concepts and a source of ground truth for the numerical models used throughout the thesis.

Chapter 3

Numerical Demonstration of Blackout Mitigation

During atmospheric reentry, plasma generated by extreme aerodynamic heating can severely attenuate radio-frequency signals, causing communication blackout and posing a critical operational risk. Among the mitigation strategies reviewed in section 1.5, surface gas injection has emerged as a particularly attractive concept because a single system can, in principle, reduce both near-wall electron number density and surface heat flux. However, its effectiveness depends on complex interactions between the high-enthalpy plasma environment and the injected cold gas: three-dimensional jet penetration and mixing, nonequilibrium chemistry and recombination, as well as modifications of the shock and boundary-layer structure. These coupled processes are difficult to isolate experimentally, especially in high-enthalpy arc-heated facilities where diagnostics, run time, and operating cost impose strict constraints.

To clarify the underlying mitigation mechanisms and to guide the design of costly experiments, high-fidelity numerical analysis is therefore indispensable. This chapter focuses specifically on gas injection as a unified mitigation strategy that establishes a thin, electrically neutral gas film along the vehicle surface. High-enthalpy computational fluid dynamics (CFD) is coupled with a frequency-dependent finite-difference time-domain (FDTD) model to resolve both the plasma flow and the propagation of electromagnetic waves around the DLR L3K flat-plate test article. The injected nitrogen forms a cold, weakly ionized film that lowers the local electron number density while simultaneously reducing convective heat flux. Consequently, the gas film provides a propagation corridor for 2.4 GHz communication, demonstrating how a

thermal protection concept can be adapted to mitigate blackout.

3.1 Numerical Framework

The numerical framework first obtains steady electron number-density fields using a high-enthalpy CFD solver, as these fields determine the local plasma permittivity and effective collision frequency that attenuate RF signals. The same methodology is applied to reproduce the arc-heated wind-tunnel conditions in chapter 4 and chapter 5, and to simulate atmospheric-entry environments in chapter 6.

To assess electromagnetic propagation, a computational electromagnetics (CEM) model is then solved in a one-way coupling with the CFD solution: plasma properties are mapped onto the CEM grid, and Maxwell’s equations are advanced in time to quantify wave propagation and attenuation. This coupled approach enables a detailed evaluation of viable transmission corridors and guides experimental verification.

3.2 Plasma Flow Modeling

3.2.1 Governing Equations

The plasma flow is assumed to be laminar and within the continuum regime. Thermochemical nonequilibrium effects are modeled using finite-rate chemical kinetics and a multi-temperature formulation. In the four-temperature model, the translational, rotational, vibrational, and electron temperatures are treated as separate energy modes, while the electronic excitation temperature is assumed equal to the electron temperature.

The governing equations consist of the Navier – Stokes equations augmented by the conservation of individual species masses and the internal energies (rotational, vibrational, and electronic), in addition to total mass, momentum, and total energy conservation. The system can be expressed as

$$\frac{\partial \mathbf{Q}}{\partial t} + \frac{\partial \mathbf{F}}{\partial x_j} = \frac{\partial \mathbf{F}_v}{\partial x_j} + \mathbf{W}, \quad (3.1)$$

where the conservative state vector is defined as $\mathbf{Q} = [\rho, \rho u_i, E, \rho_s, E_{\text{rot}}, E_{\text{vib}}, E_{\text{ele}}]^T$.

The advection and viscous flux vectors $\mathbf{F}$, $\mathbf{F}_v$ and the source-term vector $\mathbf{W}$ in

Equation 3.1 are given by

$$\begin{aligned}
 \mathbf{F} &= \begin{bmatrix} \rho u_j \\ \rho u_i u_j + \delta_{ij} p \\ (E + p) u_j \\ \rho_s u_j \\ E_{\text{rot}} u_j \\ E_{\text{vib}} u_j \\ E_{\text{ele}} u_j \end{bmatrix}, \\
 \mathbf{F}_v &= \begin{bmatrix} 0 \\ \tau_{ij} \\ q_j + \rho \sum_{s=1}^{n_s} h_s D_s \frac{\partial X_s}{\partial x_j} + u_i \tau_{ij} \\ \rho D_s \frac{\partial X_s}{\partial x_j} \\ q_{\text{rot},j} + \rho \sum_{s=M}^{n_m} e_{\text{rot},s} D_s \frac{\partial X_s}{\partial x_j} \\ q_{\text{vib},j} + \rho \sum_{s=M}^{n_m} e_{\text{vib},s} D_s \frac{\partial X_s}{\partial x_j} \\ q_{\text{ele},j} + \rho \sum_{s=M}^{n_m} e_{\text{ele},s} D_s \frac{\partial X_s}{\partial x_j} - p_{\text{ele}} u_j \end{bmatrix}, \mathbf{W} = \begin{bmatrix} 0 \\ 0 \\ 0 \\ \dot{w}_s \\ S_{\text{int,rot}} \\ S_{\text{int,vib}} \\ S_{\text{int,ele}} \end{bmatrix}. \tag{3.2}
 \end{aligned}$$

Here, δ_{ij} , S_{int} , and $\dot{w}_s$ denote the Kronecker delta, the internal energy change rate, and the mass production rate of species s , respectively. The subscript s represents each chemical species. The symbol M indicates the subset of molecular species (N_2 , O_2 , NO , N_2^+ , O_2^+ , NO^+), whereas atomic species and electrons are treated separately. The total number of species is n_s , and n_m denotes the number of molecular species.

The stress tensor τ_{ij} and heat flux q_j are expressed as

$$\begin{aligned}
 \tau_{ij} &= \mu \left(\frac{\partial u_i}{\partial x_j} + \frac{\partial u_j}{\partial x_i} \right) - \frac{2}{3} \mu \frac{\partial u_k}{\partial x_k} \delta_{ij}, \\
 q_j &= q_{\text{trs},j} + q_{\text{rot},j} + q_{\text{vib},j} + q_{\text{ele},j} \\
 &= \lambda_{\text{trs}} \frac{\partial T_{\text{trs}}}{\partial x_j} + \lambda_{\text{rot}} \frac{\partial T_{\text{rot}}}{\partial x_j} + \lambda_{\text{vib}} \frac{\partial T_{\text{vib}}}{\partial x_j} + \lambda_{\text{ele}} \frac{\partial T_{\text{ele}}}{\partial x_j}. \tag{3.3}
 \end{aligned}$$

The equation of state including the electron pressure is written as

$$p = \sum_{s \neq e}^{n_s} \rho_s R_s T_{\text{trs}} + p_{\text{ele}}, \quad (3.4)$$

$$p_{\text{ele}} = \rho_{\text{ele}} R_{\text{ele}} T_{\text{ele}}.$$

The total internal energy is given by

$$E = E_{\text{trs}} + E_{\text{rot}} + E_{\text{vib}} + E_{\text{ele}} + \sum_{s=1}^{n_s} \rho_s \Delta h_s^0 + \frac{1}{2} \rho u_i u_i, \quad (3.5)$$

where Δh_s^0 is the enthalpy of formation.

The individual mode energies are computed as

$$E_{\text{trs}} = \sum_{s=1}^{n_s} \frac{3}{2} \rho_s R_s T_{\text{trs}},$$

$$E_{\text{rot}} = \sum_{s=M}^{n_m} \rho_s R_s T_{\text{rot}}, \quad (3.6)$$

$$E_{\text{vib}} = \sum_{s=M}^{n_m} \rho_s \frac{R_s \Theta_{\text{vib},s}}{\exp(\Theta_{\text{vib},s}/T_{\text{vib}}) - 1},$$

$$E_{\text{ele}} = \frac{3}{2} \rho_{\text{ele}} R_{\text{ele}} T_{\text{ele}},$$

where $\Theta_{\text{vib},s}$ is the characteristic vibrational temperature of species s .

The above set of governing equations forms the basis for the nonequilibrium plasma simulations conducted in this study. Transport coefficients and reaction rate data are introduced in the following subsection.

3.2.2 Thermodynamic and Transport Properties

The viscosity, thermal conductivity, and binary diffusion coefficients used in the viscous fluxes are evaluated following the curve fits of Yos's work [64], which are based on the first Chapman-Enskog approximation. Species effective diffusion coefficients, D_s , are computed using the Curtiss-Hirschfelder relations [65]. Collision cross-sections are primarily taken from Gupta et al. [66], except for the e^- -N and e^- -O pairs, for which the formulations by Fertig et al. [67] are employed. The ambipolar diffusion coefficient is modeled as

$$D_s^a = D_s \left(1 + \frac{T_{\text{ele}}}{T_{\text{trs}}} \right). \quad (3.7)$$

Table 3.1: Thermochemical Properties for the 11-Species Air Model

Species	M_s [kg/mol]	$\Theta_{\text{vib},s}$ [K]	Δh_s^0 [J/kg]
N ₂	2.80×10^{-2}	3,353	0
O ₂	3.20×10^{-2}	2,239	0
NO	3.00×10^{-2}	2,699	2.995×10^6
N ₂ ⁺	2.80×10^{-2}	3,129	5.372×10^7
O ₂ ⁺	3.20×10^{-2}	2,652	3.639×10^7
NO ⁺	3.00×10^{-2}	3,373	3.282×10^7
N	1.40×10^{-2}	—	3.364×10^7
O	1.60×10^{-2}	—	1.543×10^7
N ⁺	1.40×10^{-2}	—	1.339×10^8
O ⁺	1.60×10^{-2}	—	9.787×10^7
e [−]	5.486×10^{-7}	—	0

These thermophysical models ensure consistent treatment of molecular and ionic transport processes across the broad temperature range encountered in high-enthalpy plasma flows.

3.2.3 Thermochemical Model

An 11-species air plasma model summarized in Table 3.1 is adopted to represent air plasma. The species set includes neutral, ionic, and electronic constituents relevant to hypersonic reentry conditions. The reaction mechanism comprises 49 reversible reactions—dissociation, ionization, exchange, and charge transfer—active at high temperatures. Reaction rates are expressed in Arrhenius form as

$$\begin{aligned}
 k_f(T_f) &= C_r T_f^{n_r} \exp\left(-\frac{\theta_r}{T_f}\right), \\
 k_b(T_b) &= \frac{k_f(T_b)}{K^{\text{eq}}(T_b)},
 \end{aligned} \tag{3.8}$$

where C_r , n_r , and θ_r are taken from Park [68], and backward rates use equilibrium constants $K^{\text{eq}}(T_b)$ from Park [69]. For the charge-exchange reaction $\text{N}_2 + \text{N}^+ \rightleftharpoons \text{N}_2^+ + \text{N}$ ($r = 49$ in Table 3.2), the rate coefficients reported by Gupta et al. [66] are adopted.

Table 3.2 lists the detailed reaction set and rate parameters. Here, M denotes the third-body collision partner in multi-body reactions. Two partner sets are defined: M_1 for molecular (and molecular-ion) species and M_2 for atomic (and atomic-ion)

Table 3.2: Chemical reaction rates in Arrhenius forms.

r	Reactants	Products	C_r	n_r	θ_r	T_f	T_b
1–6	$N_2 + M_1$	$\rightleftharpoons N + N + M_1$	7.0×10^{21}	-1.60	113,200	T_{fl}	T_{trs}
7–10	$N_2 + M_2$	$\rightleftharpoons N + N + M_2$	3.0×10^{22}	-1.60	113,200	T_{fl}	T_{trs}
11	$N_2 + e^-$	$\rightleftharpoons N + N + e^-$	1.2×10^{25}	-1.60	113,200	T_{ele}	T_{b1}
12–17	$O_2 + M_1$	$\rightleftharpoons O + O + M_1$	2.0×10^{21}	-1.50	59,500	T_{fl}	T_{trs}
18–21	$O_2 + M_2$	$\rightleftharpoons O + O + M_2$	1.0×10^{22}	-1.50	59,500	T_{fl}	T_{trs}
22–27	$NO + M_1$	$\rightleftharpoons N + O + M_1$	5.0×10^{15}	0.00	75,500	T_{fl}	T_{trs}
28–31	$NO + M_2$	$\rightleftharpoons N + O + M_2$	1.1×10^{17}	0.00	75,500	T_{fl}	T_{trs}
32	$NO + O$	$\rightleftharpoons N + O_2$	8.4×10^{12}	0.00	19,450	T_{trs}	T_{trs}
33	$N_2 + O$	$\rightleftharpoons NO + N$	6.4×10^{17}	-1.00	38,400	T_{trs}	T_{trs}
34	$N + N$	$\rightleftharpoons N_2^+ + e^-$	4.4×10^7	1.50	67,500	T_{trs}	T_{trs}
35	$O + O$	$\rightleftharpoons O_2^+ + e^-$	7.1×10^2	2.70	80,600	T_{trs}	T_{trs}
36	$N + O$	$\rightleftharpoons NO^+ + e^-$	8.8×10^8	1.00	31,900	T_{trs}	T_{trs}
37	$N + e^-$	$\rightleftharpoons N^+ + e^- + e^-$	2.5×10^{34}	-3.82	168,600	T_{ele}	T_{ele}
38	$O + e^-$	$\rightleftharpoons O^+ + e^- + e^-$	3.9×10^{33}	-3.78	158,500	T_{ele}	T_{ele}
39	$NO^+ + O$	$\rightleftharpoons N^+ + O_2$	1.0×10^{12}	0.50	77,200	T_{trs}	T_{trs}
40	$O_2^+ + N$	$\rightleftharpoons N^+ + O_2$	8.7×10^{13}	0.14	28,600	T_{trs}	T_{trs}
41	$O^+ + NO$	$\rightleftharpoons N^+ + O_2$	1.4×10^5	1.90	26,600	T_{trs}	T_{trs}
42	$O_2^+ + N_2$	$\rightleftharpoons N_2^+ + O_2$	9.9×10^{12}	0.00	40,700	T_{trs}	T_{trs}
43	$O_2^+ + O$	$\rightleftharpoons O^+ + O_2$	4.0×10^{12}	-0.09	18,000	T_{trs}	T_{trs}
44	$NO^+ + N$	$\rightleftharpoons O^+ + N_2$	3.4×10^{13}	-1.08	12,800	T_{trs}	T_{trs}
45	$NO^+ + O_2$	$\rightleftharpoons O_2^+ + NO$	2.4×10^{13}	0.41	32,600	T_{trs}	T_{trs}
46	$NO^+ + O$	$\rightleftharpoons O_2^+ + N$	7.2×10^{12}	0.29	48,600	T_{trs}	T_{trs}
47	$O^+ + N_2$	$\rightleftharpoons N_2^+ + O$	9.1×10^{11}	0.36	22,800	T_{trs}	T_{trs}
48	$NO^+ + N$	$\rightleftharpoons N_2^+ + O$	7.2×10^{13}	0.00	35,500	T_{trs}	T_{trs}
49	$N_2 + N^+$	$\rightleftharpoons N_2^+ + N$	1.0×10^{12}	0.50	12,200	T_{trs}	T_{trs}

species. The effective forward and backward reaction temperatures are defined as

$$\begin{aligned}
M_1 &= \{N_2, O_2, NO, N_2^+, O_2^+, NO^+\}, \\
M_2 &= \{N, O, N^+, O^+\}, \\
T_{fl} &= (T_{trs}T_{rot}T_{vib})^{1/3}, \\
T_{b1} &= (T_{trs}T_{ele})^{1/2}.
\end{aligned}$$

The rotational characteristic temperatures, $\Theta_{rot,s}$, and the rotational collision numbers, $Z_{rot,s}$, used for each molecular species are listed in Table 3.3. Together, these parameters provide a self-consistent description of the nonequilibrium thermochemical state in the simulated high-temperature air plasma.

Table 3.3: $\Theta_{\text{rot},s}$ and $Z_{\text{rot},s}$ for each species.

Species	N ₂	O ₂	NO	N ₂ ⁺	O ₂ ⁺	NO ⁺
$\Theta_{\text{rot},s}$	2.879	2.069	2.440	2.879	2.069	2.440
$Z_{\text{rot},s}$	15.70	14.40	15.05	15.70	14.40	15.05

3.2.4 Internal Energy Mode Coupling

Energy exchange among translational, rotational, vibrational, and electronic modes is taken into account. Accordingly, the internal energy source terms S_{int} in $\mathbf{W}$ (see Equation 3.2) are written as

$$\begin{aligned}
 S_{\text{int,rot}} &= Q_{\text{T-R}} - Q_{\text{R-V}} - Q_{\text{R-e}} + Q_{\text{D,rot}}, \\
 S_{\text{int,vib}} &= Q_{\text{T-V}} + Q_{\text{R-V}} + Q_{\text{e-V}} + Q_{\text{D,vib}}, \\
 S_{\text{int,ele}} &= Q_{\text{T-e}} + Q_{\text{R-e}} - Q_{\text{e-V}} + Q_{\text{D,ele}} + Q_{\text{I,ele}}.
 \end{aligned} \tag{3.9}$$

Here, the subscripts T – R, T – V, T – e, R – V, R – e, and e – V denote translational-rotational, translational-vibrational, translational-electronic, rotational-vibrational, rotational-electronic, and electronic-vibrational energy exchanges, respectively.

Translational-Rotational Energy Exchange

The exchange rate between the translational and rotational energies, $Q_{\text{T-R}}$, follows Parker’s model [70]:

$$Q_{\text{T-R}} = \sum_{s=M}^{n_m} \frac{E_{\text{rot}}^{\text{eq}}(T_{\text{trs}}) - E_{\text{rot}}(T_{\text{rot}})}{\langle \tau_s \rangle_{\text{T-R}}}. \tag{3.10}$$

The mean relaxation time $\langle \tau_s \rangle_{\text{T-R}}$ is evaluated as

$$\langle \tau_s \rangle_{\text{T-R}} = \frac{Z_{\text{rot},s} \sum_{k \neq e}^{n_s} \tau_{s,k}}{1 + \frac{\pi^{3/2}}{2} \left(\frac{\Theta_{\text{rot},s}}{T_{\text{trs}}} \right)^{1/2} + \left(\frac{\pi^2}{4} + \pi \right) \left(\frac{\Theta_{\text{rot},s}}{T_{\text{trs}}} \right)}, \tag{3.11}$$

where the relaxation time for a collision between species s and k , $\tau_{s,k}$, is given by

$$\tau_{s,k} = \left[n_k \sigma_{\text{rot}} \left(\frac{8k_B T_{\text{trs}}}{\pi \mu_{s,k}} \right)^{1/2} \right]^{-1}, \tag{3.12}$$

and $\mu_{s,k} = m_s m_k / (m_s + m_k)$ denotes the reduced mass. The rotational characteristic temperatures $\Theta_{\text{rot},s}$ and constants $Z_{\text{rot},s}$ are listed in Table 3.3, and ionic species are assumed to share the same constants as their neutral counterparts. The effective cross-section for rotational energy exchange is set to $1 \times 10^{-19} \text{ m}^2$.

Translational-Vibrational and Rotational-Vibrational Energy Exchange

The translational-vibrational relaxation follows the Landau-Teller formulation, with the vibrational-rotational coupling treated according to Millikan and White [71] and Park's high-temperature correction [72, 73]. The translational energy exchange rate is

$$Q_{\text{T-V}} = \sum_{s=M}^{n_m} \frac{0.6 [E_{\text{vib}}^{\text{eq}}(T_{\text{trs}}) - E_{\text{vib}}(T_{\text{vib}})]}{\langle \tau_s^{\text{MW}} \rangle + \langle \tau_s^{\text{P}} \rangle}. \quad (3.13)$$

Similarly, the rotational-vibrational energy exchange rate is

$$Q_{\text{R-V}} = \sum_{s=M}^{n_m} \frac{0.4 [E_{\text{vib}}^{\text{eq}}(T_{\text{rot}}) - E_{\text{vib}}(T_{\text{vib}})]}{\langle \tau_s^{\text{MW}} \rangle}. \quad (3.14)$$

The 0.6/0.4 partitioning is the conventional choice in Park-type multi-temperature models and ensures overall energy conservation by dividing the vibrational relaxation between the translational and rotational reservoirs such that $Q_{\text{TR-V}} = Q_{\text{T-V}} + Q_{\text{R-V}}$.

For temperatures between 300 K to 8,000 K, the Millikan–White relaxation time is expressed as

$$\tau_{s,k}^{\text{MW}} = \left(\frac{p}{101,325} \right)^{-1} \exp \left(A_{s,k} T_{\text{trs}}^{-1/3} - B_{s,k} \right), \quad (3.15)$$

where

$$\begin{aligned} A_{s,k} &= 1.16 \times 10^{-3} \mu_{s,k}^{1/2} \Theta_{\text{vib},s}^{4/3}, \\ B_{s,k} &= 0.015 \mu_{s,k}^{1/4} A_{s,k} + 18.42. \end{aligned} \quad (3.16)$$

The mean species relaxation time is obtained from

$$\langle \tau_s^{\text{MW}} \rangle = \frac{\sum_{k=M}^{n_m} \rho_k / M_k}{\sum_{k \neq e}^{n_s} \rho_k / (M_k \tau_{s,k}^{\text{MW}})}. \quad (3.17)$$

Table 3.4: Curve-fit coefficients of $\sigma_{e,s}(T_{\text{ele}})$

Species	N ₂	O ₂	NO	N	O
$\tilde{a}_s$	7.5×10^{-20}	2.0×10^{-20}	1.0×10^{-19}	5.0×10^{-20}	1.2×10^{-20}
$\tilde{b}_s$	5.5×10^{-24}	6.0×10^{-24}	0	0	1.7×10^{-24}
$\tilde{c}_s$	-1.0×10^{-28}	0	0	0	-2.0×10^{-29}

Park's collision-limiting correction [72] is further applied as

$$\langle \tau_s^{\text{P}} \rangle = \left[n_s \sigma_{\text{vib}} \left(\frac{8R_s T_{\text{trs}}}{\pi} \right)^{1/2} \right]^{-1}, \quad (3.18)$$

with $\sigma_{\text{vib}} = 1.0 \times 10^{-21} (50,000/T_{\text{trs}})^2 \text{ m}^2$.

Translational-Electron Energy Exchange

The energy transfer between translational and electron modes follows Appleton's model [74]:

$$Q_{\text{T-e}} = \sum_{s \neq e}^{n_s} 2\nu_{e,s} \frac{3}{2} \rho_{\text{ele}} R_s (T_{\text{trs}} - T_{\text{ele}}), \quad (3.19)$$

where $\nu_{e,s}$ is the electron-species collision frequency. For ionic species [75],

$$\begin{aligned} \nu_{e,s} &= 3.64 \times 10^{-6} n_s \ln(\Lambda) T_{\text{ele}}^{-3/2}, \\ \Lambda &= 1.24 \times 10^7 \left(\frac{T_{\text{ele}}^3}{n_{\text{ele}}} \right)^{1/2}. \end{aligned} \quad (3.20)$$

For neutral species [76],

$$\nu_{e,s} = n_s \sigma_{e,s} \left(\frac{8R_{\text{ele}} T_{\text{ele}}}{\pi} \right)^{1/2}, \quad (3.21)$$

where $\sigma_{e,s}$ is evaluated from a temperature-dependent polynomial fit:

$$\sigma_{e,s} = \tilde{a}_s + \tilde{b}_s T_{\text{ele}} + \tilde{c}_s T_{\text{ele}}^2. \quad (3.22)$$

Rotational-Electron and Electron-Vibrational Energy Exchange

The rotational – electron energy transfer is modeled according to Nishida and Matsumoto [77]:

$$Q_{R-e} = \sum_{s=M}^{n_m} 2\nu_{e,s} g_{\text{rot},s} \frac{3}{2} \rho_{\text{ele}} R_s (T_{\text{rot}} - T_{\text{ele}}), \quad (3.23)$$

where $g_{\text{rot},s}$ is the empirical coupling factor between the rotational and electron modes, taken as 10 for neutral species [78] and assumed identical for corresponding ionic species.

The electron – vibrational energy transfer follows Lee’s model [79]:

$$Q_{e-V} = \sum_{s=M}^{n_m} \frac{E_{\text{vib}}^{\text{eq}}(T_{\text{ele}}) - E_{\text{vib}}(T_{\text{vib}})}{\langle \tau_s \rangle_{e-V}}. \quad (3.24)$$

The relaxation times for O_2 and NO are taken to be 300 times that for N_2 , following Park and Lee [80].

Energy Losses due to Chemical Reactions

Energy losses in the vibrational and rotational modes due to heavy-particle impact reactions are modeled as

$$\begin{aligned} Q_{D,\text{rot}} &= \sum_{s=M}^{n_m} e_{\text{rot}} \dot{w}_{s,D}, \\ Q_{D,\text{vib}} &= \sum_{s=M}^{n_m} e_{\text{vib}} \dot{w}_{s,D}, \end{aligned} \quad (3.25)$$

where $\dot{w}_{s,D}$ represents the mass production rate for the dissociation reactions ($r = 1-10, 12-31$ in Table 3.2) [76]. Electron-impact dissociation and ionization losses are expressed as

$$\begin{aligned} Q_{D,\text{ele}} &= E_{D,\text{N}_2} \dot{w}_{e,\text{N}_2}, \\ Q_{I,\text{ele}} &= E_{I,\text{N}} \dot{w}_{e,\text{N}} + E_{I,\text{O}} \dot{w}_{e,\text{O}}, \end{aligned} \quad (3.26)$$

where $\dot{w}_{e,\text{N}_2}$, $\dot{w}_{e,\text{N}}$, and $\dot{w}_{e,\text{O}}$ are the corresponding electron-impact production rates ($r = 11, 37, 38$ in Table 3.2). Here, E_{D,N_2} , $E_{I,\text{N}}$, and $E_{I,\text{O}}$ denote the dissociation and ionization energies of N_2 , N , and O , respectively.

The above formulations collectively ensure full energy conservation among translational, rotational, vibrational, and electronic modes, and provide a physically consistent representation of nonequilibrium energy transfer in high-enthalpy air plasma flows.

3.3 CFD Solver: RG-FaSTAR

The system of Navier-Stokes equations is reformulated in delta form and solved using a finite-volume approach. Flow properties are stored at the cell centers of control volumes in the computational domain. The convective fluxes are evaluated using the simple low-dissipation advection upstream splitting method (SLAU2) scheme [81], in combination with the monotonic upstream-centered scheme for conservation laws (MUSCL) for second-order spatial accuracy. The viscous fluxes are computed using a second-order central-difference scheme, and spatial gradients of the flow variables are obtained by the Green–Gauss method.

Time integration is performed implicitly by a time-marching procedure. For robust and efficient convergence in thermochemical nonequilibrium simulations, the lower-upper symmetric Gauss-Seidel (LU-SGS) method [82] is combined with a point-implicit treatment of source terms [83].

These physical and numerical models are implemented in the high-enthalpy flow solver **RG-FaSTAR** [84], which is an extended version of the fast unstructured CFD code **FaSTAR** [85] originally developed by JAXA to include real-gas effects. The solver supports unstructured grid systems composed of tetrahedral, prismatic, pyramidal, and hexahedral elements, providing high flexibility for grid generation around complex geometries. Parallel computation is achieved through domain decomposition with a message passing interface (MPI) framework, enabling efficient scalability on high-performance computing platforms.

Within **RG-FaSTAR**, single-, two-, and four-temperature thermochemical models are available. In this study, the two-temperature model is employed, where the translational and rotational temperatures are assumed to be in equilibrium ($T_{\text{trs}} = T_{\text{rot}}$) and the electron and vibrational temperatures are likewise equilibrated ($T_{\text{ele}} = T_{\text{vib}}$).

3.4 Electromagnetic Wave Analysis

Figure 3.1 illustrates the one-way coupling framework adopted in this chapter. A steady-state plasma flow solution is first obtained using the CFD solver. Key plasma properties—particularly the electron plasma frequency and the effective collision frequency—are then interpolated onto the computational electromagnetics (CEM) grid. With these mapped fields, the electromagnetic solver advances Maxwell’s equations in the time domain to evaluate wave propagation and attenuation.

To quantify the mitigation of communication blackout by the injected gas film,

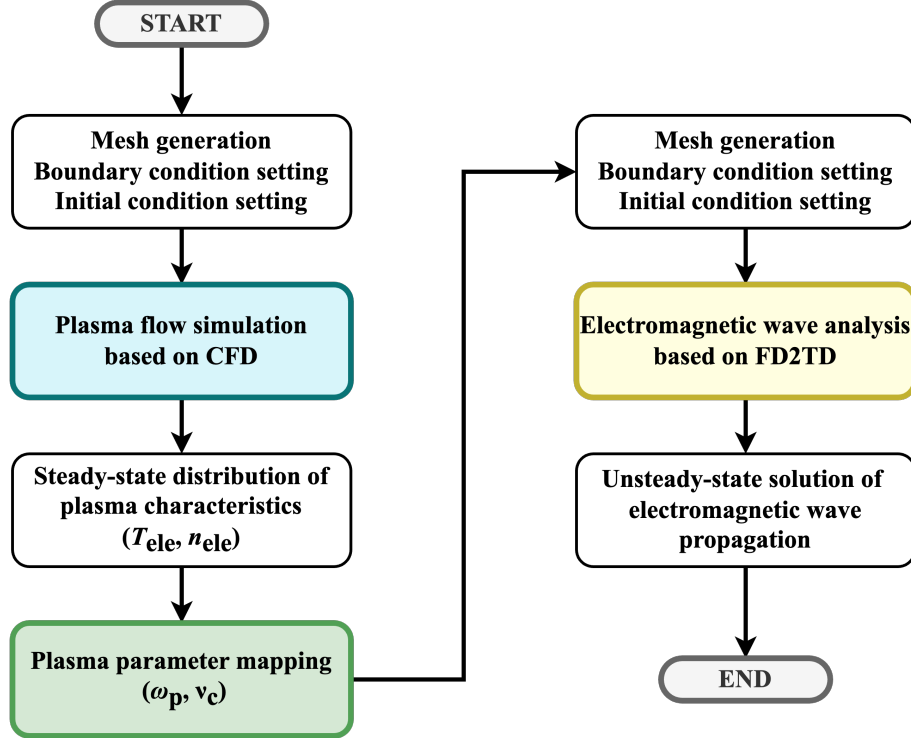


Figure 3.1: Flowchart of the coupled CFD-CEM computing process.

an electromagnetic-wave analysis based on Maxwell's equations was performed. The dielectric permittivity and conductivity of a reentry plasma are inherently frequency-dependent. The conventional finite-difference time-domain (FDTD) method assumes that these parameters are constant with respect to frequency, which may lead to inaccurate results when applied to highly dispersive plasma media. Therefore, in this study, the frequency-dependent FDTD (FD2TD) method [86] is employed to simulate electromagnetic-wave propagation in a dispersive medium. This approach enables a detailed evaluation of electromagnetic-wave behavior in high-enthalpy plasma flows by modeling the dielectric constant as a function of frequency.

Maxwell's equations are written as

$$\begin{aligned}
 \nabla \times \mathbf{E}(\mathbf{r}, t) &= -\frac{\partial \mathbf{B}(\mathbf{r}, t)}{\partial t}, \\
 \nabla \times \mathbf{H}(\mathbf{r}, t) &= \frac{\partial \mathbf{D}(\mathbf{r}, t)}{\partial t} + \mathbf{J}(\mathbf{r}, t), \\
 \nabla \cdot \mathbf{D}(\mathbf{r}, t) &= -\rho(\mathbf{r}, t), \\
 \nabla \cdot \mathbf{B}(\mathbf{r}, t) &= 0,
 \end{aligned} \tag{3.27}$$

where $\mathbf{E}$, $\mathbf{H}$, $\mathbf{D}$, $\mathbf{B}$, $\mathbf{J}$, and ρ denote the electric field, magnetic field, electric flux density, magnetic flux density, current density, and charge density, respectively. Here, $\mathbf{r}$ and t represent the position vector and time, respectively. The current

continuity equation is given by

$$\nabla \cdot \mathbf{J}(\mathbf{r}, t) + \frac{\partial \rho(\mathbf{r}, t)}{\partial t} = 0. \quad (3.28)$$

The electric and magnetic field components are staggered in space according to the Yee cell arrangement [87]. The electromagnetic domain is discretized with hexahedral meshes, and Mur's first-order absorbing boundary condition [88] is imposed on all external faces of the computational domain to minimize wave reflections.

To account for frequency dispersion, the complex relative permittivity is modeled by a first-order Drude formulation as

$$\varepsilon_r^* = \varepsilon_r + \frac{\sigma}{i\omega\varepsilon_0}, \quad (3.29)$$

where i is the imaginary unit, ω is the angular frequency of the electromagnetic wave, σ is the electrical conductivity, and ε_0 is the permittivity of free space. The real and imaginary components are expressed as

$$\begin{aligned} \varepsilon_r &= 1 - \frac{\omega_p^2}{\omega^2 + \nu_c^2}, \\ \sigma &= \frac{\varepsilon_0 \omega_p^2 \nu_c}{\omega^2 + \nu_c^2}, \end{aligned} \quad (3.30)$$

where ω_p and ν_c are the plasma angular frequency and electron collision frequency, respectively. In an ionized gas, the relative permittivity decreases as the plasma frequency and electromagnetic-wave frequency increase. When $\omega_p^2 > \omega^2 + \nu_c^2$, ε_r becomes negative, resulting in an imaginary refractive index; under such conditions, the electromagnetic wave cannot propagate and is completely reflected at the plasma boundary.

Because the propagation speed of electromagnetic waves is several orders of magnitude higher than the characteristic flow velocity, the plasma field is treated as quasi-steady (frozen) during the CEM simulation. Plasma parameters such as ε_r , ω_p , and ν_c are mapped from the CFD results to the CEM grid using the relations

$$\begin{aligned} \omega_p &= \sqrt{\frac{e^2 n_{\text{ele}}}{m_{\text{ele}} \varepsilon_0}}, \\ \nu_c &= \sum_{s=1}^{N_s} n_s \pi \Omega_{s,\text{ele}} \sqrt{\frac{8k_B T_{\text{ele}}}{\pi m_{\text{ele}}}}. \end{aligned} \quad (3.31)$$

Here, e is the elementary charge, n_s is the number density of species s , m_{ele} is the

Table 3.5: Reservoir conditions used for the L3K nozzle simulation.

Total enthalpy [MJ/kg]	15.9	
Reservoir pressure [kPa]	37.5	
Mass fraction	N ₂	0.640
	O ₂	0.0006
	NO	0.016
	N	0.118
	O	0.225

electron mass, k_B is the Boltzmann constant, and $\pi\Omega_{s,ele}$ represents the electron–species collision cross-section.

3.5 Numerical Model

3.5.1 L3K Facility Overview

The L3K test leg of DLR’s LBK arc-heated facility is designed to deliver steady high-enthalpy flows representative of atmospheric entry conditions [57]. A segmented arc heater feeds a mixing reservoir and a hypersonic nozzle, producing a plasma freestream that reproduces key aerothermal phenomena [89]. The heater assembly comprises anode and cathode columns, with the flow passing through a 29 mm throat and expanding to 300 mm at a 12° half-angle. The power supply can deliver up to 6 MW, allowing the stagnation enthalpy and mixture composition to be tailored for specific test conditions.

Table 3.5 lists the reservoir conditions adopted in this study. Although the reservoir is nearly homogeneous, viscous and nonequilibrium processes in the nozzle generate pronounced radial gradients at the exit plane. Quantifying these variations is a prerequisite for determining accurate inflow conditions for the chamber-scale gas injection analysis presented in the following sections.

3.5.2 Nozzle Flow Analysis

Because the nozzle exit distribution governs the chamber inflow conditions, the internal nozzle flow was numerically simulated to obtain consistent profiles of velocity, temperature, and species concentrations.

The Navier-Stokes equations, extended to account for thermochemical nonequilibrium, were solved for the conservation of total mass, momentum, total energy, individual species masses, rotational energy, and electron energy. The species set

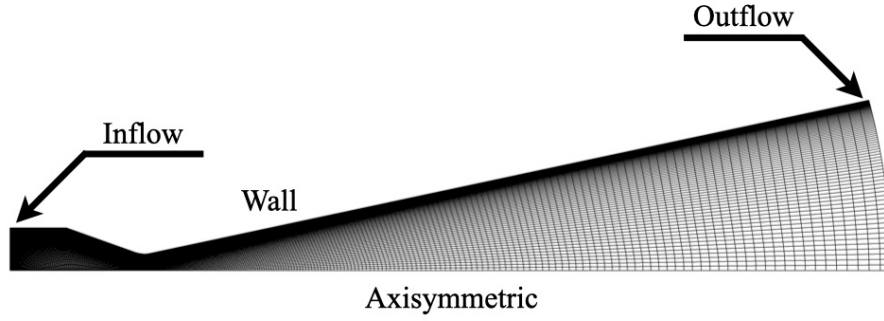


Figure 3.2: Computational grid used for the L3K nozzle simulation.

consisted of eleven air components (N_2 , O_2 , NO , N_2^+ , O_2^+ , NO^+ , N , O , N^+ , O^+ , and e^-), enabling detailed modeling of high-temperature chemistry.

The governing equations were discretized using a finite-volume formulation on a cell-centered structured mesh. Convective fluxes were evaluated with the SLAU 2 scheme [81] and a minmod limiter, viscous fluxes were computed using central differencing, and temporal advancement employed an implicit Euler method. Source terms were treated point-implicitly, and the linear systems were solved by the LU-SGS algorithm to accelerate convergence. These procedures are implemented in the in-house solvers **RG-FaSTAR** and **Arcflow**. In this thesis, **RG-FaSTAR** is used for all three-dimensional CFD analyses, whereas the two-dimensional axisymmetric simulations (such as the L3K nozzle flow) are carried out with **Arcflow**, which employs the same physical and numerical models.

Figure 3.2 shows the computational domain of the DLR L3K nozzle. The grid is axisymmetric and structured, with 295×75 points. The total pressure, total enthalpy, and species composition at the reservoir inlet were prescribed according to Table 3.5. At the nozzle exit, a zero-gradient outflow condition was applied. On the wall boundary, no-slip, isothermal, non-catalytic, and zero normal pressure gradient conditions were imposed.

3.5.3 Flow Profiles at the L3K Nozzle Exit

Figure 3.3 illustrates the radial distributions of temperature, velocity, enthalpy, and composition at the nozzle exit. The translational and rotational temperatures coincide, as do the vibrational and electron temperatures, confirming the expected two-temperature behavior of the nonequilibrium freestream. The streamwise velocity peaks along the centerline and decreases toward the wall due to boundary-layer growth along the diverging contour. The core flow is dominated by N_2 , O_2 , and N , while the electron mole fraction drops sharply near the wall, indicating strong

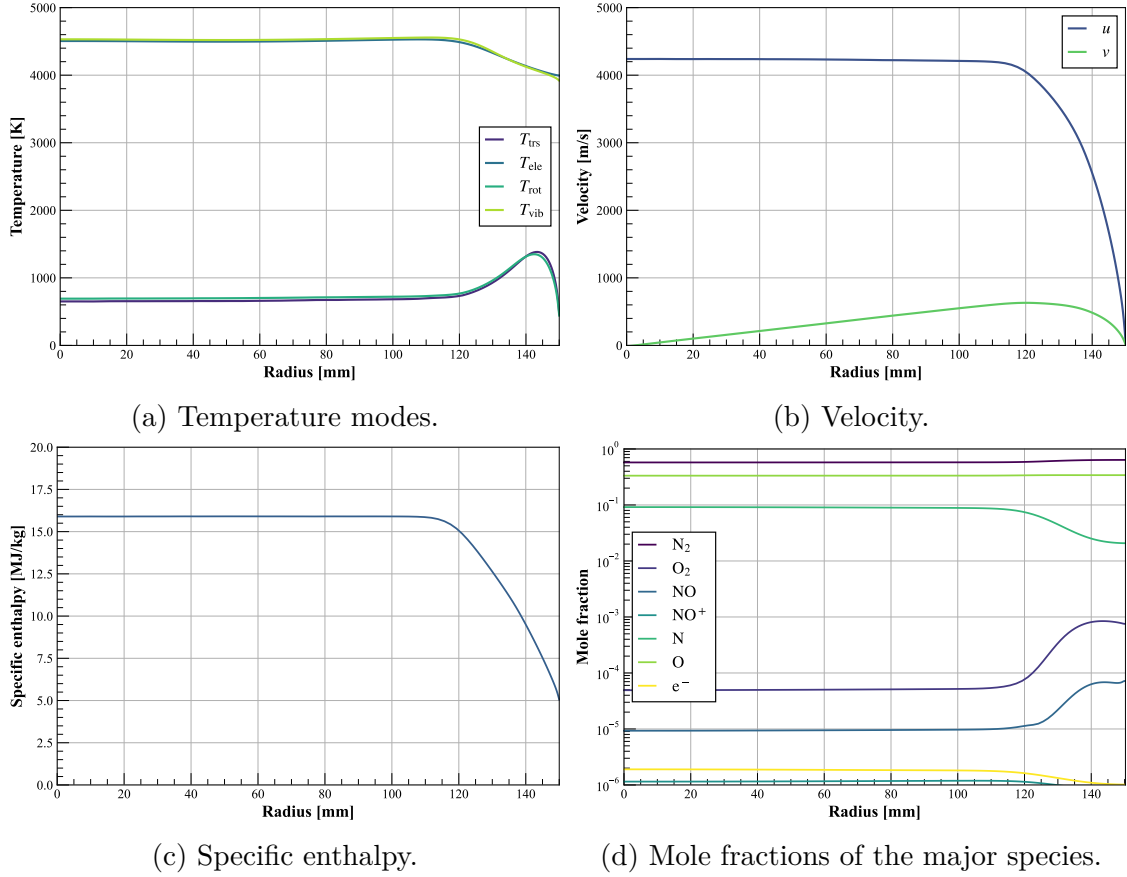


Figure 3.3: Radial profiles extracted at the L3K nozzle exit plane.

recombination with ions in the cooler peripheral region and the associated release of heat. Outside the boundary layer, the thermodynamic state remains nearly uniform across the jet radius.

3.5.4 Flat Plate Model in the L3K Chamber

Figure 3.4 shows the flat-plate model used in this analysis. The geometry is identical to the experimental model employed in previous studies [9, 90, 91]. The plate measures 188 mm in width (z -direction), 282.5 mm in length (x -direction), and 55 mm in thickness (y -direction), and is symmetric with respect to the xy -plane. The leading-edge radius is 10 mm. A square gas outlet of 60 mm per side is centered 40 mm from the model centerline. An antenna is located on the upper surface, aligned with the gas injection centerline, at $x = 340$ mm and $z = 40$ mm. In the simulation, the flat plate was positioned 200 mm downstream of the L3K nozzle exit and aligned with the nozzle centerline.

The computational domain and boundary conditions are shown in Figure 3.5 and Table 3.6. Five types of boundaries are imposed: inflow, outflow, ambient,

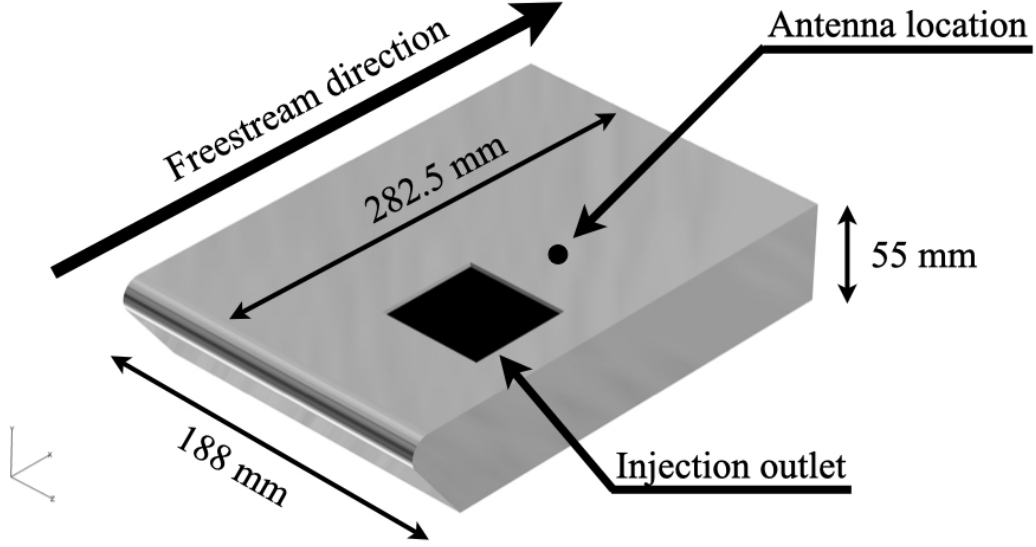


Figure 3.4: Flat-plate model geometry.

wall, and injection. The inflow boundary condition is obtained from the nozzle-exit profiles computed in the preceding analysis. A zero-gradient condition is applied at the outflow plane. At the chamber walls, a no-slip condition is applied with an isothermal temperature of 300 K and a fixed ambient pressure of 10 Pa. At the gas injection outlet, N_2 is injected isothermally at 300 K with a prescribed mass flow rate ranging from 0.00 to 0.80 g/s. The injection pressure is interpolated from the adjacent cell values.

