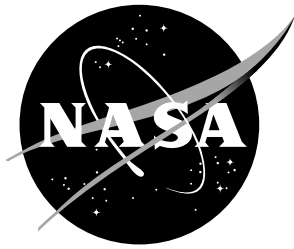


NASA/TM-20240015176



Aerothermal Analysis of the Rocket Lab Venus Probe Heatshield

Christopher O. Johnston and Alireza Mazaheri
NASA Langley Research Center, Hampton, VA 23682

November 2024

NASA STI Program Report Series

Since its founding, NASA has been dedicated to the advancement of aeronautics and space science. The NASA scientific and technical information (STI) program plays a key part in helping NASA maintain this important role.

The NASA STI Program operates under the auspices of the Agency Chief Information Officer. It collects, organizes, provides for archiving, and disseminates NASA's STI. The NASA STI Program provides access to the NTRS Registered and its public interface, the NASA Technical Report Server, thus providing one of the largest collections of aeronautical and space science STI in the world. Results are published in both non-NASA channels and by NASA in the NASA STI Report Series, which includes the following report types:

- **TECHNICAL PUBLICATION.** Reports of completed research or a major significant phase of research that present the results of NASA programs and include extensive data or theoretical analysis. Includes compilations of significant scientific and technical data and information deemed to be of continuing reference value. NASA counterpart of peer-reviewed formal professional papers, but having less stringent limitations on manuscript length and extent of graphic presentations.
- **TECHNICAL MEMORANDUM.** Scientific and technical findings that are preliminary or of specialized interest, e.g., quick release reports, working papers, and bibliographies that contain minimal annotation. Does not contain extensive analysis.
- **CONTRACTOR REPORT.** Scientific and technical findings by NASA-sponsored contractors and grantees.

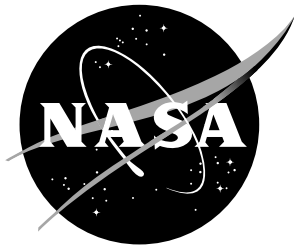
- **CONFERENCE PUBLICATION.** Collected papers from scientific and technical conferences, symposia, seminars, or other meetings sponsored or co-sponsored by NASA.
- **SPECIAL PUBLICATION.** Scientific, technical, or historical information from NASA programs, projects, and missions, often concerned with subjects having substantial public interest.
- **TECHNICAL TRANSLATION.** English-language translations of foreign scientific and technical material pertinent to NASA's mission.

Specialized services also include organizing and publishing research results, distributing specialized research announcements and feeds, providing information desk and personal search support, and enabling data exchange services.

For more information about the NASA STI Program, see the following:

- Access the NASA STI program home page at <http://www.sti.nasa.gov>
- Help desk contact information: <https://www.sti.nasa.gov/sti-contact-form/> and select the "General" help request type.

NASA/TM-20240015176



Aerothermal Analysis of the Rocket Lab Venus Probe Heatshield

Christopher O. Johnston and Alireza Mazaheri
NASA Langley Research Center, Hampton, VA 23682

National Aeronautics and
Space Administration

November 2024

The use of trademarks or names of manufacturers in this report is for accurate reporting and does not constitute an official endorsement, either expressed or implied, of such products or manufacturers by the National Aeronautics and Space Administration.

Available from:

NASA STI Program / Mail Stop 150
NASA Langley Research Center
Hampton, VA 23681-2199

Abstract

This document provides an overview of the aerothermodynamic analyses performed by the Aerothermodynamics Branch at NASA Langley Research Center for the Rocket Lab Venus Probe (RLVP). In addition to defining the baseline heating environment to the heatshield, this document pursues the experimental validation of key physical models at RLVP-relevant conditions. This experimental validation analysis, which captures the model form uncertainty, is used as one of two primary components of the margin assessment, where the other component is the parametric uncertainty. These model form (experimental) and parametric uncertainty components are used to construct a spatial and time varying margin for the heating to the RLVP heatshield. The margin is evaluated as the sum of the parametric and model form uncertainty components. The model form uncertainty is defined as the difference between the RLVP-relevant measurements and their simulations, using the upper limit uncertainty bounds for both the measurements and simulations in the comparisons. The differences in the dominant physics in the stagnation region and flank lead to the separate RLVP-relevant measurements for assessing the model form uncertainty in these two regions. These regions are addressed as follows:

Stagnation Region Heating Environment: For the high-temperature stagnation-region, both the radiative heating and impact of blowing on convective heating are significant, while the impacts of turbulence and roughness are negligible. Coupled radiation and ablation LAURA/HARA solutions with ray-tracing provide the radiative heating over the entire vehicle, including the contributions from the Venus atmosphere and ablation species. Non-ablating LAURA simulations provide the convective heating. During the material-response computation typically used for TPS sizing, this non-ablating convective heating is corrected for the impact of ablation using the blowing correction. Coupled ablation LAURA simulations that capture finite-rate surface processes show that this blowing correction may be non-conservative over most of the heatshield. This non-conservatism is due to hydrogen recombination in the finite-rate surface model, which tends to increase the coupled ablation convective heating to near the non-ablating values, therefore making any reduction in the non-ablating value through the blowing correction non-conservative. This non-conservatism due to H catalysis is captured in the parametric component of the margin.

The best available ground-test measurements that capture the impact of blowing on stagnation region convective heating, at RLVP-relevant conditions, indicate that the current blowing reduction model is non-conservative by up to 20% at RLVP-relevant blowing rates (the coupled blowing simulations were also non-conservative). Because of the relatively low velocity of the ground tests and the non-Venus atmospheric chemistry, these measurements do not capture the chemistry and therefore do not inform the uncertainty due to H catalysis. However, they do capture the fluid mechanics of blowing. The non-conservatism of the blowing correction implied by these measurements is covered by the model form component of the margin, which leads to total

margin values over 50%.

For the radiative heating, the shock-tube informed bias approach suggests a model form uncertainty of roughly 20%, while the parametric uncertainty analysis suggests values over 100%. The combined stagnation-point radiation margin of over 100% leads to peak margined radiative heating values of over 300 W/cm^2 , which remains small relative to the peak margined convective heating of nearly 2000 W/cm^2 .

Based on this analysis, at the stagnation point, the peak margined heat rate is 2203 W/cm^2 and the margined total heat load is 31.5 kJ/cm^2 for the current nominal trajectory.

Flank Heating Environment: The forebody flank (and near-shoulder) heating environment is dominated by the impact of turbulence, roughness augmentation, and ablation on the convective heating. An extensive collection of ground test measurements with RLVP-relevant turbulence and roughness is studied to show that the maximum difference between the simulated and measured convective heating is 5%. However, with the exception of the Holden measurements from the 1980s, these measurements do not include roughness elements extending into the supersonic region of the boundary layer, which is likely to occur for RLVP (due to the 45 degree sphere-cone geometry). The interaction between the supersonic flow and roughness could cause convective heating augmentation fundamentally different than for locally subsonic flow. Although these Holden measurements are consistent with the other measurements considered, another path was pursued to assure that the roughness height extending into supersonic flow does not fundamentally change the roughness augmentation. This additional path was a computational effort to resolve the roughness elements in the CFD grid, so that the interaction between the roughness elements and locally supersonic flow may be simulated in detail. This roughness-resolved CFD simulation is feasible because of the RLVP forebody TPS's patterned roughness, which may be approximated analytically, and because of the axisymmetric nominal flowfield, which allows a narrow surface region to be simulated and therefore make the computational expense feasible. These grid-resolved roughness simulations, which are performed at actual RLVP flight conditions, result in heating augmentation values that are below the design approach for roughness augmentation. This provides evidence that the design approach for RLVP roughness augmentation is sufficient.

Based on this analysis, at this flank or near-shoulder location, the peak margined total heat rate is 2088 W/cm^2 and the margined total heat load is 26.0 kJ/cm^2 for the current nominal trajectory.

Heat flux, shear, pressure and heat transfer coefficient at the RLVP stagnation point and near shoulder location are evaluated for the entire trajectory, and curved fit to a functional form of $F = A\rho_\infty^B U_\infty^C$. These simplified relationships for the nominal and margined aerothermal environments are referred to as aerothermal indicators, and presented at the end of this document.