The computational grid is illustrated in Figure 3.5b. The domain is primarily meshed with unstructured tetrahedral elements, while thin prismatic layers are applied near the model surface and inside the injection duct to resolve the boundary layer in detail. The total number of cells is approximately 6.6 million. Grid independence is confirmed using the non-dimensional wall distance y^+ , defined as

$$y^+ = \frac{yu_\tau}{\nu}, \quad (3.32)$$

where y is the wall-normal distance, u_τ is the friction velocity, and ν is the kinematic viscosity. In the present grid, the maximum value of y^+ is approximately unity, ensuring that the near-wall resolution is sufficient for accurate boundary-layer prediction.

The computed inflow and grid settings described above serve as the foundation for the chamber-flow simulations presented in the next section, where the effects of gas injection on the plasma and high-enthalpy flowfields are analyzed in detail.

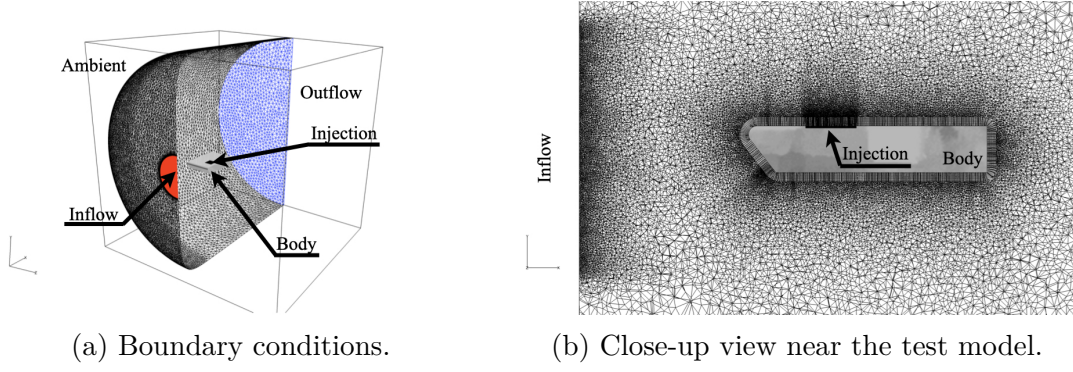


Figure 3.5: Computational domain of the L3K chamber simulation.

Table 3.6: Boundary conditions for the L3K chamber simulation.

Boundary	Description
Inflow	Mapped from 2D nozzle exit solution (rotationally extruded to 3D).
Body	No-slip, isothermal condition at 300 K.
Injection	N ₂ mass-flow injection. Total mass flow rate and temperature (300 K) are given.
Outflow	Zero-gradient condition for all variables.
Ambient	Static pressure 10 Pa.

3.6 Results and Discussion

3.6.1 Plasma Flow

A grid-dependence study was first performed for the plasma-flow simulation to establish the reliability of the numerical solution. Five computational meshes ranging from 6.1 to 11.7 million cells were prepared, differing primarily in the surface resolution and wall-normal spacing on the flat-plate model. Grid sensitivity was assessed by monitoring the electron number density within the bow-shock layer ahead of the plate, because this quantity directly governs RF attenuation and thus is critical for blackout prediction. As shown in Figure 3.6, the variations among the meshes are small, indicating that the solution is essentially grid independent and suitable for use as input to the subsequent CEM analysis.

In the converged solution, most charged species are generated within the strong bow shock forming ahead of the leading edge and are subsequently convected downstream, enveloping the model. Figure 3.7 shows the centerline distributions of temperature and species mole fractions on the upper surface. The gray band marks the gas injection outlet ($0.27 \leq x \leq 0.33$ m), while the bow shock is located approximately 0.05 m upstream of the leading edge. The electron temperature lags

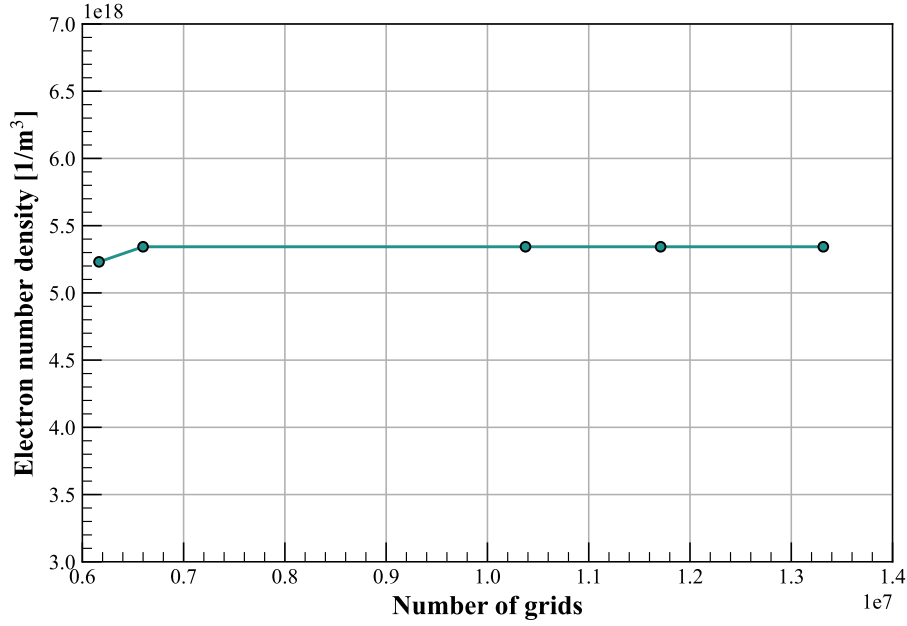


Figure 3.6: Grid dependence of the electron number density.

the translational temperature, exhibiting the characteristic relaxation behavior of nonequilibrium plasmas. Across the shock, T_{trs} rises sharply to about 6,800 K, whereas T_{ele} relaxes more slowly and reaches roughly 5,100 K downstream.

The dominant species in the shock layer are N_2 , O, and N, with secondary contributions from O_2 and NO. Ionized species such as e^- , NO^+ , and O^+ increase markedly under the high post-shock temperatures, whereas chemically unstable N_2^+ and O_2^+ rapidly convert downstream into more stable products. Comparing cases with and without injection reveals pronounced cooling and recombination in the wake: T_{trs} and T_{ele} decrease, the N_2 mole fraction approaches unity, and the mole fractions of ionized species—including electrons—decline. These trends confirm that nitrogen injection promotes recombination and suppresses ionization near the surface, laying the foundation for the blackout-mitigation effect discussed in the following sections.

Figure 3.8 and Figure 3.9 show the Mach-number and translational-temperature distributions in the cross-section at $z = 0.04\text{ m}$, which passes through the gas injection centerline. Results are presented for the baseline case without injection and for the case with nitrogen injection at a mass flow rate of 0.40 g/s. From the left side of the figures, the supersonic arc-heated flow issuing from the nozzle exit can be seen expanding and developing downstream. A detached bow shock is formed in front of the experimental model, and the Mach number at the shock near the leading edge is approximately 8. Because the arc-heated flow accelerates while expanding, the maximum Mach number in the field reaches about 11, which is characteristic

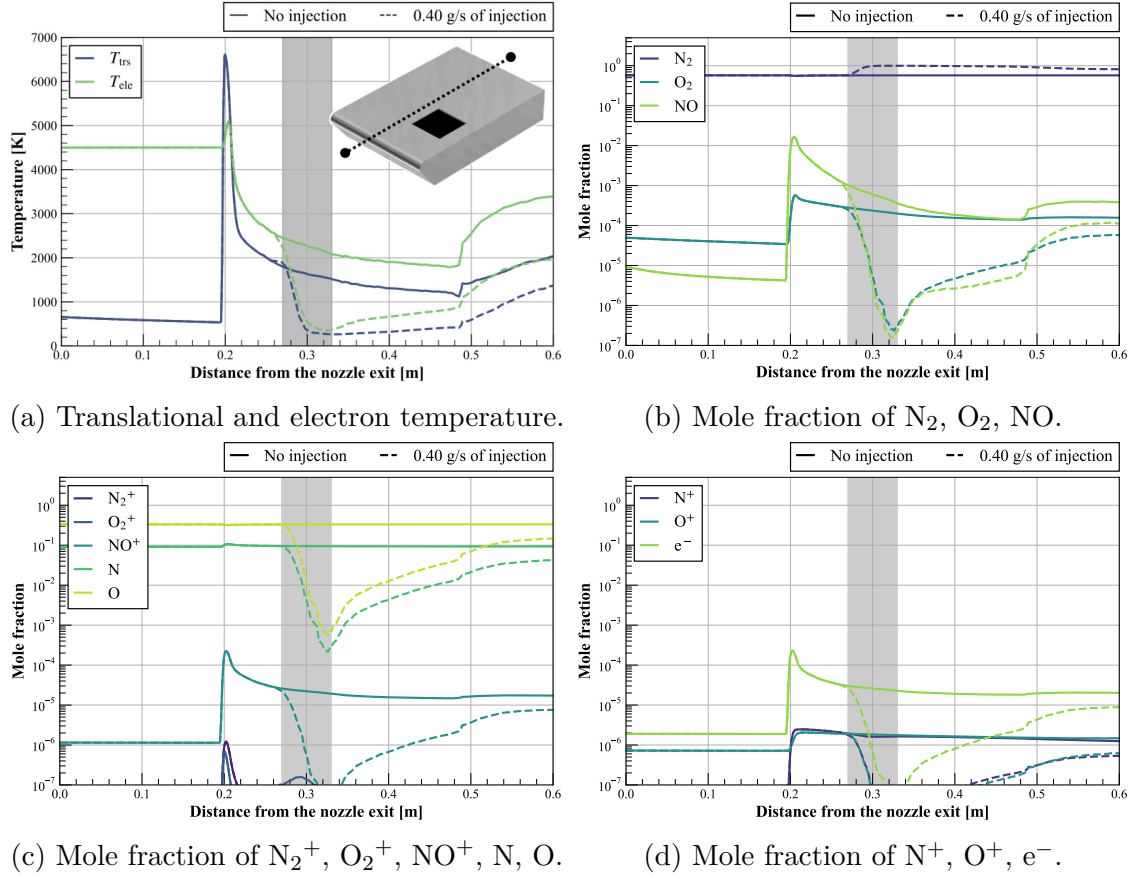


Figure 3.7: Profile along the centerline of the upper surface of the model.

of arc-heated wind-tunnel flows; this peak value is observed above and below the model. Behind the shock, adiabatic compression of the flow causes a sharp rise in temperature, and the maximum temperature near the stagnation point reaches about 8,140 K. The influence of gas injection is most clearly visible in the temperature field. Without injection, the upper surface of the model is immersed in a high-temperature flow of roughly 3,000 K to 4,000 K, whereas injection at 0.40 g/s produces a thin, low-temperature gas layer in the vicinity of the surface.

Figure 3.10 presents a close-up view of the Mach number and density distribution in the vicinity of the injection region. Pronounced gradients of Mach number and density upstream of the injection gas layer indicate the formation of a local shock. In the corresponding density field, the injected gas layer appears as a comparatively high-density region attached to the surface, confirming that the gas film maintains a thermodynamic state distinct from the surrounding, lower-density arc-heated plasma.

Figure 3.11 quantifies the cooling effect of gas injection along the injection centerline ($z = 0.04$ m). Upstream of the injector, the temperature profiles with and without injection are nearly identical, indicating that the freestream conditions are

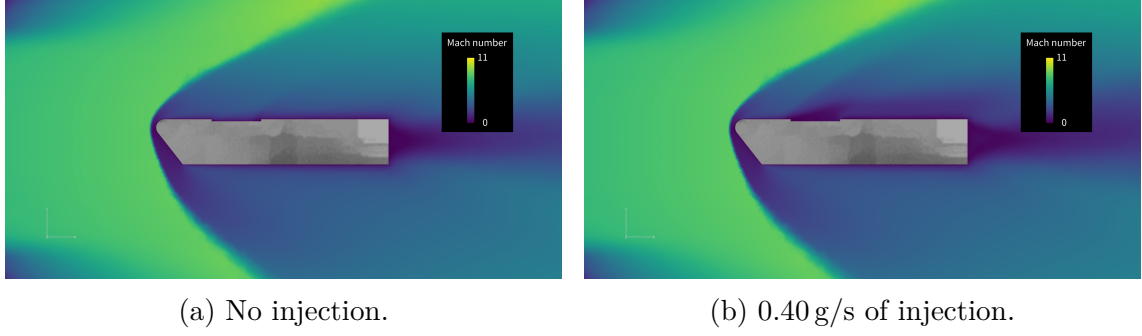


Figure 3.8: Mach number distribution in the center plane of the injection port at $z = 0.04$ m with and without gas injection.

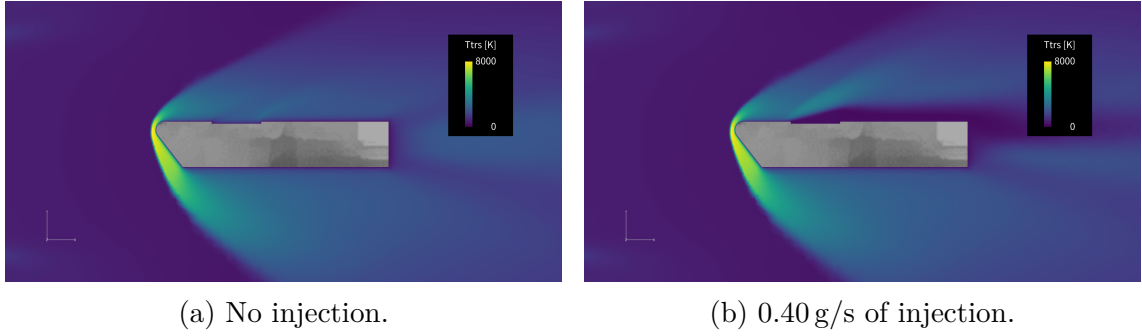


Figure 3.9: Translational temperature distribution in the center plane of the injection port at $z = 0.04$ m with and without gas injection.

unaffected. At the injection location and in the downstream wake, however, the case with nitrogen injection exhibits a pronounced temperature reduction, reflecting the formation of a cooled gas layer attached to the surface. In the injected case, the temperature within this near-wall gas layer remains almost uniform at approximately 240 K, demonstrating that the film stays cold and largely decoupled from the surrounding high-enthalpy flow.

Figure 3.12 and Figure 3.13 further illustrate how the gas film modifies the plasma environment around the plate. In this representation, regions with $n_{\text{ele}} \leq n_{\text{ele,crit}}$ correspond to subcritical plasma through which the 2.4 GHz electromagnetic wave can propagate, whereas regions exceeding $n_{\text{ele,crit}}$ behave as an effective reflecting layer that prevents transmission (see Equation 1.3). Without injection, a high electron density region envelops the entire model body and extends over the antenna region, whereas with injection the near-wall region downstream of the port becomes largely subcritical and the dense plasma is displaced away from the wall. Figure 3.13 shows that this subcritical corridor coincides with a region of nearly pure N_2 along the surface, indicating that the injected gas forms a dense, weakly ionized film that separates the antenna from the surrounding arc-heated plasma.

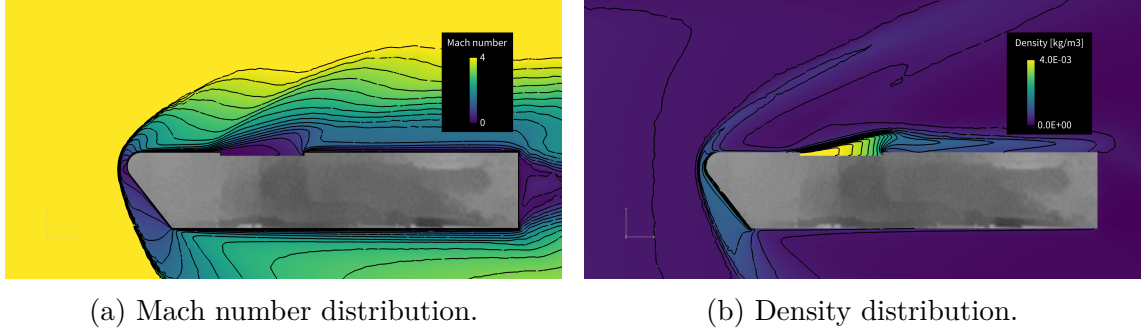


Figure 3.10: Close-up views of Mach number and density distributions in the vicinity of the gas injection region with 0.40 g/s of injection.

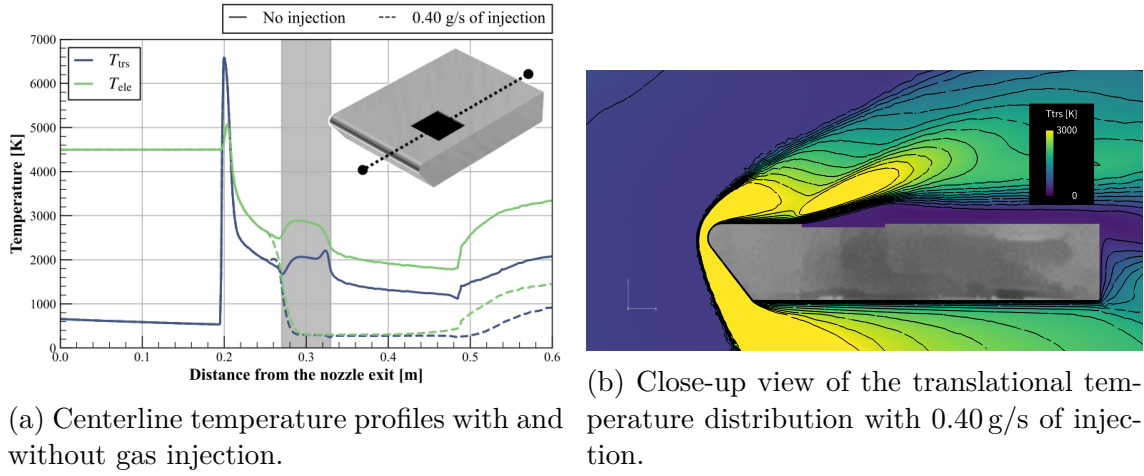


Figure 3.11: Cooling effect of nitrogen injection: centerline temperature profiles and near-wall translational temperature field with 0.40 g/s of injection.

Figure 3.14 visualizes the three-dimensional isosurface of the critical electron number density $n_{\text{ele,crit}}$ corresponding to a 2.4 GHz signal, viewed from downstream toward the test model. By definition, plasma inside this surface has $n_{\text{ele}} \geq n_{\text{ele,crit}}$ and therefore acts as an effective reflecting layer for the incident wave, whereas the surrounding region with $n_{\text{ele}} < n_{\text{ele,crit}}$ remains electromagnetically transparent. In the case without gas injection, the isosurface closely hugs the entire plate, indicating that the model is fully embedded in a supercritical plasma sheath and that no continuous propagation path exists. When nitrogen is injected, the isosurface is displaced away from the surface and a continuous opening appears along the plate, forming a low-electron-density corridor that connects the antenna region to the far field. This topological change demonstrates that blackout mitigation is achieved not by globally reducing the plasma density, but by sculpting a localized subcritical channel along the gas film through which electromagnetic waves can escape.

Figure 3.15 compares electron number density profiles along the model centerline

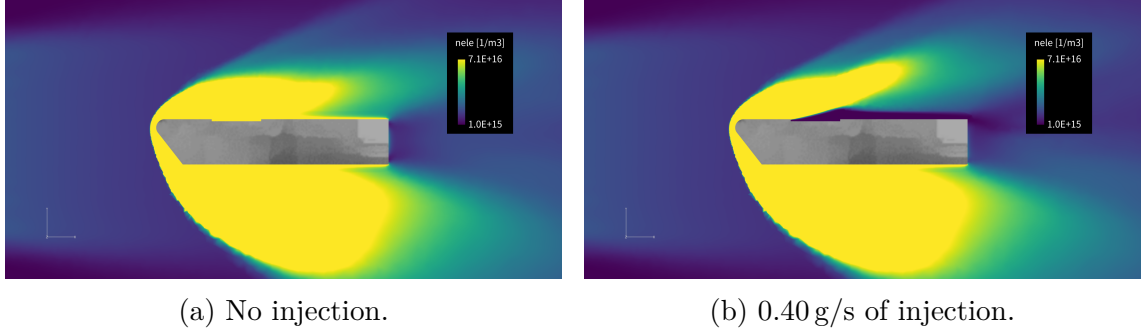


Figure 3.12: Electron number density distribution in the injection center plane at $z = 0.04$ m with and without nitrogen injection.

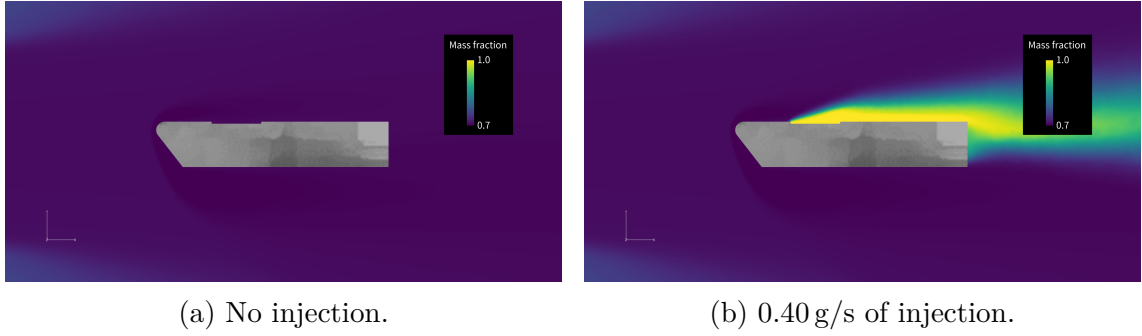


Figure 3.13: Mass fraction distribution of N_2 in the injection center plane at $z = 0.04$ m with and without nitrogen injection.

($z = 0.0$ m) and along the injection centerline ($z = 0.04$ m), for cases with and without gas injection. Both lines exhibit a sharp peak near the leading edge due to intense ionization in the bow-shock layer, followed by a gradual decay downstream. A slight secondary rise appears near the downstream edge of the injection region. At this location, the main flow collides with the geometric step of the (inactive) injection port; in the absence of injected gas, this local compression causes a slight increase in electron density. With nitrogen injection, the near-wall electron density along the injection centerline drops below the critical value $n_{ele,crit}$ over a broad wake region, while the rise associated with the step is substantially attenuated. As the gas mass flow rate increases, both the magnitude of the electron number density reduction and the spatial extent of the subcritical region grow, indicating that stronger injection more effectively clears a propagation corridor around the antenna. The profiles taken 40 mm apart in the spanwise direction demonstrate that this reduction is not confined strictly to the injector footprint: the gas film spreads laterally and establishes an extended subcritical corridor around the antenna location, reinforcing the blackout mitigation mechanism inferred from the three-dimensional fields.

Figure 3.16 quantifies the gas-film thickness at the antenna location ($x = 0.35$ m)

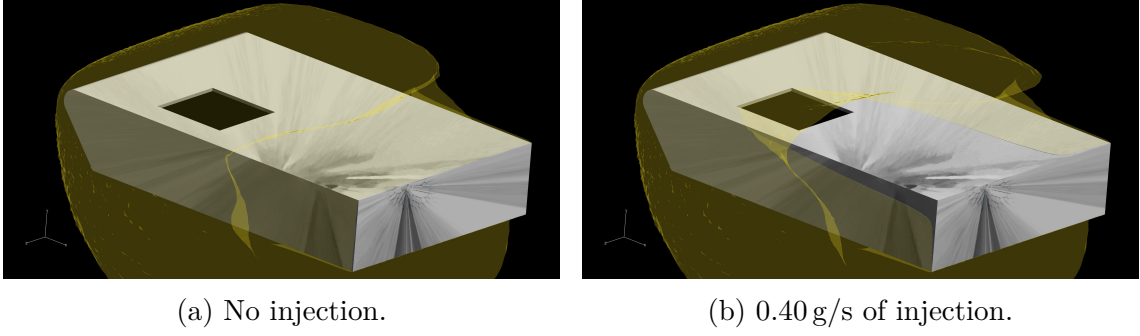


Figure 3.14: Isosurface of the critical electron number density $n_{\text{ele,crit}}$ for a 2.4 GHz signal, viewed from downstream of the flat-plate model.

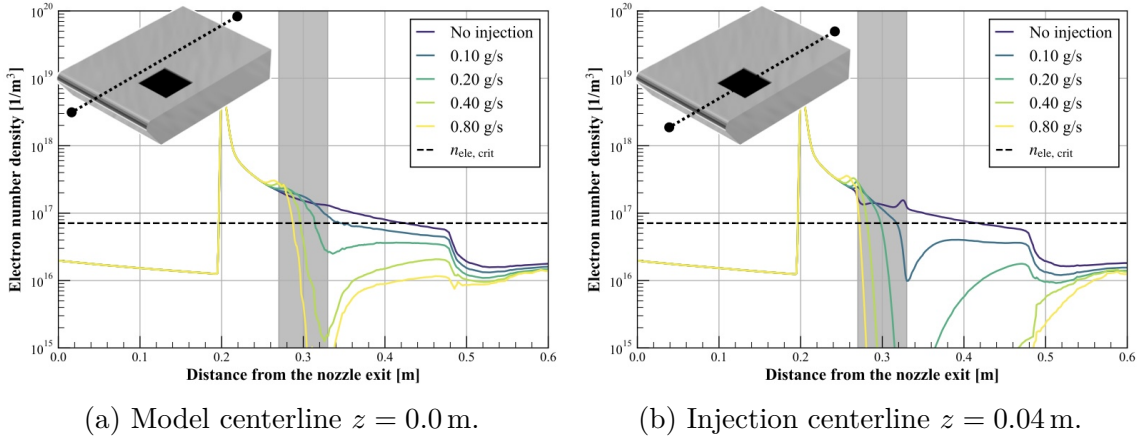


Figure 3.15: Electron number density profiles along the model centerline and the injection centerline on the upper surface, with and without nitrogen injection.

as a function of mass flow rate. The film thickens with increasing injection rate: doubling the injection rate from 0.20 g/s to 0.40 g/s raises the film height by about 0.02 m, whereas further increases yield diminishing returns, indicating that the film growth becomes limited by the surrounding flow. At 0.10 g/s, the film remains too thin to reach the antenna and cannot fully shield the propagation path. Given that the wavelength of a 2.4 GHz signal is 0.0125 m, a film thickness on the order of the wavelength is sufficient to maintain a continuous subcritical corridor; in the present configuration, this condition is satisfied for gas injection rates of 0.20 g/s and above.

3.6.2 Electromagnetic Wave Propagation

We evaluate how the injected gas modifies radio-wave transmission through the plasma using the frequency-dependent FDTD (FD2TD) framework introduced earlier. In this formulation, the dispersive plasma response is represented by a Drude-type complex permittivity parameterized by the local electron number density and the effective collision frequency. Consistent with the cutoff criterion in Equation 1.3,

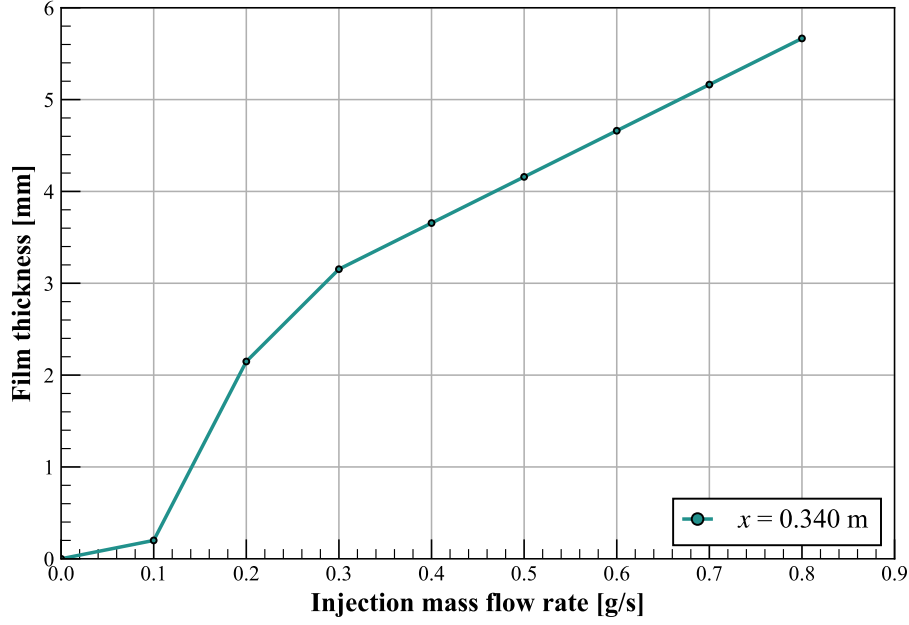


Figure 3.16: Thickness of the gas film ($x = 0.340$ m).

propagation is permitted where $n_{\text{ele}} < n_{\text{ele,crit}}$ and becomes strongly attenuated otherwise, while electron – neutral and electron – ion collisions introduce additional ohmic absorption.

The FDTD time step is set to $\Delta t = 7.5 \times 10^{-13}$ s, which is sufficiently small relative to the characteristic fluid-dynamic and chemical time scales so that the plasma properties can be regarded as frozen during the electromagnetic update. The computational domain is discretized using $250 \times 400 \times 400$ Cartesian cells with uniform spacing $\Delta x = \Delta y = \Delta z = 2.0 \times 10^{-3}$ m; its extent is chosen to fully encompass both the flat-plate model and the surrounding region where the local electron number density exceeds the critical value $n_{\text{ele,crit}}$ at 2.4 GHz. With this resolution, the Courant number based on the speed of light is approximately 0.195, well within the stability limit of the scheme. The antenna is mounted on the model surface immediately downstream of the gas injection port, at $(x, z) = (0.35 \text{ m}, 0.04 \text{ m})$.

Figure 3.17 shows the logarithm of the electric-field magnitude on the $z = 0.40$ m cross-section for three representative cases: vacuum (no surrounding plasma), plasma with gas injection at a mass flow rate of 0.40 g/s, and plasma with a mass flow rate of 0.80 g/s at $t = 1.5 \times 10^{-7}$ s. In vacuum (Figure 3.17a), the wave radiates symmetrically away from the antenna with negligible attenuation. When the plasma flow is imposed, regions where $n_{\text{ele}} > n_{\text{ele,crit}}$ behave as an opaque layer and strongly reduce the field amplitude near the surface. For the case with 0.40 g/s of gas injection (Figure 3.17b), the gas film creates a contiguous low-electron-density corridor along the plate, through which the field can penetrate downstream and

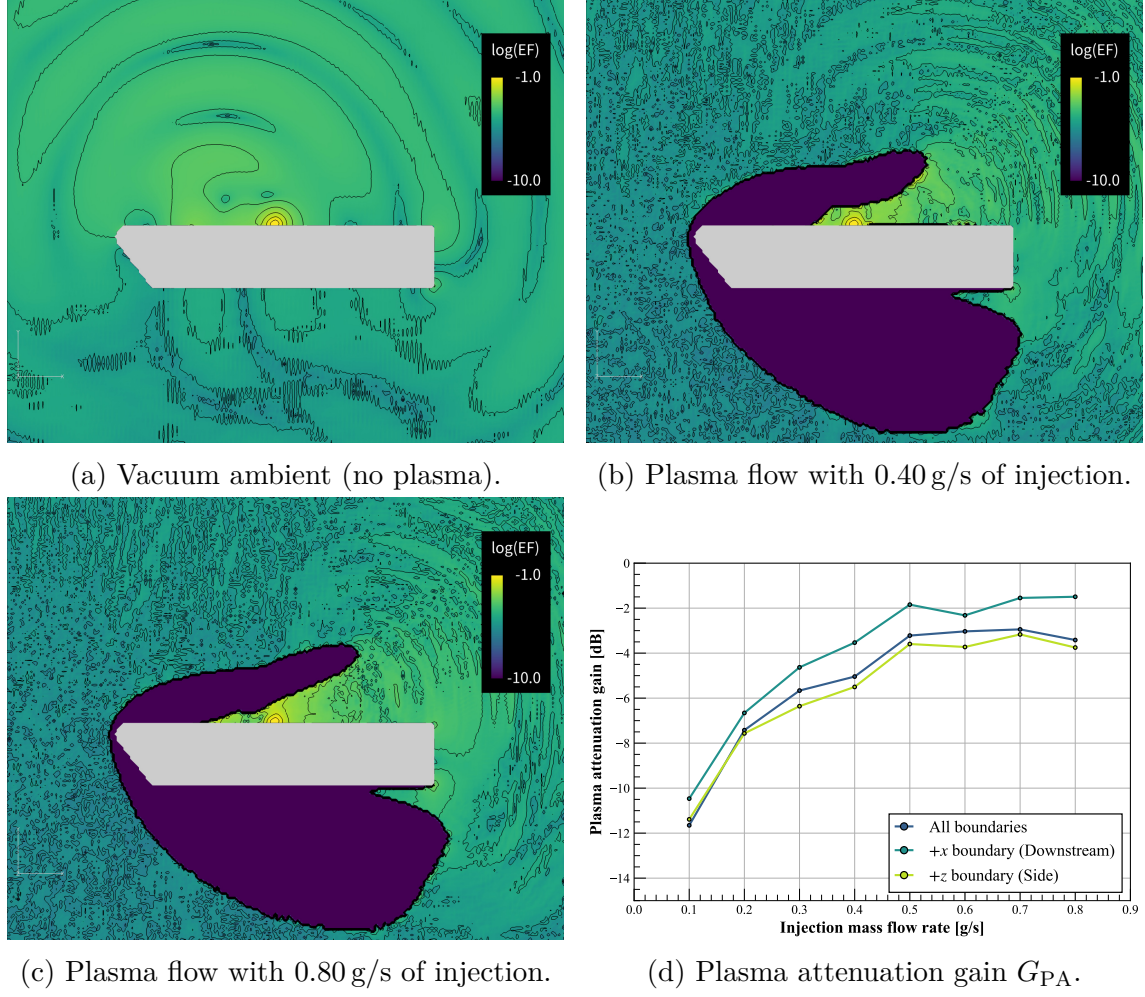


Figure 3.17: Electric-field magnitude on the $z = 0.40$ m plane around the flat-plate model (left and top-right) and plasma attenuation gain G_{PA} as a function of nitrogen mass flow rate (bottom-right).

recover strength in the far field. The 0.80 g/s case (Figure 3.17c) exhibits a qualitatively similar pattern with only modest additional improvement, indicating that once a continuous transmission corridor has formed, further increases in mass flow provide diminishing benefit.

To quantify the net impact on link communication performance, a plasma attenuation gain is defined as follows:

$$G_{PA} = 10 \log_{10} \left(\frac{\sum |\mathbf{E}_{bd}|^2}{\sum |\mathbf{E}_{bd, vacuum}|^2} \right), \quad (3.33)$$

where $\mathbf{E}_{bd}$ and $\mathbf{E}_{bd, vacuum}$ denote the electric-field magnitudes on the outer absorbing boundaries with and without plasma, respectively. A value of $G_{PA} \approx 0$ dB corresponds to negligible additional attenuation relative to vacuum, whereas large

negative values indicate strong plasma-induced loss.

As summarized in Figure 3.17d, G_{PA} improves (i.e., attenuation decreases) monotonically with nitrogen mass flow rate up to about 0.50 g/s, beyond which the curve flattens. This behavior is consistent with the structural changes in Figure 3.14 and the film-thickness scaling in Figure 3.16: once the gas film is thick enough to form a continuous subcritical transmission corridor (around 0.20 g/s at 2.4 GHz), further increases primarily affect local loss mechanisms within the corridor—ohmic absorption in the weakly ionized film and scattering at the film-plasma interface—rather than the presence or absence of cutoff. These findings indicate that moderate gas injection rates are sufficient to recover a substantial fraction of the vacuum link budget without incurring excessive mass-flow requirements.

3.7 Conclusions

This chapter has developed and applied a multiphysics numerical framework to assess gas-film injection as a blackout-mitigation strategy in a reentry-relevant arc-heated wind-tunnel environment. High-enthalpy plasma flows in the DLR L3K facility were simulated using a thermochemical nonequilibrium CFD solver, and a coupled frequency-dependent FDTD (FD2TD) model was employed to resolve electromagnetic-wave propagation through the resulting plasma field. A flat-plate model identical to that used in previous L3K experiments was selected as the test article, and simulations were conducted for nitrogen injection mass flow rates ranging from 0.00 to 0.80 g/s.

A separate axisymmetric nozzle-flow analysis was first performed to determine the inflow conditions at the chamber entrance. The nozzle calculations revealed pronounced nonequilibrium behavior and provided radial profiles of velocity, temperature, and composition at the exit plane, which were then mapped onto the three-dimensional chamber domain. The chamber simulations showed that, without gas injection, a dense plasma sheath forms around the flat-plate model and prevents radio-wave propagation, thereby reproducing the blackout phenomenon. When nitrogen is injected from the surface, an insulating gas-film layer develops along the plate and extends into the wake region. Within this layer, both temperature and electron number density are substantially reduced, forming a localized low-density corridor embedded in the surrounding high-density plasma. The analysis of electron-density distributions and isosurfaces of the critical density confirmed that blackout mitigation is achieved not by globally thinning the plasma, but by carving out a continuous subcritical channel that connects the antenna to the far field.

The FD2TD simulations further demonstrated that, in the presence of this gas film, electromagnetic waves at 2.4 GHz can propagate along the near-wall corridor and reach the chamber boundaries with significantly reduced attenuation compared with the no-injection case. A plasma attenuation gain G_{PA} was introduced to quantify the net impact on link performance, revealing that attenuation decreases monotonically with increasing injection rate up to about 0.50 g/s, beyond which additional benefit saturates. This trend is consistent with the measured film thickness and with the requirement that the gas layer be at least on the order of the RF wavelength to sustain a continuous transmission corridor. Collectively, these findings confirm that gas-film injection can simultaneously mitigate communication blackout by establishing a low-electron-density propagation path and reduce localized aerodynamic heating by maintaining a cold, dense layer along the surface. The coupled CFD – FD2TD framework and associated metrics therefore provide quantitative guidance for selecting injection conditions that balance blackout mitigation against mass-flow constraints.

At the same time, the present study is purely numerical and relies on modeling assumptions regarding chemistry, transport, and facility inflow conditions. To demonstrate that gas-film blackout mitigation is achievable in actual arc-heated plasmas and to validate the numerical framework under realistic experimental conditions, dedicated wind-tunnel tests are required. Building on the feasibility and mechanism clarified here, the next chapter moves to experimental verification in the JAXA/ISAS 1 MW arc-heated wind tunnel, where the mitigation effect is demonstrated using an instrumented model and the accompanying CFD is used to visualize the complex jet/film structure and plasma flowfield.

Chapter 4

Demonstration in the ISAS

Arc-Heated Wind Tunnel

Building on the numerical demonstration of gas-film blackout mitigation in the large-scale DLR L3K arc-heated wind tunnel presented in the previous chapter, this chapter provides an experimental validation of the concept and reproduces the observed phenomena through complementary computational fluid dynamics (CFD) analysis. Experiments were conducted in the 1 MW arc-heated wind tunnel at JAXA/ISAS under reentry-relevant enthalpy conditions. The tests demonstrated that nitrogen injection effectively restored 2.4 GHz communication through a plasma that would otherwise cause complete signal loss. Schlieren imaging captured the expansion and structure of the injected jet, confirming the formation of a near-wall insulating gas film. Subsequent CFD simulations, performed under identical wind-tunnel conditions, reproduced the interaction between the injection and freestream and confirmed a low-electron-density region along the surface.

Together, these experimental and numerical results provide direct, end-to-end evidence that gas-film injection can mitigate communication blackout in high-enthalpy plasma flows, and they supply the first validation step between the purely numerical feasibility study of chapter 3 and the larger-scale investigations presented in later chapters.

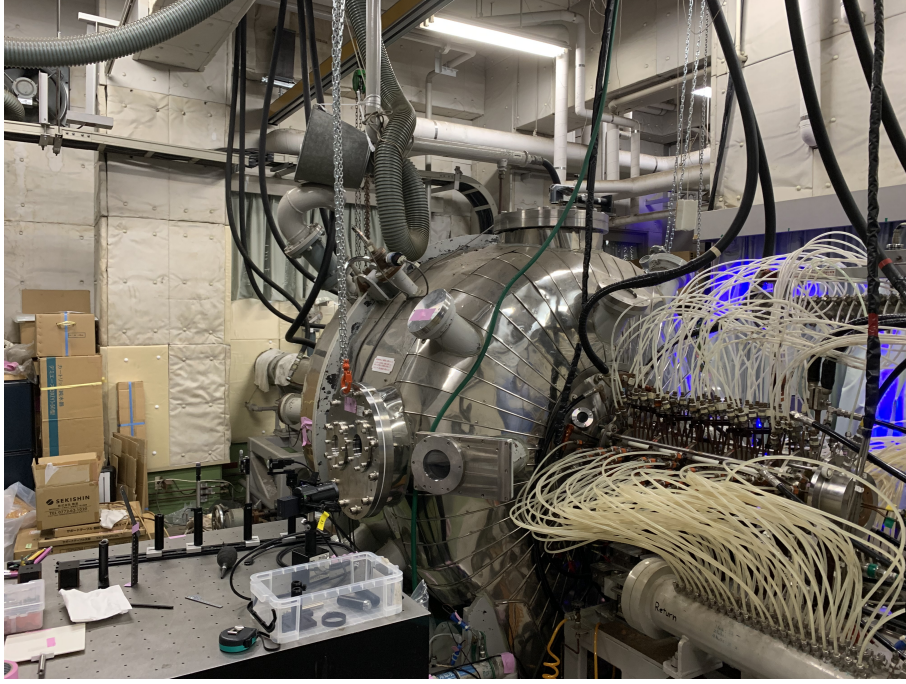


Figure 4.1: 1 MW arc-heated wind tunnel with a constrictor-type heater at ISAS.

Table 4.1: Operating conditions of the ISAS 1 MW arc-heated wind tunnel.

Test gas	Air
Input current [A]	450
Mass flow rate [g/s]	18
Total temperature of inlet gas [K]	300

4.1 Experimental Setup

4.1.1 Communication Tests

To experimentally demonstrate the mitigation of communication blackout through gas injection, a series of communication tests were conducted in the 1 MW arc-heated wind tunnel operated by JAXA/ISAS, as shown in Figure 4.1. The facility employs a segmented arc heater: the test gas is electrically heated between the anode and cathode and then expands through the nozzle throat. In the present experiment, a nozzle with a diameter of 25 mm was used. The operating conditions of the facility are summarized in Table 4.1; these parameters are also adopted for the numerical wind-tunnel analysis described in section 3.2. Air at 300 K was used as the test gas. Under similar operating conditions, the arc-heated flow inside the nozzle has been numerically reproduced in detail by Takahashi et al. [92, 93], providing a basis for the inflow specification used in the present CFD analysis.

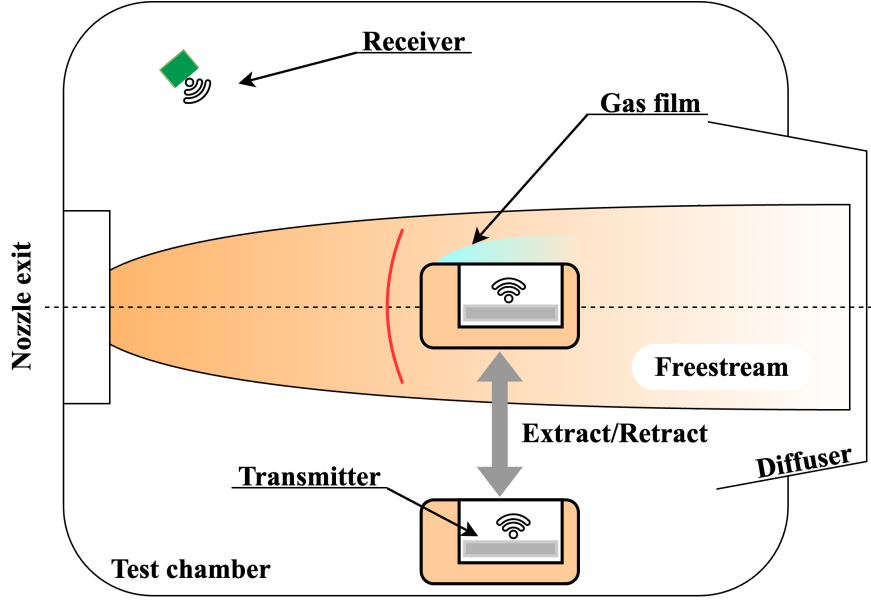


Figure 4.2: Schematic of the experiment in the ISAS 1 MW arc-heated wind tunnel.

Prior to initiating the arc discharge, the vacuum chamber was evacuated to approximately 10 Pa. During operation, the inflow gas was electrically heated by the arc discharge and expanded through the nozzle into the chamber. The average chamber pressure was maintained at around 160 Pa to reproduce conditions representative of atmospheric reentry.