Contents

1	Introduction	5
2	Aerothermal Modeling Overview	7
2.1	Simulation Approach	7
2.2	Aerothermal Margin Overview	10
2.2.1	Model Form Uncertainty Component	12
2.2.2	Parametric Uncertainty Component	12
3	Laminar/Stagnation Heating Environment	22
3.1	RLVP Laminar Heating	22
3.2	Application of Shock-Tube Informed Bias to RLVP	25
3.2.1	Evaluation of f_{adjust} for RLVP-Relevant Shock-Tube Measure- ments	26
3.2.2	Application of Shock-Tube Informed Bias	28
4	Roughness Augmented Turbulent Heating Environment	30
4.1	Smooth Wall Turbulent Heating	30
4.1.1	Orion AEDC Measurements	33
4.1.2	Orion CUBRC Measurements	35
4.2	Roughness-Induced Turbulent Convective Heating Augmentation . .	38
4.3	Langley Mach 6 Aerothermal Measurements for 3MDCP Patterned Roughness	40
4.4	Wilder and Prabhu Ballistic Range Measurements	49
4.5	Hollis Langley Mach 6 Measurements	54
4.6	Holden Measurements	56
4.7	Grid-Resolved Roughness (GRR) Simulations	63
4.7.1	GRR Simulation Approach	63
4.7.2	Establishing GRR Validity With 3MDCP Mach 6 Measurements	64
4.7.3	GRR Simulations for RLVP	66
4.8	Summary of Turbulence and Roughness Measurement Comparisons .	67
5	Impact of Ablation on Heating	70
5.1	Approximations for Modeling Impact of Ablation on Aerothermal Environment	70
5.2	Coupled Ablation RLVP Results for an Equilibrium Surface	72
5.2.1	Impact of Approximation #2	73
5.2.2	Impact of Approximation #1	76
5.2.3	Combined Impact of Both Approximations	76
5.3	Coupled Ablation Approach for a Finite-Rate Surface	79
5.3.1	Oxidation	81
5.3.2	Sublimation	82
5.3.3	Nitridation and N_2 Recombination	82
5.3.4	H_2 Recombination	83
5.3.5	Application of the Finite-Rate Surface Model to RLVP	83

5.4	Impact of Ablation on Forebody Radiative Heating	86
5.5	Comparison with CUBRC Measurements with Roughness and Blowing	88
5.6	Assessing the Potential of Cross-Hatching Patterns Forming on the RLVP Heatshield	92
6	Heatshield Margins	95
6.1	Convective Heating	96
6.2	Radiative Heating	99
6.3	Final Margined Heating	101
6.4	Aerothermal Indicators	101
6.5	Future Improvements	103

1 Introduction

This report documents the aerothermal analyses performed by the Aerothermodynamics Branch at NASA Langley Research Center for the Rocket Lab Venus Probe (RLVP). These analyses include the development of the margined aerothermal heating indicators, which are curved fit representations of the surface heat flux, shear, pressure, and heat transfer coefficient as a function of free-stream velocity and density. In addition, the experimental traceability of the tools and models used to create these values are presented in detail. These experimental traceability and parametric uncertainty analyses are used to establish a rigorous margin policy for the convective and radiative heating.

To demonstrate the dominant phenomena impacting the forebody heating for RLVP, Figs. 1 and 2 show the impact of radiation, turbulence, roughness, and ablation at two locations on the RLVP heatshield for a representative trajectory. The trajectory, RLVP geometry, and modeling assumptions inherent in these values are discussed throughout this report. For the stagnation-point, the non-ablating convective heating shown in Fig. 1 is not influenced by turbulence or roughness (so only the laminar values are shown), but radiative heating is significant. For the near shoulder point, the radiative heating is negligible (and is not shown), but the impact of turbulence and roughness are significant. The nominal, unmargined peak heat rate of nearly 1500 W/cm^2 at the near shoulder is larger than any acreage non-ablating heat rate for any NASA vehicle, other than the Galileo and Pioneer-Venus probes, which were Jupiter and Venus entry vehicles, respectively. The values presented in Fig. 1 do not include ablation or margins. Figure 2 shows the impact of ablation on the convective heating, which includes the impact of turbulence and roughness, using the uncoupled blowing correction approach typically utilized by the material response code during heatshield sizing (this heatshield sizing step is beyond the scope of the present work). This figure shows that at both the stagnation point and near shoulder locations, this blowing correction approach suggests that ablation reduces the peak heat rate by over 30%. At the stagnation point, the large reduction in the convective heating due to ablation increases the relative importance of radiation. The uncoupled blowing correction approach used for design does not account for the impact of ablation on the radiative heating. This independence of the radiation on ablation is acceptable for milder aerothermal environments. The relatively high heating (and therefore ablation) rates expected for RLVP, however, result in a large enough change to the shock layer temperatures and chemistry to strongly impact the radiative heating. The assessment of this impact of ablation on the radiative heating requires a coupled ablation approach, which is presented in this report.

The importance of radiation, turbulence, roughness, and ablation on the aerothermodynamic environment for RLVP requires that the modeling details, uncertainty, and a level of experimental validation are provided for each. This leads to the following first five sections of the present document:

2. *Aerothermal Modeling Overview:* Defines vehicle geometry and free-stream conditions considered. Briefly outlines computational models applied

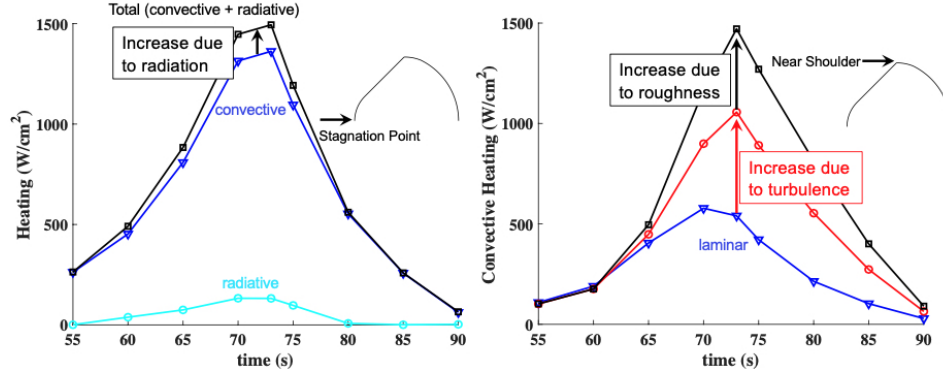


Figure 1: Contributions of turbulence, roughness, and radiation on the non-ablating forebody heating (un-margined).

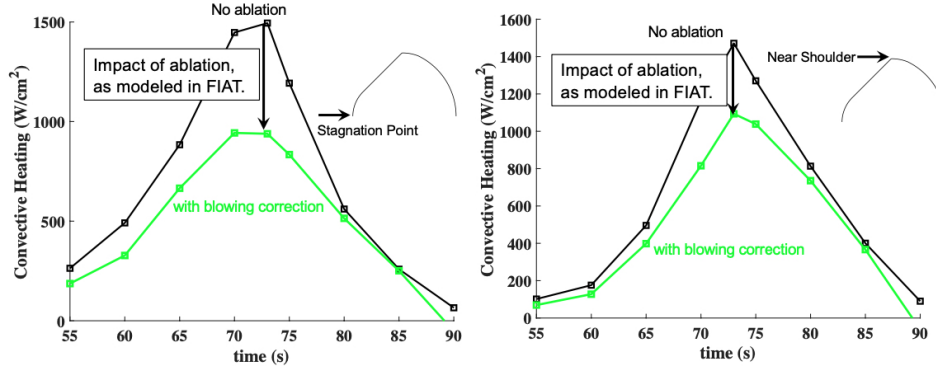


Figure 2: Impact of ablation on the forebody heating (un-margined).

throughout the analysis, many of which are discussed in more detail in subsequent sections. Provides an overview of the margin approach, which requires evaluation of the model form and parametric uncertainty components. Details for determining these components are provided.

3. Laminar/Stagnation Heating Environment: Presents laminar heating environment on the forebody heatshield, which represents the stagnation region design environment (which is not influenced by turbulence or roughness). Experimental traceability for these environments is obtained from shock-tube measurements using the shock-tube informed bias approach.

4. Roughness Augmented Turbulent Heating Environment: Presents the turbulent heating environment including turbulence and roughness augmentation. Experimental traceability for both the turbulence and roughness augmentation models is provided by comparisons between simulations and a collection of ground-test measurements. For each measurement, the similarity between the experimental case and RLVP boundary layers are presented. An effort to simulate grid-resolved roughness elements in CFD is presented to check that roughness elements extending into the supersonic region of the

boundary layer does not cause excessive roughness augmentation (since this behavior is only captured in one of the experimental measurements).

5. Impact of Ablation on Heating: RLVP TPS sizing is performed using the standard uncoupled ablation approach, where the aerothermal database assumes a non-ablating, fully-catalytic, and radiative equilibrium surface, which is subsequently corrected for ablation within the material response code using two approximations. The accuracy of these approximations for RLVP is assessed in this section using coupled ablation simulations, which model the blowing surface and additional ablation species in the flowfield simulations. Ground-based measurements with blowing are analyzed to provide experimental validation for the blowing correction. The impact of ablation products on the radiative heating is also assessed, which is found to be large, and therefore the radiative heating indicators include the impact of ablation.

6. Heatshield Margins: Both the convective and radiative heating margins are evaluated for the heatshield. Utilizing the model form uncertainty components derived from experiments in previous sections and a parametric uncertainty analysis, a time and spatial dependent margin approach is developed and implemented. The parametric uncertainty analysis includes coupled ablation and radiation. **Presents the margined and un-margined aerothermal indicators at the stagnation point and near-shoulder locations, which represent the primary result of this document.**

2 Aerothermal Modeling Overview

2.1 Simulation Approach

The aerothermal database is based on axisymmetric LAURA [1, 2] Navier-Stokes flowfield simulations. The HARA code [3, 4] is applied for radiative heating predictions, which include the impact of ablation products, as discussed in Section 5.4. The database is constructed based on the velocity-altitude space presented in Fig. 3. The nominal trajectory considered throughout this document, which is provided by the Atmospheric Flight & Entry System Branch at NASA Langley Research Center, is identified as the “a” trajectory in this figure. This nominal trajectory is tabulated in Table 1, along with the bounding shallow “b” and steep “c” cases. These bounding cases are chosen to capture trajectory dispersions and modest changes to the vehicle mass, entry velocity, and entry flight path angle relative to the nominal trajectory, but do not represent actual trajectories (therefore no times are listed for each case).

The LAURA flowfield simulations are performed using the Mars/Venus model presented by Johnston and Brandis [5], which include two-temperature, thermochemical nonequilibrium with the following 19 species: CO_2 , CO , N_2 , O_2 , NO , C , N , O , CN , C_2 , C^+ , O^+ , N^+ , NO^+ , O_2^+ , CO^+ , CN^+ , N_2^+ , and e^- . The chemical kinetics used for the LAURA simulations are presented in Tables 6–8. As will be shown in the margin assessment, the impact of the two temperature and chemical

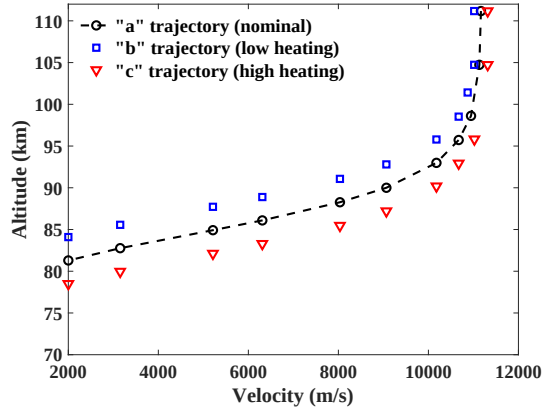


Figure 3: Aerothermal database points considered Nominal trajectory and database cases considered throughout this report.

Table 1: Nominal trajectory and database cases considered throughout this report.

Time (s)	Case #	Velocity (km/s)	Density (kg/m ³)	Temperature (K)	Altitude (km)	Mach #	Dyn. Pressure (kPa)
55.0	1a	11.167	4.16E-06	175.0	111.2	49.9	0.3
60.0	2a	11.167	2.52E-05	173.3	104.7	53.6	1.6
65.0	3a	10.947	1.24E-04	172.9	98.6	53.5	7.4
67.5	4a	10.673	2.63E-04	168.9	95.7	52.1	15.0
70.0	5a	10.178	5.21E-04	167.2	93.0	48.4	27.0
73.0	6a	9.066	1.22E-03	169.4	90.0	44.4	50.2
75.0	7a	8.035	1.92E-03	173.0	88.3	39.2	61.8
78.0	8a	6.310	3.24E-03	178.0	86.1	30.3	64.5
80.0	9a	5.213	4.19E-03	181.2	84.9	24.6	56.9
85.0	10a	3.153	6.78E-03	187.6	82.8	14.6	33.7
90.0	11a	2.001	9.28E-03	192.4	81.3	9.1	18.6
-	1b	11.017	4.16E-06	175.0	111.2	49.3	0.3
-	2b	11.017	2.52E-05	173.3	104.7	52.9	1.5
-	3b	10.872	6.15E-05	174.4	101.4	53.4	3.6
-	4b	10.673	1.29E-04	172.8	98.5	52.6	7.4
-	5b	10.178	2.64E-04	169.0	95.8	50.1	13.7
-	6b	9.066	5.66E-04	167.2	92.8	43.9	23.3
-	7b	8.035	9.31E-04	168.2	91.1	39.5	30.0
-	8b	6.310	1.62E-03	171.7	88.9	30.7	32.2
-	9b	5.213	2.19E-03	174.2	87.7	25.4	29.8
-	10b	3.153	3.56E-03	179.4	85.6	14.9	17.7
-	11b	2.001	5.09E-03	183.5	84.1	9.4	10.2
-	1c	11.317	4.16E-06	175.0	111.2	50.6	0.3
-	2c	11.317	2.52E-05	173.3	104.7	54.3	1.6
-	3c	11.022	2.64E-04	169.0	95.8	54.5	16.0
-	4c	10.673	5.37E-04	167.2	92.9	51.1	30.6
-	5c	10.178	1.17E-03	169.2	90.2	50.0	60.8
-	6c	9.066	2.45E-03	175.4	87.2	43.7	100.8
-	7c	8.035	3.71E-03	179.7	85.5	38.2	119.6
-	8c	6.310	6.06E-03	186.0	83.3	29.4	120.7
-	9c	5.213	7.66E-03	189.5	82.1	23.7	104.1
-	10c	3.153	1.22E-02	197.3	80.0	14.1	60.6
-	11c	2.001	1.58E-02	203.3	78.5	8.7	31.8

kinetic modeling differences have a small impact on the forebody radiative heating, and essentially no impact on the convective heating. The non-ablating surface is assumed to be fully-catalytic to homogenous recombination. The forebody and afterbody are assumed in radiative equilibrium with a surface emissivity of 0.85. Details of the coupled ablation simulations, used for the forebody radiative heating values, are presented in Section 5.

The Cebeci-Smith [6] turbulence model is applied for the turbulent LAURA simulations, as discussed in Section 4. This model was chosen both because it compares well with the measurements presented in Section 4, but also because it does not break down in the flank regions like the Baldwin-Lomax model [6, 7]. This breakdown is the result of the Baldwin-Lomax model being derived to reproduce the Cebeci-Smith without finding the boundary layer edge. Baldwin-Lomax’s alternative approach to finding the boundary layer edge breaks down for RLVP cases, therefore leading to erroneously high heating values. Further discussion of the turbulence modeling is provided in Section 4.

The TPS material for the RLVP forebody heatshield is woven 3MDCP. To obtain the expected roughness geometry present during RLVP peak-heating, the surface scans of 2023 AEDC post arc-jet samples are considered. These wedge measurements experience turbulent flow at RLVP-relevant heating values. For one of the runs from this test series, Fig. 4(a) present the front view of the wedge with the ablated surface pattern. The area in the black box is analyzed to provide the peak-to-valley heights presented in Fig. 4(b). The average peak-to-valley height (k) from these cases is 0.75 mm, with a peak-to-valley spacing of 2.20 mm. This 0.7 mm roughness height is currently applied as the nominal, with the slight increase to 0.75 mm implied by the recent measurements being covered in the parametric uncertainty component of the margin.