Figure 4.2 schematically illustrates the configuration of the test chamber during the experiment, and Figure 4.3 shows photographs of the interior of the facility. The experimental model equipped with a communication device was placed outside the flow initially. Once the plasma freestream stabilized, the model was inserted into the flow and positioned along the flow centerline for a few seconds. A receiver placed outside the chamber continuously recorded the received signal intensity, and the transmitter and receiver were arranged face-to-face to enable direct line-of-sight communication.

The arc-heated plasma flow, discharged from the nozzle into the low-pressure chamber, generated a detached shock wave in front of the experimental model. Behind this shock, gas molecules and atoms were ionized owing to the high temperature, leading to a dense plasma region that blocked electromagnetic-wave transmission. As a result, communication between the model inside the chamber and the receiver outside the chamber was completely interrupted, reproducing the communication blackout phenomenon. Under these conditions, nitrogen gas was injected from the surface of the experimental model, forming a thin, insulating film along the wall. An improvement in received signal intensity during gas injection demonstrated

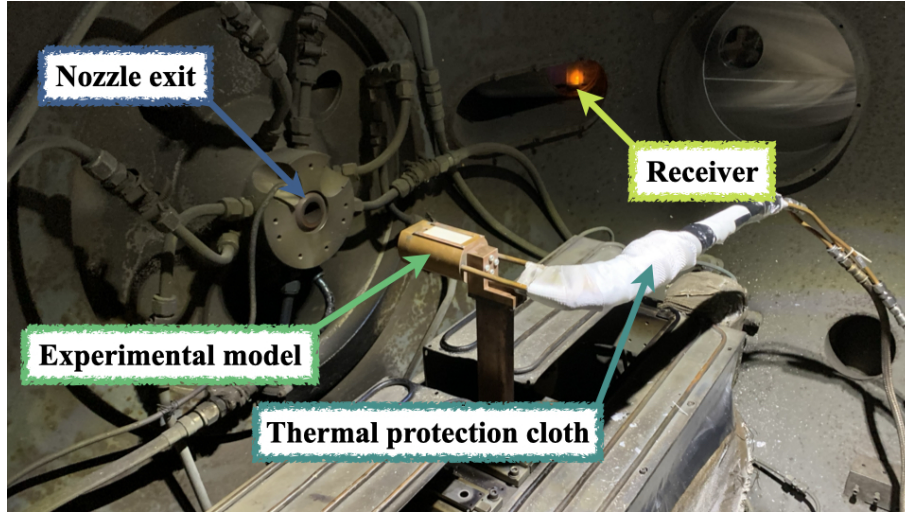


Figure 4.3: Overview photograph of the experimental setup inside the test chamber.

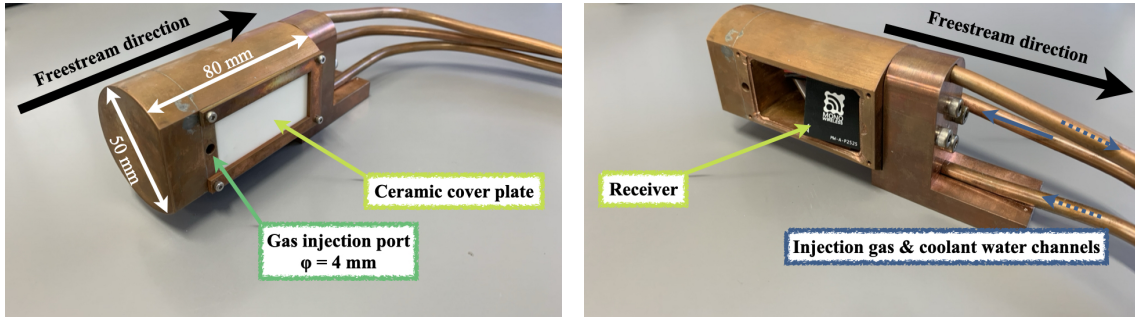


Figure 4.4: Overview of the test model, showing the cylindrical geometry, gas injection port, and transmitter housing.

the mitigation of blackout through the creation of a gas-film layer. The distance between the nozzle exit and the model center was fixed at 150 mm.

A similar experimental configuration has been previously employed in studies on blackout mitigation based on catalytic recombination of ionized gases [63]. The present work, in contrast, focuses on the effect of non-reactive gas injection as a purely physical shielding mechanism.

The experimental model used in the communication test is shown in Figure 4.4. It consists of a cylindrical copper body with a diameter of 50 mm and a length of 80 mm. Cooling water was circulated inside the cylinder to withstand the high thermal loads generated during plasma exposure. The transmitter compartment was thermally protected by a ceramic plate and insulation material. The gas-injection port was located on the front face of the communication housing, allowing room-temperature nitrogen to be supplied through an internal feed line. The flow rate was regulated by a mass flow controller located outside the facility and delivered to the model via stainless-steel tubing. The transmitter operated at a frequency of

2.4 GHz, communicating directly with the receiver positioned outside the vacuum chamber.

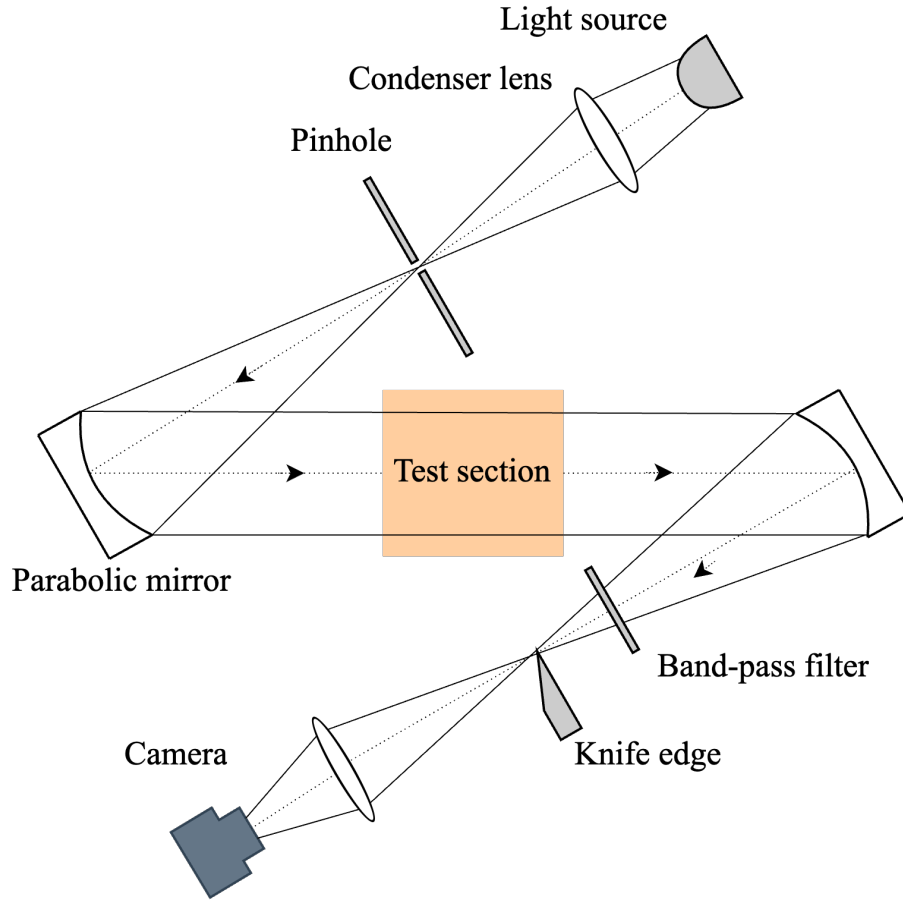


Figure 4.5: Schematic of the schlieren optical system.

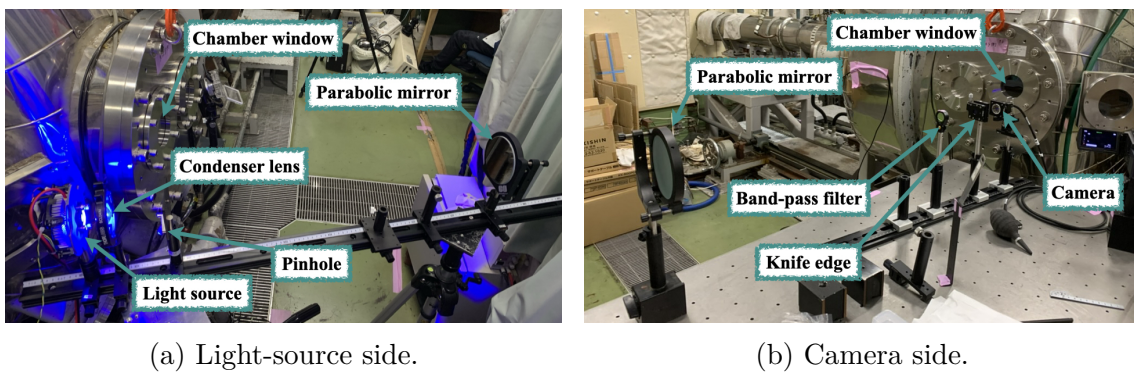


Figure 4.6: Installation of the schlieren optical system around the ISAS wind tunnel.

4.1.2 Schlieren Visualization

This subsection documents schlieren visualization performed to qualitatively map the injected jet and gas film and their interaction with the arc-heated flow in the

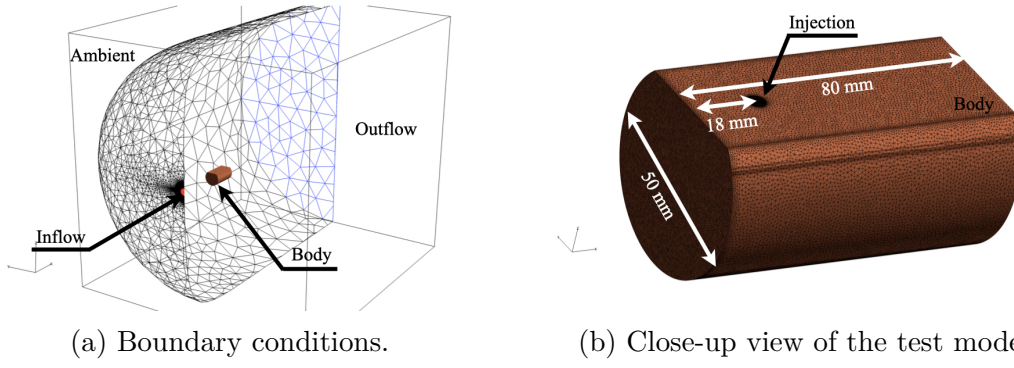


Figure 4.7: Computational domain and boundary conditions for the plasma-flow simulation in the ISAS 1 MW arc-heated chamber.

vacuum chamber. The method highlights line-of-sight density gradients near the gas-injection region and provides visual evidence of shock structures, shear layers, and the near-wall gas layer.

A collimated beam produced by a blue LED light source with a center wavelength of 455 nm was focused by a convex lens and passed through a pinhole to homogenize intensity. The beam was reflected by a concave mirror to form parallel rays, which traversed the test section where refraction occurred due to local density variations. After passing through the flowfield, the light was refocused using a parabolic mirror and partially blocked by a vertically oriented knife edge to detect horizontal density gradients along the main flow direction. A band-pass filter was placed in front of the camera to suppress plasma emission. The optical setup is illustrated in Figure 4.5, and its installation around the facility is shown in Figure 4.6.

Two parabolic mirrors with a diameter of 102 mm and a focal length of 700 mm were used. A 12-bit gray-scale CMOS camera recorded schlieren images at 60 fps for 10 s. The field of view was centered 180 mm downstream of the nozzle exit and adjusted to capture the region above the gas jet during exposure to the high-enthalpy flow. Images were normalized using background frames (no injection) to enhance contrast.

4.2 Numerical Model

Numerical simulations were performed to reproduce the plasma flow inside the 1 MW arc-heated wind tunnel at JAXA/ISAS [94]. This section summarizes the physical and numerical models employed. The governing equations and thermochemical models follow the high-enthalpy solver RG-FaSTAR [84].

Table 4.2: Boundary conditions for the ISAS 1 MW arc-heated chamber simulation.

Boundary	Description
Ambient	Fixed pressure of 10 Pa, temperature of 300 K, and velocity of 0 m/s.
Inflow	Mapped from a two-dimensional nozzle-exit solution (rotationally extruded to 3D).
Body & ceramic plate	No-slip, isothermal condition at 300 K.
Injection	N ₂ injected at fixed temperature at 300 K and prescribed mass flow rate; static pressure interpolated from neighboring cells.
Outflow	Zero-gradient condition for all variables.

4.2.1 Plasma Flow Simulation

The thermochemical nonequilibrium CFD formulation introduced in section 3.2 is adopted here. Only the geometry, inflow conditions, and wall boundary conditions differ from the L3K configuration. Figure 4.7 shows the computational domain and boundary conditions for the ISAS chamber simulation, and Figure 4.7b illustrates the mesh refinement near the experimental model and gas-injection region, which is required to resolve the bow shock, jet structure, and wall heat flux with sufficient accuracy.

4.2.2 Calculation Conditions

The ISAS 1 MW chamber domain is illustrated in Figure 4.7. An unstructured mesh is employed throughout, with local hexahedral refinement near the model surface (wall boundary) and the nozzle inlet (inflow boundary). The numerical model reproduces the experimental configuration: a cylindrical copper body with a diameter of 50 mm and a length of 80 mm. A 4 mm-diameter gas outlet is installed along the model centerline, located 18 mm downstream of the front face and 168 mm from the nozzle exit. The model is positioned on the nozzle centerline and offset by 3 mm from the nozzle exit plane. The injection passage and internal cavity are explicitly meshed using hexahedral cells. The total number of computational cells is approximately 2.79×10^7 .

The boundary conditions are summarized in Table 4.2. The inflow boundary condition is taken from Takahashi et al. [95], who conducted a two-dimensional, axisymmetric nozzle simulation using a four-temperature model with eleven air species. Figure 4.8 presents radial profiles of temperature, axial velocity ($z = 0$ m), total enthalpy, and mole fractions of the major species at the nozzle exit.

A large temperature gap between the translational and electron modes indicates

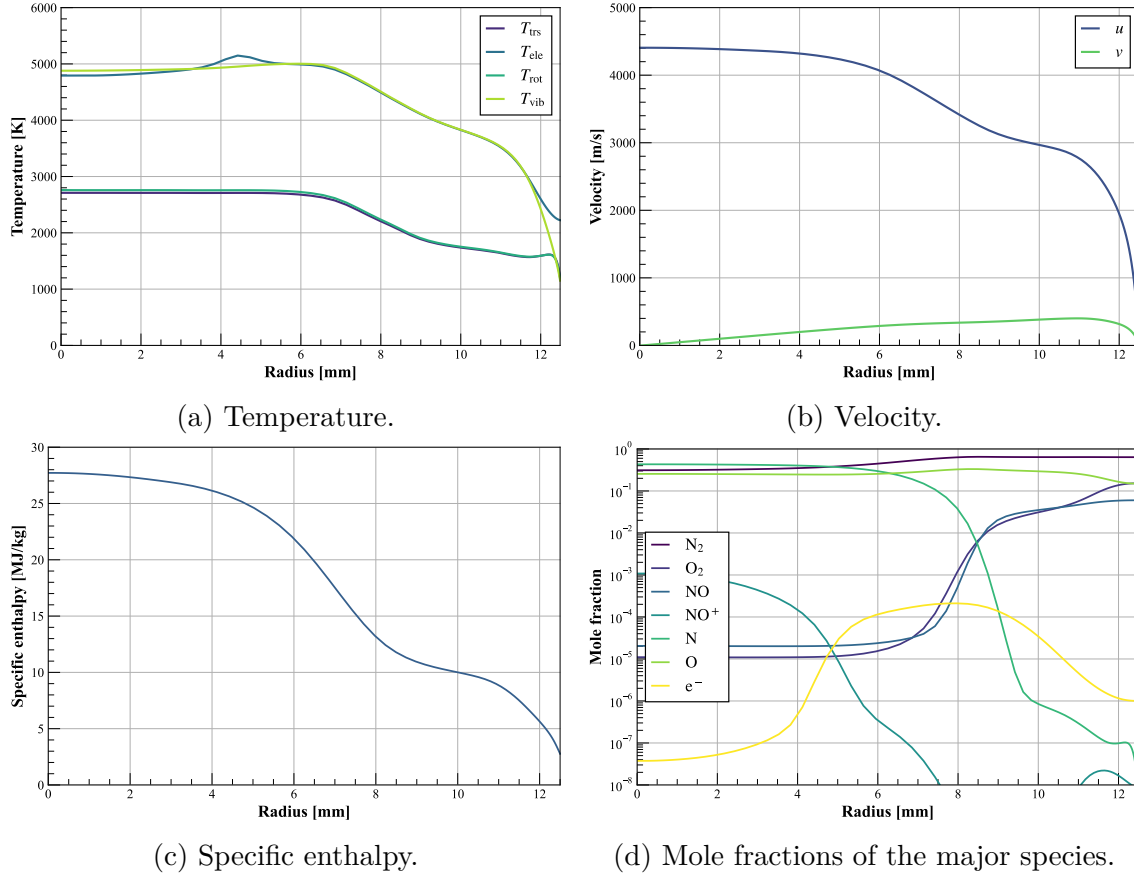


Figure 4.8: Radial profiles extracted at the ISAS nozzle exit.

strong thermochemical nonequilibrium. Because the translational and rotational temperatures and the electron and vibrational temperatures are nearly equilibrated, a two-temperature model can be adopted for the chamber simulations. The flow is predominantly axial, with negligible radial velocity components. In Figure 4.8d, the dominant species are N_2 , N , and O , with ionized components concentrated near the nozzle centerline, consistent with the locally elevated temperature and enthalpy.

At the chamber outlet boundary, all variables are extrapolated with zero gradients. The model surfaces are treated as isothermal, non-catalytic, and no-slip at 300 K. The ambient region is set to a static pressure of 10 Pa, a temperature of 300 K, and a velocity of 0 m/s. The injected gas is pure N_2 at 300 K, supplied at four mass flow rates: 0.25, 0.50, 0.75, and 1.00 g/s.

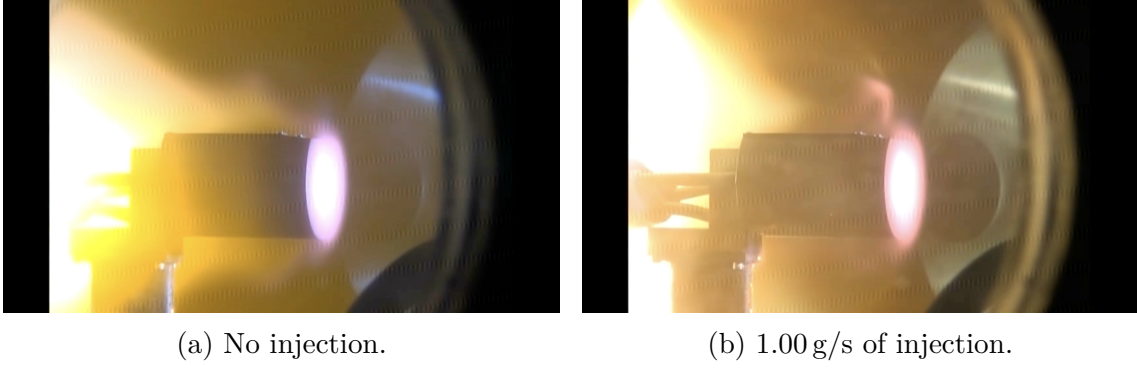


Figure 4.9: Experimental model positioned in the arc-heated plasma flow.

4.3 Results and Discussion

4.3.1 Communication Blackout Mitigation

Communication tests were conducted in the ISAS 1 MW arc-heated wind tunnel to evaluate how surface gas injection mitigates communication blackout. The experimental procedure was as follows. The arc heater first generated a steady plasma flow while the model equipped with a communication device was kept off the nozzle centerline. After stabilizing the nitrogen mass flow rate at the prescribed value, the model was translated onto the nozzle axis and positioned 150 mm downstream of the nozzle exit. The on-axis exposure lasted approximately 4 s, after which the model was retracted. Throughout each run, the received signal strength indicator (RSSI), expressed in dBm, was recorded at the receiver.

Photographs of the model during operation are shown in Figure 4.9. A luminous emission from the forebody indicates a detached bow shock and strong surface heating. When gas injection was activated, an additional emission region appeared near the upper edge of the injection port. This feature is consistent with compression heating in front of the gas film layer, where the injected jet expands into the low-pressure chamber and interacts with the oncoming plasma freestream.

Figure 4.10 presents time histories of the RSSI for different injection mass flow rates. In these plots, the gray shaded intervals indicate periods when the model was outside the freestream. The blue curve shows the commanded injection mass flow rate, which remained constant during the exposure interval. The transmitter operated at a data-sampling rate of 10 Hz, and the pre-exposure communication level was stable in all cases, indicating good repeatability of the initial test conditions.

Without gas injection, the RSSI dropped to zero during exposure, reproducing a full communication blackout in the arc-heated wind tunnel. With gas injection, par-

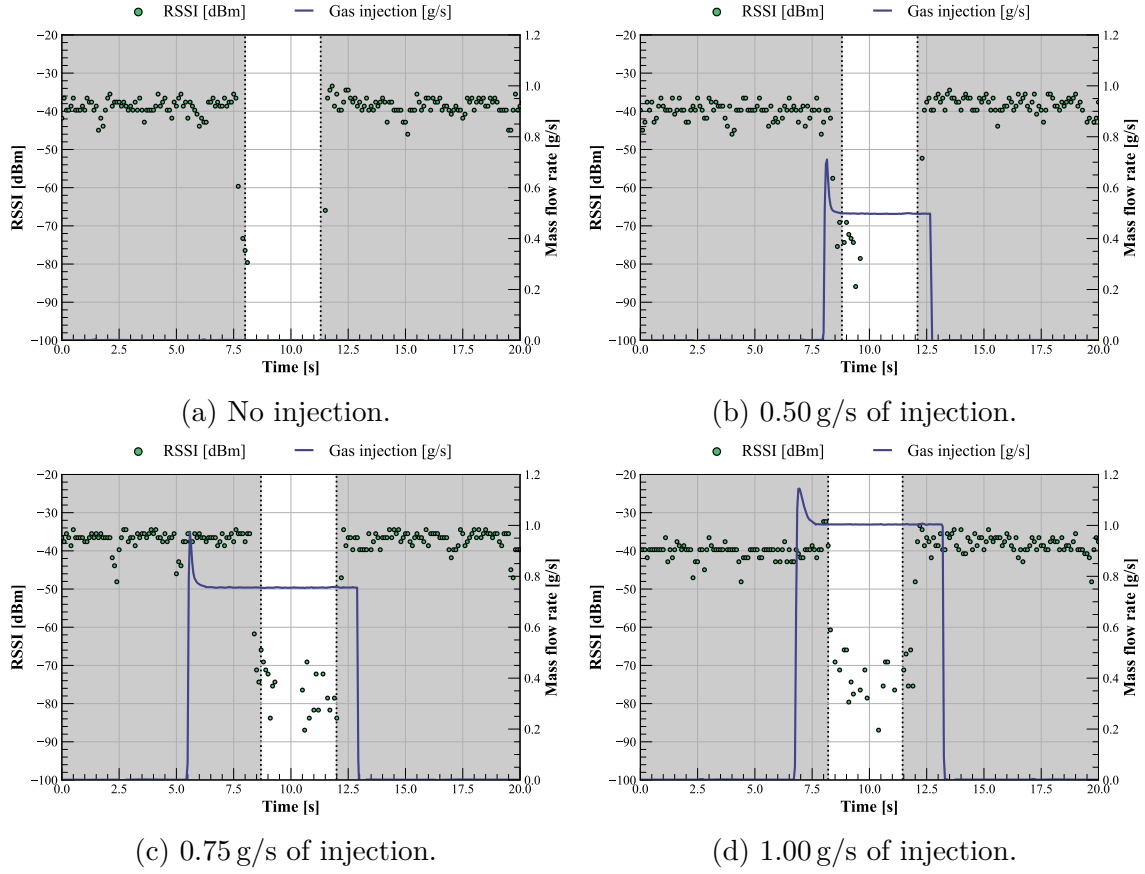


Figure 4.10: Time histories of RSSI for different injection mass flow rates.

tial communication was recovered; the recovery level increased monotonically with the injection mass flow rate. After the model was retracted, nominal communication resumed immediately. These trends confirm that a surface gas film can counter plasma-induced attenuation under high-enthalpy facility conditions.

The background chamber pressure (< 100 Pa) corresponds to the rarefied regime where communication blackout typically occurs in flight at altitudes of 50 km to 80 km. The observed dependence of communication recovery on injection mass flow rate can be rationalized by the jet-to-plasma momentum-flux ratio: larger injection rates thicken and expand the near-wall gas film, lowering the local electron number density and opening a wider transmission corridor. At sufficiently high injection rates, diminishing returns are expected once a contiguous corridor spans the antenna location. Optimizing the injection angle to promote surface attachment would further enhance the continuity and coverage of the film.

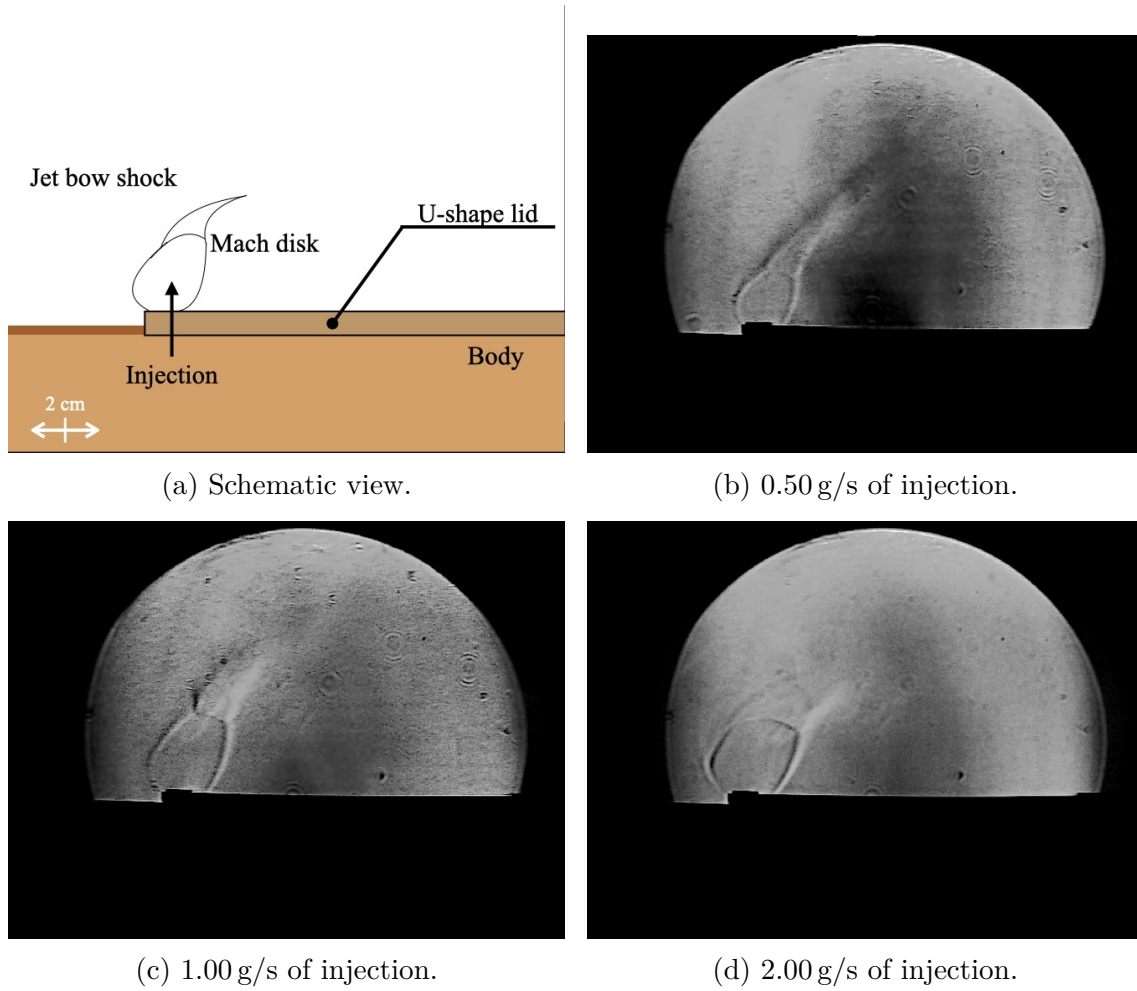


Figure 4.11: Schlieren images of the injected jet at selected injection mass flow rates.

4.3.2 Schlieren Visualization

In addition to the communication tests, schlieren imaging was used to visualize the injected gas flow structure and its interaction with the arc-heated freestream. The schlieren results are presented in Figure 4.11. The main flow direction is from left to right, and with the knife edge oriented vertically the setup is primarily sensitive to horizontal density gradients. The lower portion of each frame is partially occluded by a support structure mounted from below. A dark fringe near the injection port is an optical artifact from the U-shaped metal lid that secures the 1 mm-thick Al_2O_3 cover plate on the transmitter housing shown in Figure 4.4; the lid does not obstruct the injection outlet center and has negligible influence on the observed flow features.

Pronounced dark/bright bands delineate the gas jet, corresponding to line-of-sight density gradients. As the injection mass flow rate increases, band contrast strengthens, indicating a larger density gradient (refractive-index) difference be-

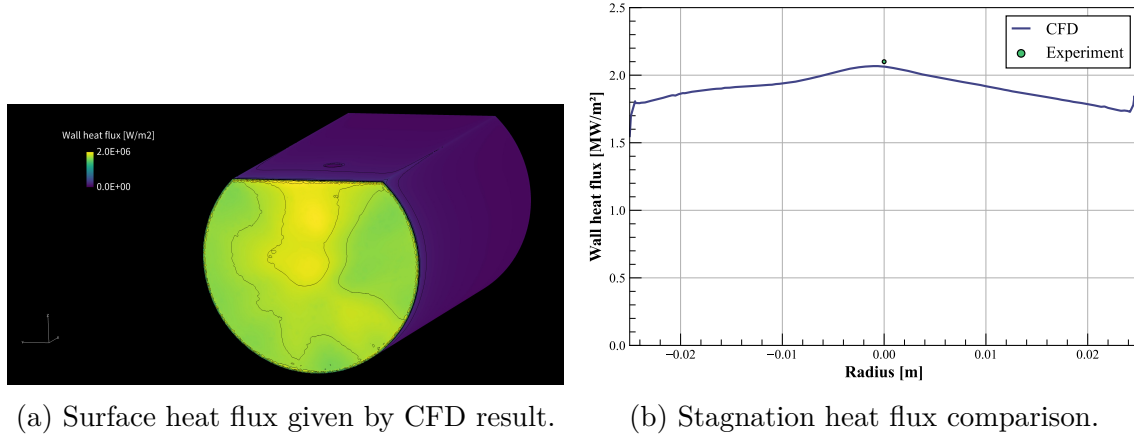


Figure 4.12: Verification of the wall heat flux at the stagnation point.

tween the injected gas and the surrounding plasma. Strong gradients appear both upstream and downstream of the injection outlet, consistent with a shear layer and compression/expansion around the jet boundary.

The injected nitrogen expands toward the low-pressure chamber while being deflected by the high-speed main flow, bending the jet downstream. At higher injection rates, a clear Mach disk is visible, and the distance from the model surface to the Mach disk grows with jet momentum, consistent with underexpanded jet behavior. These trends agree with classic vertical gas injection characteristics [96]. The observed growth of the jet stand-off distance with injection rate is also consistent with the thickening of the near-wall gas film inferred from the CFD and electron-density fields.

4.3.3 Verification of Computational Results

Both experiment and computation indicate high temperatures in the region upstream of the injection port, shown in Figure 4.19. The wall-heat-flux field reflects the same interaction: a bow-shaped band of enhanced heat flux forms ahead of the injection port, consistent with the shock structure produced by the underexpanded jet.

To quantitatively validate the model, the stagnation-point wall heat flux on the experimental model was measured with a Gardon gauge during the 1 MW tests. The comparison with the CFD prediction is shown in Figure 4.12. Because the nozzle flow is nominally axisymmetric and the model is centered on the nozzle axis, the wall-heat-flux distribution is expected to be symmetric with a central peak. Minor asymmetries in the computed profile are attributable to grid discretization and interpolation and do not affect the peak magnitude. The CFD results correspond to

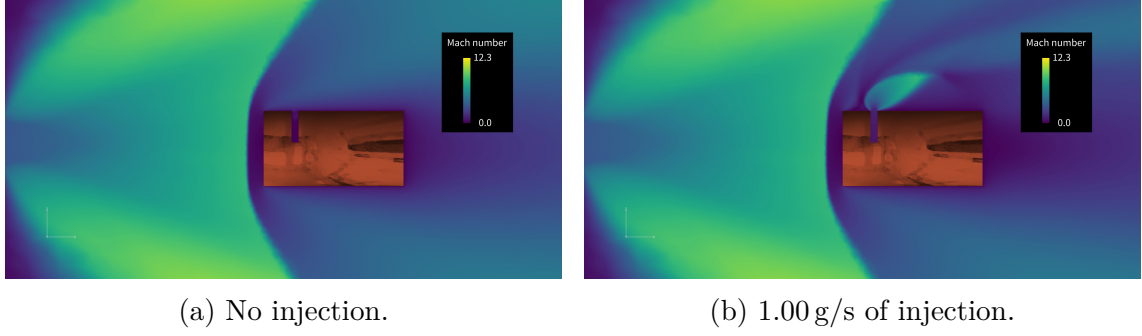


Figure 4.13: Mach number distribution in the center plane of the experimental model with and without gas injection.

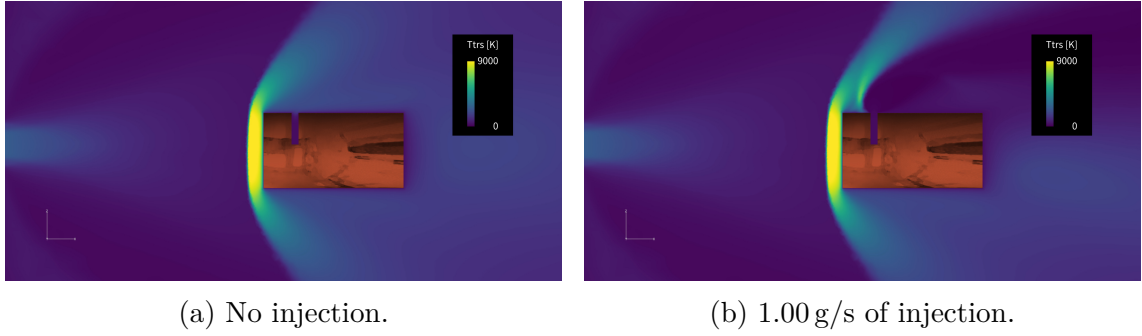


Figure 4.14: Translational temperature distribution in the center plane of the experimental model with and without gas injection.

the baseline case without gas injection.

The simulated peak wall heat flux agrees closely with the measurement, confirming that the solver faithfully reproduces the facility’s aerothermal environment. This agreement supports the reliability of the CFD solution used here to analyze plasma characteristics and blackout-mitigation mechanisms.

4.3.4 Numerical Reconstruction of the ISAS Chamber Flow

The center-plane Mach number and translational temperature fields without gas injection are shown in Figure 4.13 and Figure 4.14, respectively. The Mach-number distribution exhibits a strong detached bow shock in front of the model. Locally high Mach-number regions also appear above and below the body due to supersonic expansion of the arc-heated freestream jet into the low-pressure chamber. Such features are characteristic of arc-heated facilities and differ from the shock-layer structure encountered in realistic atmospheric reentry.

The injected gas spreads toward the low-pressure chamber as an underexpanded jet, as shown in Figure 4.13b. The injection reaches supersonic speed, and the numerical results agree qualitatively with the schlieren images in Figure 4.11. Ad-

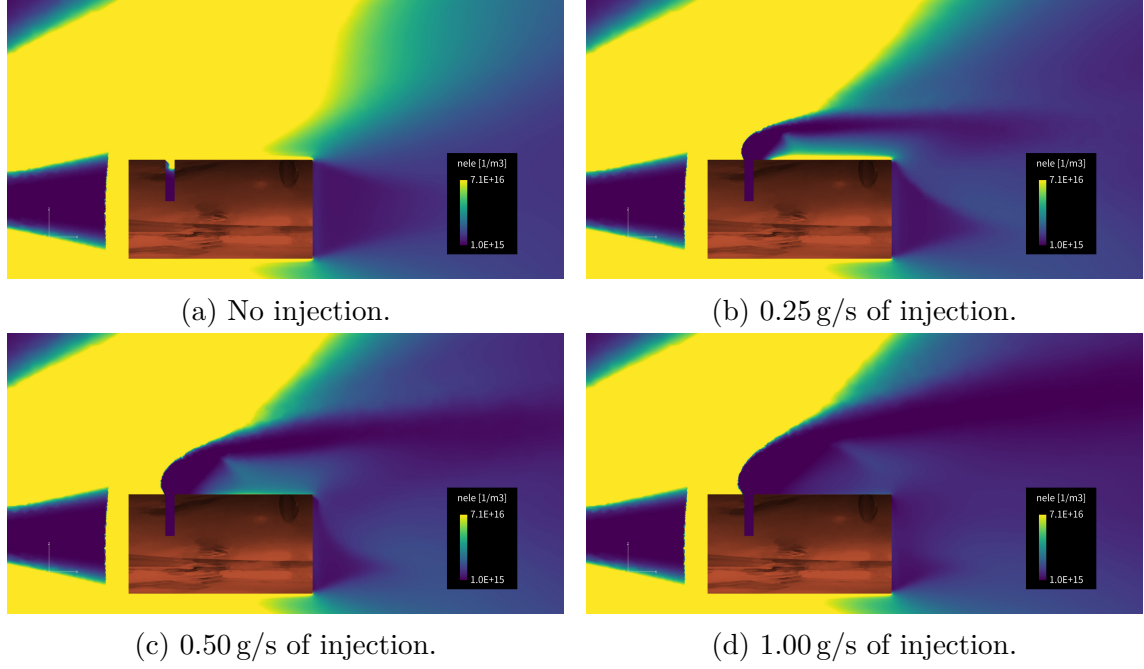


Figure 4.15: Electron number density distributions at 2.4 GHz for different gas injection mass flow rates; the color scale is limited to the critical density $n_{\text{ele, crit}}$.

ditionally, the temperature inside the gas film created by the injection drops due to rapid adiabatic expansion.

Downstream of the bow shock, the Mach number decreases because of compression, while the translational temperature rises sharply in the post-shock region. This localized heating corresponds to the luminous emission observed experimentally, confirming that the simulation captures the distinctive flow characteristics of the arc-heated test chamber.

As the injected N_2 expands, its temperature falls rapidly and a broad low-temperature region forms above the model. Immediately upstream of this film, a localized temperature rise appears where the jet impinges on the arc-heated flow; this high-temperature zone coincides with the bright emission region seen in Figure 4.9 and the compression heating captured in Figure 4.11. The suppressed temperature within the film limits further ionization and dissociation, maintaining a plasma-free layer around the surface. Consequently, the electron production rate does not increase with higher injection rates, confirming that the injected gas acts as a neutralizing barrier to the surrounding plasma.

Figure 4.15 presents spatial distributions of electron number density for several injection rates. The color scale is clipped at the critical density $n_{\text{ele, crit}}$ defined in Equation 1.3. Regions with $n_{\text{ele}} > n_{\text{ele, crit}}$ act as reflecting layers that cause communication blackout, whereas subcritical regions allow wave propagation. As

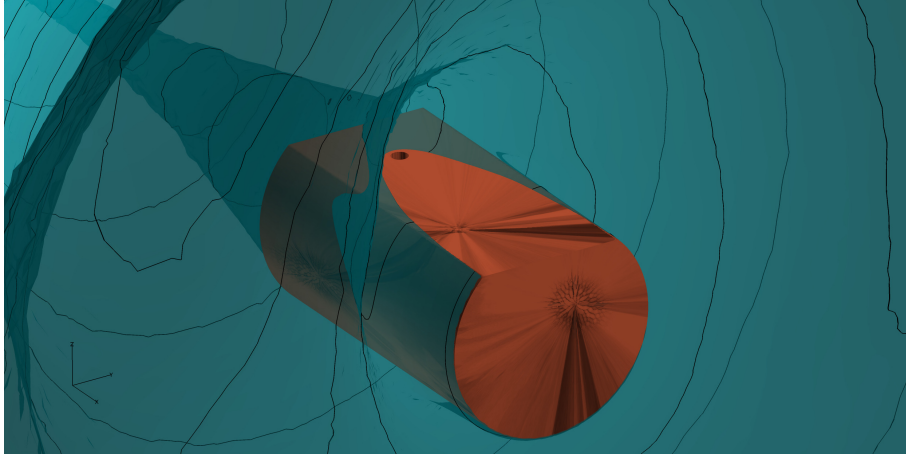


Figure 4.16: Isosurface of the critical electron number density $n_{\text{ele,crit}}$ for 1.00 g/s of injection, showing the plasma-free corridor surrounding the model.

the injection mass flow rate increases, the near-wall subcritical region expands along the surface and the high-density plasma is displaced away from the wall, gradually forming a continuous transmission corridor.

Figure 4.16 shows the isosurface at $n_{\text{ele,crit}}$ for an injection mass flow rate of 1.00 g/s. The electron number density near the model sidewall falls well below the critical level, creating a three-dimensional plasma-free region large enough to enable wave transmission around the antenna. These results demonstrate that gas injection opens a propagation corridor through the surrounding plasma, mitigating communication blackout in the arc-heated environment.

The streamwise temperatures and mole fractions of the eleven air-plasma species along the upper surface are plotted in Figure 4.17. Solid lines correspond to the case without injection, whereas dotted lines represent injection at 1.00 g/s. The gray band marks the location of the injection port, and the model center is positioned 150 mm downstream of the nozzle exit. Upstream of approximately 140 mm from the nozzle exit, ionic species and electrons increase due to post-shock compression. The electron temperature lags the translational temperature, exhibiting the characteristic relaxation behavior of nonequilibrium plasmas. Near and downstream of the injection region, the N_2 fraction rises markedly while the other species decrease, indicating strong dilution of the arc-heated air by the injected nitrogen. A localized region of elevated N_2^+ and electron concentrations appears around $x = 160$ mm in Figure 4.17d, identifying the interaction zone where the injected jet impinges on the arc-heated main flow. Farther downstream, a low-plasma-density layer forms near the surface, covering the vicinity of $x \approx 200$ mm where the communication antenna is located. In this region, the N_2 concentration approaches unity, showing that the

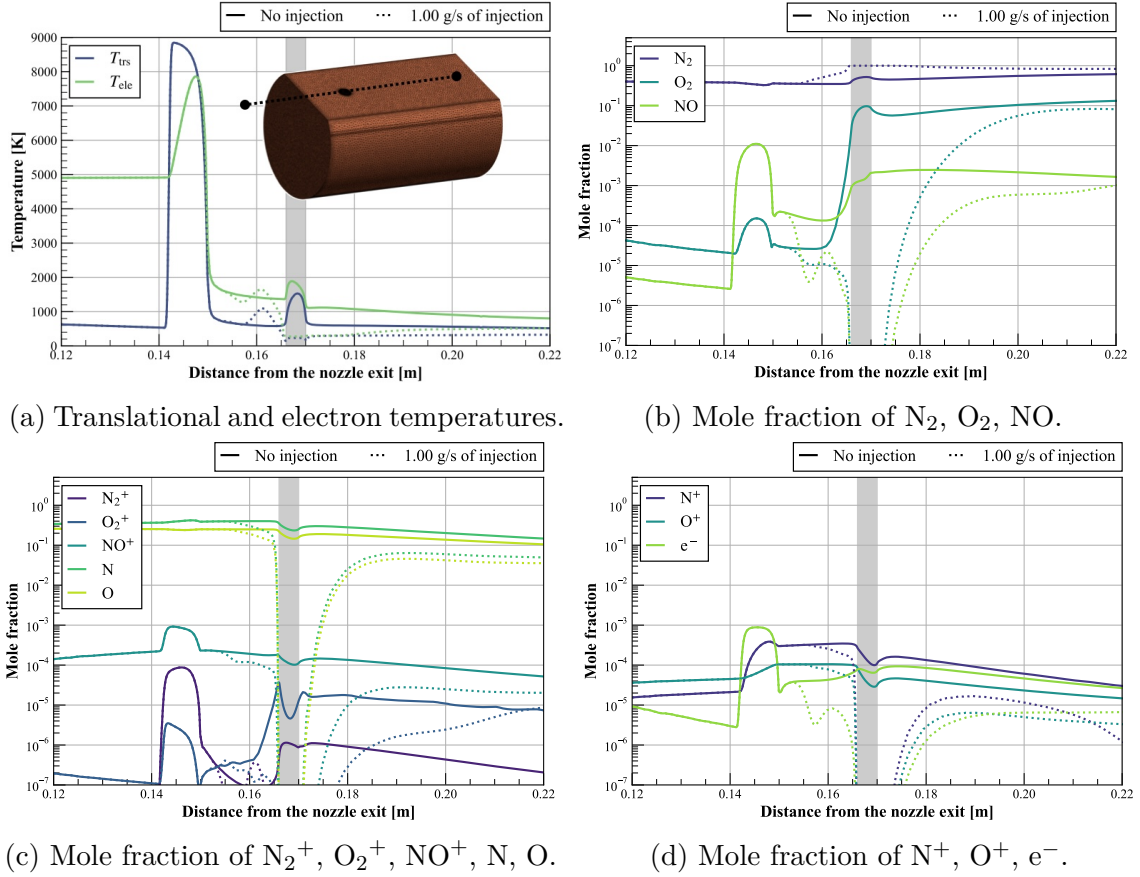


Figure 4.17: Profile along the centerline of the upper surface of the model.

plasma freestream is kept away from the model surface and that a cold, nearly neutral gas layer has been established. This layer constitutes a propagation corridor for electromagnetic waves.

Figure 4.18 compares wall-heat-flux distributions with and without gas injection. The injected cold nitrogen markedly reduces the surface heat flux, demonstrating that the expanded N_2 layer acts as an insulating film separating the high-temperature plasma from the body. The antenna embedded within this film remains in a low-density region, allowing radio transmission through the surrounding plasma.

The temperature and emission behavior around the injection region are shown in Figure 4.19. The high-temperature region immediately ahead of the jet reflects the interaction between the expanding injected gas and the high-speed plasma freestream of the wind tunnel. Both CFD and experiment capture this localized emission, further confirming that the numerical model reproduces the mechanisms observed in the arc-heated wind-tunnel tests as seen in Figure 4.9b.

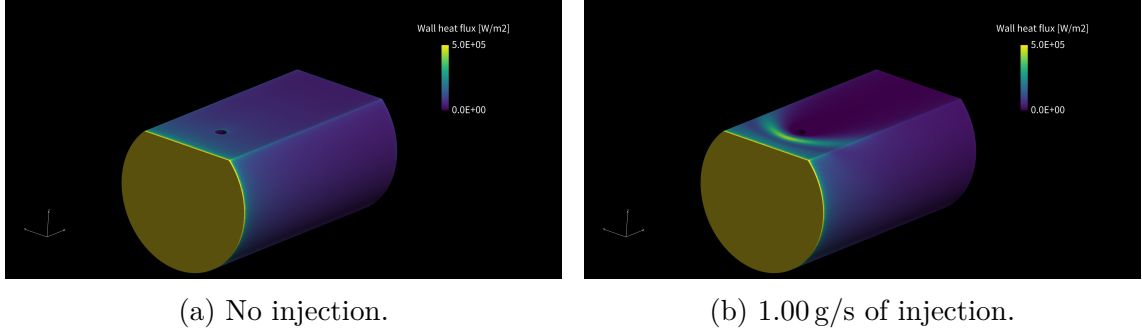


Figure 4.18: Wall heat flux distributions with and without nitrogen injection, showing the formation of the near-wall insulating layer.

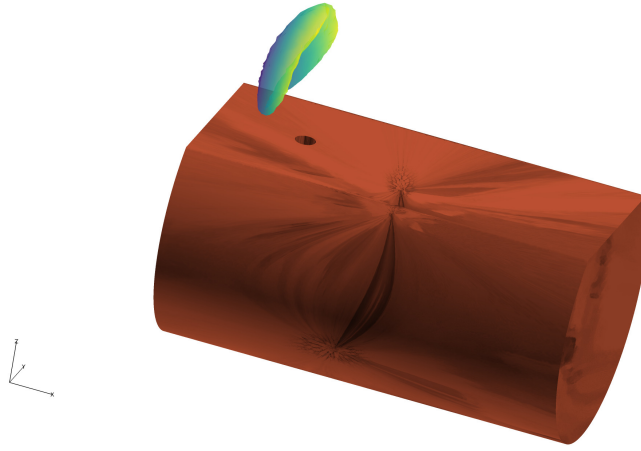


Figure 4.19: Isothermal surface of the interacting region at $T_{\text{trs}} = 5,000$ K with 1.00 g/s of injection.

4.4 Conclusions

An experimental demonstration of communication blackout mitigation by surface gas injection was conducted in the large-scale 1 MW arc-heated wind tunnel at JAXA/ISAS. Complementary plasma-flow simulations clarified the governing mechanisms and enabled quantitative interpretation of the measurements. A compressible, thermochemical nonequilibrium solver reproduced the steady plasma field in the chamber and resolved the interaction between the injected gas and the surrounding high-enthalpy flow under the experimental operating conditions.

In the wind-tunnel tests, a transmitter embedded in the model communicated with a receiver outside the chamber. Without gas injection, the received signal vanished during plasma exposure, demonstrating complete blackout. With nitrogen injection from the model surface, the signal remained detectable throughout exposure and the recovery level increased with injection rate. Schlieren imaging captured the jet structure and its interaction with the main flow, and wall heat-flux measure-

ments using a Gardon gauge agreed with CFD predictions, providing confidence in the numerical model.

The simulations reproduced the characteristic arc-heated environment: a high-temperature, high-density plasma layer formed ahead of the model owing to strong shock compression and subsequent ionization. Under these conditions, the electron number density exceeded the critical threshold at 2.4 GHz, prohibiting electromagnetic-wave propagation. Nitrogen injection generated a near-surface region in which the N_2 fraction approached unity and ionization was strongly suppressed, forming a continuous plasma-free layer that extended into the wake. Increasing the injection rate reduced the near-wall plasma density and matched the measured improvement in received power. The CFD also captured local heating and shock formation at the jet-plasma interface, consistent with the localized luminous emission observed above the injection port, followed by adiabatic cooling that produced a low-temperature insulating layer along the surface.

Overall, these results establish gas-film injection as an effective dual-mitigation strategy that both reduces local aerodynamic heating and creates a plasma-free corridor enabling electromagnetic-wave transmission. In the present campaign, blackout mitigation was assessed primarily in terms of link loss and recovery under a single facility configuration, so detailed, repeatable characterization of attenuation levels and scaling effects remains limited. The next chapter therefore extends the investigation to the larger DLR L2K facility, where higher-enthalpy, more reproducible experiments and companion CFD analysis enable a scaled evaluation of gas-film blackout mitigation and a more quantitative assessment of its performance.

Chapter 5

Large-Scale Experiments in DLR

L2K Facility

This chapter extends the investigation of gas-film blackout mitigation to a larger-scale arc-heated facility and examines its performance under higher-enthalpy, more repeatable conditions. It positions the DLR L2K campaign as the large-model, repeatability-focused counterpart to the ISAS experiments, providing the first detailed, repeated examination of simultaneous reductions in RF attenuation and surface heating at scale. Building on the numerical study in chapter 3 and the 1 MW-class ISAS experiments in chapter 4, the present work conducts systematic tests in the DLR L2K arc-heated wind tunnel to quantify how gas injection simultaneously affects communication blackout and aerodynamic heating.

Experiments were performed in high-enthalpy, weakly ionized flows representative of atmospheric entry. A wedge-shaped test model equipped with a surface gas injector and an embedded communication antenna was exposed to two freestream conditions: pure argon and an argon-nitrogen mixture. The pure-argon flow produced electron number densities sufficient to cause severe signal attenuation and complete blackout at 2.4 GHz, while the addition of nitrogen reduced the electron population and partially alleviated blackout. Surface gas injection was then applied as an additional control parameter: the injected nitrogen formed a near-wall gas film that lowered local electron number density and surface temperature, enabling quantitative assessment of mitigation effectiveness as a function of freestream composition and injection rate.

By combining scaled-up experiments, repeatable operating conditions, and detailed diagnostics, the L2K campaign provides a more comprehensive data set than

was possible in the ISAS tests. The experimental results presented in this chapter demonstrate the robustness of gas-film injection as a dual-purpose mitigation strategy and supply validation and guidance for the CFD analyses.

5.1 Experimental Setup

5.1.1 Arc-Heated Wind Tunnel L2K

The experiments were conducted in the arc-heated wind tunnel L2K, operated by the Supersonic and Hypersonic Technologies Department at DLR. L2K is a continuous-flow, high-enthalpy facility specifically designed for testing thermal protection system (TPS) materials and assessing the demisability of reentry components. The facility is powered by a 1.4 MW Huels-type arc heater, which generates high specific enthalpy flow through electrical discharge. A key advantage of L2K is the large core flow that can be generated at the nozzle exit. The resulting plasma freestream is wide enough to fully envelop comparatively large test articles, enabling experiments with geometrically complex models that integrate communication hardware, gas-supply plumbing, and internally water-cooled structures. This capability is essential for evaluating gas-film blackout mitigation on configurations that closely mimic flight-relevant systems.

L2K enables a wide range of operating conditions by allowing control over parameters such as reservoir pressure, mass flow rate, nozzle exit geometry, background chamber pressure, and test sample position. While the resulting low-density environment leads to lower Reynolds numbers than those typical of atmospheric reentry, it permits long-duration high-enthalpy exposure, making the facility suitable for investigating reacting, nonequilibrium flows. These conditions have also been extensively characterized in prior flow diagnostics studies, including optical velocimetry and spectroscopy [97, 98, 99].

Various intrusive and non-intrusive diagnostics are available, including thermocouples, pyrometers, infrared thermography, Pitot probes, and advanced optical techniques such as tunable diode laser absorption spectroscopy (TDLAS), laser-induced fluorescence (LIF), particle image velocimetry (PIV), UV-VIS spectroscopy, microwave interferometry (MWI), and Fourier transform infrared spectroscopy (FTIR).

Such plasma conditions in L2K have been characterized in previous studies [59, 100]. The detailed flow conditions and nozzle configuration employed in this study are presented in subsection 5.1.3.

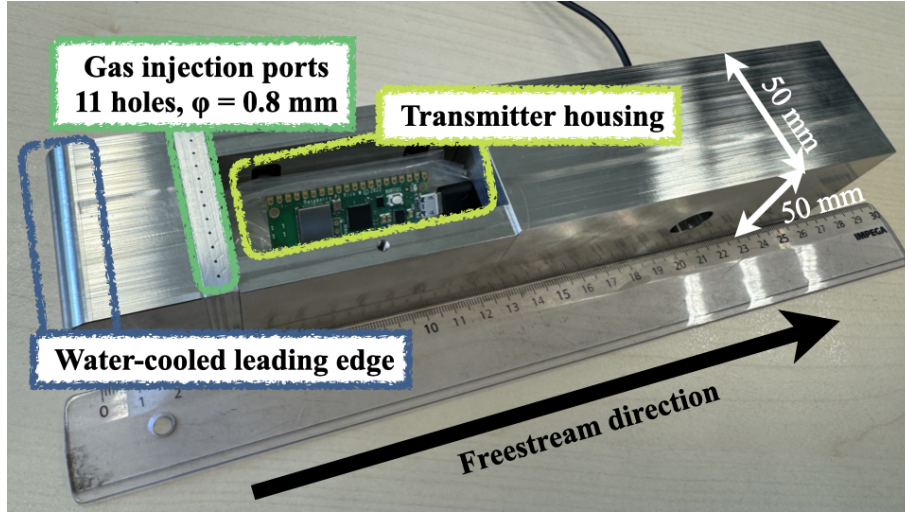


Figure 5.1: Overview of the test model, showing the wedge-shaped geometry, gas injection ports, and transmitter housing.

5.1.2 Test Model

The test model, shown in Figure 5.1, has a wedge-shaped geometry with a cross-sectional area of 50 mm by 50 mm. The model was fabricated from stainless steel (SUS) by 3D printing. Internal cooling channels are used to withstand the high thermal loads encountered during testing in the arc-heated flow.

A gas injection port was located on the upstream-facing surface to generate a thin gas layer on the model surface. The injector consisted of eleven cylindrical channels, with the centers of the injection holes spaced 4.0 mm apart. Each gas outlet had a diameter of 0.8 mm. Room-temperature nitrogen gas was supplied through these ports, and the mass flow rate was precisely controlled by a mass flow controller.

A transmitter for evaluating communication blackout was embedded inside the model within a socket located immediately downstream of the gas injection port. The socket was covered by a 2 mm-thick alumina (Al_2O_3) ceramic plate with 96 % purity, as shown in Figure 5.2. This material was selected for its high thermal resistance and dielectric properties, which allow electromagnetic wave transmission while providing thermal protection to the transmitter.

5.1.3 Flow Conditions

The inflow conditions at the nozzle inlet are summarized in Table 5.1. These flow configurations were designed to reproduce both aerodynamic heating and communication signal attenuation caused by plasma in the L2K facility, and to investigate the effectiveness of gas injection for simultaneous mitigation of these issues. Argon

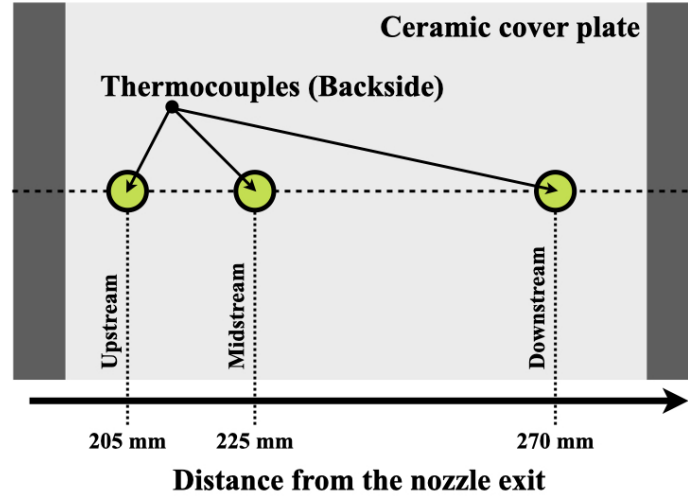


Figure 5.2: Ceramic cover plate on the transmitter housing, with locations of thermocouples for backside temperature measurements.

was selected as the primary test gas because, under the attainable L2K operating conditions, argon ionizes much more readily than air and can generate freestream electron number densities large enough to reproduce RF attenuation at 2.4 GHz; comparable air flows in L2K do not reach such ionization levels and thus would not produce a clear blackout signature.

Two flow conditions, referred to as Flow Condition 1 (FC1) and Flow Condition 2 (FC2), were established. FC1 was configured to induce strong communication signal attenuation by utilizing pure argon flow, which facilitates plasma generation due to its low ionization energy. The freestream electron number density under this condition was measured in previous studies with MWI to be approximately $6.1 \pm 0.5 \times 10^{17} / \text{m}^3$, exceeding the critical electron density for blackout at the communication frequency of 2.4 GHz [59]. This ensured that FC1 provided a plasma condition capable of inducing communication blackout.

FC2 maintained the same reservoir pressure and total mass flow rate as FC1 but introduced a 5 % nitrogen admixture by mass (corresponding to a mass ratio of approximately 19:1 between argon and nitrogen) to reduce the free electron population through chemical recombination [101]. By comparing FC1 and FC2, any observed change in communication performance can be directly attributed to the presence or absence of a highly ionized argon plasma, rather than to variations in the RF system or facility environment. The total mass flow rate and stagnation pressure were kept constant between the two cases to isolate the effect of plasma chemistry. Under FC2, the plasma luminosity was visibly reduced, indicating a significant decrease in the degree of ionization, and thus the electron density, compared with FC1. A

Table 5.1: Inflow conditions for the L2K nozzle

	FC1	FC2
Reservoir pressure [kPa]	37.5	37.5
Total mass flow rate [g/s]	20	20
Estimated temperature* [K]	3,886	3,713
Estimated specific enthalpy* [MJ/kg]	2.0	2.1
Gas composition	100 % Ar	95 % Ar + 5 % N ₂

* Estimated using a 1D flow solver (NATA code) [102].

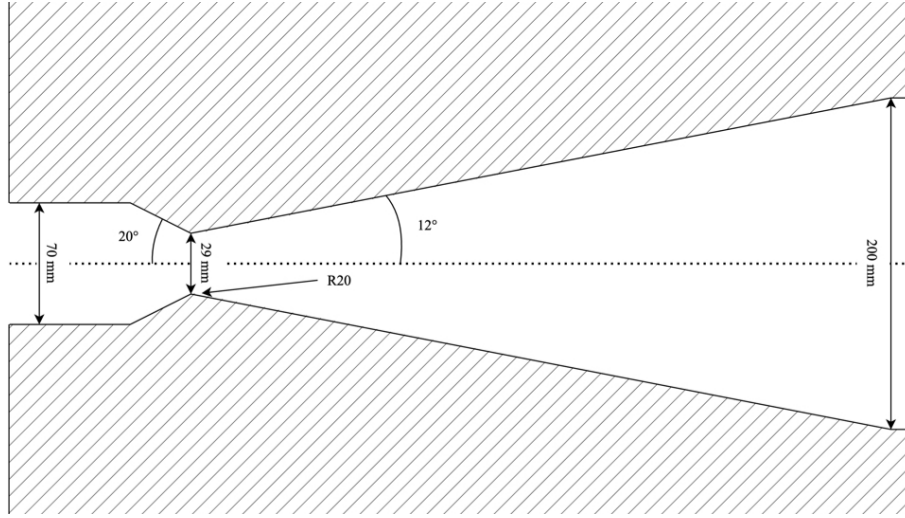


Figure 5.3: Geometry of the L2K nozzle with 200 mm exit diameter.

comparison between these two flow conditions eliminates confounding effects such as heating and flow exposure, providing clearer insight into the influence of freestream plasma.

These thermodynamic properties are summarized in Table 5.1. The temperature and specific enthalpy at the nozzle inlet were estimated using a one-dimensional flow solver (NATA code [102]).

The high-enthalpy flow generated by the 1.4 MW arc heater was accelerated and expanded through a nozzle with a throat diameter of 29 mm and an exit diameter of 200 mm, as illustrated in Figure 5.3.

5.1.4 Test Procedure and Measurement Setup

The experimental setup inside the L2K facility is shown in Figure 5.4. The locations of the nozzle exit, the experimental model with the ceramic cover plate, and three receiver antennas are indicated for clarity. A close-up of the model installation is provided in Figure 5.5, highlighting the ceramic plate and the proximity to the

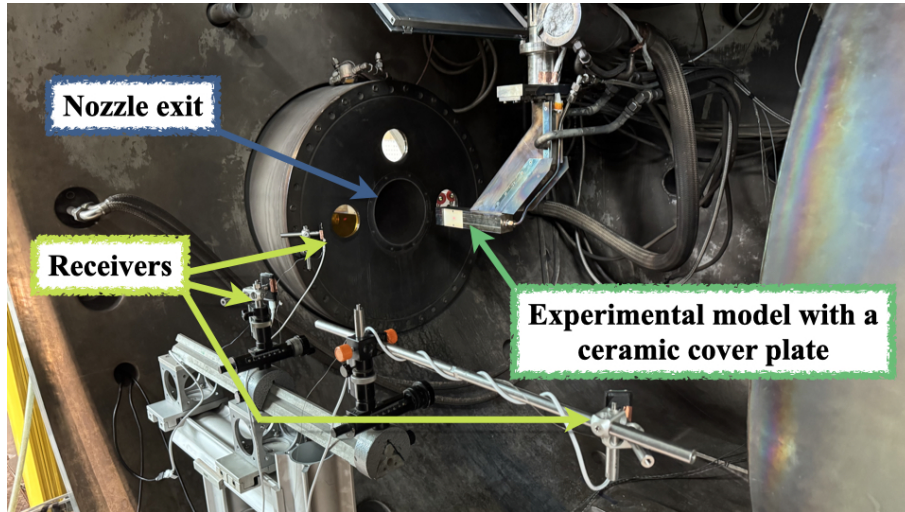


Figure 5.4: Overview photograph of the experimental setup inside the L2K test chamber.

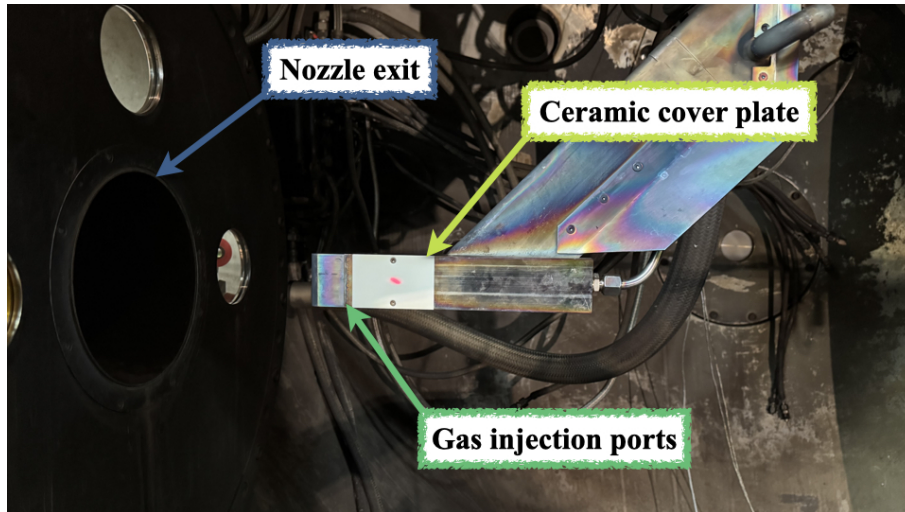


Figure 5.5: Close-up photograph of the experimental model mounted at the *on-axis* position.

nozzle exit. The distance between the nozzle exit and the leading edge of the model was 160 mm.

To evaluate the cooling effect of gas injection, three thermocouples were installed on the backside of the ceramic plate at upstream, midstream, and downstream positions, as shown in Figure 5.2. These thermocouples enabled measurement of the temperature distribution along the flow direction during testing. The surface temperature of the ceramic plate was monitored externally using an infrared camera. Due to the uncertainty in the ceramic plate's emissivity, the emissivity used for data processing was set to 1, which yields the coldest possible surface temperature. IR data are reported to show the qualitative effects of film cooling.

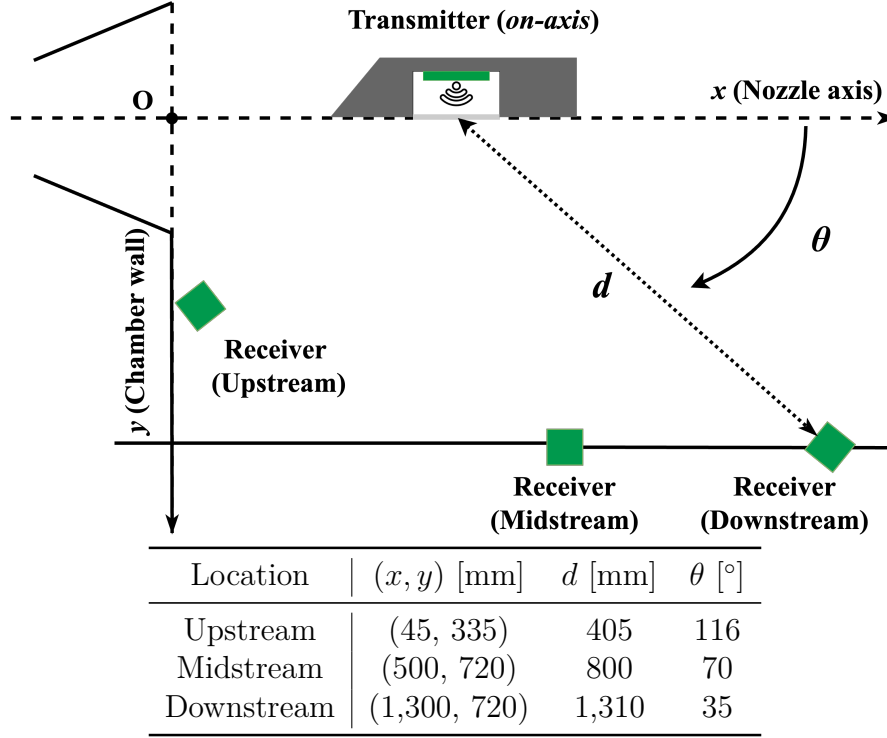


Figure 5.6: Locations of the receivers relative to the transmitter. Distances d and angles θ are defined as shown.

During the experiments, the transmitter continuously emitted a 2.4 GHz Wi-Fi signal. The signal strength was monitored by three receiver antennas positioned inside the test chamber but outside the freestream, as shown in Figure 5.4. The locations of three receivers are summarized in Figure 5.6. Raspberry Pi Pico W boards were selected to implement the transmitter and receivers. Thanks to their compact size and integrated 2.4 GHz Wi-Fi capability provided by the Infineon CYW43439 wireless interface, which supports IEEE 802.11 b/g/n standards over the 2.4 GHz band, reliable transmission and monitoring of the signal during plasma exposure were possible. These measurements were used to evaluate the effectiveness of the gas injection in mitigating both aerodynamic heating and radio-frequency signal attenuation caused by the high-enthalpy plasma flow.

Prior to the tests with plasma flow, preliminary measurements were conducted to confirm that the communication signal strength is not affected by the model's position relative to the receivers when the plasma flow is absent. In these pre-tests, the experimental model was placed both at the *off-axis* position (outside the flow path) and the *on-axis* position (aligned with the flow axis), and signal strength was recorded. No significant difference was observed between these positions in

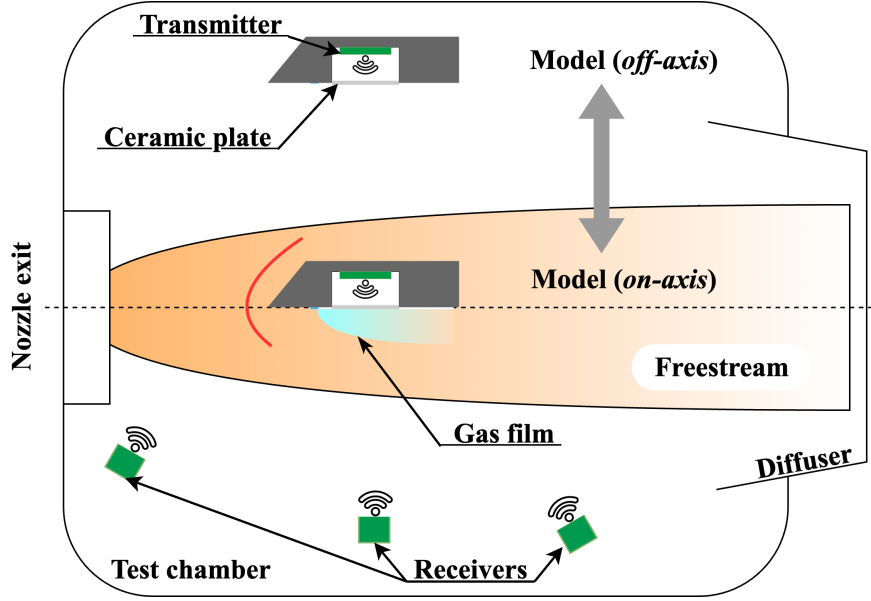


Figure 5.7: Schematic of the experiment in the L2K facility.

the absence of the plasma flow, indicating that the model's location alone does not influence the communication results. Therefore, the subsequent experiments focus solely on the effects of the high-enthalpy plasma flow.

A schematic of the experimental procedure is shown in Figure 5.7. During the actual wind tunnel tests, the following sequence was employed to measure changes in surface temperature and communication signal strength:

1. The model is initially positioned *off-axis*, outside the freestream. The arc heater is then activated, and the high-enthalpy flow is established in the chamber.
2. After the flow stabilizes, the model is moved to the *on-axis* position and exposed to the plasma flow for approximately 10 s without gas injection.
3. Subsequently, the gas injection is activated at a prescribed mass flow rate. The injection continues for approximately 60 s after stabilization.
4. Following the injection period, the model remains exposed to the flow (*on-axis*) for an additional 10 s with no gas injection.
5. Finally, the model is retracted to the *off-axis* position. After 10 s, the arc heater is turned off to complete the test.

In the case of FC2, the experimental procedure was slightly modified compared to FC1. To prevent discoloration of the ceramic plate before the start of gas injection,

the gas injection was initiated prior to moving the model into the *on-axis* position. This precaution ensured that the thermal and optical properties of the ceramic plate remained consistent throughout the test and did not introduce additional uncertainty into the measurements.

This procedure allows for systematic observation of the effects of plasma flow and gas injection under controlled and repeatable conditions. The total operation time of the arc heater was approximately 150 s per test. To maintain consistent initial conditions, the ceramic cover plate was cooled to room temperature before each run.

During the entire test, the transmitter continuously emitted a 2.4 GHz Wi-Fi signal. Three receiver antennas, placed within the test chamber shown in Figure 5.6, monitored the received signal strength at a sampling rate of approximately 800 Hz.

Simultaneously, an infrared camera was used to measure the 2D surface temperature distribution of the ceramic plate. Its backside temperature was measured with thermocouples at three locations along the centerline of the model as shown in Figure 5.2. The surface temperature evolution was extracted from the IR data at the same points. The comparison between IR and thermocouple data is only qualitative, since the two techniques probe the temperature on opposite sides of the ceramic plate. Note that, since the thermocouples are positioned on the backside, a time delay in the thermocouple data is expected with respect to the IR data. In addition, due to the uncertainty in determining the emissivity value of the plate, the IR data were processed with an emissivity $\varepsilon = 1$, and therefore they should be considered for qualitative analysis only. IR and thermocouple data were recorded at a sampling rate of 30 Hz and 50 Hz, respectively. Before each run, the ceramic plate was inspected and cleaned to restore a uniform matte finish. Slight surface discoloration was observed only after FC2 exposures. To minimize possible emissivity drift, the IR images are used solely as qualitative support, and all quantitative heating-rate reductions reported in this chapter are derived from the thermocouples mounted behind the plate.

This measurement setup was designed to evaluate the simultaneous mitigation of aerodynamic heating and communication blackout through gas injection under high-enthalpy plasma flow conditions.

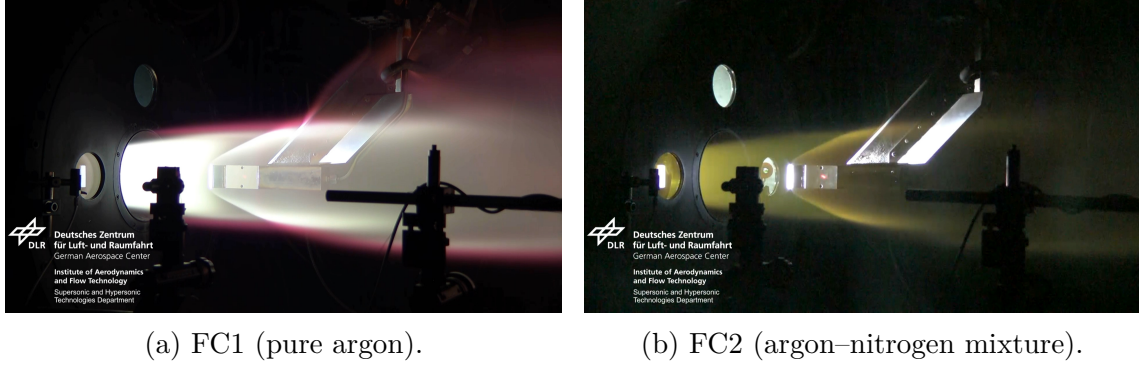


Figure 5.8: Freestream appearance without the model under FC1 and FC2 conditions.

5.2 Results and Discussion of the Experimental Demonstration

5.2.1 Comparison of Visual Flow Features

Figure 5.8 shows the appearance of the freestream within the L2K chamber under the two inflow conditions, FC1 and FC2. The addition of a small amount of nitrogen gas in FC2 significantly alters the visible emission characteristics of the flow compared to FC1. This observation indicates a change in the plasma properties, particularly in the degree of ionization and chemical composition, between the two conditions.

Figure 5.9 and Figure 5.10 show the experimental model and plasma flow under the two flow conditions, FC1 and FC2. In both cases, a shock wave is clearly formed at the leading edge of the model.

Under FC1, gas injection produces a distinct blue luminous region that extends from the injection port on the upstream surface toward the downstream side. The emission area becomes larger and spreads farther from the model as the injection mass flow rate increases. This region corresponds to the interface between the primary flow and the injected gas, where compression locally raises the temperature. The observed blue emission is attributed to the excitation of argon plasma.

In contrast, under FC2, no such prominent luminous structure can be observed. This is likely because light reflection from the model’s leading edge masks the emission from the interface between the freestream and the injected gas.

With appropriate optical filtering, it would be possible to capture the emission from the gas interface more clearly. Such observations may allow estimation of the injected gas momentum flux based on its penetration height into the main flow of the wind tunnel. In the future, this kind of observation, combined with emission

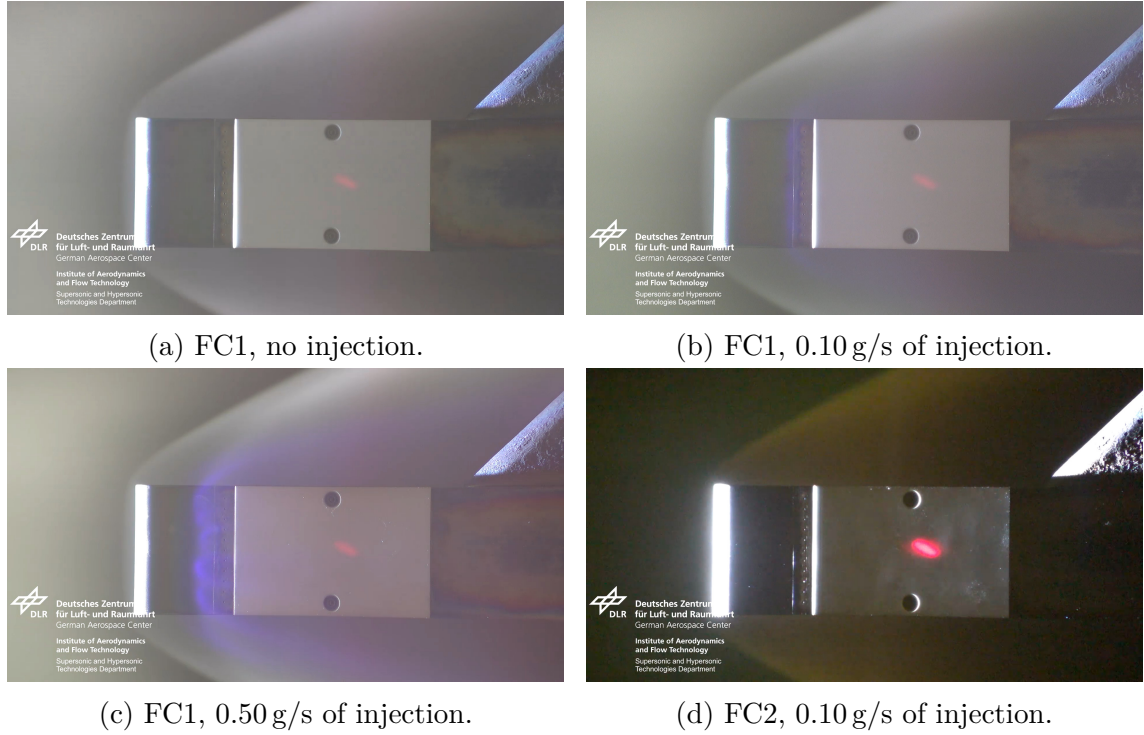


Figure 5.9: Frontal-view photographs of the experimental model under FC1 and FC2 conditions with and without gas injection.

spectroscopy, could be applied to gain further insights.

5.2.2 Aerothermal Heating Mitigation

The mitigation of aerothermal heating is evaluated based on the temperature rise rate, which is correlated with the wall heat flux. Surface temperature data are reported to show the effects of film cooling. These values should be considered qualitative because of the uncertainty in the plate's emissivity, which was set to 1 for data processing. In Figure 5.11, temperature distributions for different tests are shown at four time instants chosen to highlight the impact of film cooling. The time instants are denoted as $\Delta t = \{10, 50, 70, 90\}$ s, where Δt is the time elapsed since the arrival of the model on the flow axis. At the top of the figure is the FC1 case with a 0.10 g/s injection rate, while the bottom shows the case with a 0.50 g/s injection rate. For each image, the flow comes from the left.

Each image shows peak temperatures on the top and bottom sides of the plate, indicating that the model might be subject to 3D flow phenomena. Until $\Delta t = 10$ s, the injection rate is 0 g/s. Here, the upstream locations are generally hotter than the downstream locations: as expected, upstream locations are subject to stronger heat loads without film cooling.

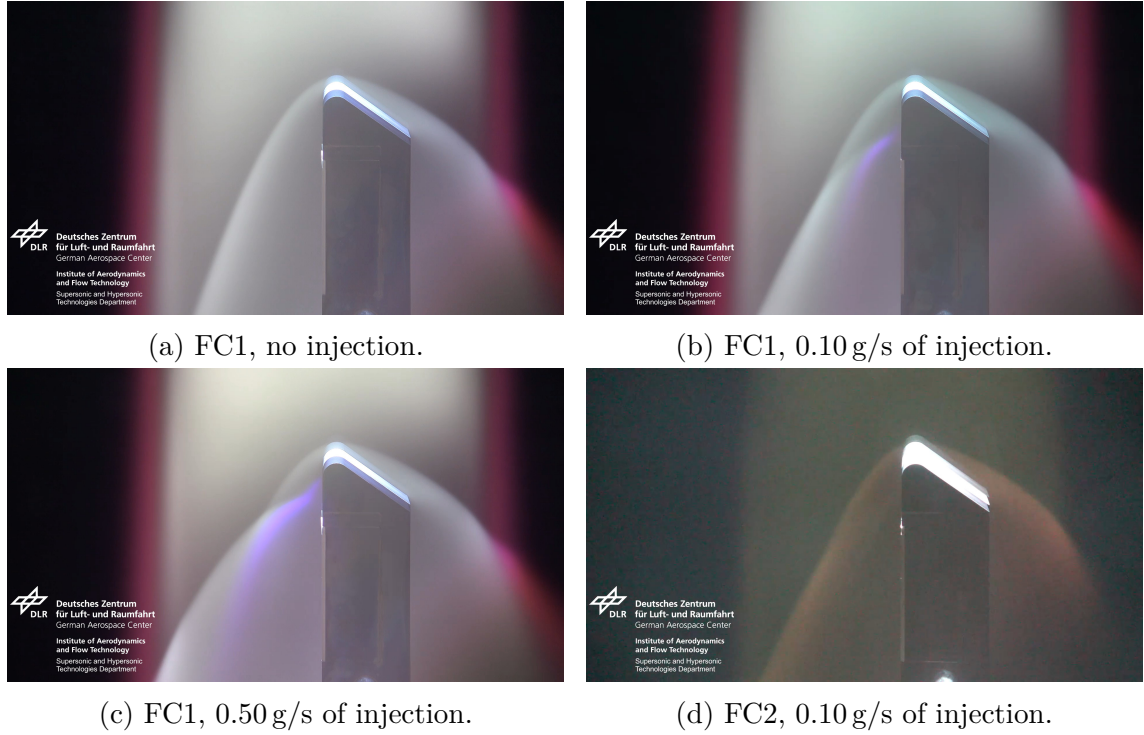


Figure 5.10: Side-view photographs of the experimental model under FC1 and FC2 conditions with and without gas injection.

At $\Delta t = 50$ s, the film cooling injection rate has been stable for at least 20 s. Here, the surface temperature distribution starts to change: the injected gas cools the upstream locations better than the downstream ones. Therefore, the high-temperature locations start shifting downstream. This is evidence that film cooling is effective.

At $\Delta t = 70$ s, this film cooling effect is well established in the case of 0.50 g/s injection rate, and less evident in the 0.10 g/s case. This highlights that mitigation of aerothermal heating depends on the cooling-gas injection mass flow rate.

At $\Delta t = 90$ s, the injection gas has been switched off for at least 10 s. Here, the high-temperature locations start moving back to upstream locations, as the film cooling effect is absent.

The high-temperature region downstream can appear larger in the 0.50 g/s snapshots than in the 0.10 g/s case. A plausible cause is partial lift-off or thinning of the coolant film at the higher jet momentum, which reduces downstream coverage and allows the hot freestream to reattach locally. The present diagnostics cannot resolve this near-wall topology.

The surface temperature evolution extracted from IR data at the thermocouple locations is shown in Figure 5.12, along with the recorded injection mass flow rate. The thermocouple locations are illustrated in Figure 5.2. The blue lines repre-

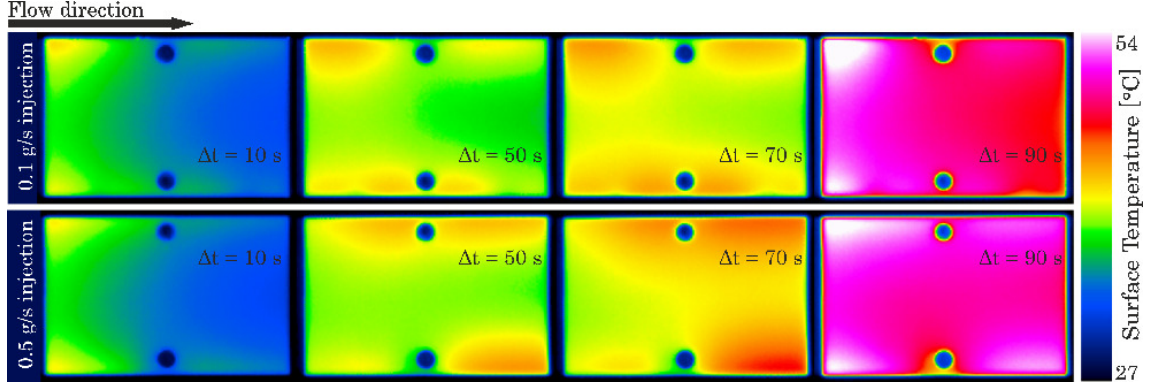


Figure 5.11: Qualitative surface temperature distribution evolution in FC1 with 0.10 g/s of injection (top) and 0.50 g/s of injection (bottom).

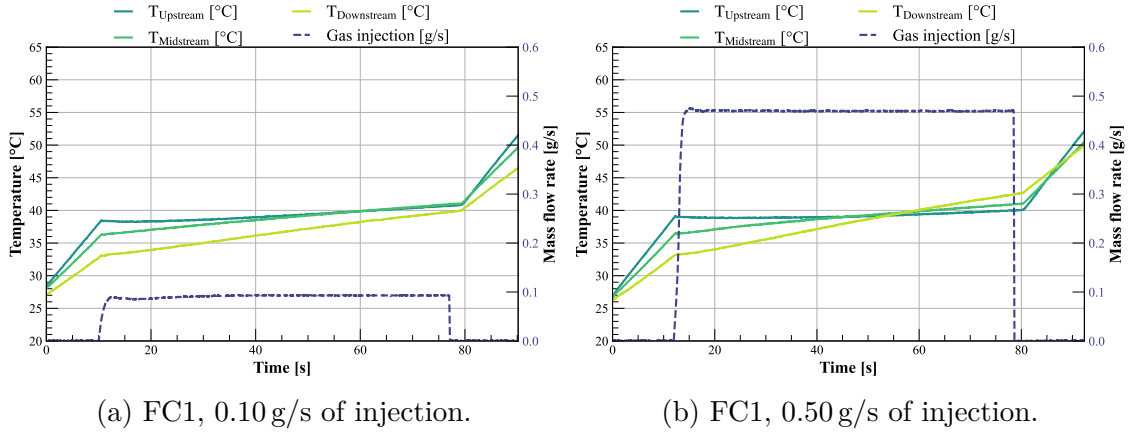


Figure 5.12: Qualitative surface temperature time history at three specific points, corresponding to thermocouple locations.

sent the temperature measurements at the upstream, midstream, and downstream thermocouples, while the orange line shows the gas injection mass flow rate.

Temperature trends are mostly linear, with the exception of the first seconds after injection starts, because the injection rates need some time to stabilize. The temperature rate decreases significantly when the injection process starts. After it ends, the temperature rate increases again, with a slight delay due to the gas still present in the injector reservoir after it is switched off.

Without injection, the upstream temperature rates are higher than the downstream ones. With injection, the upstream temperature rates drop below the downstream ones—in particular, with 0.50 g/s injection, the temperature rate of the upstream location is negative during injection. This causes a “temperature inversion” between upstream and downstream locations, which occurs after a certain time depending on the injection mass flow rate. In fact, with 0.50 g/s injection, the inversion clearly happens at approximately $\Delta t = 50$ s. With 0.10 g/s injection, the inversion

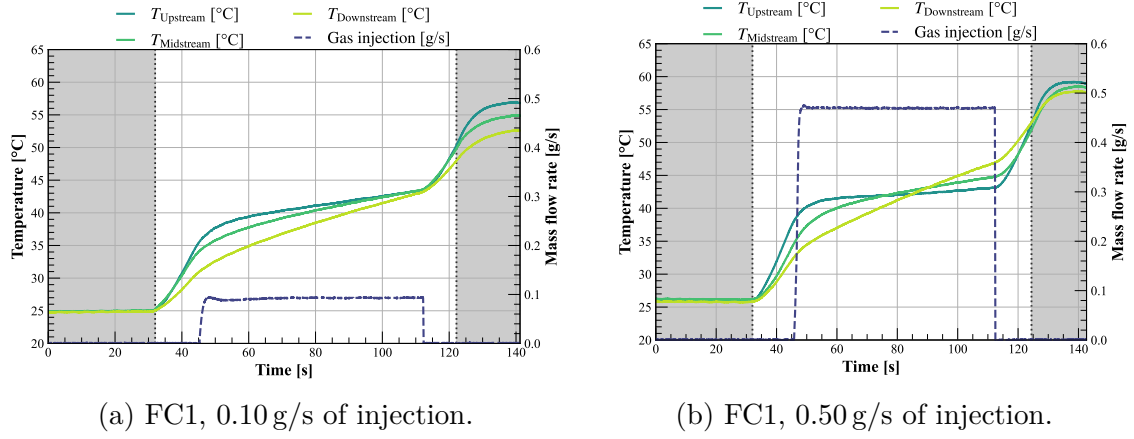


Figure 5.13: Time history of the backside temperature of the ceramic plate measured by thermocouples.