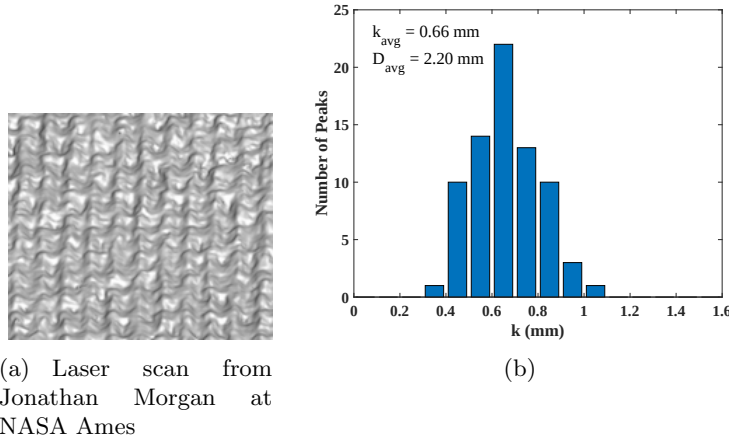


Figure 4: Example roughness pattern and peak to valley roughness heights for 3MDCP.

To account for the impact of this roughness on the convective heating and shear, a roughness augmentation multipliers are applied to the convective heating and shear as a post-processing step to the flowfield simulation. These roughness augmentation models are discussed and compared with measurements in Section 4. The model

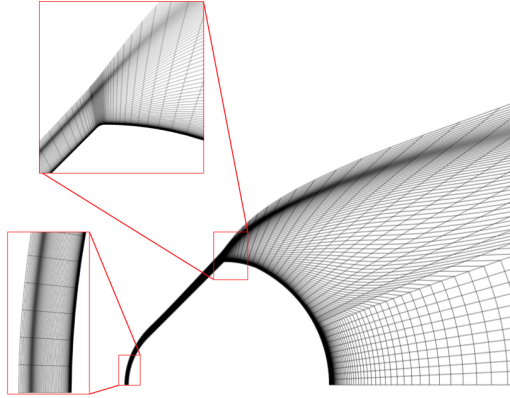


Figure 5: Axisymmetric grid overview.

referred to as the “Dahm” model, which is defined in Section 4.2, is applied as the nominal model in the database.

The HARA radiative heating is computed as a post-processing step to the LAURA flowfield simulation. This computation applies ray-tracing radiative transport [8], which rigorously accounts for three-dimensional radiative transport. The LAURA flowfields used for these radiation computations include radiation coupling [9], which captures the radiation cooling effect. This radiative cooling effect accounts for radiative energy loss from the shock layer, which reduces shock layer temperatures and the resulting radiative flux. In addition to coupled radiation, the radiative heating also includes coupled ablation. This coupled ablation impact, which is discussed in Section 5.4, was added after finding that ablation products contribute significantly to the flank and backshell radiative heating. Since no correction exists in the material response code to account for the impact of ablation on radiative heating, as it does for convective heating, the database includes the impact of coupled ablation on the baseline radiative heating.

Figure 5 presents the grid used for the database. The cone half angle for this geometry [10] is 45 degrees, the nose radius is 11 cm, the vehicle diameter is 43 cm, and the shoulder radius is 1.1 cm. The backshell was included in all simulations, however, this report focuses on the results of the heatshield only.

2.2 Aerothermal Margin Overview

While Section 6 presents the final aeroheating margins, the approach used to evaluate them is presented in this section. This is done because the model form (or experimental) components of the margin are evaluated in the sections leading up to Section 6, as the relevant experimental measurements and comparisons are presented. The margin approach adopted for RLVP is based on the approach discussed by West and Bynum [11], which is illustrated in Fig. 6 for a general case. The “Model Input Uncertainty” component will be referred to in this work as the parametric uncertainty component, the verification component will be referred to as the code-to-code comparison component, and the validation component will be referred to as the model form uncertainty component. These components are summed to

obtain the final margin. The parametric uncertainty component is simplified for RLVP aerothermal environments because all of the input uncertainties are treated as intervals, which is due to their epistemic nature and the insufficient data available to assess a distribution. These interval uncertainties result in upper and lower parametric uncertainty bounds, instead of the probability density shown in Fig. 6. For RLVP, Fig. 6 simplifies to Fig. 7, which shows that the total margin is the sum of the parametric and model form components, with the code-to-code component being negligible (and being mostly captured in the parametric component). This figure shows that both of these components are allowed to vary along the heatshield surface, and the parametric component varies with free-stream conditions. These components are defined in the following subsections, and then evaluated later in Section 6.

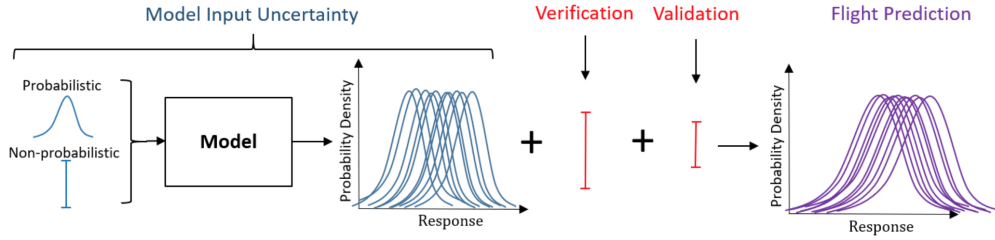


Figure 6: General approach for margin evaluation proposed by West and Bynum [11].

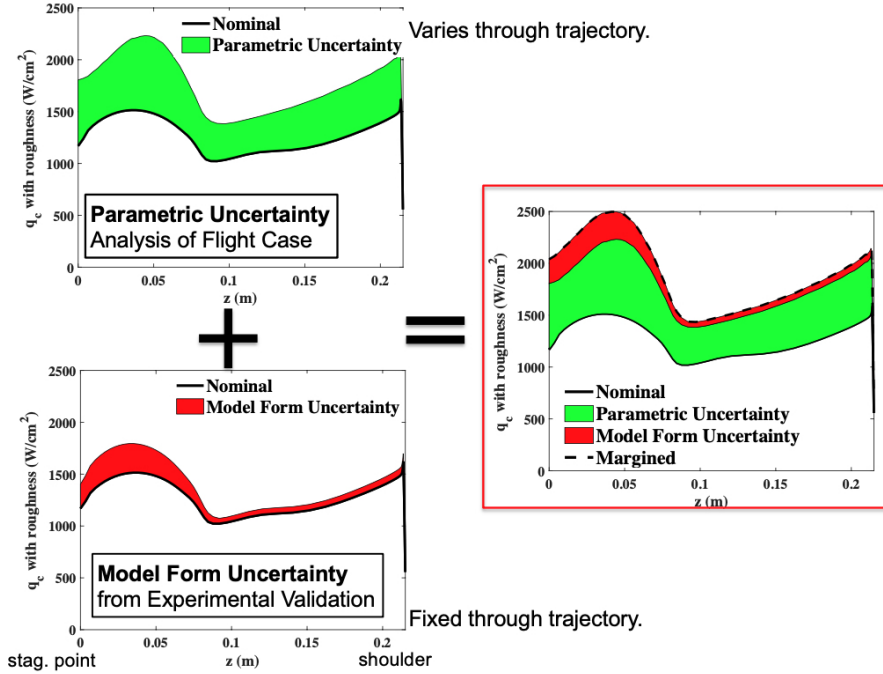


Figure 7: Example of the RLVP margin buildup resulting from the parametric and model form components.