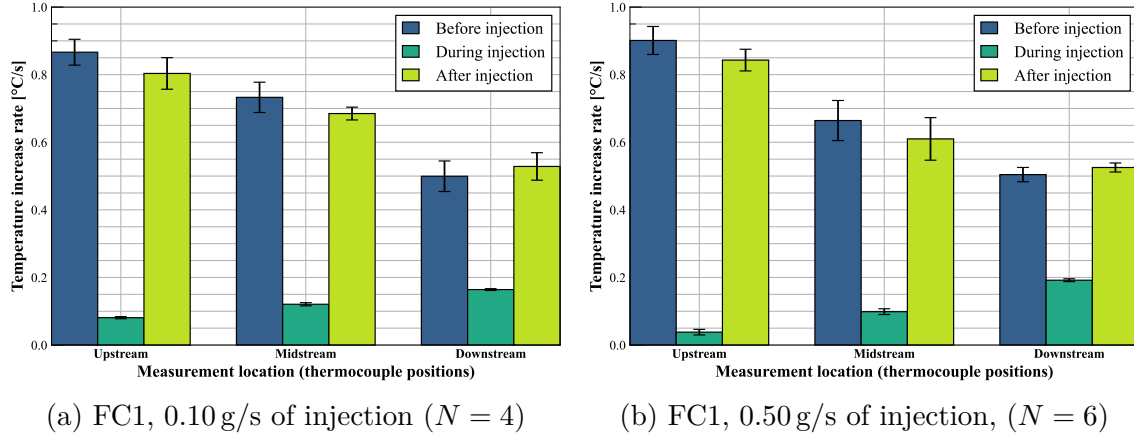


Figure 5.14: Mean rate of temperature increase during each phase. Error bars indicate standard deviation.

is visible between upstream and midstream locations only. Lower injection rates would require a longer test time to cause a temperature inversion with the downstream location. These observations show that the film cooling process depends on the injection mass flow rate; the higher the injection, the stronger the effect.

Thermocouple data measured on the backside of the ceramic plate at the same axial locations are shown in Figure 5.13, for FC1 with two targeted gas injection rates of 0.10 g/s and 0.50 g/s, respectively, with the same layout as in Figure 5.12. In the figure, the horizontal axis represents the time elapsed since stabilization of the plasma freestream. The left vertical axis shows the measured temperature, while the right vertical axis indicates the mass flow rate of the injected gas. These results should not be confused with surface temperature, as the measuring point lies on the backside of the 1 mm-thick ceramic plate, which introduces a response-time delay. However, their interpretation clearly highlights the mitigating effect

of film cooling on aerothermal heating. By comparing thermocouple data with IR data, it is clear that thermocouple profiles show less abrupt temperature-rate variation when injection starts: a relaxation time is needed to establish the linear temperature trend. This relaxation is due to the heat conduction process across the ceramic plate. No quantitative comparisons should be made due to the unknown plate emissivity; the slightly higher thermocouple temperatures compared with the IR measurements are due to the emissivity value of 1 used for the plate. In reality, one would expect the IR-measured surface temperatures to be higher than those recorded by the thermocouples.

To aid interpretation of the time histories in Figure 5.13, the colored background bands indicate the five steps introduced in subsection 5.1.4. Below, we summarize the observed temperature response in each step.

Step 1 (initial gray band; *off-axis*): temperatures remain stable at room temperature. Step 2 (*on-axis*, without injection): temperatures at all stations increase almost linearly. The upstream thermocouple shows the highest heating rate due to stronger stagnation heating. Step 3 (*on-axis*, injection on): once the mass flow rate of the injection stabilizes (~ 10 s), the heating rate drops markedly, indicating the effectiveness of the film cooling in reducing the surface heat flux. The reduction is stronger at 0.50 g/s, and at 0.50 g/s an upstream-downstream inversion of the temperature appears after ~ 30 s. Step 4 (*on-axis*, injection off): after a short delay, heating resumes rapidly, consistent with residual gas contained in the injector supply channel. Step 5 (final gray band; *off-axis*): temperatures gradually decrease as the model exits the freestream.

To confirm the repeatability of the experiments, identical tests were performed multiple times. The results are summarized in Figure 5.14. In this figure, the period during which the experimental model was exposed to the freestream under FC1 conditions is divided into three phases: before gas injection, during gas injection, and after gas injection. For each phase, the temperature increase rate was calculated by linear interpolation of the thermocouple measurements. The error bars in the figure indicate the standard deviation of the repeated measurements.

The error bars are sufficiently small, indicating that the thermal environment inside the L2K facility is highly repeatable. Across all measurement positions, the temperature increase rate before gas injection was the highest, approximately 0.9°C/s at the upstream location. Upon gas injection, the rate of increase in temperature was significantly reduced to less than 10 % of the pre-injection rate.

At the upstream measurement point, the temperature increase rate was reduced to 0.067°C/s at 0.10 g/s injection, and further reduced to 0.035°C/s at 0.50 g/s

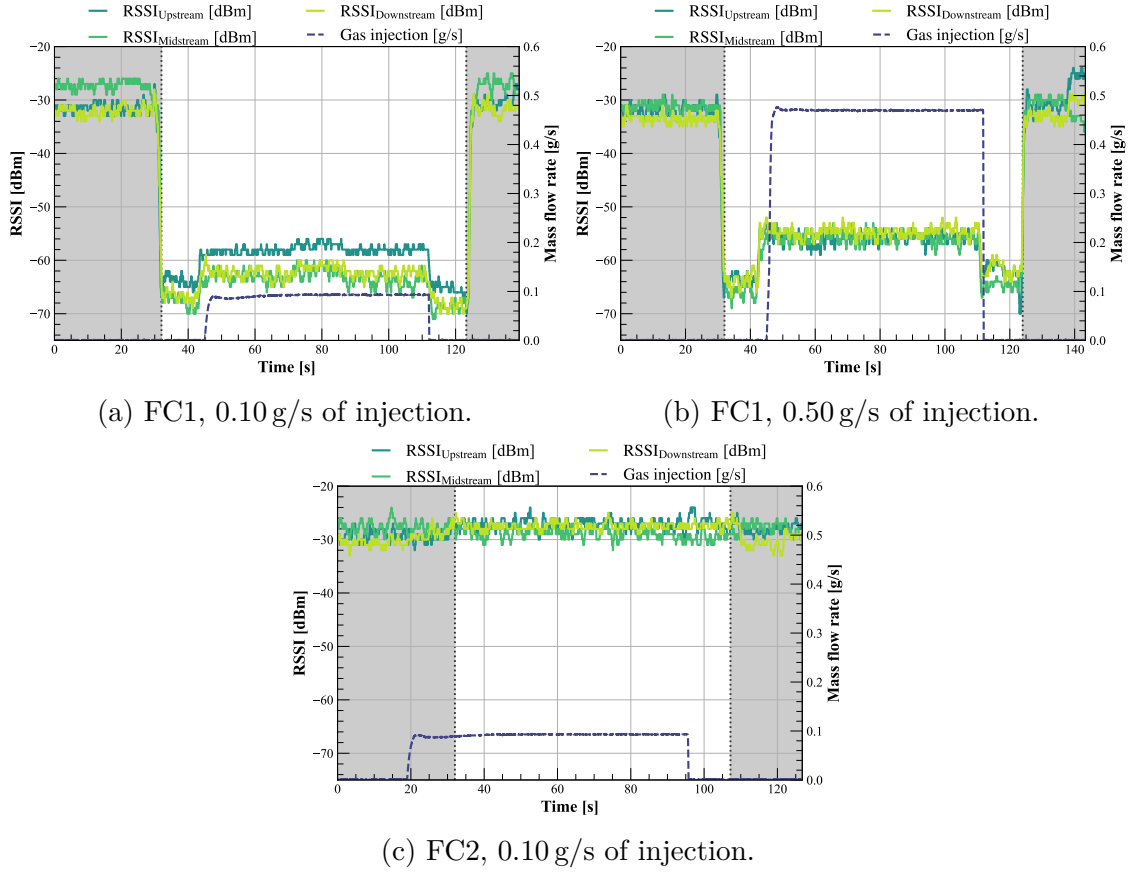


Figure 5.15: Time history of receiving signal strength intensity (RSSI).

injection. This indicates a slight improvement in cooling effectiveness with higher injection rates at the upstream location. However, at the midstream and downstream positions, no significant differences were observed between the two injection cases.

The results therefore suggest that under the present L2K test conditions, gas injection at 0.10 g/s is already sufficient to achieve a substantial reduction in aerodynamic heating.

It is also noted that the temperature increase rate after gas injection remained lower than the pre-injection rate. This behavior is attributed to the elevated surface temperature at the start of each phase, which affects the heat transfer dynamics.

Thus, it was experimentally confirmed that gas injection effectively reduces aerodynamic heating in the L2K facility.

5.2.3 Communication Blackout Mitigation

Figure 5.15 shows the time history of the received signal strength indicator (RSSI) under inflow conditions FC1 and FC2. The RSSI was measured by receivers located

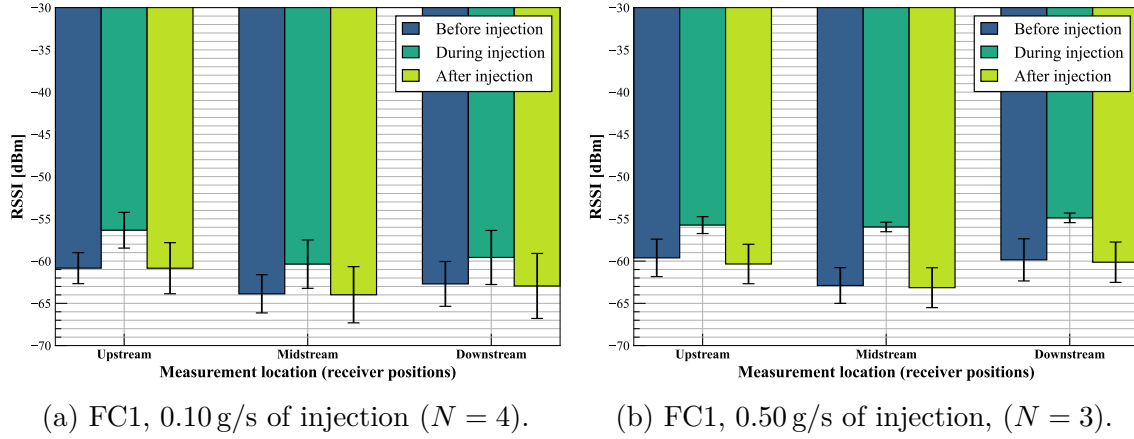


Figure 5.16: Mean signal intensity during each phase. Error bars indicate standard deviation.

outside the main flow at the upstream, midstream, and downstream positions, as illustrated in Figure 5.6. RSSI is expressed as the logarithmic power of the received signal intensity relative to a 1 mW reference, where a higher RSSI value indicates stronger reception and better communication quality. The gray regions in the figure correspond to the phases when the model was outside the main flow.

In FC1, when the model was moved into the arc-heated flow (Step 2), a sharp decrease in RSSI was immediately observed, as shown in Figure 5.15a and Figure 5.15b. In contrast, under FC2, where nitrogen was added to the freestream, no significant change in RSSI occurred throughout the experiment (Figure 5.15c). The signal strength remained at approximately -30 dBm, regardless of the model position or gas injection. This value is nearly identical to the RSSI recorded under FC1 before the model was exposed to the flow, indicating that electron number density in FC2 was significantly suppressed by the nitrogen admixture. These results confirm that communication attenuation observed under FC1 was caused by the plasma in the freestream, successfully reproducing the blackout phenomenon associated with atmospheric reentry.

Once gas injection began under FC1 (Step 3), RSSI increased and stabilized after the injection flow rate reached the target value. This improvement in communication was consistently observed at all three receiver locations, demonstrating the effectiveness of gas injection in mitigating signal attenuation. Furthermore, the results suggest that the degree of improvement becomes more pronounced with higher gas injection mass flow rates.

After gas injection terminated (Step 4), the communication signal was attenuated again, and the RSSI decreased to the same level as observed before the gas injection. This occurred because the protective effect of the injected gas on electromagnetic

wave propagation ceased, allowing the plasma to once again attenuate the wireless communication.

When the model was retracted to the *off-axis* position, RSSI quickly recovered to approximately -30 dBm, matching the baseline value prior to exposure to the plasma flow.

These observations confirm that the reduction in RSSI was caused by the weakly ionized argon freestream and that the improvement during gas injection was due to the film cooling effect, which reduced electron number density near the model.

As with the aerodynamic heating reduction tests, repeated communication tests were conducted to verify the experimental reproducibility of the results. The average RSSI values during three distinct phases, before gas injection, during gas injection, and after gas injection, are summarized in Figure 5.16. Error bars represent the standard deviation. The small deviations observed confirm that the L2K arc-heated wind tunnel provided a stable and repeatable flow environment from the perspective of communication characteristics as well.

Furthermore, it is notable that the RSSI values before and after gas injection are identical in all cases, indicating that the plasma flow conditions remained consistent throughout the tests. These results, combined with observations from the cooling experiments, suggest that the L2K is capable of maintaining a stable and homogeneous high-enthalpy flow environment for several minutes with high reproducibility, both thermally and electromagnetically.

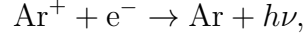
In all measurement positions and for both gas flow rates, an improvement in the received signal strength was clearly observed during gas injection. This improvement is statistically significant. In particular, at the midstream position, a gas flow rate of 0.50 g/s resulted in an approximately 5 dB increase in RSSI, demonstrating the effectiveness of higher injection rates.

The variation in communication attenuation and recovery trends among the upstream, midstream, and downstream receivers was relatively small. Possible reasons for this limited variation include the measurement resolution of the RSSI and the reflections of the electromagnetic waves from the chamber walls.

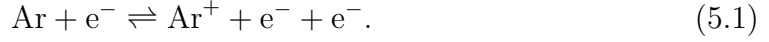
5.3 Argon Flow Model

To reproduce the flow conditions of the L2K arc-heated facility, the plasma-flow model described in section 3.2 is extended from an air-based chemical system to an argon plasma system. Several reaction models for argon plasma have been proposed, including those that explicitly account for metastable species. However,

Katsurayama et al. [103] reported that the mole fraction of metastable argon atoms in arc-heated flows is below 0.001%, implying a negligible contribution to the overall plasma composition. Furthermore, Sanden et al. [104] and Skrzypkowski et al. [105] showed that collisional – radiative recombination,



becomes significant only in low-temperature, low-density plasmas where the electron temperature is on the order of 300 K and the electron number density is approximately $1.0 \times 10^{19}/\text{m}^3$. By contrast, for argon plasmas typical of arc-heated conditions with $1,000 \text{ K} < T_{\text{ele}} < 5,000 \text{ K}$ and $n_{\text{ele}} > 1.0 \times 10^{18}/\text{m}^3$, the effect of radiative recombination is negligible. Accordingly, in this study only the electron-impact ionization reaction is considered:



The backward rate coefficient k_{b} was originally given by Owano et al. [106] as

$$k_{\text{b}} = 3.30 \times 10^{-32} \left(\frac{\Theta_{\text{a1}}^*}{T_{\text{ele}}} + 2 \right) \exp \left(\frac{\Theta_{\text{ion}} - \Theta_{\text{a1}}^*}{T_{\text{ele}}} \right) N_A \quad [\text{cm}^6/\text{s}], \quad (5.2)$$

where $\Theta_{\text{a1}}^* = 135,300 \text{ K}$ and $\Theta_{\text{ion}} = 183,100 \text{ K}$ are the characteristic temperatures associated with the first excited level and the ionization energy of argon, respectively; thus $\Theta_{\text{ion}} - \Theta_{\text{a1}}^* = 47,800 \text{ K}$ corresponds to single ionization from the first excited state.

Because the above model deteriorates in accuracy at lower temperatures such as $T_{\text{ele}} < 3,000 \text{ K}$ [107], the formulation by Funahashi and Takeda is adopted below a switching temperature:

$$k_{\text{b}} = 5.6 \times 10^{-21} T_{\text{ele}}^{-9/2} \quad [\text{m}^6/\text{s}], \quad (5.3)$$

and the two expressions are smoothly connected at $T_{\text{ele}} = 3,770 \text{ K}$.

Following Hoffert and Lien [108], the equilibrium constant for the above reaction can be expressed as

$$K^{\text{eq}}(T_{\text{ele}}) = 2.9 \times 10^{24} T_{\text{ele}}^{3/2} \exp \left(-\frac{\theta_{\text{ion}}}{T_{\text{ele}}} \right) \quad [1/\text{cm}^3]. \quad (5.4)$$

The forward rate coefficient k_{f} is then obtained from detailed balance as

$$k_{\text{f}} = k_{\text{b}} K^{\text{eq}}. \quad (5.5)$$

Table 5.2: Argon chemical reaction rates in Arrhenius form.

r	Reactants	Products	C_r	n_r	θ_r [K]	Range
50	$\text{Ar} + \text{e}^-$	$\rightleftharpoons \text{Ar}^+ + \text{e}^- + \text{e}^-$	9.8×10^{31}	-3.0	183,100	$T_{\text{ele}} < 3,770 \text{ K}$
			7.8×10^{21}	0.5	135,300	$T_{\text{ele}} > 3,770 \text{ K}$

In the present analysis, the factor $\Theta_{\text{a1}}^*/T_{\text{ele}} + 2$ in Equation 5.2 is approximated by $\Theta_{\text{a1}}^*/T_{\text{ele}}$, based on $\Theta_{\text{a1}}^* \gg T_{\text{ele}}$. This approximation enables an Arrhenius-type representation of k_{f} , analogous to Equation 3.8, which facilitates implementation in the existing CFD solvers. Under this assumption, the forward rate in the Owano model becomes

$$k_{\text{f}} = 7.798 \times 10^{13} T_{\text{ele}}^{1/2} \exp\left(-\frac{\Theta_{\text{a1}}^*}{T_{\text{ele}}}\right) \quad [\text{cm}^6/\text{s}]. \quad (5.6)$$

Likewise, the Funahashi model can be cast into the Arrhenius form; following the convention of Table 3.2, the chemical reaction rate is summarized as $r = 50$ in Table 5.2.

Binary collision cross-sections required for transport and reaction modeling are taken from Laricchiuta et al. [109].

5.4 L2K Arc Heater Flow Model

Arc heaters generate high-enthalpy test flows by means of an arc discharge that deposits electrical energy into the working gas. Within the discharge channel, a high-temperature, low-density, high-velocity arc column is surrounded by a low-temperature, high-density, low-velocity cold-gas region, so that the flowfield inside the heater is highly nonuniform in the radial direction. In principle, such nonuniformity can be resolved by coupled simulations of the discharge field and the plasma flow CFD, and this approach has been successfully demonstrated for the ISAS 1 MW tunnel [95] and for L2K with air plasma condition [110]. However, these coupled computations are extremely expensive: the rapid arc dynamics impose time steps several orders of magnitude smaller than the flow convective timescale, and a very large number of iterations is required to obtain a converged, statistically steady solution.

To avoid this cost while retaining the essential physics of the core flow, the heating section of the L2K arc heater under argon plasma conditions was modeled assuming local thermochemical equilibrium with prescribed radial distributions of temperature and velocity. The total pressure, mass flow rate, and mass-averaged

Table 5.3: Reservoir conditions for the L2K configuration FC1.

Parameter	Value
Total pressure	37.5 kPa
Total mass flow rate	20.0 g/s
Total temperature*	3,886 K
Specific enthalpy*	2.0 MJ/kg
Working gas	Ar

*Calculated using the one-dimensional flow solver NATA [102].

enthalpy were fixed to the experimentally measured values for the test condition FC1 (Table 5.3). Under these assumptions, the arc-heater exit condition was defined as a one-temperature, axisymmetric, steady flow in which pressure is spatially uniform and chemical compositions correspond to the equilibrium state determined by local temperature and density.

5.5 Flowfield Reconstruction

CFD analysis was conducted to reproduce the argon plasma flowfield under FC1 inside the L2K facility. Because the arc-heated flow primarily consists of argon, only the ionization reaction



was considered in the present modeling. In an initial, conventional approach, the inflow at the nozzle entrance was assumed to be radially uniform, neglecting the nonuniform energy deposition in the discharge channel. Simulations based on this simplified inflow model produced electron number densities near the nozzle exit that were roughly four orders of magnitude smaller than both the experimental measurements and the critical density required to cause measurable RF attenuation at 2.4 GHz, indicating that the model could not numerically reproduce the plasma freestream.

To address this discrepancy, the inflow conditions obtained from the arc-heater model were prescribed with a radial distribution representative of the nonuniform arc-heated gas. This approach emulates the energy deposition within the heater without the need for direct coupling with the discharge field, thereby reducing computational cost while retaining the essential nonuniformity of the core flow.

5.5.1 Radial Profiles at the Heater Exit

The inflow profiles of temperature, pressure, velocity, and chemical composition at the exit of the arc heater were prescribed to emulate the nonuniform core flow generated by the arc discharge. The radial temperature distribution was modeled by a Gaussian function centered on the axis:

$$T(r) = \frac{T_0}{\sqrt{2\pi}\sigma} \exp\left[-\frac{(r/R - \mu)^2}{2\sigma^2}\right], \quad (5.7)$$

where T_0 represents the reference (peak) temperature, σ controls the radial width of the high-temperature core, and $R = 15$ mm is the nozzle radius. Since the highest temperature is located at the center of the arc column, the mean position parameter is set to $\mu = 0$. This Gaussian form reproduces the characteristic profile of an arc-heated flow, featuring a sharply peaked temperature near the centerline and a rapid decay toward the wall.

The static pressure $p(r)$ is assumed to be uniform across the radius and set equal to the experimentally measured value in Table 5.3. The corresponding density $\rho(r)$ is then calculated from the ideal-gas equation of state using the prescribed temperature and pressure fields.

$$p(r) = \rho(r)R_gT(r), \quad (5.8)$$

where R_g is the specific gas constant of argon.

The flow velocity distribution is approximated by a parabolic pipe-flow profile in the axial direction:

$$u(r) = u_0 \left[1 - \left(\frac{r}{R}\right)^2\right], \quad (5.9)$$

where u_0 is the reference velocity at the centerline. This parameter is determined so that the integral of $\rho(r)u(r)$ over the cross-section equals the experimentally obtained total mass flow rate of $\dot{m} = 20.0$ g/s:

$$\dot{m} = 2\pi \int_0^R \rho(r) u(r) r dr. \quad (5.10)$$

Assuming local thermochemical equilibrium, the molar fractions of Ar, Ar^+ , and e^- are evaluated using the open-source thermochemical library **Mutation++** [111]. **Mutation++** provides equation of state, thermodynamic, and transport properties for multi-species, multi-temperature gas mixtures in high-enthalpy and plasma conditions; in the present study it is used to obtain equilibrium compositions and specific internal energies from the local state. For a given pair of $(T(r), \rho(r))$ values, the

equilibrium composition is uniquely determined.

Finally, the reference temperature T_0 is adjusted iteratively so that the mass-averaged enthalpy of the inflow matches the experimentally measured value of $H_{\text{ave}} = 2.0 \text{ MJ/kg}$:

$$h(r) = e(T) + \frac{p}{\rho} + \frac{u^2}{2},$$

$$H_{\text{ave}} = \frac{\int_0^R \rho(r) u(r) h(r) r dr}{\int_0^R \rho(r) u(r) r dr}. \quad (5.11)$$

Here, $e(T)$ denotes the specific internal energy per unit mass, so that $h(r)$ represents the total specific enthalpy including kinetic contributions. This iterative matching ensures that the prescribed inflow maintains thermodynamic consistency with the arc-heated reservoir conditions while retaining a realistic radial gradient representative of the L2K core flow.

5.5.2 Radial Flow Profiles at the Nozzle Inlet

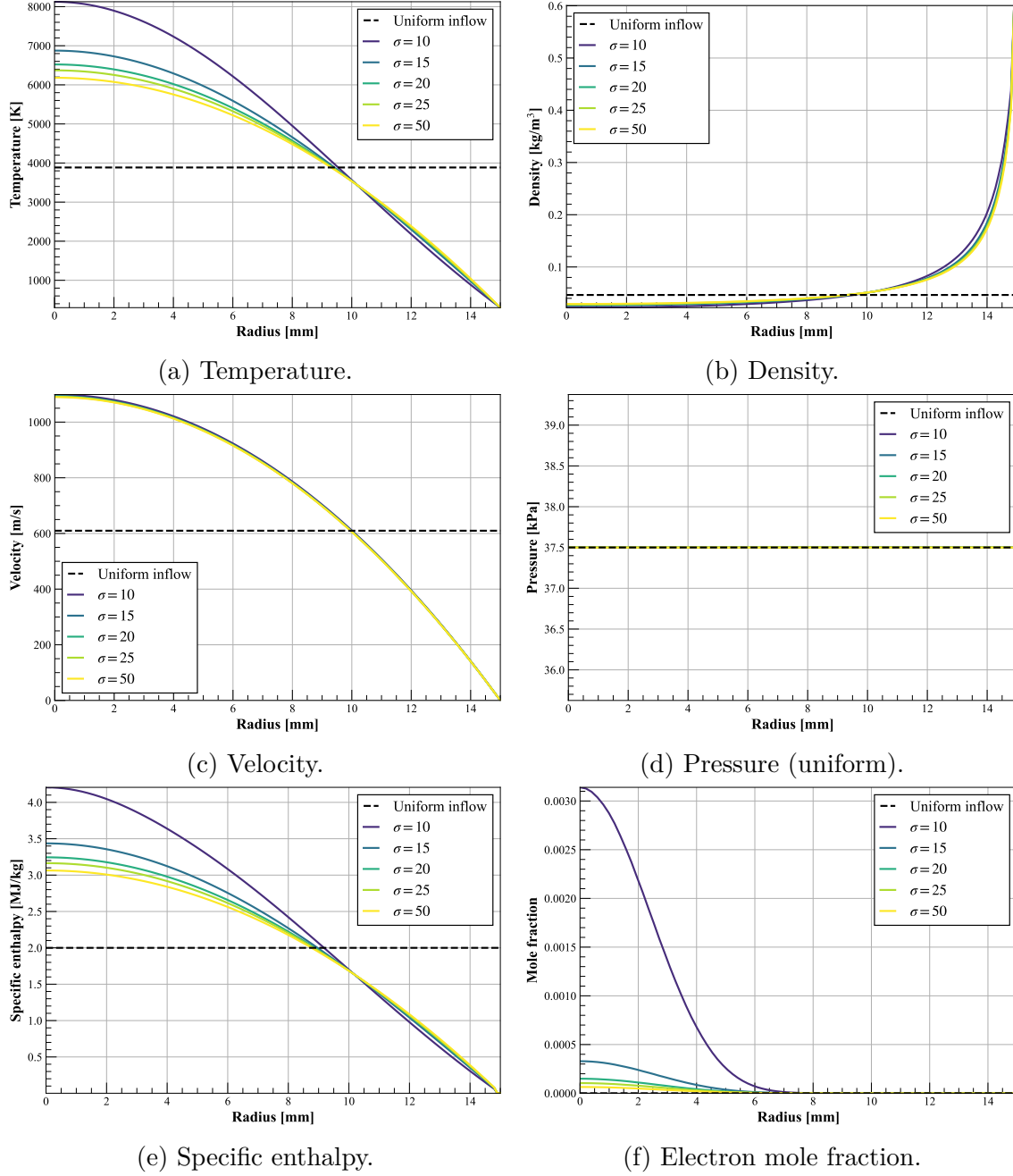


Figure 5.17: Radial distributions at the nozzle inlet for different σ values.

The prescribed profiles of temperature, pressure, density, velocity, specific enthalpy, and electron mole fraction at the heater exit are shown in Figure 5.17. These distributions define the inflow condition for the L2K nozzle simulation. The parameter σ controls the radial width of the high-temperature core in the Gaussian temperature profile defined by Equation 5.7. By decreasing σ , the temperature becomes more

concentrated near the centerline, representing a narrower and hotter arc column.

The pressure is uniform across the radius and fixed at the experimentally measured value of 37.5 kPa, consistent with the assumption of a uniform static pressure in the heating section. The density profile, computed from the ideal-gas equation of state, decreases toward the center where the temperature is highest; an inverse correlation between T and ρ is observed.

The velocity follows a parabolic distribution with the maximum value on the centerline. As σ decreases, the high-temperature region contracts and the central density decreases, slightly increasing the core velocity to satisfy mass conservation. The resulting enthalpy profile shows a strong dependence on σ ; smaller σ values produce higher enthalpy near the axis, effectively reproducing the high-enthalpy core flow characteristic of the L2K arc-heated stream.

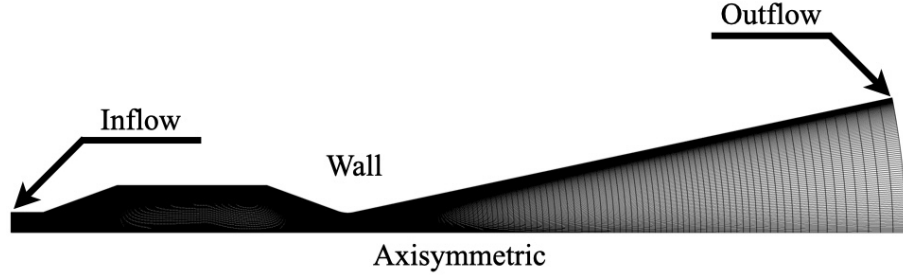
Assuming local thermochemical equilibrium, the electron mole fraction X_{ele} is determined from (T, ρ) through the thermochemical library, `Mutation++` [111]. For $\sigma = 10$, the centerline electron mole fraction reaches 3.0×10^{-3} , whereas it drops to 3.0×10^{-4} for $\sigma = 15$. This trend confirms that the reduced width parameter enhances both ionization and enthalpy in the core region, aligning with experimental observations of the nonuniform plasma jet in the L2K facility.

5.6 Numerical Simulation of the L2K Nozzle Flow

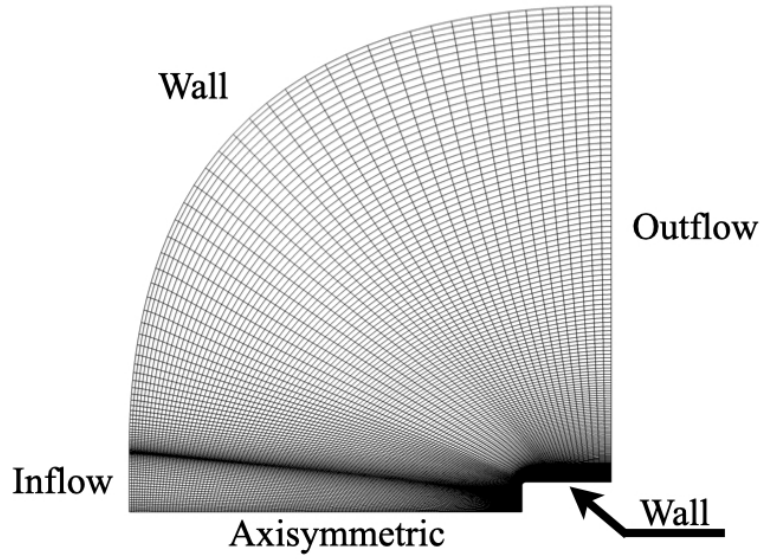
The prescribed nonuniform radial profiles with various σ are applied as the inflow condition for a two-dimensional axisymmetric nozzle flow simulation. This analysis aims to investigate how the degree of nonuniformity in the heater exit flow, characterized by the width parameter σ , influences the internal core flow structure of the L2K nozzle. By comparing these cases, the role of the nonuniform core flow in shaping the thermochemical state at the nozzle exit can be quantitatively assessed.

5.6.1 Computational Grid

Figure 5.18a shows the computational domain and grid structure used for the two-dimensional axisymmetric nozzle flow simulation. The grid extends from the heater exit to the nozzle throat and further to the nozzle exit plane. The total number of grid points is 335×101 . A structured body-fitted mesh is generated with finer spacing near the wall and the throat region to resolve strong gradients in velocity and temperature. The wall boundary is treated as a no-slip, isothermal wall at 300 K, whereas the outlet boundary was assigned a zero-gradient condition.



(a) Nozzle.



(b) Chamber.

Figure 5.18: Computational grid for the two-dimensional axisymmetric CFD analysis.

5.6.2 Flow Characteristics inside the Nozzle

The distribution of Mach number, temperature, and electron number density for different inflow profiles are presented in Figure 5.19, Figure 5.20, and Figure 5.21. All cases exhibit similar Mach number field. The flow accelerates from subsonic to supersonic speeds through the throat, reaching $M = 1$ at the minimum cross-section and continuing to increase toward the nozzle exit. This indicates that the overall expansion behavior is nearly identical regardless of the assumed radial nonuniformity at the inlet.

In contrast, the translational temperature field shows a distinct radial nonuniformity upstream of the throat, especially in the cases with smaller σ . Downstream of the throat, the temperature decreases monotonically as the gas expands, and the

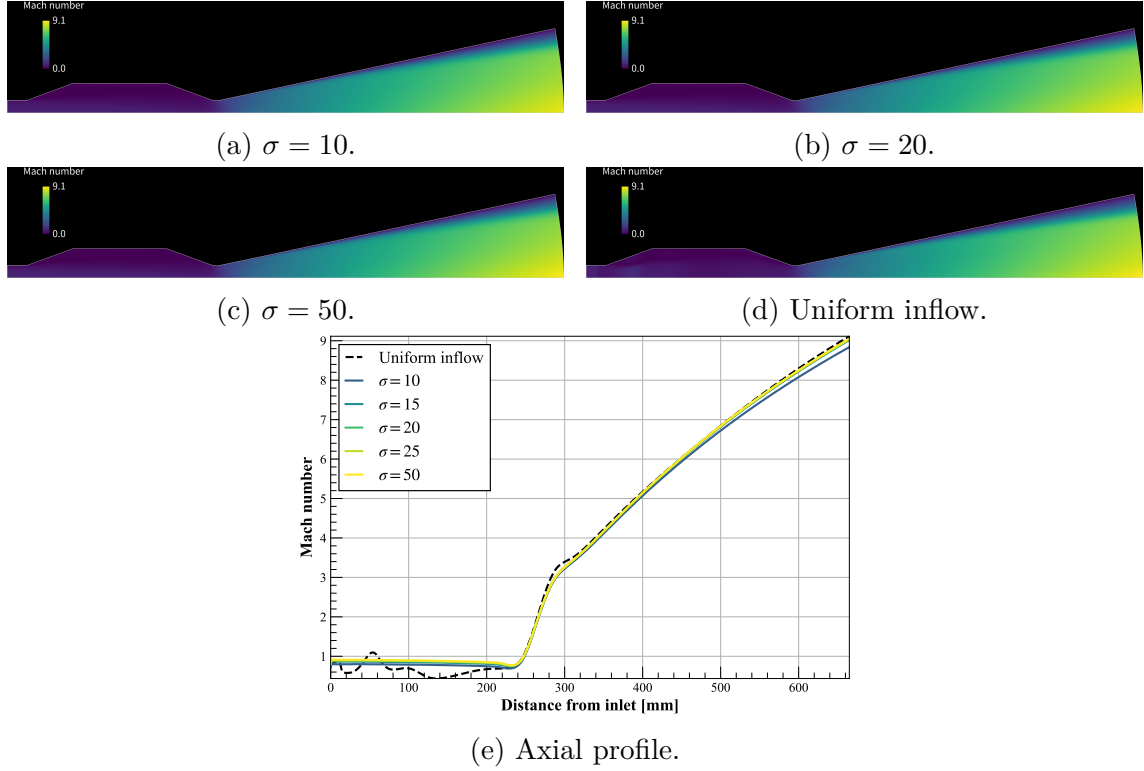


Figure 5.19: Mach number distributions inside the nozzle for different inflow profiles.

profiles among all cases become similar due to adiabatic cooling. Thus, the thermal nonequilibrium created in the arc-heated region is gradually smoothed out by the expansion process.

A remarkable difference appears in the electron number density distributions. For $\sigma = 10$, the peak electron density near the axis reaches approximately 10^{21} m^{-3} , while the Uniform-inflow case yields only about 10^{15} m^{-3} . This indicates that the degree of ionization and the resulting plasma conductivity strongly depend on the radial concentration of thermal energy at the heater exit. The nonuniformity in electron density persists up to the nozzle exit, implying that the energy deposition localized in the core region produces a highly ionized plasma jet surrounded by a colder, weakly ionized sheath.

These centerline profiles confirm that, although the overall flow expansion is nearly identical among the cases, the electron number density—and thus the plasma conductivity—strongly depends on the degree of radial nonuniformity in the arc-heated inflow.

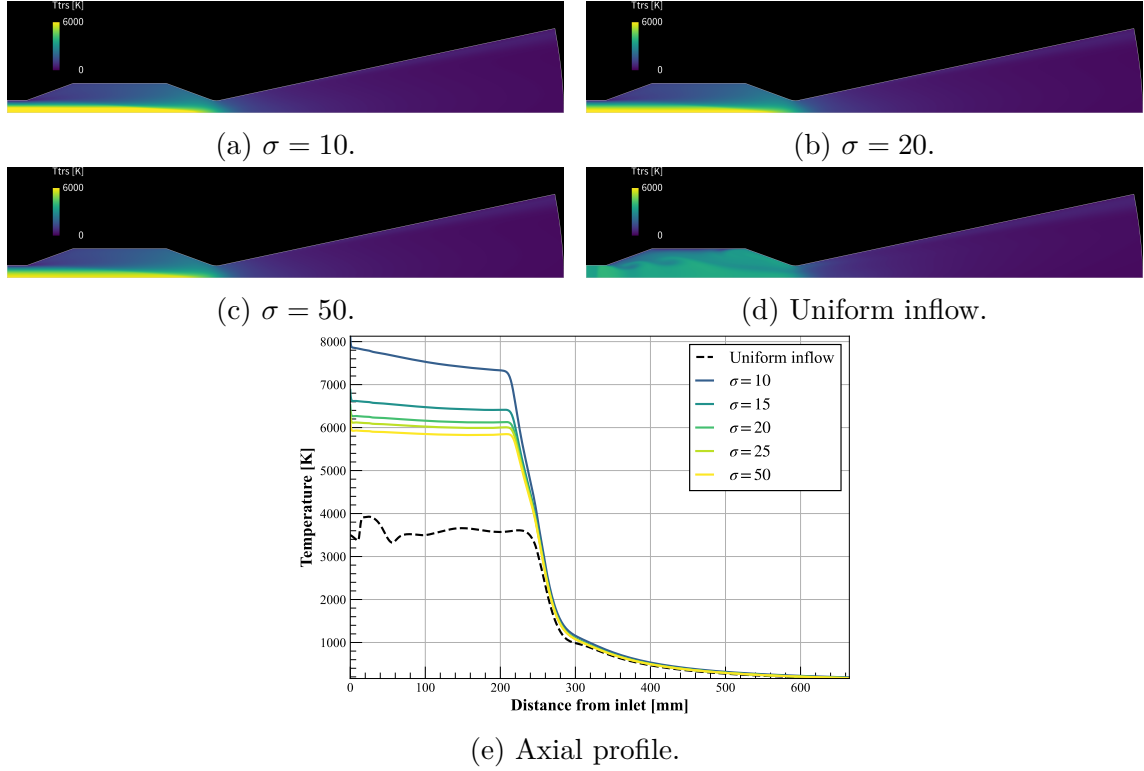


Figure 5.20: Temperature distributions inside the nozzle for different inflow profiles.

5.7 Chamber Flow Analysis

The chamber flow was simulated using the radial profiles at the nozzle exit obtained from the preceding nozzle analysis as inflow boundary conditions. The objectives of this analysis are twofold: first, to validate the present modeling approach by comparing the computed flow characteristics with the experimental measurements by Steffens et al. [59], and second, to identify the value of σ that best reproduces the measured distributions, which will then be adopted for the subsequent three-dimensional simulation.

5.7.1 Computational Grid

The computational grid for the two-dimensional axisymmetric chamber is shown in Figure 5.18b. The inflow boundary conditions were prescribed using the flow properties obtained at the nozzle exit from the preceding analysis. The experimental probe is located 330 mm downstream of the inflow boundary. The chamber wall and the surface of the probe were treated as no-slip and isothermal boundaries with a wall temperature of 300 K. At the outflow boundary, a zero-gradient condition was applied for all variables. The computational domain consisted of 181×96 cells.

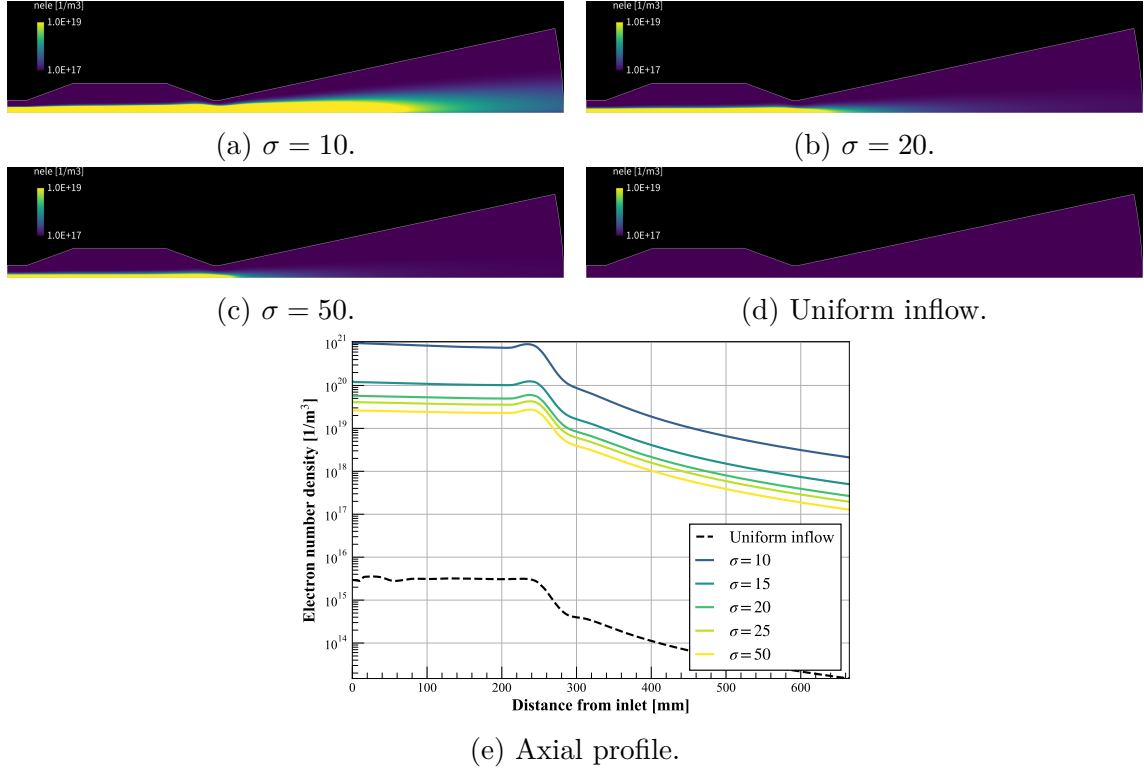


Figure 5.21: Electron number density distributions inside the nozzle for different inflow profiles.

5.7.2 Flow Characteristics inside the Chamber

The distributions of Mach number, temperature, and electron number density for different inflow profiles are presented in Figure 5.22, Figure 5.23, and Figure 5.24, respectively. A strong shock wave forms in front of the probe, followed by a steep temperature rise due to adiabatic compression in the post-shock region.

Similar to the nozzle analysis, both the Mach number and temperature distributions exhibit little dependence on σ . The flow accelerates to a maximum Mach number of approximately 11, while the stagnation temperature remains around 4,000 K. The position of the shock wave is almost independent of the assumed inflow nonuniformity, suggesting that the overall compressive behavior of the flow is primarily governed by the geometric configuration rather than by the radial inflow distribution.

In contrast, the electron number density shows a strong sensitivity to the inflow condition. Electrons generated in the nozzle are advected downstream into the chamber without significant recombination losses. A local increase in electron number density is observed just ahead of the probe, where gas compression raises the total density. However, this enhancement originates from mechanical compres-

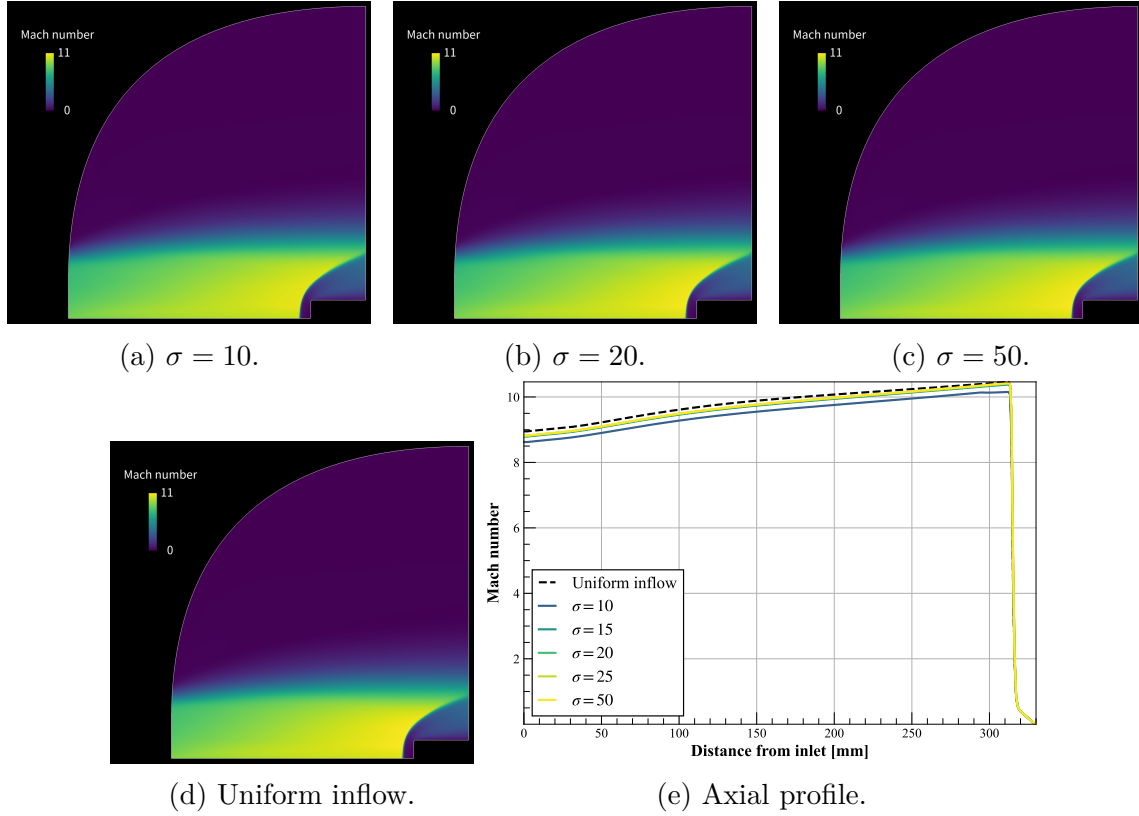


Figure 5.22: Mach number distributions inside the chamber for different inflow profiles.

sion rather than ionization or dissociation reactions, indicating that no chemical reactions occur in the chamber under the present test conditions.

When a spatially uniform inflow was prescribed, the electron number density at the stagnation point was on the order of 10^{13} m^{-3} . In contrast, when radial nonuniformity was introduced, the cases with smaller σ exhibited much higher electron number densities, reaching 10^{17} – 10^{18} m^{-3} . These values exceed the critical electron number density at 2.4 GHz (see Equation 1.3), which corresponds to the cutoff condition for electromagnetic wave propagation. Therefore, the numerical results successfully explain the communication attenuation observed in the experiment.

A quantitative comparison between the computed and experimental results is summarized in Table 5.4. The flow velocity and electron number density were measured using microwave interferometry (MWI), which provides the refractive index distribution based on the phase shift of transmitted microwaves. The mean flow velocity was also estimated from the time-of-flight between two interferometers. Temperature measurements were obtained using diode laser absorption spectroscopy (DLAS), in which a small amount of CO was seeded into the flow, and the absorption peaks near 2,330 nm were analyzed to infer the gas temperature. Although

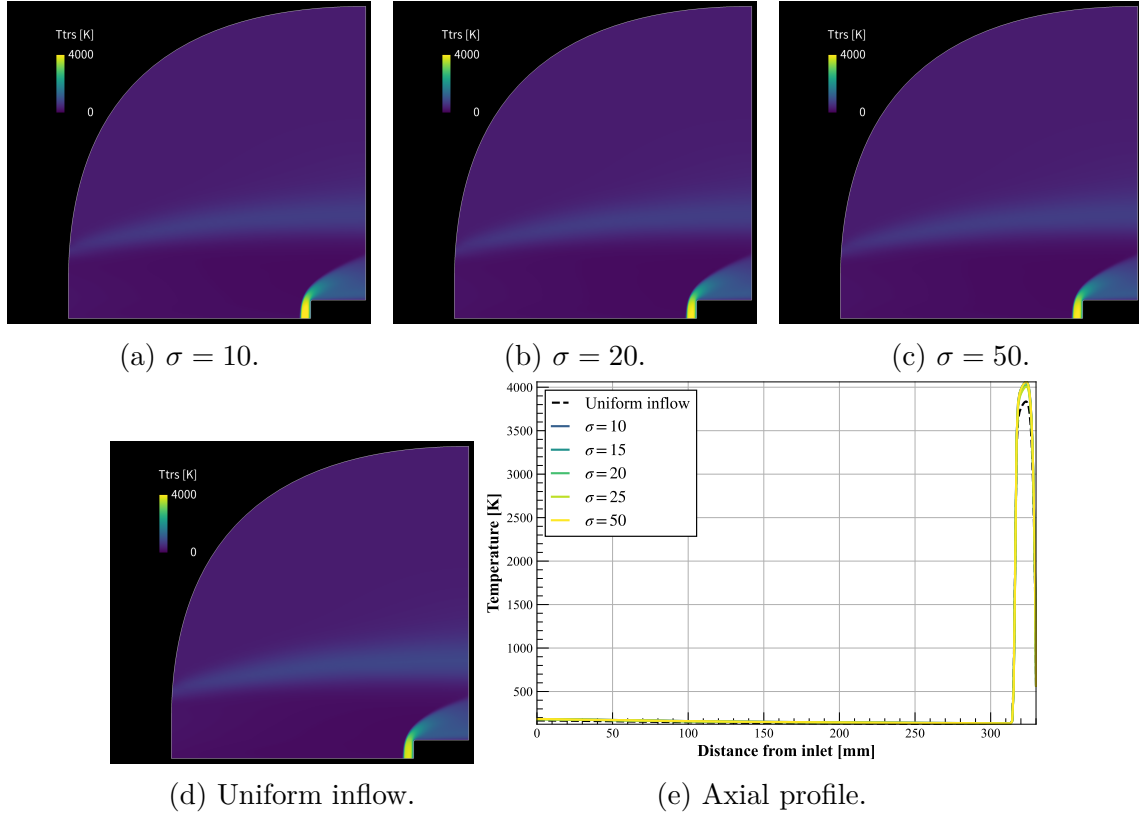


Figure 5.23: Temperature distributions inside the chamber for different inflow profiles.

flow velocity was also deduced from the Doppler shift of the spectral lines, previous studies reported that DLAS tends to slightly overestimate the velocity. Both MWI and DLAS were non-intrusive diagnostics that allow measurement of freestream quantities in the high-enthalpy plasma flow; however, the high-temperature, low-density, and high-velocity conditions in arc-heated flows make precise measurements extremely challenging.

In the present comparison, the electron number density is taken as the primary indicator of model validity, as it is the most relevant parameter for evaluating radio communication blackout. Among the simulated cases, $\sigma = 10$ and $\sigma = 15$ produced electron number densities closest to the experimental value, while the density decreases monotonically with increasing σ .

This suggests that the value of σ that provides the experimental electron number density lies within this range. Considering the measurement uncertainties, however, an exact identification is unnecessary. For the subsequent three-dimensional chamber simulations, the case with $\sigma = 10$ is selected as representative, corresponding to the condition most susceptible to communication blackout.

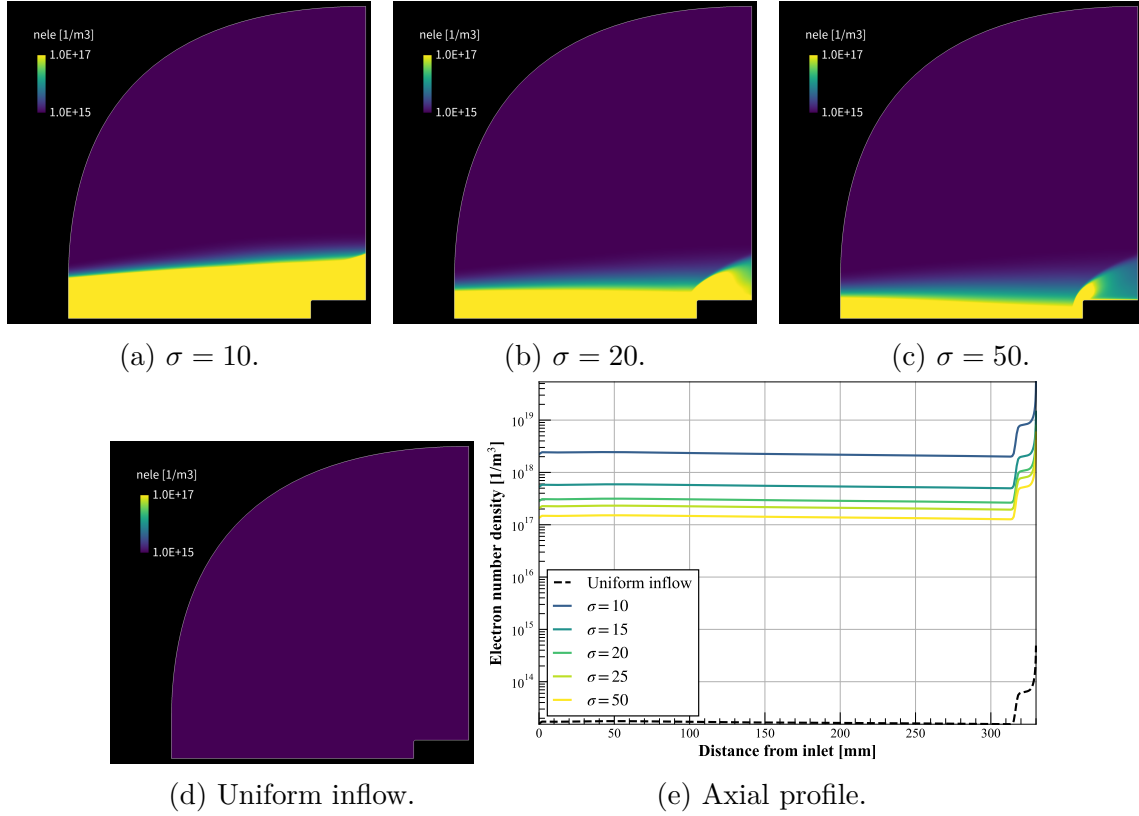


Figure 5.24: Electron number density distributions inside the chamber for different inflow profiles.

5.8 Three-Dimensional Chamber Flow Analysis

To further investigate the three-dimensional characteristics of the argon plasma flow and to evaluate the influence of gas injection on plasma distribution and wall heating, a full three-dimensional chamber simulation is performed. The inflow boundary condition is taken from the two-dimensional axisymmetric nozzle simulation corresponding to the representative nonuniform case ($\sigma = 10$), which was shown to reproduce the experimental electron density most accurately. The analysis is conducted for three cases: without injection, and with nitrogen gas injection at mass flow rates of 0.10 g/s and 0.50 g/s.

5.8.1 Computational Grid and Boundary Conditions

The three-dimensional chamber analysis is conducted using a computational model that replicates the wedge-shaped test model employed in the L2K experiments, shown in Figure 4.4. As shown in Figure 5.25a, the model features eleven cylindrical injection ports located on the upper surface, corresponding to the gas injection

Table 5.4: Comparison between experimental measurements and CFD results.

	Velocity [m/s]	Temperature [K]	Electron number density [$1/\text{m}^3$]
Experiment [59]			
MWI	$2,150 \pm 73$	–	$6.1 \pm 0.5 \times 10^{17}$
DLAS	2,350	1,800	–
CFD			
$\sigma = 10$	2,080	1,440	2.0×10^{18}
$\sigma = 15$	2,044	1,340	5.0×10^{17}
Uniform inflow	1,986	1,250	1.6×10^{13}

configuration used in the experiment. The model is positioned 160 mm downstream of the nozzle exit, matching the experimental setup.

A hybrid unstructured mesh is employed for the simulation. In the vicinity of the inflow region and along the model surface, a prism-layer mesh is generated to resolve near-wall gradients and capture the boundary-layer development. The remainder of the chamber is discretized using tetrahedral elements, providing geometric flexibility for the complex domain including the injection ports. The total number of cells in the computational domain is approximately 15.8 million. Figure 5.25a shows the overall computational grid, while Figure 5.25b provides a magnified view of the mesh around the model and injection region.

The boundary conditions for the chamber simulation are summarized in Table 5.5. The inflow boundary corresponds to the nozzle exit, where the radial profiles of velocity, temperature, pressure, and electron number density are mapped from the two-dimensional axisymmetric nozzle simulation and rotationally extruded to define the three-dimensional inlet condition. All solid walls are treated as no-slip and isothermal at 300 K. The outflow boundary is assigned a zero-gradient (Neumann) condition for all flow and plasma variables.

At the injection boundaries, pure nitrogen gas (N_2) is supplied at a fixed temperature of 300 K and a prescribed mass flow rate. The static pressure at each port is interpolated from the surrounding cell values to ensure local pressure continuity and numerical stability. Two injection conditions are simulated, corresponding to total mass flow rates of 0.10 g/s and 0.50 g/s.

To maintain numerical stability under low ambient pressure conditions, all external boundaries of the chamber (previously treated as open ambient regions) are modeled as adiabatic slip walls. The outer walls are placed sufficiently far from the model so that the reflected flow or pressure waves from the boundaries do not interfere with the shock wave formed in front of the model. This assumption effectively suppresses artificial inflow and numerical oscillations, while having a negligible influ-

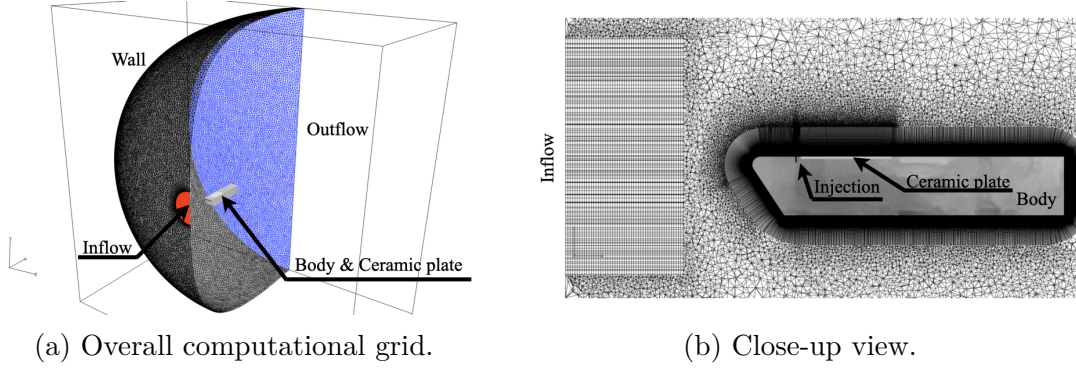


Figure 5.25: Computational mesh for three-dimensional CFD analysis in the L2K.

Table 5.5: Boundary conditions for the three-dimensional L2K chamber simulation.

Boundary	Description
Inflow	Mapped from 2D nozzle exit solution (rotationally extruded to 3D)
Body & Ceramic plate	No-slip, isothermal condition at 300 K.
Injection	N ₂ gas injected at fixed temperature (300 K) and mass flow rate; static pressure interpolated from neighboring cells.
Outflow	Zero-gradient condition for all variables.
Wall	No-slip, isothermal condition at 300 K.

ence on the internal flow structure, as the flow velocity near the chamber periphery is significantly smaller than that in the core region.

5.8.2 Flowfield without Gas Injection

To establish a reference flowfield for evaluating the effect of gas injection, a baseline simulation was first conducted for the L2K configuration without gas injection. Figure 5.26 shows the computed baseline flowfield in the L2K chamber without gas injection. Contours of Mach number, temperature, and electron number density are presented on the symmetry plane, while a three-dimensional view of the velocity field with streamlines near the model is shown to illustrate the local flow structure. The simulation captures key characteristics of an arc-heated supersonic plasma flow interacting with a wedge-shaped test article.

As seen in Figure 5.26a, the supersonic argon jet expands from the nozzle throat and reaches a Mach number of approximately 11 in the chamber core. Upon impingement on the leading edge of the model, a detached bow shock is formed, resulting in a subsonic stagnation region immediately ahead of the model. This is accompanied by a rapid increase in static temperature, which reaches about 4,000 K at the stagnation point shown in Figure 5.26b. Downstream of the model, the flow separates

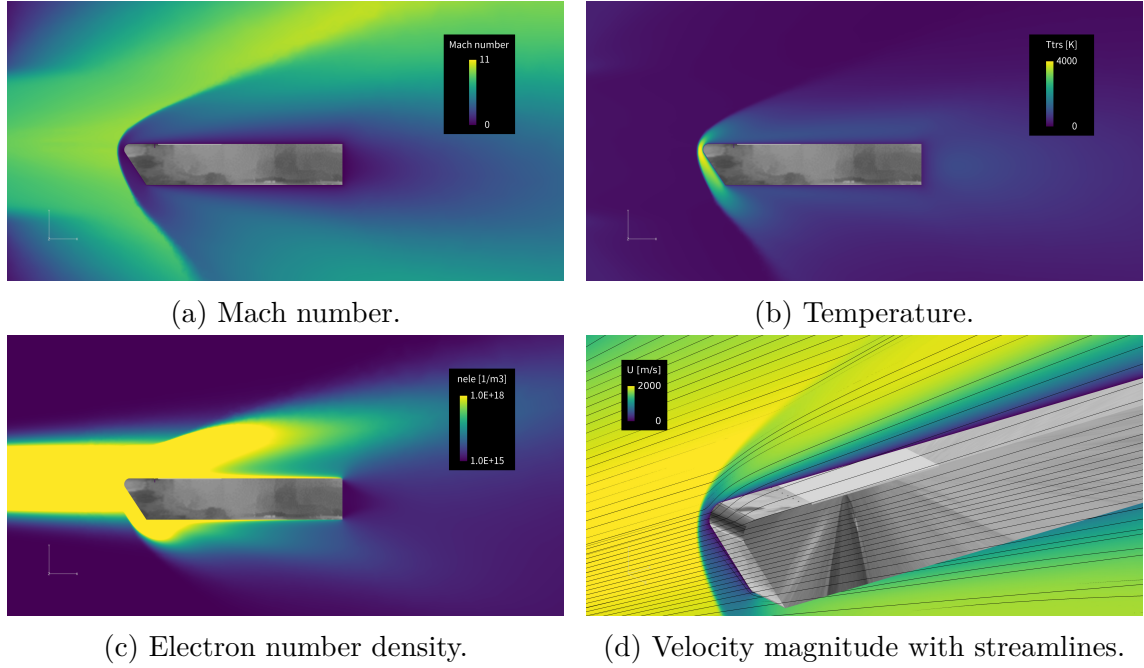


Figure 5.26: Baseline flowfield in the L2K chamber without gas injection.

and forms a low-velocity recirculation region that gradually merges with the main flow, consistent with the typical behavior observed in arc-heated facilities.

The electron number density distribution in Figure 5.26c shows a high-density plasma region extending from the nozzle core to the vicinity of the model front surface. The peak electron density occurs immediately behind the bow shock, where the local gas density is elevated due to compression. Because the local temperature of 4,000 K remains below the ionization threshold of argon, the high electron density is attributed mainly to convection from the nozzle core rather than in-situ ionization. The presence of a dense plasma layer covering the model surface is consistent with the experimental observation of radio-signal attenuation, reproducing the blackout condition within the chamber.

Figure 5.26d highlights the local three-dimensional flowfield around the model with the distribution of the freestream direction velocity, u . The flow maintains an overall steady and symmetric pattern, indicating that the steady-state CFD solution appropriately captures the key flow physics near the test model.

These results demonstrate that the present model successfully captures the essential features of the L2K argon plasma flow without gas injection, and thus provides a physically consistent baseline for analyzing the subsequent gas-injection cases and their influence on plasma attenuation and thermal loads.

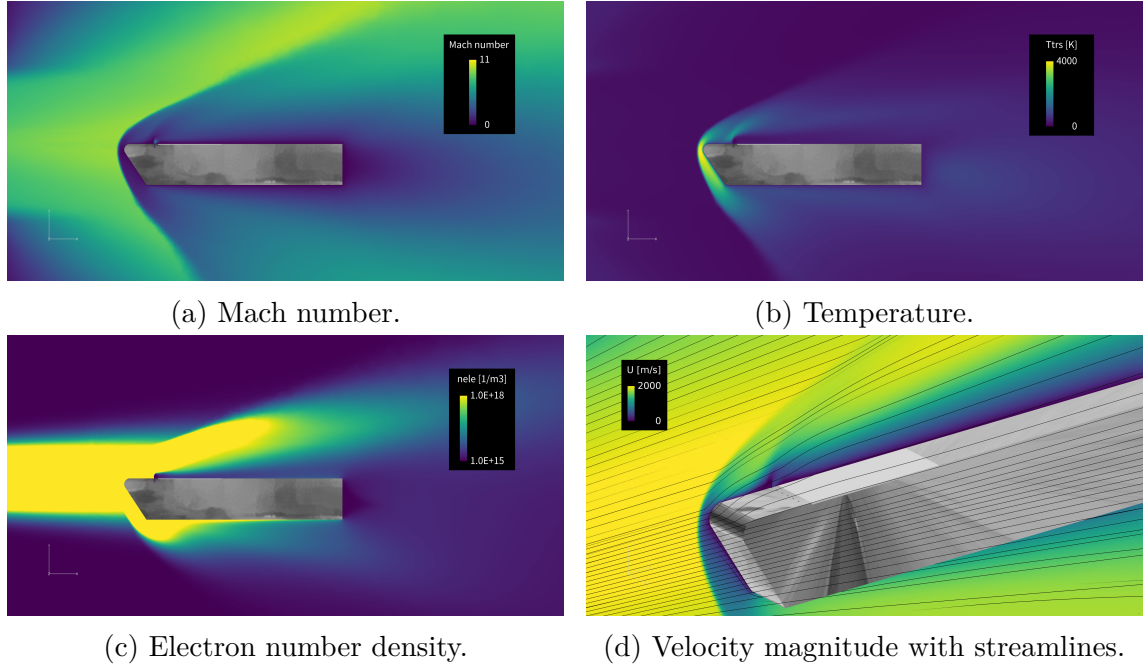


Figure 5.27: Flowfield in the L2K chamber with nitrogen gas injection of 0.10 g/s.

5.8.3 Formation of a Low Temperature Film

To investigate the effect of nitrogen gas injection on the plasma flow, a simulation was conducted for a mass flow rate of 0.10 g/s. Figure 5.27 compares the distributions of Mach number, temperature, electron number density, and streamwise velocity with those of the baseline case without injection.

As seen in the Mach number field, the global structure in the chamber remains similar to the no injection case: the supersonic freestream coming from the nozzle impinges on the wedge model and forms a detached bow shock ahead of the leading edge. In the vicinity of the injection port, however, a low velocity pocket appears just downstream of the injector, indicating that the injected nitrogen locally disturbs the near-wall region without significantly altering the core jet. This behavior is consistent with the underexpanded jet structures observed in the ISAS arc-heated experiments in subsection 4.3.2, where schlieren imaging revealed a bent injection jet and a downstream Mach disk whose stand-off distance increased with injection momentum.

The translational temperature field (Figure 5.27b) reveals a thin low-temperature layer along the upper surface of the model. This layer, with temperatures below approximately 2,000 K, originates from the rapid adiabatic expansion of the injected nitrogen as it exits. Advected by the supersonic argon freestream, the cooled gas is stretched into a continuous film that adheres to the surface and extends into the

wake region, forming a thermally insulating barrier between the hot plasma and the solid wall. Immediately upstream of the nitrogen film, a localized temperature rise is observed where the injected jet interacts with the argon freestream; this high-temperature zone corresponds to the bright emission region seen in the frontal and side-view photographs in Figure 5.9 and Figure 5.10. The presence and downstream growth of this low-temperature film directly mirror the thickening near-wall gas layer inferred from the schlieren visualizations in the ISAS facility.

The electron number density distribution (Figure 5.27c) shows that, although a high-density plasma region still extends from the nozzle core toward the model, the near-wall electron population is markedly reduced where the nitrogen film is present. To emphasize relative variations rather than absolute magnitudes, the upper limit of the color scale is set to $4 \times 10^{18} \text{ 1/m}^3$, which is larger than the critical value at 2.4 GHz. This distribution highlights the sharp gradient across the film interface, where the transition from cold, weakly ionized nitrogen to hot, highly ionized argon occurs.

The streamline pattern in Figure 5.27d confirms that the injected nitrogen expands into the low-pressure wake while remaining attached to the surface, forming a coherent shield-like layer around the model. The interface between the injected jet and the main argon stream coincides with strong gradients in temperature and electron number density, analogous to the dark/bright bands seen in the schlieren images of the ISAS experiments.

5.8.4 Three-Dimensional Structure of the Injected Gas Film

To visualize the spatial extent and morphology of the injected nitrogen gas, Figure 5.28 presents the three-dimensional isosurface of nitrogen mass fraction $Y_{\text{N}_2} = 0.50$, together with the corresponding temperature distribution. The isosurface delineates the interface between the injected nitrogen and the surrounding argon plasma, highlighting the region where the cold gas dominates the near-surface composition.

At the lower injection rate of 0.10 g/s, the nitrogen film remains thin and closely attached to the model surface. The gas expands rapidly after ejection and forms a smooth layer that extends downstream, providing continuous coverage of the upper wall. In contrast, the higher injection case of 0.50 g/s exhibits a noticeably thicker and more spatially extended nitrogen region, indicating that the injected nitrogen's influence expands farther into the chamber due to its increased momentum and mass flux.

The temperature contours in Figure 5.28 show a localized high-temperature zone

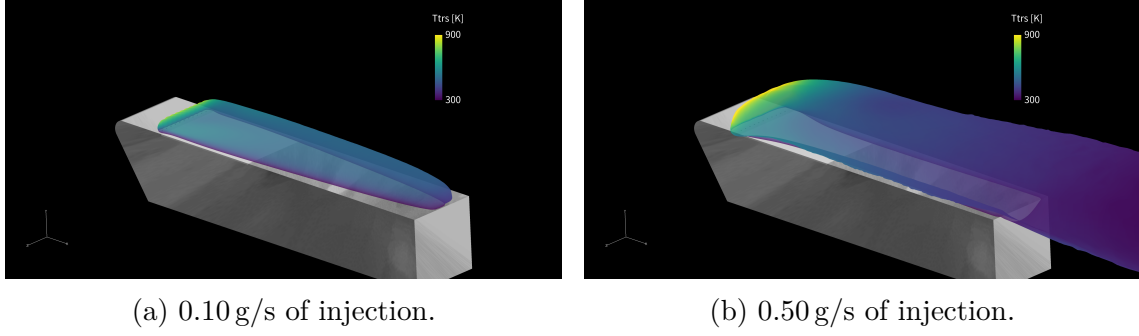


Figure 5.28: Three-dimensional structure of the injected nitrogen gas film, visualized by the isosurface of $Y_{N_2} = 0.50$ and colored by temperature.

immediately upstream of the film layer, caused by the bow shock generated ahead of the injected jet. This shock-induced heating is confined to a narrow region and does not penetrate the low-temperature layer enveloping the model. The high-temperature region corresponds to the bright blue emission observed in the frontal and side optical images (Figure 5.9 and Figure 5.10).

A distinct difference, however, appears in the surface coverage of the film between the two cases. For 0.50 g/s, the contact area between the nitrogen film and the model surface decreases, particularly around the ceramic plate located on the upper surface. This partial detachment of the film corresponds to the temperature rise observed by infrared thermography in the L2K experiment, which revealed a localized hot spot in the downstream region (Figure 5.11). The most plausible explanation is that the increased jet momentum at higher injection rates causes the film to lift off from the surface, reducing its direct shielding effect in that area.

These observations suggest that there exists an optimal range of injection momentum: while insufficient injection fails to sustain a continuous film, excessive injection leads to local film separation and diminished thermal protection. This trade-off between film thickness and attachment is essential for maximizing the combined mitigation of aerodynamic heating and plasma-induced communication blackout.

Figure 5.29 compares the wall-normal distributions of nitrogen mass fraction at four streamwise positions: the injector location ($x = 0.196$ m) and the upstream, midstream, and downstream measurement points ($x = 0.205$, 0.225 , and 0.270 m, respectively), which correspond to the experimental thermocouple locations. The horizontal axis represents the distance from the model surface.

Near the injector, a high nitrogen concentration is observed close to the wall, with the $Y_{N_2} = 1.00$ region extending up to about 10 mm for the 0.50 g/s case. Downstream, however, the nitrogen-rich region gradually shifts away from the wall,

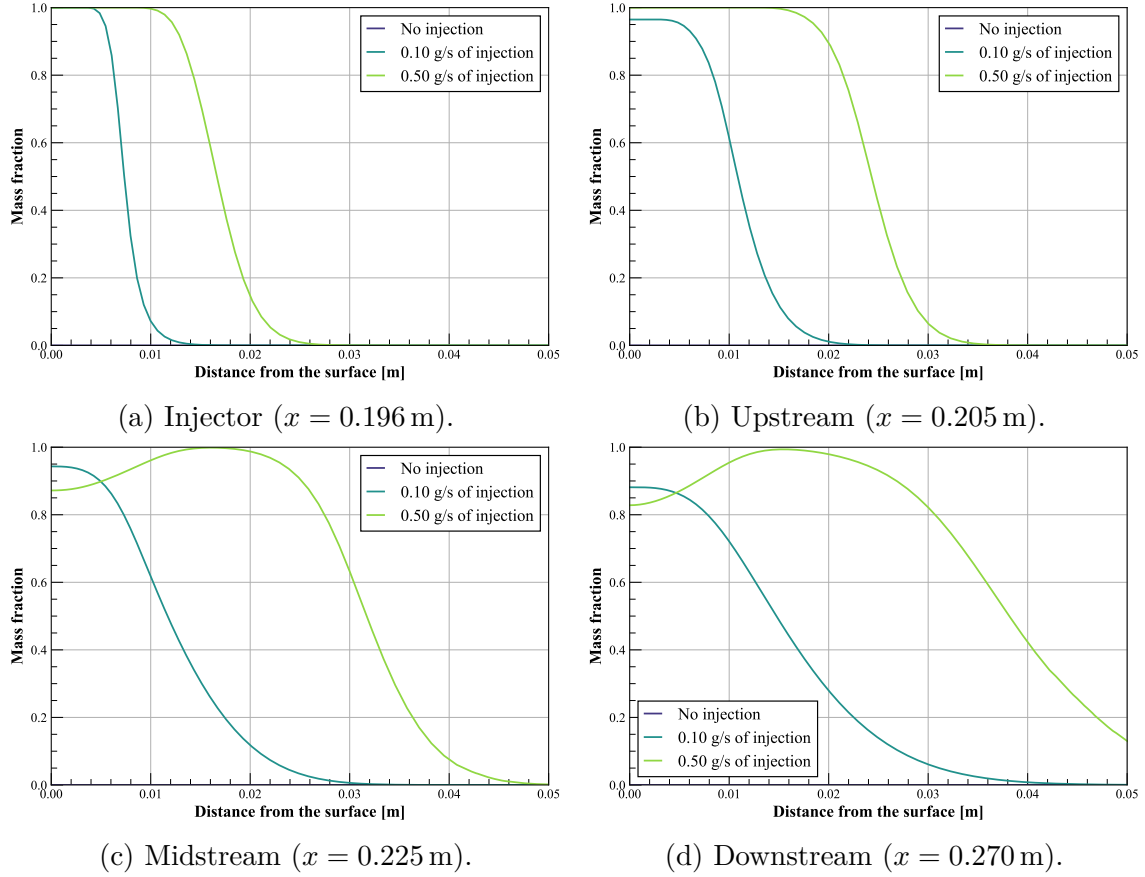


Figure 5.29: Wall-normal distribution of Y_{N_2} at four streamwise positions on the model surface.

and the maximum concentration occurs at a finite distance of approximately 1.5 mm from the surface. This indicates that the nitrogen film detaches from the surface as the flow develops, consistent with the three-dimensional visualization.

Interestingly, at the midstream and downstream positions, the near-wall nitrogen concentration is higher in the low-injection case than in the high-injection one. This trend confirms that an excessively strong jet can separate from the surface due to its large momentum, whereas a moderate injection rate maintains better surface attachment and hence more effective coverage.

These tendencies are further visualized in Figure 5.30, which provides close-up views of the nitrogen distribution around the ceramic plate. The 0.50 g/s case clearly exhibits a detached nitrogen layer hovering above the surface. This observation implies that the ratio of the jet-to-freestream momentum flux plays a key role in determining film attachment. Therefore, maximizing the mitigation of both radio-frequency blackout and aerodynamic heating cannot be achieved merely by increasing the mass flow rate; rather, it requires balancing the film's momentum and adhesion.

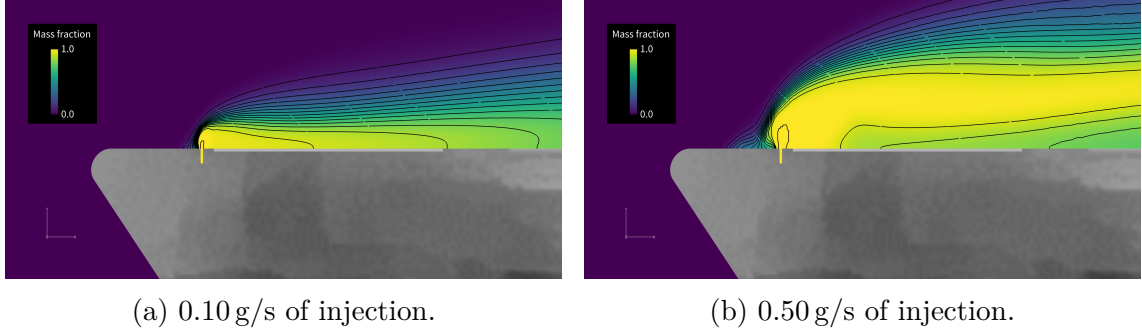


Figure 5.30: Close-up view of Y_{N_2} distribution around the ceramic plate, showing partial detachment of the film for higher injection momentum.

5.9 Three-Dimensional Heat Conduction Analysis

To quantify the thermal mitigation effect of the gas film observed in the L2K experiments, a three-dimensional transient heat-conduction analysis was performed for the test model. The numerical domain reproduces the experimental geometry, consisting of a copper base and a ceramic plate mounted on its upper surface. The spatial distribution of wall heat flux obtained from the CFD analysis is imposed as a boundary condition on the external surfaces, enabling direct comparison between the simulated temperature evolution and the infrared (IR) and thermocouple measurements.

5.9.1 Governing Equation

The transient temperature field inside the ceramic plate exposed to the arc-heated flow is obtained by solving the three-dimensional unsteady heat-conduction equation

$$\rho c_p \frac{\partial T}{\partial t} = \frac{\partial}{\partial x} \left(k \frac{\partial T}{\partial x} \right) + \frac{\partial}{\partial y} \left(k \frac{\partial T}{\partial y} \right) + \frac{\partial}{\partial z} \left(k \frac{\partial T}{\partial z} \right), \quad (5.12)$$

where ρ is the material density, c_p is the specific heat at constant pressure, and k is the thermal conductivity. Assuming that k is spatially uniform and temperature independent, Equation 5.12 reduces to

$$\frac{\partial T}{\partial t} = \alpha \left(\frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} + \frac{\partial^2 T}{\partial z^2} \right), \quad (5.13)$$

where $\alpha = k/(\rho c_p)$ is the thermal diffusivity. All computations assumed constant thermophysical properties, as listed in Table 5.6.

Table 5.6: Thermophysical properties of the ceramic plate.

Property	Value
Material	96% Al_2O_3
Density ρ [kg/m^3]	3730
Specific heat capacity c_p [$\text{J}/(\text{kg K})$]	650
Thermal conductivity k [$\text{W}/(\text{m K})$]	22.0
Plate size ($x \times y \times z$) [m]	$0.077 \times 0.002 \times 0.050$
Emissivity (Estimated)	1

A schematic of the thermal boundary conditions is shown in Figure 5.31. On surfaces directly exposed to the plasma flow—the upper surface and the side faces in the xy -plane—a prescribed convective heat flux obtained from the steady CFD solution is imposed together with thermal radiation, yielding a Robin-type condition:

$$\begin{aligned} q_{\text{in}} &= q_{\text{fluid}} + q_{\text{rad}}, \\ q_{\text{rad}} &= \varepsilon \sigma_{\text{SB}} (T^4 - T_{\text{env}}^4), \end{aligned} \quad (5.14)$$

where q_{fluid} denotes the convective heat flux predicted by the CFD solution, q_{in} is the total heat flux imposed on the solid surface, ε is the surface emissivity, $\sigma_{\text{SB}} = 5.670 \times 10^{-8} \text{ W}/(\text{m}^2 \text{ K}^4)$ is the Stefan – Boltzmann constant, and T_{env} is the temperature of the surrounding environment.

On the side faces in the spanwise yz -planes, which are in mechanical contact with the model only over a very small area and are not actively water cooled, an adiabatic boundary condition is imposed, $\mathbf{n} \cdot k \nabla T = 0$. In the present analysis, the backside of the plate is likewise treated as adiabatic, so that all heat entering from the plasma-exposed faces is retained within the solid.

The surface heat flux distribution obtained from the CFD analysis (Figure 5.32) is applied to the corresponding surface nodes as a Neumann-type boundary condition at every time step. Under the operating conditions considered, the incoming convective heat flux provided from the flowfield q_{fluid} is on the order of $1.0 \times 10^4 \text{ W}/\text{m}^2$ without gas injection.

At a surface temperature of 350 K with $\varepsilon = 1$ and $T_{\text{env}} = 300 \text{ K}$, the radiative cooling is $q_{\text{rad}} \approx 3.9 \times 10^2 \text{ W}/\text{m}^2$, i.e., only a few percent of the imposed convective flux. The thermophysical properties used in this heat-conduction analysis are summarized in Table 5.6; the plate consists of 96% alumina (Al_2O_3), and its properties are treated as temperature-independent constants based on standard data for dense alumina ceramics employed in the experiment.

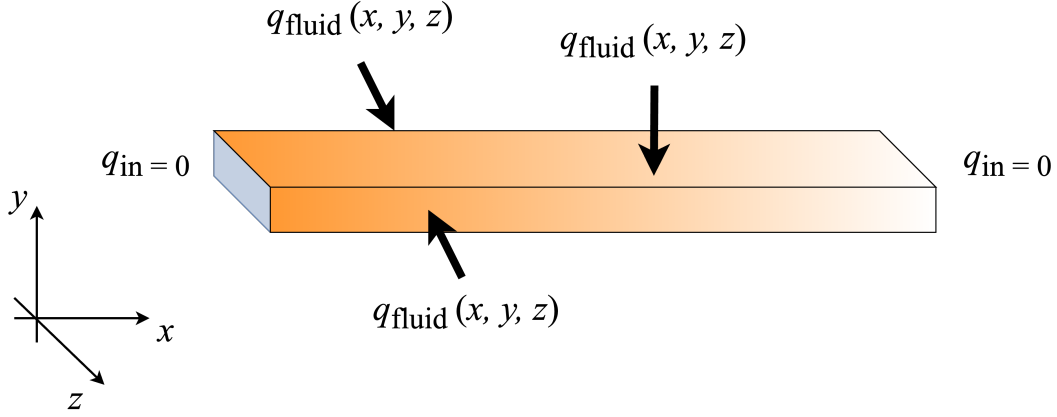


Figure 5.31: Boundary conditions for the three-dimensional heat-conduction calculation.

5.9.2 Numerical Implementation

The three-dimensional heat conduction equation given in Equation 5.12 was solved using an explicit finite-difference scheme. Spatial derivatives were discretized on a uniform Cartesian grid, and the temporal integration was carried out using a fourth-order Runge–Kutta (RK4) method. The explicit formulation enables a straightforward implementation and an accurate representation of transient heat transfer; however, the time step must satisfy the stability criterion for the diffusion equation.

The grid spacing was set to $\Delta x = \Delta z = 5.0 \times 10^{-4}$ m and $\Delta y = 2.0 \times 10^{-5}$ m, which is sufficient to resolve the temperature gradient across the 2.0 mm-thick ceramic plate. The time step was chosen as $\Delta t = 2.0 \times 10^{-5}$ s, satisfying the stability condition for the explicit scheme:

$$\Delta t < \frac{1}{2\alpha \left(\frac{1}{\Delta x^2} + \frac{1}{\Delta y^2} + \frac{1}{\Delta z^2} \right)}, \quad (5.15)$$

where $\alpha = k/(\rho c_p)$ is the thermal diffusivity.

5.9.3 Results and Discussion

The initial thermal load imposed on the ceramic plate from the fluid side is summarized in Table 5.7. In the no-injection case, the total heat input at $T = 300$ K is approximately 42 W, of which nearly 37 W is supplied through the top surface and about 2.3 W through each side wall. With gas injection at 0.10 g/s, the total thermal load decreases to 8.0 W, and the top surface contribution is reduced by

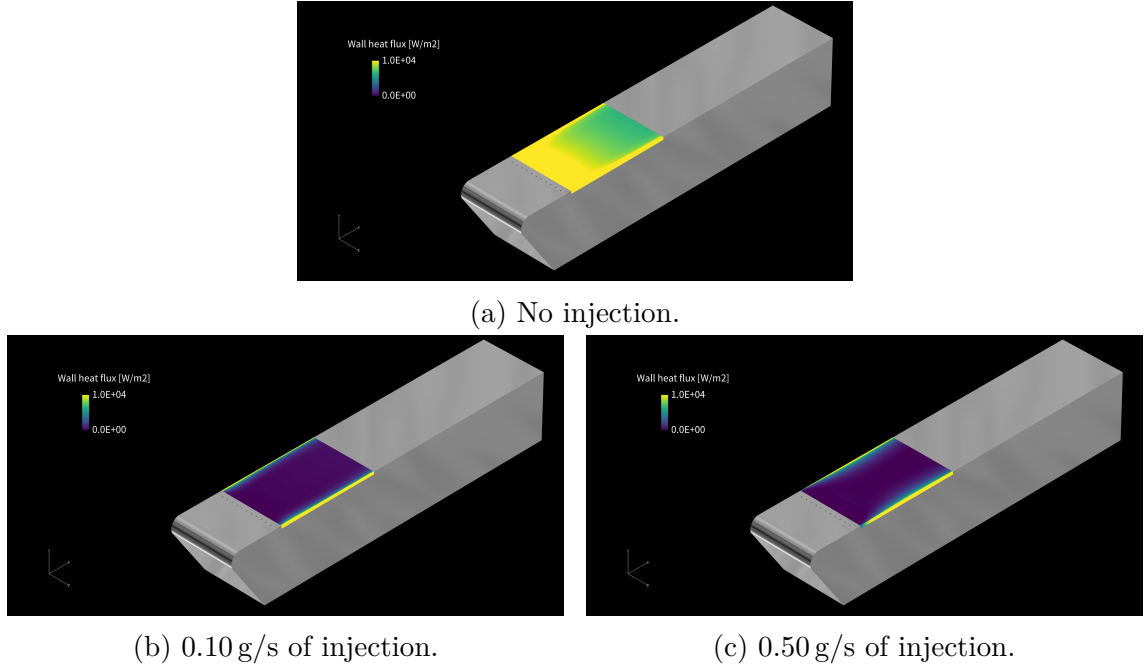


Figure 5.32: Surface heat flux on the ceramic cover plate.

Table 5.7: Initial thermal load input at 300 K [W].

	+z side wall	-z side wall	+y top surface	Total
No injection	2.3	2.3	37.1	41.6
0.10 g/s of injection	1.8	1.8	4.4	8.1
0.50 g/s of injection	2.3	2.3	5.1	9.7

almost an order of magnitude. A slightly larger total load of 9.7 W is obtained for 0.50 g/s. These values provide the baseline input for the subsequent transient conduction analysis and quantify how the gas film modifies the net thermal load on the ceramic plate. The total radiative heat loss over the plate remains below 0.1 W for the conditions considered and is therefore negligible compared with the imposed convective heat load.