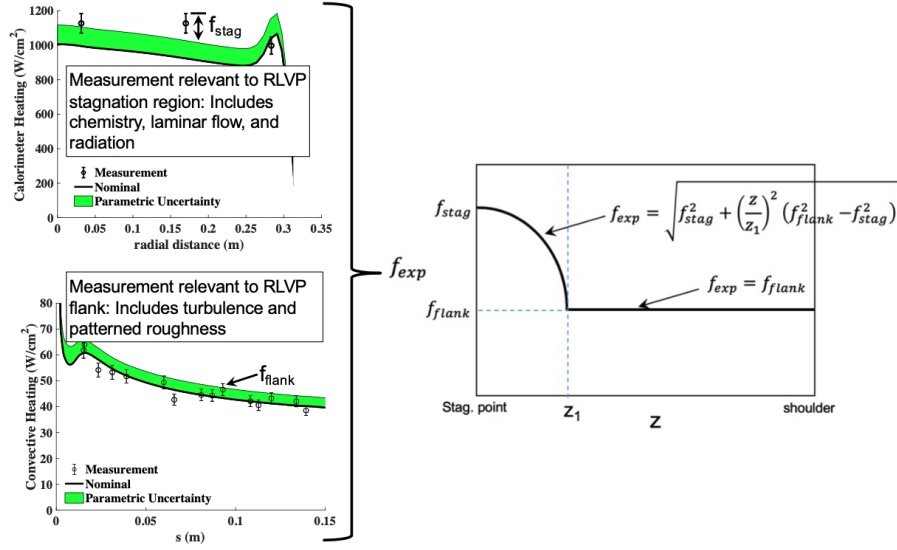


Figure 8: Approach for evaluating the model form uncertainty component.

2.2.1 Model Form Uncertainty Component

The model form uncertainty component is also referred to as the experimental validation component because it is assessed through comparisons with experimental measurements. Each measurement considered throughout this report will be simulated with the same approach used for the RLVP flight case simulations. Furthermore, the same parametric uncertainty approach, discussed in the next subsection, is applied to these measurement cases. As discussed by West and Bynum [11], the model form uncertainty is the difference between the upper limit of the measurement error bars and the upper parametric uncertainty limit of the simulation. By using the upper parametric uncertainty limit of the simulation in this evaluation, the model form uncertainty, meaning that from inadequacies in the governing equations or model formulation, is isolated from the parametric uncertainty. Figure 8 shows two examples of this process, one for a stagnation-relevant measurement and one for a flank-relevant measurement. Note that only the upper limit uncertainties are considered. Because different physics dominate the heating for these different surface regions, a model form uncertainty is evaluated for the stagnation region as f_{stag} and the flank region as f_{flank} , and then the equation shown in Fig. 8 provides the distribution along the RLVP forebody surface. The final values for f_{stag} and f_{flank} for both the convective and radiative heating are presented in Section 6, which will summarize the values presented throughout this report for the various measurement cases.

2.2.2 Parametric Uncertainty Component

The parametric uncertainty component captures the uncertainty in the convective or radiative heating due to uncertainties in the modeling parameters. As mentioned previously, all of the input uncertainties are treated as intervals due to their

epistemic nature and the insufficient data available to assess a distribution. These interval uncertainties result in upper and lower parametric uncertainty bounds for the resulting heating instead of a distribution. The parametric uncertainties for both the RLVP flight and measurement cases are evaluated using the approach developed by West [12] using polynomial chaos and least angle regression, which enables an accurate parametric uncertainty result using less than 100 samples, even for cases with over 300 parameters. This approach is implemented in the LAURA_UQ code, which allows automatic parametric analyses for LAURA simulations. This capability enabled the margin approach adopted for RLVP, both because it allows the parametric uncertainty to be evaluated for the RLVP flight cases and because it allows the parametric uncertainty to be evaluated for the measurement cases, which is required to evaluate the model form uncertainty. Without the convenient and efficient LAURA_UQ capability, these simulations would not be possible.

Details of the parametric uncertainty algorithm implemented in LAURA_UQ are presented by West [12]. The uncertainty ranges for the parameters relevant to RLVP convective and radiative heating, as well as the measurement cases, were reevaluated for RLVP and are reviewed in the following paragraphs.

Turbulence Modeling Parameters For the reacting flow flight cases, the largest contributor to the parametric uncertainty is the turbulent Schmidt number (Sc_T), which relates the species diffusion due to turbulence to the eddy viscosity. The exact value of Sc_T , especially for high enthalpy flows, is uncertain due to the difficulty in measuring species diffusion due to turbulence at these conditions. Most ground test measurements of turbulent convective heating are made for flows where the impact of chemistry on the convective heating is negligible. At these ground test conditions, which are typically below Mach 10, the dominant parametric uncertainty is the turbulent Prandtl number (Pr_T), which relates the thermal conductivity due to turbulence to the eddy viscosity, and therefore impacts the turbulent heating for cases with negligible chemistry.

The nominal values for both Sc_T and Pr_T are 0.9. To assess the uncertainty range for these values, the relevant insight is provided by recent direct numerical simulations (DNS). Duan and Martin [13] perform DNS simulations for the reacting hypersonic flow on a 35 degree wedge with a free-stream Mach number of 21. This results in a boundary layer edge Mach number of 3.4, which is similar to RLVP. From Duan and Martin’s DNS simulation, the Sc_T and Pr_T values are inferred from the time-averaged species and temperature profiles. The resulting Sc_T and Pr_T curves show values above 0.7 throughout most of the boundary layer (note that the lower limit for Sc_T and Pr_T govern the upper limit of the convective heating). Values below 0.7 are seen for regions where the Reynolds flux goes to zero, which leads to singularities in Sc_T and Pr_T that are unphysical. Similar results are presented by Di Renzo and Urzay [14] for both Sc_T and Pr_T and Cogo et al. [15] for Pr_T . These studies show that Sc_T and Pr_T vary between 0.7 and 1.0, except for regions where the species mass fractions become less than 0.01 or for y^+ values less than 10. These outlier cases should have a negligible impact on the resulting convective heating, due to the low mass fractions or the small impact of turbulence in the region

Table 2: Coupled ablation uncertainties.

Parameter	Uncertainty	Reference
$O + C(s) \rightarrow CO$ Oxidation Rate	[0.5, 10.0]	[17]
$O_2 + 2C(s) \rightarrow 2CO$ Oxidation Rate	[0.5, 1.5]	[17]
C Sublimation Rate	[0.1, 10.0]	[18]
C2 Sublimation Rate	[0.1, 10.0]	[18]
C3 Sublimation Rate	[0.1, 10.0]	[18]
$N + C(s) \rightarrow CN$ Nitridation Rate	[0.2, 5.0]	[19]
$N + N(s) \rightarrow N_2$ Recombination Rate	[0.1, 10.0]	[19]
$H + H(s) \rightarrow H_2$ Recombination Rate	[0.1, 10.0]	[20, 21]

below $y^+ = 10$, which is the laminar sublayer and therefore the eddy viscosity and influence of Sc_T or Pr_T is small. Another insight into the Sc_T range is provided by recent work by Erb et al. [41] on Galileo probe forebody recession reconstruction, which suggested that the lower bound on the turbulent Schmidt number should be 0.8. However, the massive blowing by the Galileo probe TPS makes it unique and may not be representative of the Venus entry environments considered in this study. While the current baseline solution employs values of Sc_T and $Pr_T = 0.9$, the full range of $0.7 \leq Sc_T \leq 1.0$ is considered in this study for both parameters.

Another parametric uncertainty relevant to the turbulence model is the roughness height applied to the Krogstad roughness augmentation model. This model is applied for the margin assessment, even though the aerothermal database applies the Dahm roughness augmentation model [16] as a post-processing step, because the Krogstad model can be used in coupled ablation simulations (these roughness augmentation models are discussed in Section 4.2). The term R^+/k is the primary modeling parameter in the Krogstad model, where R^+ is a damping parameter and k is the peak-to-valley roughness height. An uncertainty of $\pm 30\%$ is placed on the R^+/k term, which may be interpreted as either an uncertainty in the roughness height or in the damping parameter. In terms of the roughness height, the $\pm 30\%$ value is chosen based on the possible variation in the mean roughness height of post arc-jet samples, such as that presented in Fig. 4(a).

Finite-Rate Surface and Char Rate Model A nonequilibrium char ablation model is implemented for the RLVP flight cases, and therefore uncertainties associated with the surface chemistry model are considered. These uncertainties are given in Table 2, along with references for each uncertainty bound. Finite rate surface model uncertainty sources considered included surface oxidation rates, carbon sublimation rates, the surface nitridation rate, and the N_2 H_2 catalytic recombination rates. Note that C and O recombination processes are negligible because all available elemental C and O near the wall forms CO or CO_2 , leaving essentially no free atomic C and O. Further details on the finite-rate surface model are presented in Section 5.3.