Figure 5.33 shows the predicted temperature histories on the backside of the ceramic plate at the upstream, midstream, and downstream measurement locations for cases with and without nitrogen injection. In the no-injection case, the mean rate of temperature increase (dT/dt) follows the streamwise ordering upstream > midstream > downstream, reflecting the larger convective load near the leading edge. With injection, the ordering reverses to downstream > midstream > upstream: the near-wall gas film shields the upstream region first and reduces its heating rate most strongly within the exposure window, while the downstream location experiences a delayed and weaker protection. Spatial snapshots of the computed top surface

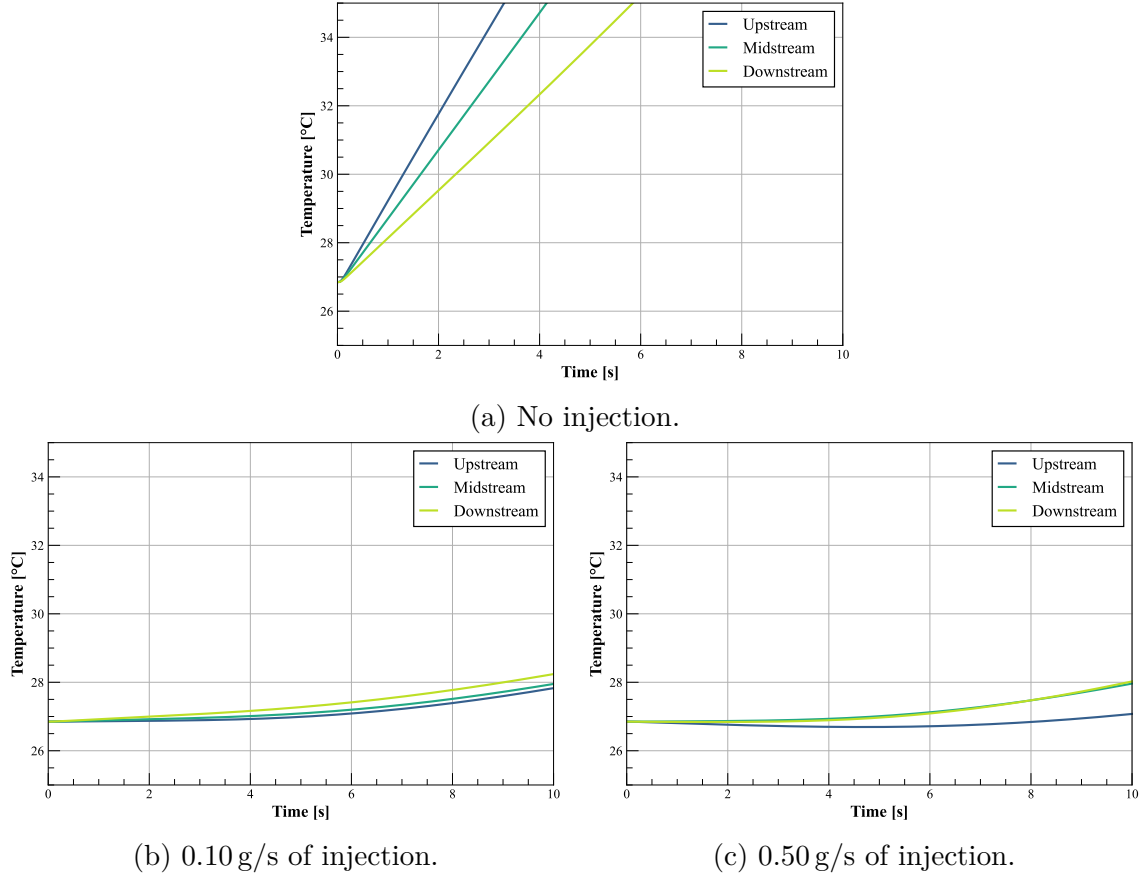


Figure 5.33: Backside temperature history on the ceramic cover plate.

temperature shown in Figure 5.34 likewise show a pronounced reduction of the near-wall gradient in the upstream sector under injection, consistent with the reduced surface heat flux imposed by the gas film.

For the numerical results, the rates are evaluated from an initially uniform 300 K; the experimental rates are derived from thermocouple data during the quasi-steady exposure, and the injection cases include an initial phase without injection, so their absolute levels differ from the simulation. In the experiment, as the temperature increases, the thermal input from the fluid decreases and radiative cooling increases. Furthermore, heat conduction occurs from the ceramic plate to the water-cooled metal components of the experimental model. Consequently, the numerical analysis results overestimate the rate of temperature rise compared to the experimental values. Accordingly, this analysis aimed to reproduce the distribution of temperature rise in the ceramic plate rather than fully capturing the temporal evolution trend of the temperature. The consistency between the numerical analysis results and the experimental results was evaluated by comparing the temperature rise rates at the other two points at the instant when the temperature rise rate at the upstream measurement point matched the experimental value.

Table 5.8: Comparison of temperature increase rate [$^{\circ}\text{C/s}$].

	Upstream	Midstream	Downstream
Experiment			
No injection	0.89	0.69	0.50
0.10 g/s of injection	0.08	0.12	0.16
0.50 g/s of injection	0.04	0.10	0.19
Computation			
No injection, $t = 0.05\text{ s}$	0.89	0.70	0.49
0.10 g/s of injection, $t = 5.0\text{ s}$	0.08	0.10	0.13
0.50 g/s of injection, $t = 6.2\text{ s}$	0.04	0.14	0.16

A quantitative comparison of the temperature increase rates between the experimental measurements and numerical predictions is summarized in Table 5.8. The numerical results for the no-injection case at $t = 0.05\text{ s}$ show good agreement with the experimental values, with deviations within 5 %. In this early stage, the temperature is close to the initial condition of 300 K, and the thermal conduction is primarily governed by the imposed heat flux distribution from the CFD analysis. For the 0.10 g/s injection case, the numerical prediction at $t = 5.0\text{ s}$ closely matches the experimental rates, with deviations within 25 %. Similarly, for the 0.50 g/s injection case, the numerical results at $t = 6.2\text{ s}$ align well with the experimental data, with deviations within 20 %. These comparisons indicate that the present conduction analysis successfully captures the relative thermal mitigation effects of gas injection, despite some discrepancies in absolute values.

Figure 5.33 presents the surface temperature distribution on the top surface of the ceramic plate at $t = 10.0\text{ s}$ for all three cases. In the no-injection case, a pronounced high-temperature region is observed near the upstream edge, corresponding to the area of maximum heat flux from the numerical analysis. With 0.10 g/s of injection, the overall surface temperature is significantly reduced, and the high-temperature region shrinks considerably, demonstrating the effectiveness of the gas film in mitigating thermal loads. At 0.50 g/s of injection, a localized high-temperature spot reemerges downstream, consistent with the combined numerical prediction of a detached nitrogen film and increased heat flux in that region. This spatial pattern aligns qualitatively with the infrared thermography results from the experiment, further validating the CFD and conduction analysis. Therefore, the combined CFD and transient conduction analysis effectively elucidates the thermal mitigation mechanisms of gas injection in the L2K arc-heated flow, capturing both the overall reduction in heat load and the localized effects associated with film attachment and detachment.

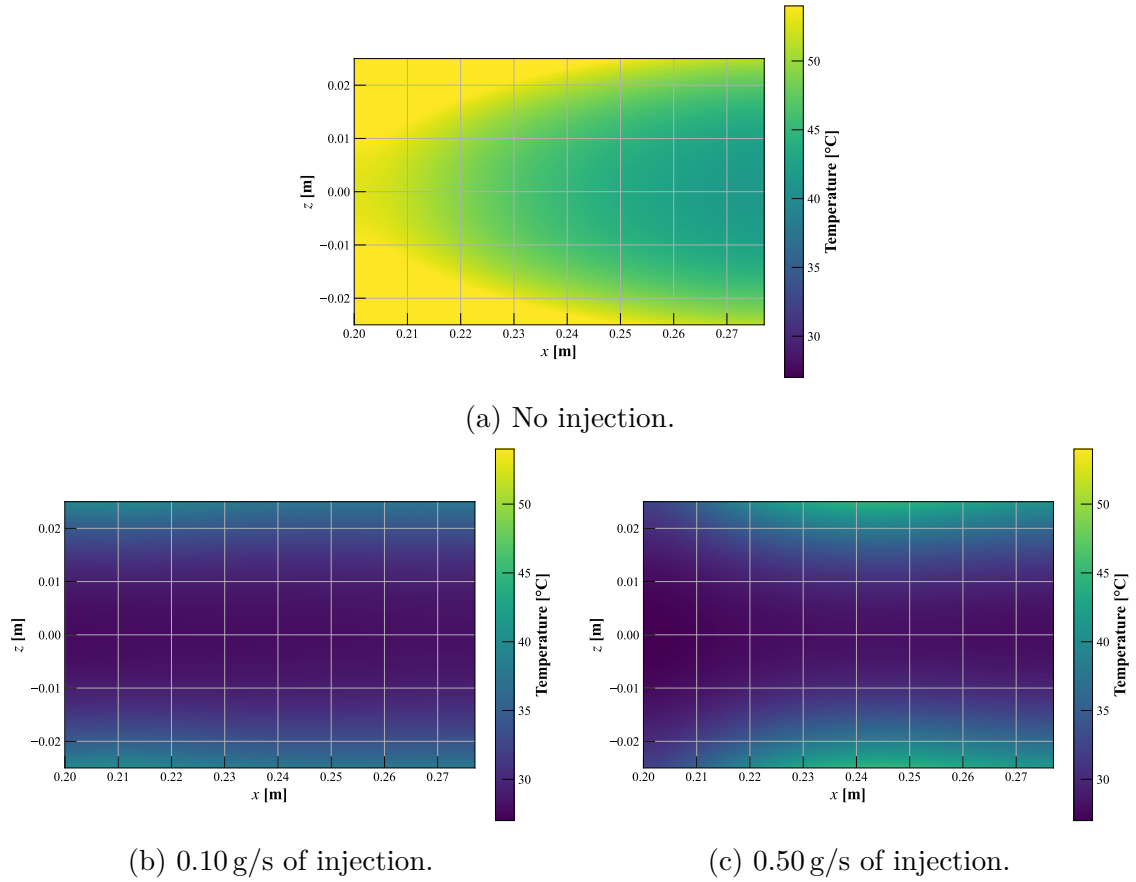


Figure 5.34: Surface temperature distribution on the top surface of the ceramic cover plate ($t = 10.0$ s).

5.10 Conclusions

This chapter demonstrated, in the DLR L2K arc-heated facility, that surface gas injection can simultaneously mitigate aerodynamic heating and communication blackout under high-enthalpy, weakly ionized flows. Under the FC1 argon condition, a dense plasma layer produced severe signal attenuation, while the FC2 argon-nitrogen mixture reduced the free-electron population and partially alleviated blackout. In both conditions, nitrogen injection from the model surface measurably lowered the surface temperature seen in both the IR camera and the thermocouple measurements and improved received-signal strength, confirming the dual-mitigation concept.

The argon-flow modeling reproduced the nozzle-to-chamber evolution of Mach number, temperature, and electron number density, as well as the formation of a dense plasma layer responsible for radiative emission and communication attenuation. Radial profiles at the nozzle exit and chamber cross-sections matched measured trends within experimental and modeling uncertainties. With injection, the three-

dimensional CFD revealed the emergence of a low-temperature, nitrogen-rich layer along the model surface, consistent with the observed cooling and signal recovery.

A three-dimensional heat-conduction analysis, driven by spatially resolved wall-heat-flux boundary conditions mapped from the steady CFD solution, with radiative exchange on exposed faces and adiabatic treatment of the backside and the small mechanical contact interface, captured the streamwise evolution of plate temperature. While exact pointwise agreement is not expected due to the sensitivity of transient conduction to initial conditions, surface boundary conditions, and material/contact parameters, the model reproduced the key trend: without injection, the mean temperature-rise rates order upstream $>$ midstream $>$ downstream; with injection, the ordering reverses to downstream $>$ midstream $>$ upstream. This inversion corroborates the formation of a near-wall gas film that first shields the upstream sector and progressively extends downstream over the exposure window.

Overall, the experimental evidence and the supporting CFD-conduction modeling are consistent and establish surface gas-film injection as an effective dual-mitigation strategy in L2K: (1) reduction of local aerodynamic heating via a low-temperature near-wall layer, and (2) creation of a low-electron-density corridor that enables radio-wave transmission through the surrounding plasma. Future work will focus on optimizing injection distribution and angle for sustained film attachment, refining the conduction model with tighter constraints on initial/contact conditions and time-resolved heat-flux inputs, and extending the tests to higher-enthalpy/longer-duration exposures to quantify facility-to-flight scaling.

Building on these arc-tunnel results, the next chapter applies the developed CFD framework to the RAM C reentry vehicle in order to assess the feasibility and limitations of gas-film blackout mitigation under actual atmospheric flight conditions.

Chapter 6

Effectiveness Under Atmospheric Reentry Conditions

In the preceding chapters, gas-film injection was investigated in arc-heated wind tunnels using a combination of high-fidelity CFD and targeted experiments. The L3K numerical study (chapter 3) established the basic mitigation mechanism, while the ISAS and L2K campaigns (chapter 4, chapter 5) experimentally confirmed that a surface gas film can simultaneously reduce aerodynamic heating and plasma-induced communication blackout under megawatt-class facility conditions.

Arc-heated tunnels are designed to reproduce key near-wall quantities—such as stagnation-point heat flux and electron number density—encountered in atmospheric reentry. However, their flowfields differ from true flight environments in several important respects: the freestream Mach and Reynolds numbers, shock-layer structure, and even the dominant electron-production mechanisms are not identical to those in a high-altitude, chemically reacting atmosphere. To assess whether the gas-film concept remains effective under realistic entry conditions, it is therefore necessary to extend the analysis to an actual flight configuration.

This chapter evaluates gas-film blackout mitigation for the RAM C-I reentry vehicle using the validated nonequilibrium CFD framework. By applying the injection concept to flight-representative freestreams and geometries, the analysis bridges the gap between arc-heated wind-tunnel demonstrations and practical implementation in atmospheric reentry.

6.1 Evaluation under Atmospheric Reentry Conditions

This chapter investigates the effectiveness of gas-injection-based blackout mitigation under realistic atmospheric reentry conditions through flight-condition numerical simulations. While the preceding chapters demonstrated the physical feasibility of the proposed mitigation concept using arc-heated wind-tunnel experiments, quantitative evaluation under actual flight environments remains indispensable.

Arc-heated wind tunnels provide a unique capability to reproduce high-enthalpy plasma flows under controlled laboratory conditions. Using such facilities, the present study confirmed that plasma-induced communication blackout can be experimentally reproduced and that surface gas injection can locally suppress plasma density and recover electromagnetic-wave propagation. These experiments serve as proof of the underlying physics of the proposed mitigation concept.

However, the physical processes governing the formation of high-temperature plasma differ fundamentally between arc-heated wind tunnels and atmospheric reentry. In arc-heated facilities, the freestream is generated by Joule heating and subsequent nozzle expansion, whereas in atmospheric reentry the plasma is produced by strong aerodynamic compression behind a detached bow shock. Owing to this difference in flow-generation mechanisms, the thermodynamic state, density level, and momentum balance of the surrounding flow near the vehicle surface are not identical between the two environments.

As a consequence, direct quantitative extrapolation of injection conditions from wind-tunnel experiments to flight environments is not straightforward. In particular, the relative strength of gas injection with respect to the surrounding flow may differ significantly, even when comparable injection mass flow rates are applied.

For this reason, flight-condition CFD analysis is introduced in this chapter to complement the experimental findings. The objective of the present flight analysis is not to replace wind-tunnel experiments, but to evaluate blackout mitigation performance under realistic reentry conditions and to assess the applicability of the wind-tunnel-based observations to actual flight environments.

6.2 Reentry Vehicle: RAM C

The Radio Attenuation Measurement (RAM) program conducted three dedicated reentry flights—RAM C-I (1967), RAM C-II (1968), and RAM C-III (1970)—to acquire flight data for developing flowfield and electromagnetic wave propagation mod-

els during atmospheric entry [112]. All three vehicles were launched from NASA’s Wallops Flight Facility in Virginia and released at altitudes of 220 km and higher, with entry velocities of approximately 7,400 m/s.

A schematic view of the RAM C-I vehicle is shown in Figure 6.1. The configuration is a blunt cone with an overall length of 1.295 m and a nose radius of 0.1595 m. Multiple antennas distributed over the vehicle surface supported links at several frequencies, including VHF (259.7 MHz), X-band (9,210 MHz), and C-band (5,700 MHz). In addition, thermocouples and electrostatic (Langmuir) probes mounted near the base provided measurements of gas temperature and electron number density in the freestream.

During the RAM C-I and RAM C-III flights, water—known to promote electron recombination—was injected from the vehicle surface during atmospheric entry. Communication outages were recorded for multiple antennas between altitudes of approximately 80 km and 40 km. Nevertheless, post-flight analysis confirmed that the water injection reduced the local free-electron population measured at the aft probes and improved link performance [113].

RAM C-I is therefore highly relevant to the present study: it provides one of the few flight datasets in which both electron number density and communication performance were measured in conjunction with an active mitigation technique. Accordingly, RAM C-I is selected as the reference configuration for the CFD analysis. Previous work has investigated the mitigation effect of water-vapor injection under RAM C-I conditions using CFD [52]; here, we extend the concept to gas-film injection and assess its potential to reduce blackout and heating in a fully flight-representative environment.

6.3 Flowfield Simulation for the RAM C Flight

This section outlines the physical and numerical models used to simulate the flowfield of the RAM C reentry flight condition.

6.3.1 Numerical Model

The RAM C flowfield is evaluated using the RG-FaSTAR [84] formulation summarized in section 3.2. The computation assumes a steady, laminar, continuum flow and solves the thermochemical nonequilibrium Navier–Stokes system with Park’s two-temperature model [72]. The 11-species air chemistry set, reaction mechanism, and transport properties are identical to those listed in section 3.2. Consequently, only

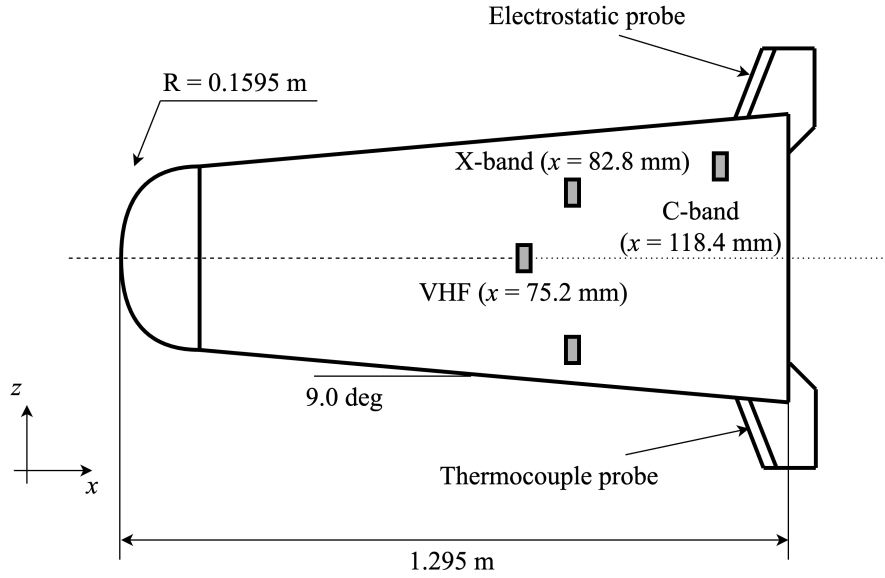


Figure 6.1: Schematic of the RAM C-I vehicle.

Altitude [km]	79.2	68.6
Velocity [m/s]	7,654	7,665
Temperature [K]	206.8	227.4
Density [kg/m ³]	1.98×10^{-5}	9.16×10^{-5}

Table 6.1: Freestream conditions for the RAM C-I atmospheric reentry trajectory.

the geometry, inflow conditions, and surface boundary conditions are specific to the present vehicle and are described below.

6.3.2 Calculation Conditions

The computational domain is illustrated in Figure 6.2. The domain is sufficiently large to encompass the RAM C-I vehicle, with a uniform freestream entering through the inflow boundary (black) and exiting through the circular outflow boundary (blue), which has a diameter of 6.0 m. The domain is three-dimensional; tetrahedral elements are used in the far field, while near-wall regions employ a boundary-fitted mesh to resolve the boundary layer and bow shock around the vehicle. Depending on the case, multiple computational meshes were employed, with total cell counts ranging from 12 to 20 million. Grid-independence checks confirmed that the solution is insensitive to this variation in resolution.

The freestream conditions used in the simulation are listed in Table 6.1. This study focuses on altitudes of 79.2 km and 68.6 km. The corresponding velocities were taken from the RAM C-I flight data [24], whereas the ambient temperature

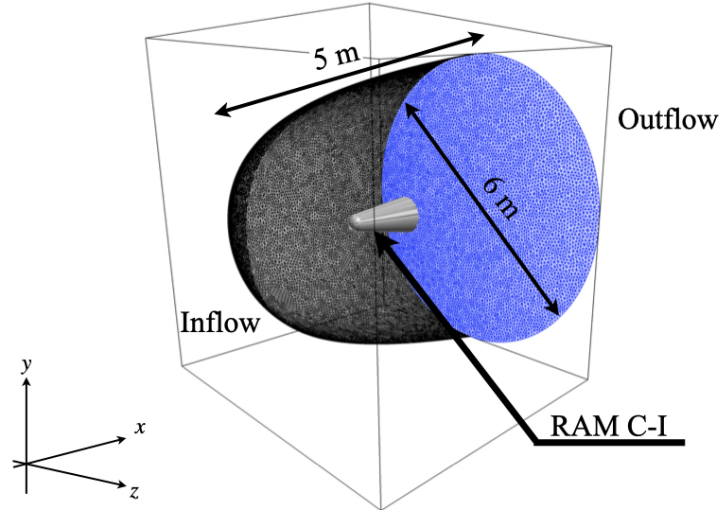


Figure 6.2: Computational domain encompassing the RAM C-I vehicle.

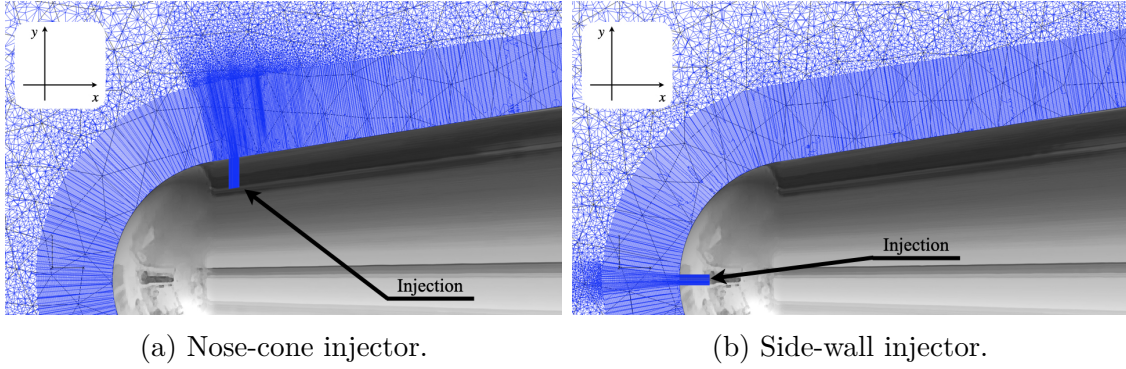


Figure 6.3: Computational grid in the vicinity of the RAM C-I vehicle with a nose-cone injector and a side-wall injector.

and density were obtained from the NRLMSISE-00 atmospheric model [114]. The flight velocity is nearly constant between these points, but the atmospheric density at 68.6 km is roughly five times higher than at 79.2 km.

Jones and Cross reported that the RAM C vehicles maintained a stable attitude during reentry, with an angle of attack of less than 5.0° [115]. Following the numerical analysis of a previous study [84], the angle of attack in all cases in this work is set to 5.0° . The freestream composition is assumed to be 76% N_2 and 24% O_2 by mole fraction. At the outflow boundary, all flow variables are extrapolated with zero gradients. The vehicle surface is treated as no-slip, non-catalytic, and isothermal at 300 K.

Gas injectors are located either at the tip of the blunt leading edge or on the upper side wall, as shown in Figure 6.3. In both configurations, the injector outlet has a diameter of 28 mm and a cylindrical feed channel. Injector boundary conditions are specified in terms of total mass flow rate, total temperature, and gas composition.

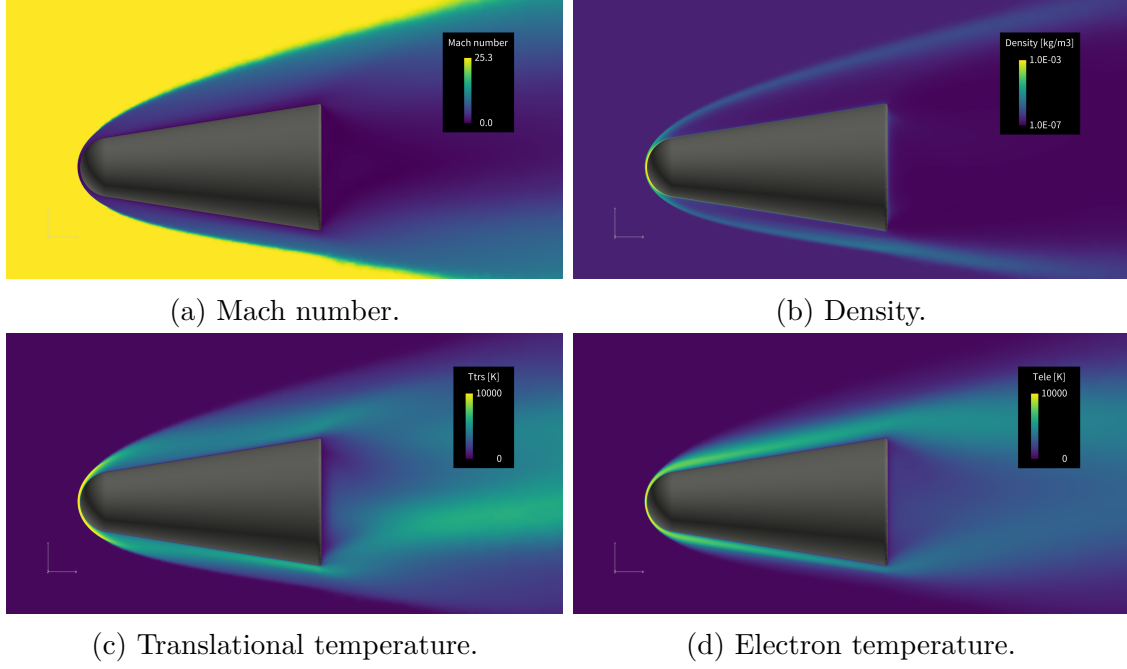


Figure 6.4: Baseline flow distribution around the RAM C-I at an altitude of 68.6 km.

In this study, N_2 at 300 K is used as the injection gas.

6.4 Results and Discussion

In this section, we first examine how gas injection modifies the wall heat flux and, subsequently, analyze the distribution of electron number density—a key quantity for assessing RF blackout. The influence of altitude, injector location, and injection mass flow rate on both heating and communication attenuation is discussed.

Figure 6.4 illustrates the baseline flow distribution around the RAM C-I vehicle at an altitude of 68.6 km, showing Mach number, density, translational temperature, and electron temperature. As the vehicle reenters the atmosphere at hypersonic speed, a strong bow shock forms in front of the blunt nose. Behind this shock, the gas temperature increases significantly, with maximum translational and electron temperatures reaching approximately 22,000 K and 11,000 K, respectively. These high temperatures accelerate ionization and dissociation reactions of the atmospheric gas, generating electrons and forming a plasma sheath surrounding the vehicle.

A noticeable spatial offset exists between regions of peak translational temperature and peak electron temperature, reflecting relaxation dynamics between these temperature modes and highlighting thermochemical nonequilibrium in the shock layer. The stagnation pressure p_{stg} is about 1,070 Pa at 79.2 km and 5,060 Pa at 68.6 km. The corresponding stagnation heat flux q_{stg} is 1.18 MW/m² at 79.2 km and

Table 6.2: Boundary conditions for the RAM C-I flowfield simulation.

Boundary	Description
Inflow	Uniform freestream specified by Table 6.1: velocity from RAM C-I flight data [24], temperature and density from the NRLMSISE-00 atmosphere model [114], angle of attack fixed at 5.0° , composition 76% N_2 and 24% O_2 (mole fraction).
Outflow	Circular outflow boundary of diameter 6.0 m; all flow variables extrapolated with zero normal gradients.
Far field	Lateral and upstream boundaries set to the same uniform freestream as at the inflow plane.
Vehicle surface	No-slip, non-catalytic, isothermal wall at 300 K.
Injector outlet	Circular nozzle of diameter 28 mm (nose-cone or side-wall location); total mass flow rate, total temperature, and composition prescribed. Injection gas is N_2 at 300 K.

1.52 MW/m^2 at 68.6 km, confirming that the lower-altitude case represents a more severe aerothermal environment.

6.4.1 Nose-Cone Injector

The computed results for the nose-cone injector cases are summarized in Figure 6.5 and Figure 6.6. These figures compare the baseline case without injection and cases with 2.00 g/s and 3.00 g/s of injected N_2 at the two reference altitudes. Wall-heat-flux distributions are plotted on the vehicle surface. As expected, the highest heat flux occurs near the nose due to the strong bow shock, and elevated heat flux is also observed on the lower surface because of the 5.0° angle of attack. The injection cases (Figure 6.5b, Figure 6.5c, Figure 6.6b, and Figure 6.6c) additionally show regions with high N_2 mass fraction corresponding to the injected gas. In these visualizations, grayscale shading on the isosurfaces represents the translational temperature distribution.

At an altitude of 79.2 km, where the freestream density is relatively low, the injected N_2 remains close to the vehicle surface and a significant fraction of the jet stays attached even toward the aft regions. The area covered by injected nitrogen increases as the injection mass flow rate increases. Because the vehicle travels at over Mach 25, the jet is advected downstream along the body before it can largely lift off from the surface. In contrast, at 68.6 km, where the freestream density is higher, the region of high N_2 mass fraction is confined to the vicinity of the injector; the jet penetrates less deeply along the surface and spreads more into the surrounding flow.

In terms of surface heating, a localized reduction in wall heat flux is observed

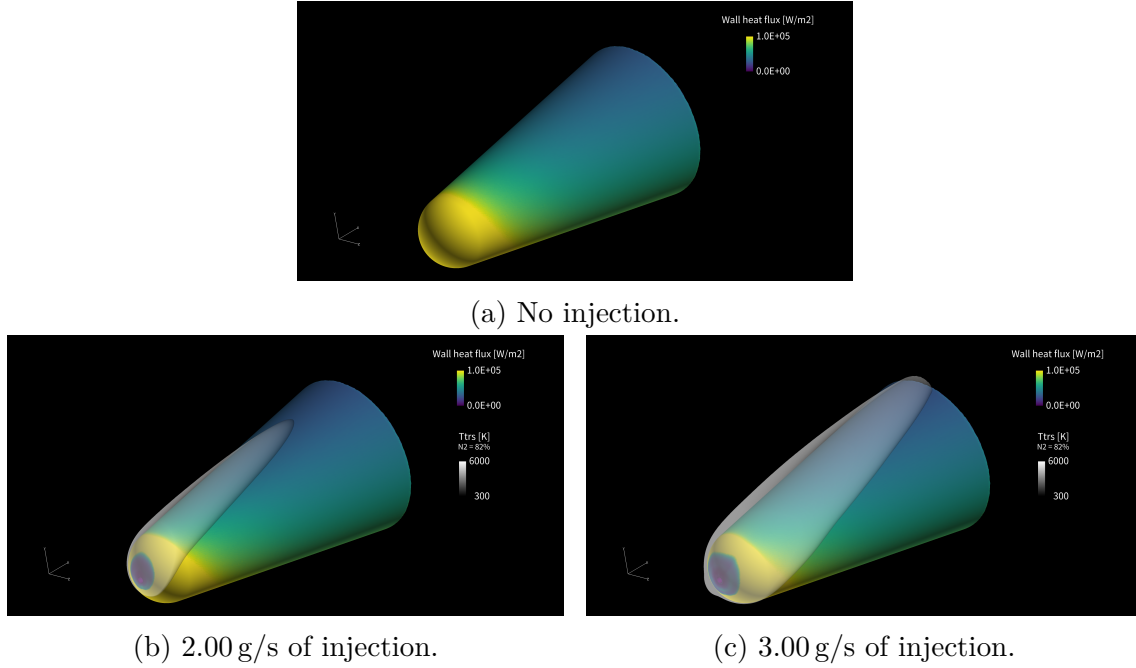


Figure 6.5: Wall heat flux distribution and isosurface of $Y_{N_2} = 0.82$ for injection from the nose-cone injector at an altitude of 79.2 km.

near the injection port for both altitudes, particularly on the upper surface. This indicates that gas injection from the nose can provide some degree of local film cooling.

However, the nose-cone injector is less effective from an RF-mitigation standpoint. The shock layer heats the injected gas to high temperatures, enhancing the ionization and dissociation of N_2 . The increased nitrogen content promotes the formation of N_2^+ , N , N^+ , and electrons through chemical reactions, which prevents the plasma density near the surface from decreasing. As a result, a dense plasma sheath remains attached to the vehicle and continues to interfere with communication. Moreover, the incremental benefit of increasing the injection rate from 2.00 g/s to 3.00 g/s is modest, especially at the lower altitude, indicating that nose-cone injection is not particularly efficient for blackout mitigation.

The circumferential distributions of wall heat flux at $x = 0.02, 0.30, 0.80$, and 1.10 m along the vehicle surface are summarized in Figure 6.7. The horizontal axis of each plot denotes the z coordinate; because the body is flared, the z -range increases toward the aft. Curves are color-coded by axial location and differ in line style according to injection mass flow rate. Each polar plot is circular, with the upper half corresponding to the lower (windward) surface ($y \leq 0$ m) and the lower half representing the upper surface ($y \geq 0$ m).

At an altitude of 79.2 km, injection at 3.00 g/s reduces the local heat flux at

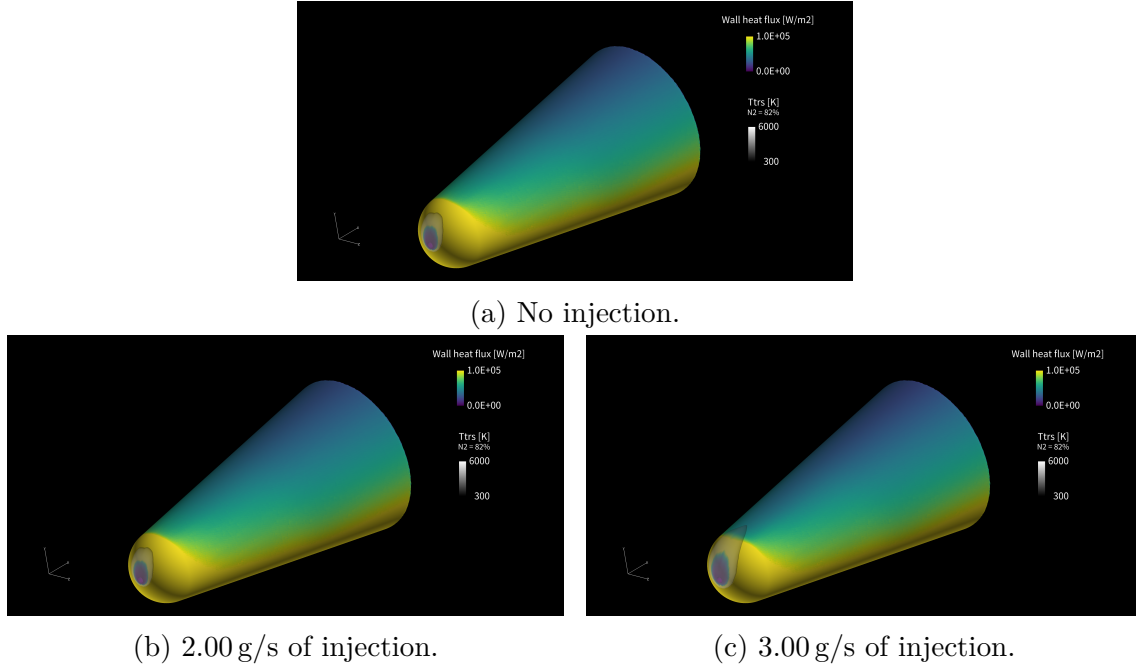


Figure 6.6: Wall heat flux distribution and isosurface of $Y_{N_2} = 0.82$ for injection from the nose-cone injector at an altitude of 68.6 km.

$x = 0.02$ m by approximately 28.6 % on the upper surface and 11.2 % on the lower surface. Even in the rear windward region, reductions of about 5.1 % and 9.4 % are obtained on the lower and upper surfaces, respectively, at $x = 1.10$ m.

At an altitude of 68.6 km, the wall heat flux on the underside reaches a minimum near the mid-body and increases slightly toward the rear. This trend reflects the fact that the standoff distance between the surface and the underside shock is not uniform and becomes smaller around the middle of the body. The body diameter increases rearward at a flare angle of 9.0° (Figure 6.1), and the vehicle maintains an angle of attack of 5.0° , which together shape the shock geometry. In this denser atmosphere, nose-cone injection has limited influence on the lower surface, but substantial reductions of 43.7 % at $x = 0.02$ m and 22.2 % at $x = 1.10$ m are realized on the upper surface. The higher density at lower altitude enhances the downward and lateral spreading of the injected gas, leading to a significant reduction of wall heat flux on the upper surface, while the effect on the lower surface remains comparatively modest.

6.4.2 Side-Wall Injector

The results of the computational analysis with side-wall injection are illustrated in Figure 6.8. In contrast to nose-cone injection, the side injector produces a reduction in wall heat flux over a much broader region at both altitudes. The cooling effect

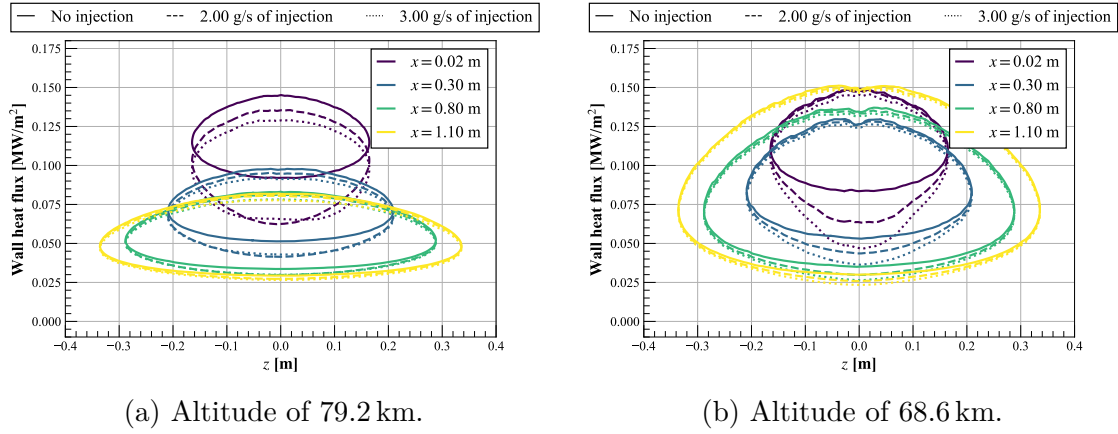


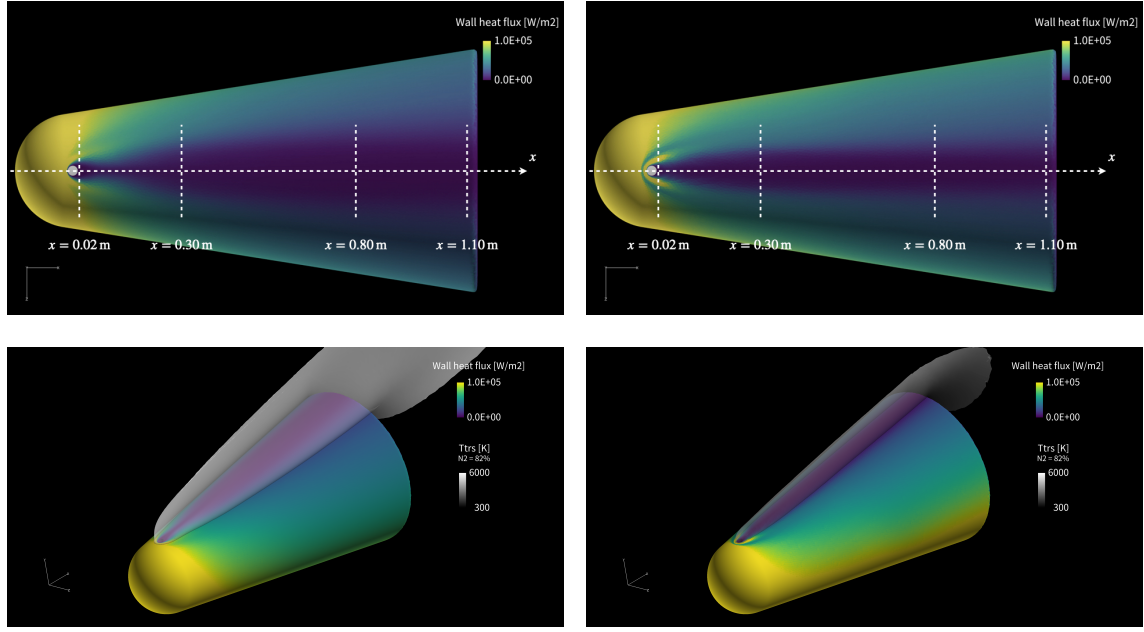
Figure 6.7: Effectiveness of cooling with various injection rates from the nose-cone injector at two altitudes.

extends well downstream, indicating that the injected gas is advected along the vehicle and covers the surface while remaining at a comparatively low temperature. This behavior is consistent with the formation of a near-wall gas film that inhibits chemical reactions and thus maintains a low local plasma density.

The thickness of a film formed by a jet injected normal to the freestream is known to scale with the ratio of jet to freestream momentum flux [96]. In the present cases, the jet diameter and jet mass flow rate are fixed, while the freestream density at 68.6 km is about five times higher than at 79.2 km. Consequently, at the lower altitude the jet is more strongly compressed toward the surface and the film remains thinner, leading to a narrower region of effective cooling.

An increase in surface heat flux is also evident immediately upstream of the injection port, particularly at 68.6 km. This local heating can be attributed to the shock generated by rapid compression of the injection jet as it impinges on the freestream. A similar phenomenon has been observed in plasma wind-tunnel experiments chapter 4 and chapter 5.

Figure 6.9 shows circumferential distributions of wall heat flux at various axial positions x . Without gas injection, the profiles are nearly elliptical at all locations and altitudes, with larger heat flux on the lower (windward) side ($y \leq 0$ m) and smaller values on the upper wall ($y \geq 0$ m) where the side injector is located, reflecting the vehicle's angle of attack. The maximum heat flux at each x exhibits different trends: at 79.2 km it is nearly constant beyond $x = 0.80$ m, whereas at 68.6 km it decreases from $x = 0.02$ m near the injector and then increases again toward $x = 1.10$ m at the tail. This behavior arises from the combined effect of the conical body flare and the higher atmospheric density at lower altitude, which reduces the standoff distance between the underside shock and the vehicle surface.



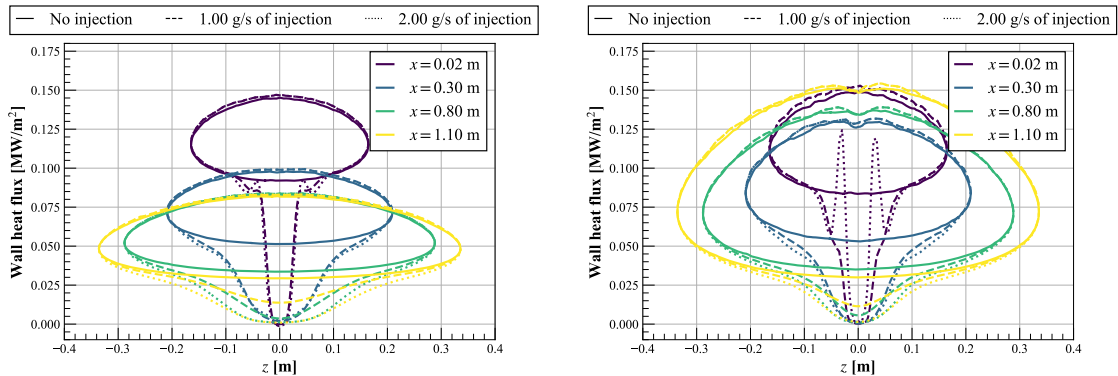
(a) Altitude of 79.2 km.

(b) Altitude of 68.6 km.

Figure 6.8: Wall heat flux distribution and isosurface of $Y_{N_2} = 0.82$ for 2.00 g/s of injection from the side injector.

The most pronounced differences between cases with and without gas injection appear on the upper side of the vehicle, as shown in Figure 6.8. At both altitudes, a maximum injection rate of 2.00 g/s reduces the wall heat flux to nearly zero at the injector center, demonstrating a strong local mitigation of aerodynamic heating.

At 79.2 km, the peak reduction near the injector does not change substantially with further increases in mass flow rate, but the cooled region expands downstream, suggesting that higher injection rates primarily increase the area over which the heat load is reduced. A similar trend is observed at 68.6 km; however, for 2.00 g/s



(a) Altitude of 79.2 km.

(b) Altitude of 68.6 km.

Figure 6.9: Effectiveness of cooling with various injection rates from the side injector.

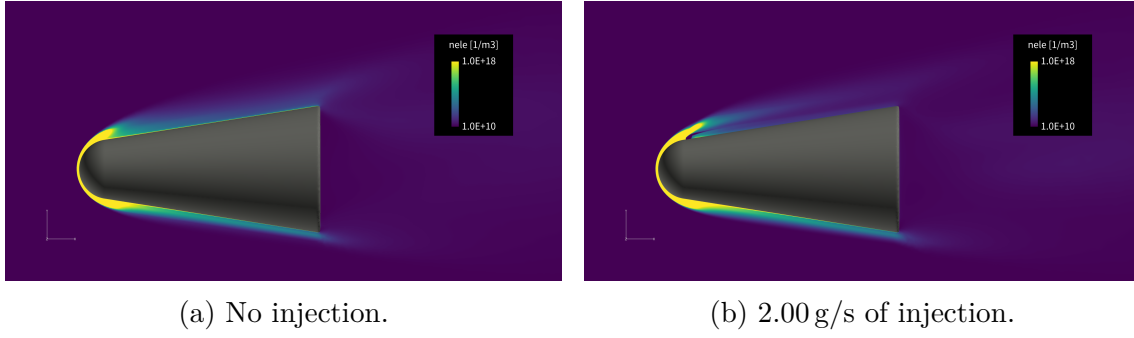


Figure 6.10: Electron number density distribution at an altitude of 68.6 km for cases with side-wall injection.

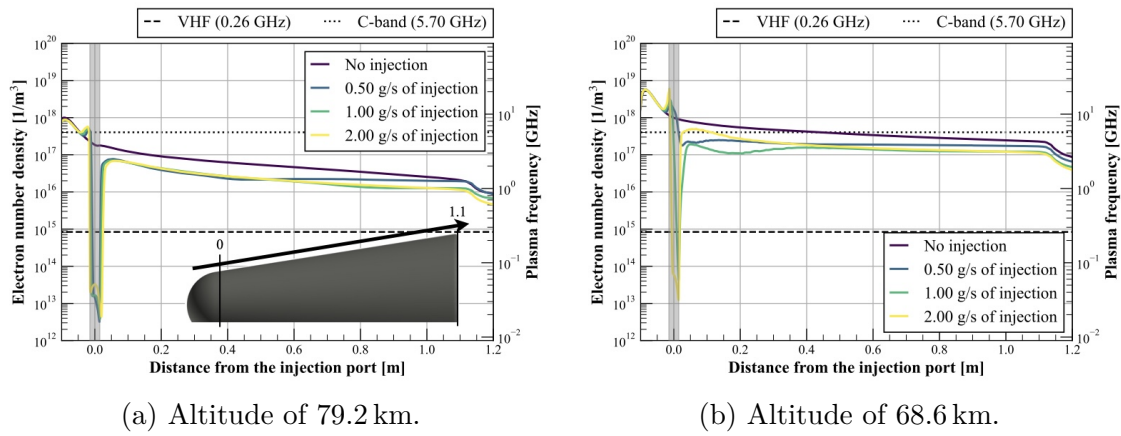


Figure 6.11: Electron number density profile along the vehicle's upper surface with side-wall injection.

a localized increase in heat flux appears just upstream of the port, associated with the compression shock discussed above.

Figure 6.10 shows the electron number density on the upper side of the RAM C-I vehicle at an altitude of 68.6 km. Electrons produced in the high-temperature region behind the bow shock are convected along the surface, and the electron number density gradually decreases with distance from the stagnation region. Because of the 5.0° angle of attack, the electron number density on the upper surface is smaller than on the lower (windward) surface.

300 K of N_2 gas is injected through the side port. Since the jet is much colder than the post-shock gas, the electron number density within the jet core is low. The injected nitrogen expands rapidly toward the low-pressure ambient, pushing the surrounding plasma layer away from the vehicle and creating a low-electron-density region around the outlet. The jet is then advected downstream along the body with the high-velocity freestream. Compared with the no-injection case, the electron number density near the surface is reduced over a broad region, including

the aft portion of the vehicle.

Figure 6.11a shows the electron number density along a line parallel to the upper surface at an altitude of 79.2 km. The horizontal axis is the streamwise coordinate x ; the vehicle extends from approximately $x = -0.16$ m to $x = 1.13$ m, with the side-port outlet located at $x = 0$ m. The vertical axis is the electron number density, which directly governs RF blackout.

For all cases, increasing the side-wall injection rate up to 2.00 g/s reduces the electron number density around the vehicle relative to the no-injection baseline. The injected nitrogen forms a thin, three-dimensional gas film that lowers the local plasma density near the surface. Within the range investigated, however, the results for 1.00 g/s and 2.00 g/s are nearly indistinguishable, indicating that simply increasing the mass flow beyond 1.00 g/s yields little additional benefit. This tendency is more pronounced at 79.2 km, where the freestream momentum is smaller due to the lower atmospheric density.

The electron number density profile along a line parallel to the upper surface at 68.6 km is shown in Figure 6.11b. Despite the ambient electron number density being roughly ten times larger than at 79.2 km, numerical analysis still confirms a substantial reduction in near-wall electrons with side-wall injection.

Finally, the effect of side injection on vehicle attitude is assessed. Figure 6.12 shows the area-integrated momentum flux (thrust) of the jet at the outlet of the 28 mm-diameter port. Because thrust scales linearly with mass flow rate and the flow inside the channel is underexpanded, the total thrust remains below 0.8 N even at the highest injection rate considered. This value is negligible compared with the aerodynamic drag force.

The drag force F_D is given by

$$F_D = \frac{1}{2} C_D A \rho u^2, \quad (6.1)$$

where C_D is the drag coefficient, A is the projected frontal area, and ρ and u are the freestream density and velocity, respectively. Using the freestream conditions at each altitude and adopting $C_D \approx 1.0$, the drag force is about 210 N at 79.2 km and 974 N at 68.6 km. Accordingly, the additional thrust from side-wall gas injection is insignificant compared with the aerodynamic forces and is not expected to destabilize the flight attitude or adversely affect the thermal environment by altering the angle of attack.

These flight-representative findings are consistent with the arc-heated wind-tunnel studies in chapter 3, chapter 4, and chapter 5. In the ISAS and L2K facilities, surface-aligned injection likewise produced a cold, near-wall gas film that simultane-

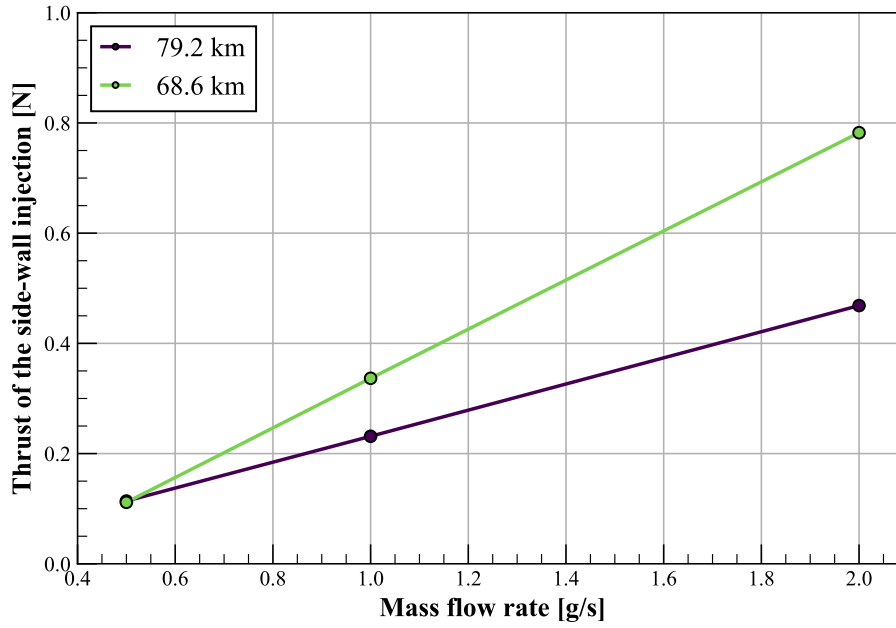


Figure 6.12: Thrust generated by side-wall gas injection.

ously reduced wall heat flux and electron number density, with diminishing returns once a continuous film of sufficient thickness was established. The RAM C-I analysis shows that the same trade-offs between injector location, jet momentum, film attachment, and blackout mitigation persist under realistic freestream Mach and Reynolds numbers and with atmospheric air chemistry, reinforcing the conclusion that gas-film injection is a robust, scalable mechanism for concurrent thermal and RF blackout mitigation from laboratory conditions to actual reentry trajectories.

6.4.3 Injector Type Comparison

The aerodynamic heating reduction achieved by nose-cone and side-wall injectors is compared in Figure 6.13, which shows the specific wall heat flux around the body at $z = 0$ m. The specific wall heat flux is defined as the ratio of the local heat flux to the stagnation heat flux at each altitude, q/q_{stg} . The heat flux on the lower surface of the vehicle is largely insensitive to injector type, whereas a pronounced reduction is observed on the upper surface.

In the frontal hemisphere ($x \leq 0$ m), the nose-cone injector significantly reduces the heat flux near the stagnation region. Downstream of the injector location ($x \geq 0$ m), the side-wall injector, located at $x = 0$ m, more effectively reduces the heat flux toward the rear, with reductions of order 2 % of q_{stg} even near the tail.

Figure 6.14 summarizes the mitigation efficiency for 2.00 g/s of injection from each injector type. The vertical axis represents the ratio of wall heat flux with

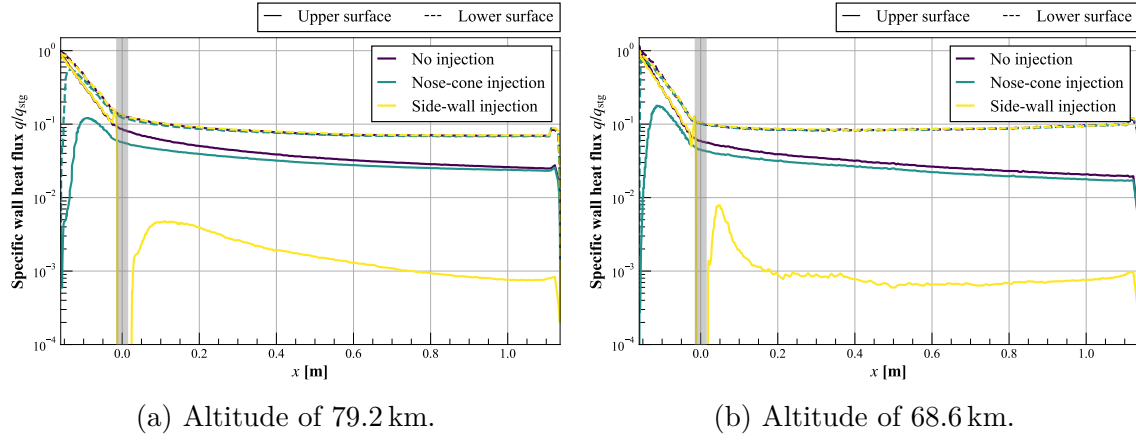


Figure 6.13: Normalized wall-heat-flux profiles, q/q_{stg} , around the vehicle circumference.

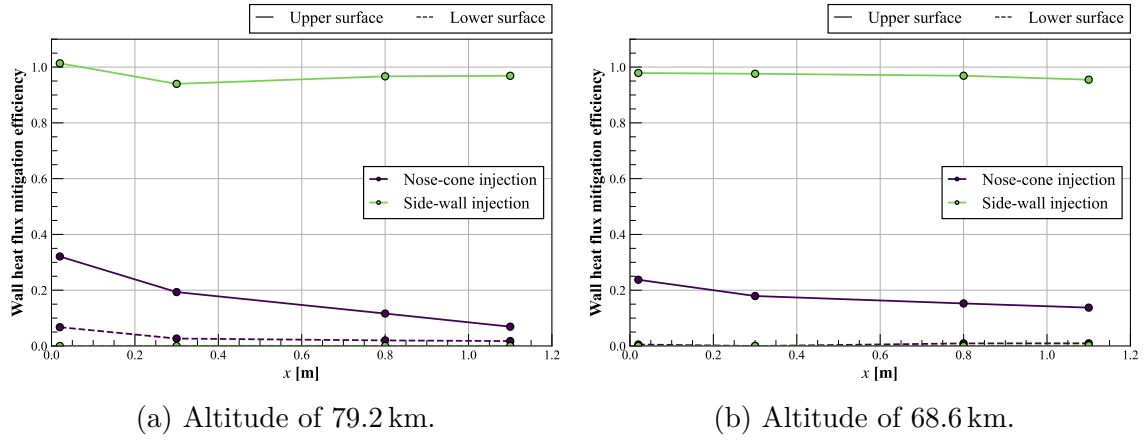


Figure 6.14: Heat-flux mitigation efficiency with 2.00 g/s of injection for the nose-cone and the side-wall injectors.

injection to that without injection. For the side-wall injector, the heat flux on the upper surface at $x = 1.10$ m is reduced by about 95.5 % at both altitudes, whereas the nose-cone injector achieves less than 18 % reduction at the same location.

The temperature and number-density profiles of chemical species along a normal line to the upper surface at $x = 0.30$ m are shown in Figure 6.15 and Figure 6.16, respectively. The species plotted comprise a subset of the 11-species air model; for clarity, only the six dominant reentry species are depicted.

At 79.2 km, side-wall injection produces a low-temperature region extending to about 0.15 m away from the surface, whereas at 68.6 km the cooling effect extends to roughly 0.05 m. In the higher-altitude case, the injected gas dominates the vicinity of the wall around $y \approx 0.05$ m: temperatures are minimized, N_2 number density is high, and other species are strongly depleted.

In the immediate vicinity of the vehicle, both translational and electron tempera-

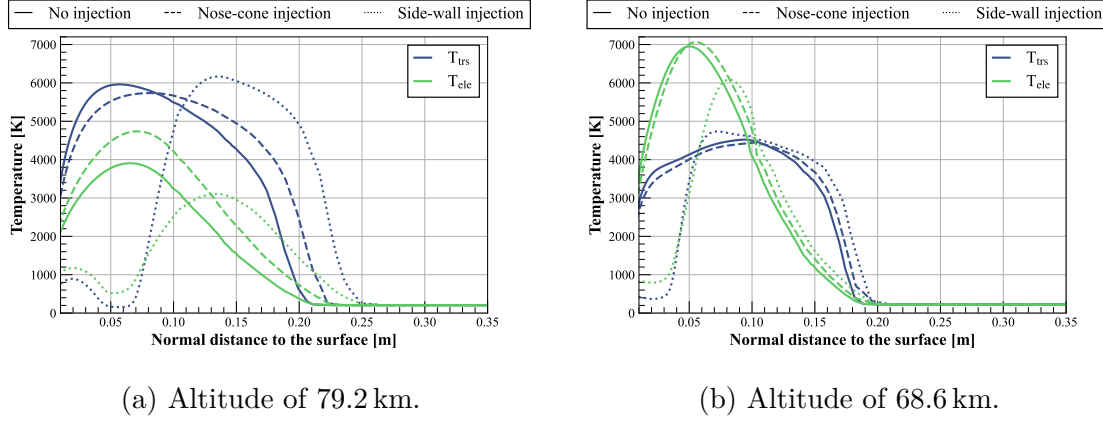


Figure 6.15: Translational and electron temperature profiles along a normal line to the vehicle's upper surface at $x = 0.30$ m with 2.00 g/s of injection.

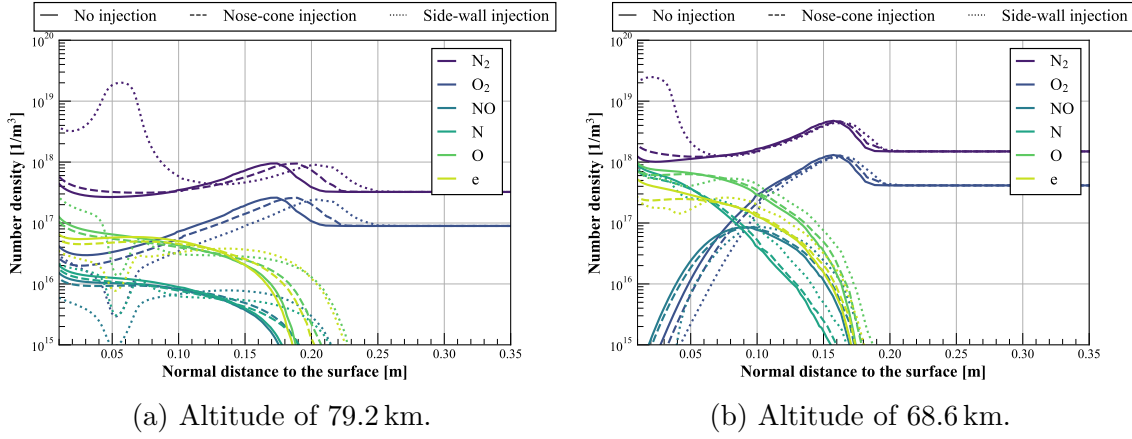


Figure 6.16: Number-density profiles of major chemical species along a normal line to the vehicle's upper surface at $x = 0.30$ m with 2.00 g/s of injection.

tures fall below 1,000 K, and the elevated N_2 concentration indicates that ionization and dissociation reactions are strongly suppressed within the cooled gas layer. A corresponding decrease in electron number density is observed in this region, implying reduced attenuation of RF signals propagating through the film. These results suggest that RF blackout mitigation using side-wall gas injection remains effective even under realistic reentry conditions.

6.4.4 Comparison with Arc-heated Wind Tunnel Environments

Although arc-heated wind tunnels can reproduce near-wall heat fluxes and electron densities comparable to reentry, the underlying flow environments differ in several fundamental respects. In arc-heated facilities, energy is deposited as internal energy

in a relatively low-velocity, low-density jet that expands into a low-pressure chamber, producing a long residence time and a thick shock layer around the model. By contrast, in flight the flow is driven by kinetic energy at hypersonic velocity, and the strong bow shock compresses and ionizes atmospheric air in a thin shock layer with steep gradients and shorter residence times. These differences also affect where the plasma is generated: in tunnels a highly ionized core formed in the heater is convected to the test section, whereas in flight most ionization occurs locally behind the shock.

Such distinctions imply that the boundary-layer momentum at the injector location can differ between wind tunnels and flight, even when the wall heat flux or electron density levels appear similar. Consequently, the jet-to-boundary-layer momentum ratio that governs film attachment may not be directly transferable without accounting for local boundary-layer structure and freestream composition.

Boundary-layer Profiles at the Injector Location

To further clarify how the near-wall momentum environment differs between arc-heated facilities and flight, Figure 6.17 compares wall-normal profiles of tangential velocity and momentum flux at the injector centerline for the no-injection baseline. The wall-normal coordinate is normalized by the boundary-layer thickness $\delta_{u,\text{ref}}$; here $\delta_{u,\text{ref}}$ is defined pragmatically as the location where the velocity gradient becomes small and the profile approaches a plateau. This definition is adopted because arc-heated tunnel flows continue to accelerate and expand in the test section and can be influenced by shock interactions, which makes a unique edge velocity difficult to identify. With this definition, the boundary-layer thicknesses at the injector locations are 33 mm for ISAS, 20 mm for L2K, 249 mm for RAM C-I at 79.2 km, and 234 mm for RAM C-I at 68.6 km. The RAM C-I profiles shown here are all taken from the side-wall injection configuration.

The velocity profile shows that the boundary-layer edge velocity in arc-heated tunnels is roughly one-half to one-third of the corresponding reentry values. This indicates that, even at the outer edge of the boundary layer, the local flow momentum in arc-heated facilities is substantially lower than that under flight conditions. The smallest momentum flux appears in the L2K arc-heated wind tunnel, while the profile obtained in the ISAS facility and in the high-altitude RAM C-I case exhibit comparable levels. In contrast, the low-altitude RAM C-I flight condition shows a substantially higher momentum flux throughout the boundary layer.

This trend reflects the progressive increase in the near-wall flow momentum from arc-heated wind tunnel environments to realistic reentry conditions. In the arc-

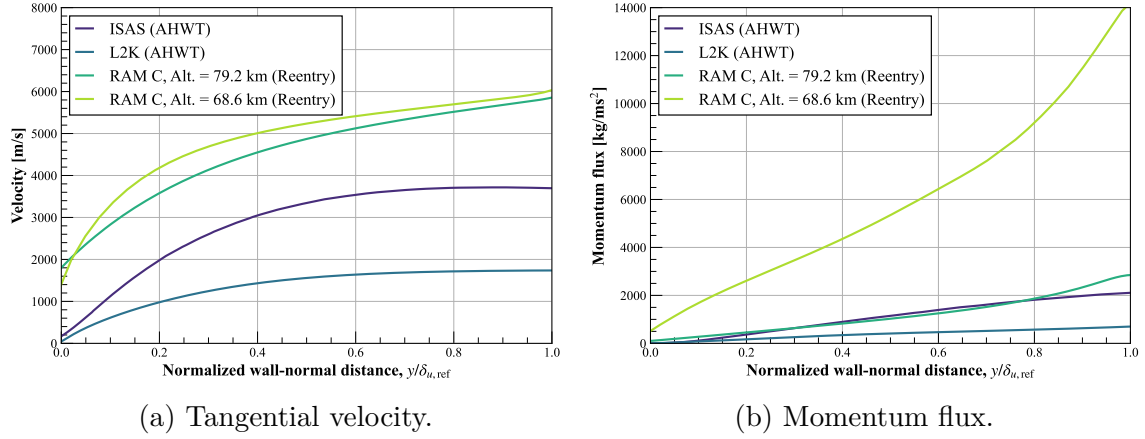


Figure 6.17: Wall-normal profiles at the injector location for the no-injection baseline. The wall-normal coordinate is normalized by the boundary-layer thickness $\delta_{u,\text{ref}}$, defined by the location where the velocity plateaus.

heated facility, the combination of low density and moderate velocity results in a reduced near-wall momentum flux. The comparison between the two RAM C-I cases further highlights the effect of atmospheric density. As the vehicle descends to lower altitude, the increase in ambient density produces a markedly stronger momentum environment near the surface, despite similar freestream Mach numbers.

Definition of Injection Parameters

To characterize the relative strength of gas injection with respect to the surrounding flow, the momentum flux ratio J is introduced as a representative nondimensional parameter.

The momentum flux ratio is defined as

$$J = \frac{(\rho u^2)_j}{(\rho U^2)_{\text{ref}}}, \quad (6.2)$$

where $(\rho u^2)_j$ denotes the momentum flux of the injected gas evaluated at the injector exit, and $(\rho U^2)_{\text{ref}}$ represents the local momentum flux of the baseline flow at the injector location without gas injection.

This parameter expresses the balance between the jet momentum and the momentum of the surrounding near-wall flow, and is therefore widely used in studies of transverse-jet interactions. In the present study, J is employed as a convenient indicator to compare injection conditions across different environments with markedly different density and velocity levels.

To quantify the geometric extent of the injected jet, the penetration height is introduced as a representative measure of jet penetration into the surrounding flow.

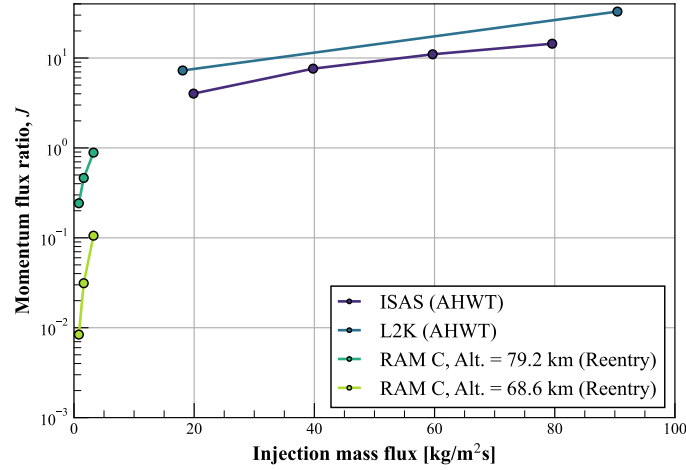


Figure 6.18: Injection mass flux and resulting momentum flux ratio under different environments.

In the present study, the penetration height is defined based on the mixture fraction of nitrogen, which is employed as a tracer species. The mixture fraction is defined as

$$Z = \frac{Y_{N_2} - Y_{N_2,\infty}}{Y_{N_2,j} - Y_{N_2,\infty}}, \quad (6.3)$$

where $Y_{N_2,\infty}$ denotes the nitrogen mass fraction in the baseline flow without injection, and $Y_{N_2,j}$ represents that of the injected gas.

For air-flow cases (ISAS and RAM C-I), $Y_{N_2,\infty}$ corresponds to the background nitrogen content in air, and the mixture fraction therefore represents the enrichment of injected nitrogen above the ambient level. For the argon-flow case in L2K, the baseline freestream contains no nitrogen ($Y_{N_2,\infty} = 0$), so the mixture fraction reduces to the local nitrogen mass fraction normalized by $Y_{N_2,j}$. In this case, the penetration height directly reflects the extent of injected nitrogen into the argon plasma.

With this definition, $Z = 0$ corresponds to the baseline flow and $Z = 1$ corresponds to the injected gas. The penetration height h is then defined as the maximum wall-normal distance from the surface at which the mixture fraction exceeds a prescribed threshold value.

In this study, a threshold value of $Z_{th} = 0.1$ is adopted as an operational definition of the outer extent of the injected gas, while avoiding numerical sensitivity associated with extremely low concentration levels.

Injection Mass Flux and Momentum Flux Ratio

Figure 6.18 summarizes the relationship between the injection mass flux at the injection outlet center and the resulting momentum flux ratio J for all considered

environments. For a given injection mass flux, the momentum flux ratio J varies significantly among the different conditions. Although comparable injection mass flows are applied, the resulting values of J differ significantly depending on both the injector geometry and the surrounding flow. In particular, arc-heated wind tunnel cases exhibit substantially larger momentum flux ratios than the flight conditions.

This difference originates from the strong variation in the near-wall momentum flux of the surrounding flow, as discussed above. The present comparison indicates that the momentum flux ratio cannot be treated as an environment-independent parameter when comparing gas injection performance across wind tunnel and flight conditions. Therefore, the penetration behavior of the injected jet should be evaluated with careful consideration of both the injection condition and the surrounding flow environment.

Momentum Flux Ratio and Penetration Height

To illustrate the spatial characteristics of the injected gas under different flow environments, Figure 6.19 presents the distribution of the nitrogen-based mixture fraction for representative injection conditions.

The contours are drawn at intervals of $Z = 0.1$, spanning from the baseline flow ($Z = 0$) to the injected gas ($Z = 1$). Although the injection mass flow rates are different among the cases, the figure highlights the characteristic structure of the injected jet in each environment, including the jet trajectory, the outer boundary of the injected gas, and the extent of the high-concentration core region.

In particular, the contour corresponding to $Z = 0.1$ represents the outer envelope of the injected gas and is used to define the penetration height, while the contour of $Z = 0.9$ indicates the jet core region. These definitions are applied consistently across all environments to enable a unified comparison of injection characteristics.

While the overall flow topology differs among arc-heated wind-tunnel and flight conditions, the mixture fraction distributions provide a common basis for evaluating the geometric extent of gas injection. Based on this definition, the penetration height is quantitatively evaluated at an injection port and downstream location.

Figure 6.20 shows the normalized penetration height h/d evaluated at the injector location as a function of the momentum flux ratio J . The penetration height is normalized by the injector diameter d and is defined based on the mixture fraction threshold of $Z_{\text{th}} = 0.1$.

Despite the large differences in flow conditions between arc-heated wind tunnel environments and the reentry cases, the single-hole injection results (ISAS and RAMC-I) exhibit a similar overall trend when organized by the momentum flux

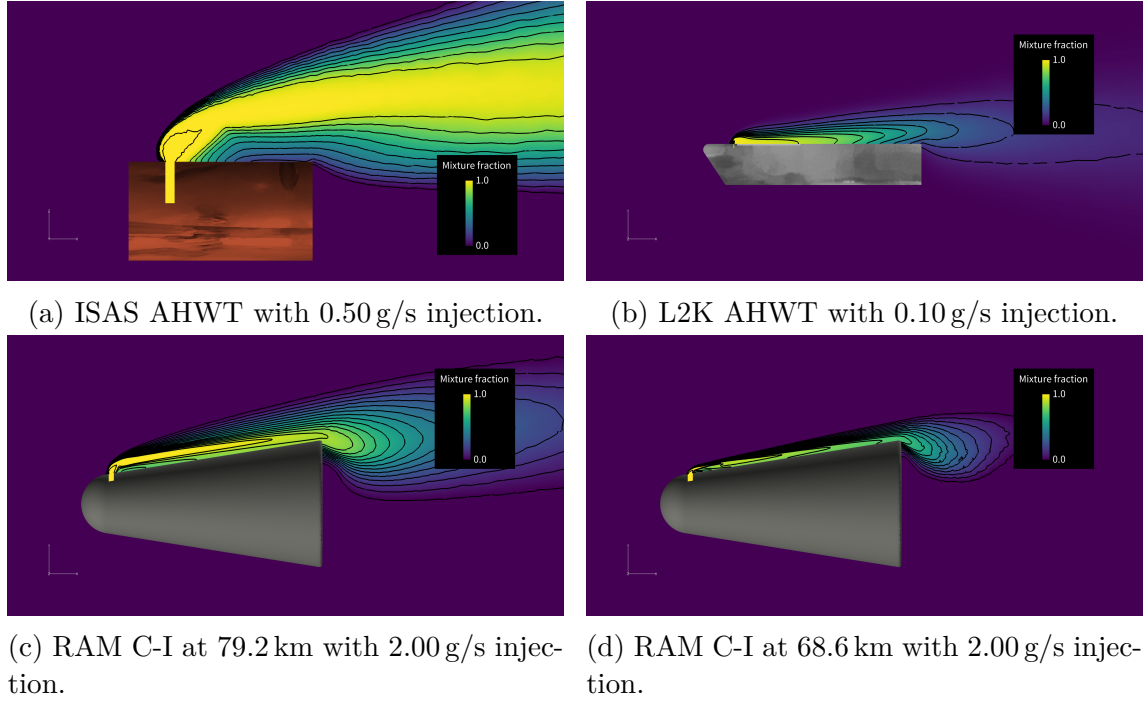


Figure 6.19: Mixture fraction distribution with z contours at intervals of 0.1 for various environments.

ratio. This indicates that, in the vicinity of the injection location, the penetration behavior is primarily governed by the local momentum balance between the injected jet and the surrounding near-wall flow.

At the same time, the absolute value of the momentum flux ratio required to achieve penetration differs significantly among the environments. In particular, arc-heated wind tunnel conditions require substantially larger values of J than the reentry environments to obtain similar normalized penetration heights. This discrepancy reflects the strong dependence of the momentum flux ratio on the local density and velocity levels of the surrounding flow.

The L2K results deviate from the single-hole trend due to the multi-hole injector configuration, which forms a distributed film rather than a single jet structure. Therefore, while the penetration height in the single-hole cases can be reasonably interpreted through the momentum flux ratio, multi-hole injection requires additional considerations of jet interaction and film formation dynamics.

These nondimensional comparisons provide a common basis for assessing gas injection across arc-heated wind tunnels and flight environments, but they do not remove the fundamental differences in flow generation and near-wall momentum. Accordingly, arc-heated facilities remain indispensable for verifying and demonstrating the underlying mitigation concept, while independent evaluation under flight-

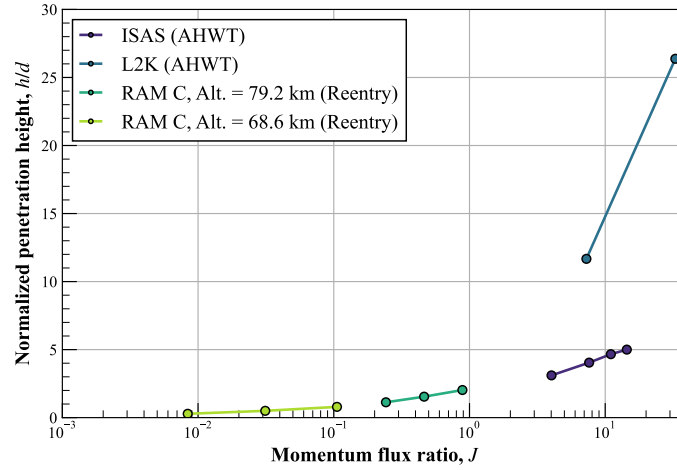


Figure 6.20: Normalized penetration height at the injector location.

representative conditions is essential to confirm effectiveness in actual reentry.

6.5 Conclusions

This chapter has numerically evaluated gas-film injection for simultaneous mitigation of aerodynamic heating and communication blackout under realistic atmospheric reentry conditions, using the RAM C-I vehicle as a representative configuration. The validated nonequilibrium CFD framework from the preceding chapters was applied to flight-relevant freestreams at altitudes of 79.2 km and 68.6 km, where RAM C-I experienced pronounced plasma-induced attenuation.

Baseline simulations reproduced the expected hypersonic flow distribution: a strong bow shock formed ahead of the blunt nose, generating a high-temperature, high-electron-density plasma sheath around the vehicle. Peak translational and electron temperatures reached approximately 22,000 K and 11,000 K, respectively, and thermochemical nonequilibrium was evident from the spatial offset between these temperature modes. The corresponding stagnation heat fluxes of 1.18 MW/m² at 79.2 km and 1.52 MW/m² at 68.6 km confirmed that the lower-altitude case represents a more severe aerothermal environment.

Two injection configurations were examined. For nose-cone injection, room temperature N₂ provided local film cooling near the port and modest reductions in wall heat flux, but the injected gas rapidly encountered the strong stagnation shock, was heated to high temperatures, and underwent intense ionization and dissociation. As a result, plasma densities near the surface remained high and the mitigation of blackout was limited. Increasing the leading-edge injection rate from 2.00 g/s to 3.00 g/s produced only marginal additional benefit, indicating that nose-cone injection is not

an efficient strategy for RF attenuation in this configuration.

In contrast, side-wall injection produced a cold gas film that remained attached to the upper surface over a broad downstream region. At both altitudes, the side injector substantially reduced wall heat flux on the upper surface—by up to 95.5 % at $x = 1.10$ m for 2.00 g/s of injection—while leaving the lower-surface heat flux essentially unchanged. Profiles of temperature and species number density along surface-normal lines showed that, within the film, translational and electron temperatures fell below 1,000 K, N_2 became the dominant species, and the concentrations of ionized components were strongly suppressed. Consequently, the electron number density near the wall was markedly reduced, forming a low-plasma-density corridor consistent with RF blackout mitigation.

Parametric variations in the side-wall injection rate revealed a saturation behavior: beyond approximately 1.0 g/s, further increases in mass flow did not significantly lower near-wall plasma density. At higher injection momentum, the jet was pushed farther away from the surface rather than strengthening the shielding effect at the wall, so the near-wall mitigation effect plateaued. The total thrust generated by the side jet remained below 0.8 N, which is negligible compared with aerodynamic drag forces of order 200 N to 1,000 N at the altitudes considered, indicating that gas-film injection in the examined range is not expected to destabilize the vehicle attitude.

Taken together, these results demonstrate that the gas-film injection concept established in arc-heated wind-tunnel studies can be extended to a realistic reentry trajectory. Side-wall injection, in particular, offers a promising balance between thermal protection and RF transparency for RAM C-class vehicles, while the observed saturation with mass flow highlights the need to optimize injection conditions under strict propellant and system-mass constraints.

In addition to the direct evaluation of gas-film injection under flight conditions, this chapter has clarified the relationship between wind-tunnel and reentry environments from the viewpoint of jet – crossflow interaction. By organizing the penetration behavior using the momentum flux ratio, it was shown that single-hole injection cases in arc-heated facilities and flight conditions exhibit similar penetration trends when expressed in terms of normalized penetration height. At the same time, the absolute value of the momentum flux ratio required to achieve comparable penetration differs significantly among environments, reflecting strong variations in the near-wall momentum environment.

These findings indicate that, while arc-heated wind-tunnel experiments provide valuable insight into the fundamental physics of gas-film injection, quantitative assessment of blackout mitigation performance requires complementary flight-relevant

numerical analysis. The present flight CFD results therefore play a critical role in bridging laboratory-scale experiments and realistic atmospheric reentry conditions.

Chapter 7

Conclusion

This thesis has examined surface gas-film injection as a unified strategy to mitigate two long-standing challenges in atmospheric reentry: plasma-induced radio-frequency (RF) blackout and severe aerodynamic heating. Building on the background and objectives summarized in chapter 1, the work has combined high-enthalpy experiments in arc-heated wind tunnels with thermochemical nonequilibrium computational fluid dynamics (CFD) and, where needed, frequency-dependent computational electromagnetics (CEM), in order to clarify whether an injected gas film can simultaneously open an RF transmission corridor and provide thermal protection under realistic conditions.

The numerical study in chapter 3 developed and applied a coupled CFD–FD2TD framework to the DLR L3K arc-heated facility. Using a nonequilibrium air-chemistry model together with a dispersive Drude permittivity, the simulations demonstrated that nitrogen injection from a surface port forms a cold gas film that both lowers near-wall electron number density below the critical value for a 2.4 GHz link and reduces convective heat flux. This study provides high-fidelity, multiphysics confirmation that an advected gas film can act as an RF waveguide while also functioning as a thermal barrier in a megawatt-class plasma tunnel.

chapter 4 and chapter 5 extended this concept to two arc-heated facilities of different scales: the 1 MW ISAS tunnel and the DLR L2K tunnel. In both facilities, communication tests showed that, in the absence of injection, a dense plasma sheath caused RF communication attenuation, whereas surface injection of cold gas restored the link and reduced surface heating. Schlieren visualization and wall-heat-flux measurements, together with companion CFD analyses, confirmed that the injected jet expands into the low-pressure chamber, forms a cold, weakly ionized film along

the model surface, and displaces the plasma away from critical antenna regions. These chapters thus provide systematic experimental evidence that gas-film injection can deliver simultaneous blackout and heating mitigation in controlled high-enthalpy ground facilities, and they validate the numerical framework against facility data.

chapter 6 then translated the gas-film concept to the flight regime by analyzing the RAM C-I reentry vehicle under realistic freestream conditions at altitudes where blackout was observed in the original missions. Using the validated CFD framework and atmospheric conditions from NRLMSISE-00 and RAM C-I flight data, the simulations showed that nose-cone injection offers only limited blackout mitigation, because the injected gas is rapidly heated and ionized in the stagnation shock. By contrast, side-wall injection produces a cold, nitrogen-rich film that remains attached over a large portion of the upper surface, significantly reduces wall heat flux, and establishes a near-wall region of lowered electron number density even in the denser lower-altitude case. These results demonstrate that the dual mitigation mechanism observed in arc-heated tunnels can be extended to a realistic reentry trajectory, and they clarify the importance of injector placement and injection momentum for flight applications.

Viewed as a whole, the thesis makes three main contributions. First, it establishes and validates a multiphysics CFD – CEM framework that captures the coupled plasma, gas-injection, and RF-propagation phenomena relevant to blackout mitigation. Second, it provides the first coordinated experimental and numerical demonstration spanning megawatt-class arc-heated facilities that a cold gas film can simultaneously reduce near-wall plasma density and aerodynamic heating. Third, it extends the concept to a flight-representative configuration, identifying injector configurations and mass-flow ranges for which gas-film injection remains effective under realistic Mach numbers, Reynolds numbers, and atmospheric chemistry. Within the broader development path, this work serves as a pre-flight stage that provides combined numerical and experimental evidence to underpin future flight demonstrations.

At the same time, the work reveals important design trade-offs and open questions. The mitigation effect saturates beyond a certain mass-flow rate: once a sufficiently thick, attached film is formed, additional momentum tends to lift the jet away from the surface rather than strengthening the shielding effect at the wall. This suggests that practical systems must be optimized for injector location, orientation, and mass flow, rather than simply maximizing gas injection. Moreover, the present study has focused on a single carrier frequency and on nitrogen as the injection gas; future work should explore other RF bands, mixed or electronegative

injectants, and more complex antenna configurations. Further high-enthalpy facility experiments that combine side-wall injection with detailed plasma diagnostics and multi-frequency RF measurements, together with three-dimensional CEM analyses of realistic vehicle and antenna geometries, will be essential to refine quantitative design margins.

In conclusion, this thesis has moved gas-film injection from a largely conceptual mitigation idea, supported by limited laboratory evidence, to a quantitatively supported approach spanning arc-heated facilities and a realistic reentry trajectory. The results indicate that, when properly configured, gas-film injection can provide a robust, scalable mechanism for simultaneous blackout and heating mitigation, and they lay a foundation for the design and optimization of gas-film systems in future atmospheric-entry missions.

Looking ahead to flight experiments, the next steps require end-to-end design and analysis: optimizing injection gas species and hardware, selecting vehicle geometry and trajectory, sizing carried gas mass, designing the injection mechanism, and evaluating how the expected mitigation translates to landing-dispersion reduction.

Acknowledgments

本博士論文の執筆にあたり，多くの方々からご指導とご支援を賜りました．ここに感謝の意を表します．

まず，本研究を進めるにあたり，終始丁寧かつ的確なご指導を賜りました指導教官の高橋裕介准教授に，心より感謝申し上げます．研究テーマの選定から論文の推敲に至るまで，自主性を尊重していただき，自由な発想のもとで研究に取り組める環境を与えてくださいました．さらに，国内外の多様な研究者との交流の機会を数多くご提供いただき，研究の視野を大きく広げることができました．とりわけ，宇宙航空研究開発機構 宇宙科学研究所（JAXA/ISAS）や German Aerospace Center（DLR）との共同研究は，研究活動全体に大きな刺激と示唆を与えるものでした．

副指導教員として貴重なご助言を賜りました永田晴紀教授および田坂裕司教授，ならびに本論文の審査にあたり有益なご指摘をいただきました審議委員の先生方に，厚く御礼申し上げます．

本研究の一部は，JAXA/ISAS における共同研究の成果に基づくものです．共同研究者である JAXA/ISAS の高澤秀人博士，山田和彦准教授，東京科学大学の永田靖典博士，九州大学の木原尚助教をはじめとする関係者の皆様には，実験の実施ならびに結果に関する議論に際して多くの貴重なご助言をいただきました．ここに深く感謝の意を表します．

また，本研究の一部は，DLR における国際共同研究の成果に基づくものです．Ciro Salvi 氏，Oliver Hohn 博士，Ali Gülhan 博士をはじめとする共同研究者の皆様には，実験設備の提供ならびに専門的知見に基づく有益なご議論を通じて，多大なご協力をいただきました．ここに記して感謝申し上げます．

本研究の遂行にあたって，計算流体力学研究室の大島伸行教授および寺島洋史准教授には，ゼミでの議論を通じて多くの示唆をいただきました．また，研究室の先輩・後輩の皆様にも，日々の研究活動を通じて多くの議論や助言を賜りました．ともに研究生活を送る中で得られた経験は，本研究のみならず今後の研究活動においても大きな財産となるものです．心より感謝申し上げます．

最後に，これまで長きにわたり温かく見守り，支え続けてくれた家族に深く感謝

します。皆様の理解と支援があつてこそ、本研究を最後まで遂行し、本博士論文を完成させることができました。

本研究は、日本学術振興会 科学研究費補助金（課題番号：17KK0123）の支援を受けて実施されました。本研究の数値解析は、HPCI システム利用課題（課題番号：hp210097, hp220007, hp230009, hp240003, hp250169）を通じて、九州大学が提供するスーパーコンピュータ ITO および 玄海、北海道大学が提供するスーパーコンピュータ Grand Chariot および Grand Chariot 2, ならびに理化学研究所が提供するスーパーコンピュータ 富岳 の計算資源を利用して実施されました。本研究の流体解析結果は、宇宙航空研究開発機構が所有する高速流体解析ソフトウェア「FaSTAR」を利用して得られたものです。本研究における JAXA/ISAS アーク加熱風洞実験は、惑星大気突入環境模擬装置の大学共同利用（課題番号：WA21-05, WA21-06, WA22-04, WA23-11）により実施されました。さらに、本研究における DLR での実験滞在は、2023 年度日本学術振興会「若手研究者海外挑戦プログラム」、2024 年度北海道大学「研究者海外派遣支援事業」、ならびに DLR Program Directorate for Space Research and Development の支援を受けて実施されました。

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