Collision Cross Sections Transport properties (diffusion, thermal conductivity, and viscosity) were approximated using binary collision integrals given by Gupta et al. [22]. Available higher order curve fits provided by Wright et al. [23, 24], who also estimated collision pair specific uncertainties, were used when possible. Uncertainty

for all collision pairs not identified by Wright et al. was assumed to be $\pm 30\%$. In total, 210 collision pair uncertainties were considered.

Uncertainty in Radiation Properties The molecular band systems treated in the present study, excluding those present in high temperature air, are listed in Table 3. The oscillator strengths presented by Babou et al. [25] are applied for all C_2 and CN band systems, along with the CO 4th Positive and CO Infrared systems. The oscillator strengths presented by da Silva and Dudeck [26] are applied for the remaining CO band systems.

The variation in the oscillator strengths proposed by various researchers [25–27], and the inconclusive validation of these values for $\Delta v < -4$ leads to the choice of a $\pm 40\%$ uncertainty for the oscillator strengths of the CO 4th Positive band system.

For the CO_2 Infrared (IR) bands, which provide the dominant contribution to the afterbody radiation, the band strength uncertainty was assessed through comparisons with EAST measurements. These comparisons were made for cases where the chemistry was frozen, and the two temperatures were strongly equilibrated. These constraints allowed the remaining discrepancy to be attributed to the oscillator strengths of the rotational lines, which are taken from the CDSD database. This assessment is documented in the Mars 2020 aerothermal document.

Other than CO 4th Positive and CO_2 IR, the rest of the band systems for a Venus entry shock layer are essentially optically thin on the forebody, with CN Violet being only slightly non-optically-thin. This means the vibrational bands that contribute the most to the radiative flux, and are therefore of most interest in the uncertainty assessment, are those with $\Delta v = 0$. The oscillator strength uncertainties chosen for these band system are listed in Table 3. These values were obtained by comparing the values presented by the cited sources. The spectroscopic constants for the energy levels of molecules listed in this table were taken from Babou et al. [28]. The oscillator strengths and spectroscopic constant for air band systems applied in this study are presented by Johnston [3].

The non-Boltzmann rate model applied in this work is an updated version of the heavy-particle and electron-impact excitation rate models compiled by Johnston et al. [31], which was an extension of the model presented by Park [32,33]. These rates model the electronic levels listed in Table 4, which provide the upper and lower electronic number densities required for the absorption and emission coefficients for the processes listed in Table 3. The updates to the rates presented by Johnston et al. [31] are presented in Table 5. The model for the upper and lower levels of the CO 4th-Positive band were updated by Johnston and Brandis [5], while those for CN were updated by Johnston et al. [34]. The remaining non-Boltzmann rates are the same as presented by Johnston et al. [31].

The rates are computed from the coefficients presented in Table 5 using the following equation:

$$K_{f,ij}^{ex} = A_{ex} T_a^{n_{ex}} \exp(-E_{ex}/T_a) \quad (1)$$

where $T_a = (T_{ve} T_{tr})^{1/2}$ for the heavy particle impact processes and $T_a = T_{ve}$ for the electron impact processes. The uncertainties for the rates listed in Table 5 are based on the discussions in Johnston et al. [31,34]. For the rates not listed here, a

Table 3: Molecular band processes applied in the present study, excluding those present in high temperature air.

Molecule	Upper State – Lower State	Band Name	λ Range (nm)	+/- Uncertainty
CO	$A^1\Pi - X^1\Sigma^+$	4th Positive	120 – 280	40% [25–27]
CO	$b^3\Sigma^+ - a^3\Pi_r$	3rd Positive	250 – 450	50% [25, 26]
CO	$d^3\Delta_g - a^3\Pi_r$	Triplet	320 – 2500	50% [26]
CO	$a'^3\Sigma^+ - a^3\Pi_r$	Asundi	370 – 2500	50% [26]
CO	$B^1\Sigma^+ - A^1\Pi$	Angstrom	400 – 700	50% [26]
CO	$X^1\Sigma^+ - X^1\Sigma^+$	Infrared	1200 – 7000	50% [25, 26]
CN	$A^2\Pi_g - X^2\Sigma^+$	Red	400 – 2800	30% [25–27]
CN	$B^2\Sigma^+ - X^2\Sigma^+$	Violet	300 – 550	15% [25–27]
C ₂	$d^3\Pi_g - a^3\Pi_u$	Swan	390 – 1000	50% [25–27]
C ₂	$b^3\Sigma_g^- - a^3\Pi_u$	Ballik-Ramsay	500 – 3000	50% [25, 26, 29, 30]
C ₂	$A^1\Pi_u - X^1\Sigma_g^+$	Phillips	350 – 1200	50% [25, 26]
C ₂	$D^1\Sigma_u^+ - X^1\Sigma_g^+$	Mulliken	200 – 250	50% [25, 26]
C ₂	$C^1\Pi_g - A^1\Pi_u$	Des.-D'Azam.	280 – 700	50% [25, 26]
C ₂	$e^3\Pi_g - a^3\Pi_u$	Fox-Herzberg	200 – 500	50% [25, 26]
CO ₂	$X^1\Sigma_g^+ - X^1\Sigma_g^+$	2700 nm band	2600 – 3300	70%
CO ₂	$X^1\Sigma_g^+ - X^1\Sigma_g^+$	4500 nm band	4000 – 5000	20%

$\pm$ one-half order of magnitude uncertainty is applied, which represent multipliers of 0.31 and 3.1.

An important process in the non-Boltzmann computation is the radiative transition rate, which tends to depopulate the upper-levels of molecular band systems. The radiative transition rates for this process was computed from the radiative properties used for the spectral modeling. These rates vary along a line of sight because they are dependent on the temperatures, which assume Boltzmann distributions of the vibrational and rotational levels.

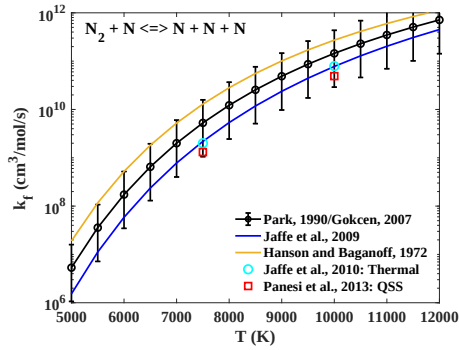
Chemical Kinetics The nominal flowfield chemical kinetic rates and their evaluated uncertainties are listed in Tables 6–8. The uncertainties (U_i) are presented in terms of multipliers to be applied to the nominal rate to provide the lower and upper uncertainty bounds. For processes that are not likely to impact the convective or radiative heating noticeably, a default uncertainty of $\pm$ one-half order of magnitude is applied, which results in lower and upper multipliers of 0.32 and 3.2, respectively. For processes that are identified to impact the convective or radiative heating significantly, uncertainty bounds were chosen based on a review of values proposed in the literature. Examples of this are shown in Figs. 9–15. Consider Fig. 9, which presents proposed rates for the important N₂ dissociation rate [57, 80–83]. For the M=N rate in Fig. 9(a), the lower uncertainty bound of -80% is similar to that listed by Gökçen. As shown in the figure, this lower limit is chosen to capture the rates computed by Panesi et al. [83], which represent the highest fidelity source for this rate. Similarly for the M=N₂ rate in Fig. 9(b), the -50% lower limit is chosen to capture the recent rates from Macdonald et al. [84] and others [57, 85–87]. For the N₂ dissociation rate with M=H, Table 6 lists uncertainties of -90,+700%, which were chosen following Bose et al. [88] due to lack of any experimental or computational data.

Table 4: Electronic levels treated in the present non-Boltzmann model.

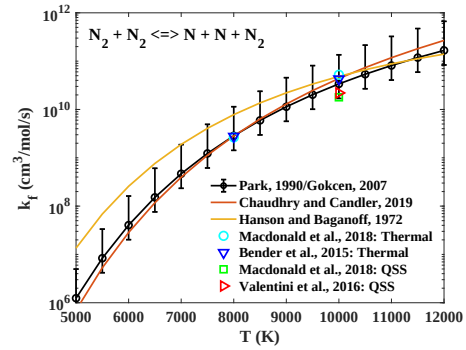
i	Term	$E_{e,i}$	$g_{e,i}$
CN			
1	$X^2\Sigma^+$	0	2
2	$A^2\Pi$	9245	4
3	$B^2\Sigma^+$	25753	2
4	$a^4\Sigma^+$	32400	4
5	$D^2\Pi^+$	54486	4
CO			
1	$X^1\Sigma^+$	0	1
2	$a^3\Pi$	48687	6
3	$a'^3\Sigma^+$	55836	3
4	$d^3\Delta$	61120	6
5	$e^3\Sigma^-$ (group)	64230	6
6	$A^1\Pi$	65076	2
C_2			
1	$X^1\Sigma^+$ (group)	613.9	7
2	$b^3\Sigma^-$ (group)	7217	5
3	$c^3\Sigma^+$ (group)	11157.7	6
4	$d^3\Pi$	20022.5	6
5	$C^1\Pi$ (group)	31249.6	26

Table 5: Non-Boltzmann rates (cm^3/s) applied in the present study.

#	Reaction	A_{ex}	n_{ex}	E_{ex}	Source	Uncertainty
1	$\text{CO}(X^1\Sigma^+) + M \leftrightarrow \text{CO}(A^1\Pi) + M$	1.86e-10	0.344	93669	Schofield [35]	0.3, 3
2	$\text{CO}(X^1\Sigma^+) + e^- \leftrightarrow \text{CO}(A^1\Pi) + e^-$	3.82e-09	0.12	95850	Park [32]	0.3, 3
3	$\text{CN}(X^2\Sigma^+) + M \leftrightarrow \text{CN}(A^2\Pi) + M$	1.29e-13	0.50	13300	Based on Tereshchenko and Dodonova [36] ccs Based on Tereshchenko et al. [37] ccs	0.3, 2
4	$\text{CN}(X^2\Sigma^+) + M \leftrightarrow \text{CN}(B^2\Sigma^+) + M$	1.03e-12	0.50	37000		0.1, 2
5	$\text{CN}(X^2\Sigma^+) + e^- \leftrightarrow \text{CN}(A^2\Pi) + e^-$	6.41e-09	0.20	18303	Based on Harrison [38] ccs	0.3, 3
6	$\text{CN}(X^2\Sigma^+) + e^- \leftrightarrow \text{CN}(B^2\Sigma^+) + e^-$	6.83e-10	0.39	40428	Based on Harrison [38] ccs	0.3, 4



(a)



(b)

Figure 9: Summary of proposed N_2 dissociation rates.

Table 6: Summary of flow field dissociation reactions and their rate data.

Reaction	$A_{f,i} \left(\frac{\text{cm}^3}{\text{mol-s}} \right)$	$n_{f,i}$	$D_{f,i}(K)$	$T_{f,i}$	Third Body, M	U_i	Reference
$\text{C}_2 + \text{M} \leftrightarrow 2\text{C} + \text{M}$	4.50e+18	-1.00	7.15e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	All	0.32, 3.2	
$\text{C}_2\text{H} + \text{M} \leftrightarrow \text{C}_2 + \text{H} + \text{M}$	1.70e+35	-5.16	5.74e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	All	0.32, 3.2	
$\text{C}_2\text{H}_2 + \text{M} \leftrightarrow \text{C}_2\text{H} + \text{H} + \text{M}$	4.00e+16	0.00	5.40e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	All	0.32, 3.2	
$\text{C}_3 + \text{M} \leftrightarrow \text{C}_2 + \text{C} + \text{M}$	1.68e+21	-1.50	8.77e+04	$T_{tr}^{0.7} T_{ve}^{0.3}$	H, C, N, O	0.32, 3.2	
	8.40e+20	-1.50	8.77e+04	$T_{tr}^{0.7} T_{ve}^{0.3}$	others	0.32, 3.2	
$\text{CH} + \text{M} \leftrightarrow \text{C} + \text{H} + \text{M}$	1.90e+14	0.00	3.37e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	All	0.5, 2.0	[39]
$\text{CH}_2 + \text{M} \leftrightarrow \text{CH} + \text{H} + \text{M}$	4.00E+15	0.00	4.18e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	All	0.32, 3.2	
$\text{CH}_2 + \text{M} \leftrightarrow \text{C} + \text{H}_2 + \text{M}$	1.30E+14	0.00	2.97e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	All	0.32, 3.2	
$\text{CH}_3 + \text{M} \leftrightarrow \text{CH} + \text{H}_2 + \text{M}$	5.00E+15	0.00	4.28e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	All	0.32, 3.2	
$\text{CH}_3 + \text{M} \leftrightarrow \text{CH}_2 + \text{H} + \text{M}$	1.02E+16	0.00	4.56e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	All	0.32, 3.2	
$\text{CH}_4 + \text{M} \leftrightarrow \text{CH}_3 + \text{H} + \text{M}$	2.25E+27	-1.87	5.29e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	All	0.32, 3.2	
$\text{CN} + \text{M} \leftrightarrow \text{C} + \text{N} + \text{M}$	6.00e+15	-0.40	7.10e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	All	0.3, 1.9	[34], Fig. 15(a)
$\text{CO} + \text{M} \leftrightarrow \text{C} + \text{O} + \text{M}$	2.70e+21	-1.00	1.29e+05	$T_{tr}^{0.5} T_{ve}^{0.5}$	H, C, N, O	0.2, 3.5	
	1.20e+21	-1.00	1.29e+05	$T_{tr}^{0.5} T_{ve}^{0.5}$	others	0.2, 3.5	[40–43], Fig. 10
$\text{CO}_2 + \text{M} \leftrightarrow \text{CO} + \text{O} + \text{M}$	1.38e+22	-1.50	6.33e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	H, C, N, O	0.3, 1.2	
	6.90e+21	-1.50	6.33e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	others	0.3, 1.2	[44–48], Fig. 10
$\text{H}_2 + \text{M} \leftrightarrow 2\text{H} + \text{M}$	8.50e+19	-1.10	5.23e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	H	0.25, 1.5	
	9.00e+14	0.00	4.84e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	others	0.25, 1.5	
$\text{HCN} + \text{M} \leftrightarrow \text{CN} + \text{H} + \text{M}$	3.57e+26	-2.60	6.28e+04	$T_{tr}^{0.7} T_{ve}^{0.3}$	All	0.15, 3.0	[34], Fig. 15(b)
$\text{N}_2 + \text{M} \leftrightarrow 2\text{N} + \text{M}$	3.00e+22	-1.60	1.13e+05	$T_{tr}^{0.7} T_{ve}^{0.3}$	C, N, O	0.2, 3.0	[34, 49–52], Fig. 9
	3.00e+22	-1.60	1.13e+05	$T_{tr}^{0.7} T_{ve}^{0.3}$	H	0.1, 8.0	[34]
	7.00e+21	-1.60	1.13e+05	$T_{tr}^{0.7} T_{ve}^{0.3}$	others	0.5, 4.0	[34, 49–51], Fig. 9
$\text{NO} + \text{M} \leftrightarrow \text{N} + \text{O} + \text{M}$	4.40e+16	0.00	7.55e+04	$T_{tr}^{0.7} T_{ve}^{0.3}$	H, C, N, O, NO, CO2	0.2, 2.5	
	2.00e+15	0.00	7.55e+04	$T_{tr}^{0.7} T_{ve}^{0.3}$	others	0.2, 2.5	[5, 53–56], Fig. 13
$\text{O}_2 + \text{M} \leftrightarrow 2\text{O} + \text{M}$	1.00e+22	-1.50	5.94e+04	$T_{tr}^{0.7} T_{ve}^{0.3}$	H, C, O, N	0.3, 1.3	
	2.00e+21	-1.50	5.94e+04	$T_{tr}^{0.7} T_{ve}^{0.3}$	others	0.3, 1.3	[50, 57–59], Fig. 1

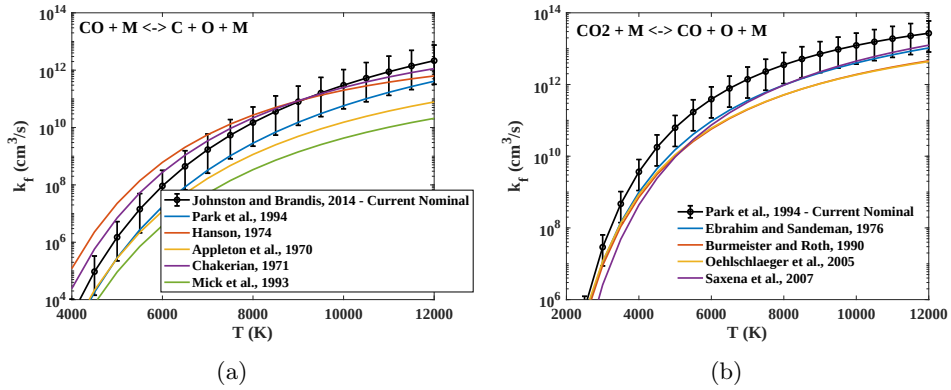
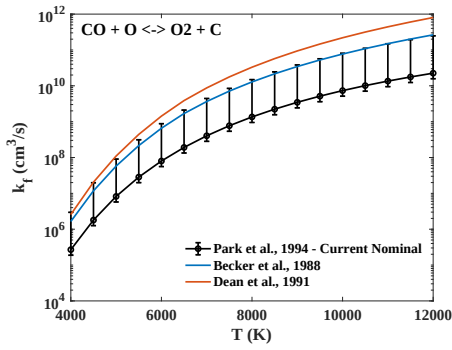


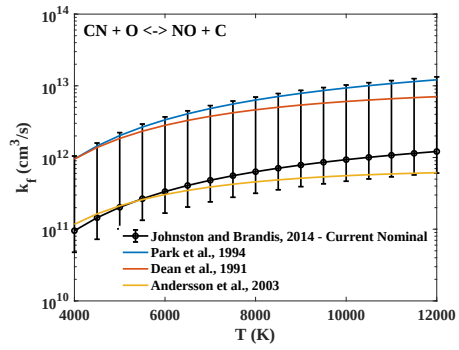
Figure 10: Summary of proposed CO_2 and CO dissociation rates.

Table 7: Summary of flow field exchange reactions and their rate data.

Reaction	$A_{f,i} \left(\frac{\text{cm}^3}{\text{mol-s}} \right)$	$n_{f,i}$	$D_{f,i}(K)$	$T_{f,i}$	U_i	Reference
$\text{C}_2 + \text{C}_2\text{H}_2 \leftrightarrow 2\text{C}_2\text{H}$	1.10e+14	-0.38	6.87e+03	T_{tr}	0.32, 3.2	[34]
$\text{C}_2 + \text{N}_2 \leftrightarrow \text{CN} + \text{CN}$	1.50e+13	0.00	2.10e+04	T_{tr}	0.5, 150.0	
$\text{C}_2\text{H} + \text{H} \leftrightarrow \text{C}_2 + \text{H}_2$	1.60e+13	0.15	1.46e+04	T_{tr}	0.32, 3.2	
$\text{C}_3 + \text{C} \leftrightarrow \text{C}_2 + \text{C}_2$	6.00e+11	1.07	1.65e+04	T_{tr}	0.32, 3.2	
$\text{C}_3 + \text{N} \leftrightarrow \text{CN} + \text{C}_2$	1.00e+12	0.00	3.42e+04	T_{tr}	0.32, 3.2	
$\text{CH} + \text{C} \leftrightarrow \text{C}_2 + \text{H}$	2.00e+14	0.00	0.00e+04	T_{tr}	0.32, 3.2	
$\text{CH} + \text{CO} \leftrightarrow \text{C}_2\text{H} + \text{O}$	2.50e+10	0.67	3.90e+04	T_{tr}	0.32, 3.2	
$\text{CH} + \text{N}_2 \leftrightarrow \text{HCN} + \text{N}$	4.40e+12	0.0	1.11e+04	T_{tr}	0.32, 3.2	
$\text{CH}_2 + \text{H} \leftrightarrow \text{CH} + \text{H}_2$	6.03e+12	0.00	-9.00e+02	$T_{tr}^{0.5} T_{ve}^{0.5}$	0.32, 3.2	
$\text{CH}_2 + \text{N} \leftrightarrow \text{HCN} + \text{H}$	5.00e+13	0.00	0.00e+00	$T_{tr}^{0.5} T_{ve}^{0.5}$	0.32, 3.2	
$\text{CH}_3 + \text{H} \leftrightarrow \text{CH}_2 + \text{H}_2$	6.03e+12	0.00	7.60e+03	$T_{tr}^{0.5} T_{ve}^{0.5}$	0.32, 3.2	[34]
$\text{CH}_3 + \text{N} \leftrightarrow \text{HCN} + 2\text{H}$	7.00e+13	0.00	0.00e+00	$T_{tr}^{0.5} T_{ve}^{0.5}$	0.32, 3.2	
$\text{CN} + \text{C} \leftrightarrow \text{C}_2 + \text{N}$	3.00e+14	0.00	1.81e+04	T_{tr}	0.5, 3.0	
$\text{CN} + \text{H}_2 \leftrightarrow \text{HCN} + \text{H}$	2.95e+05	0.00	1.13e+03	T_{tr}	0.5, 1.5	
$\text{CN} + \text{O} \leftrightarrow \text{NO} + \text{C}$	1.60e+12	0.10	1.46e+04	T_{tr}	0.50, 10.0	
$\text{CO} + \text{C} \leftrightarrow \text{C}_2 + \text{O}$	2.40e+17	-1.00	5.80e+04	T_{tr}	0.32, 3.2	
$\text{CO} + \text{C}_2 \leftrightarrow \text{C}_3 + \text{O}$	1.00e+12	0.00	4.12e+04	T_{tr}	0.32, 3.2	
$\text{CO} + \text{N} \leftrightarrow \text{CN} + \text{O}$	1.00e+14	0.00	3.86e+04	T_{tr}	0.32, 3.2	
$\text{CO} + \text{NO} \leftrightarrow \text{CO}_2 + \text{N}$	8.00e+06	0.88	1.33e+04	T_{tr}	0.32, 3.2	
$\text{CO} + \text{O} \leftrightarrow \text{O}_2 + \text{C}$	3.90e+13	-0.18	6.92e+04	T_{tr}	0.8, 10.0	[44, 62, 63], Fig. 11(a)
$\text{CO}_2 + \text{O} \leftrightarrow \text{O}_2 + \text{CO}$	2.71e+14	0.00	3.38e+04	T_{tr}	0.32, 3.2	
$\text{H} + \text{C}_2\text{H}_2 \leftrightarrow \text{C}_2\text{H} + \text{H}_2$	1.00e+16	-0.50	1.55e+04	T_{tr}	0.32, 3.2	
$\text{H} + \text{CH}_4 \leftrightarrow \text{CH}_3 + \text{H}_2$	1.32e+04	3.00	4.05e+03	T_{tr}	0.32, 3.2	
$\text{H} + \text{CN} \leftrightarrow \text{CH} + \text{N}$	1.50e+15	-0.12	4.978e+04	T_{tr}	0.32, 3.2	
$\text{H} + \text{CO} \leftrightarrow \text{CH} + \text{O}$	6.70e+14	0.15	8.85e+04	T_{tr}	0.32, 3.2	
$\text{H}_2 + \text{C} \leftrightarrow \text{CH} + \text{H}$	4.00e+14	0.00	1.17e+04	T_{tr}	0.32, 3.2	
$\text{H}_2 + \text{H} \leftrightarrow 2\text{H} + \text{H}$	8.50e+19	-1.10	5.23e+04	$T_{tr}^{0.5} T_{ve}^{0.5}$	0.32, 3.2	
$\text{N} + \text{CO} \leftrightarrow \text{NO} + \text{C}$	1.10e+14	0.07	5.35e+04	T_{tr}	0.32, 3.2	
$\text{N}_2 + \text{C} \leftrightarrow \text{CN} + \text{N}$	5.24e+13	0.0	2.26e+04	T_{tr}	0.8, 1.3	[44, 64, 65], Fig. 12(a)
$\text{N}_2 + \text{CO} \leftrightarrow \text{CN} + \text{NO}$	1.20e+16	-1.23	7.70e+04	T_{tr}	0.32, 3.2	
$\text{N}_2 + \text{O} \leftrightarrow \text{NO} + \text{N}$	6.00e+13	0.10	3.80e+04	T_{tr}	0.4, 1.1	
$\text{O}_2 + \text{N} \leftrightarrow \text{NO} + \text{O}$	2.49e+09	1.18	4.01e+03	T_{tr}	0.32, 3.2	



(a)

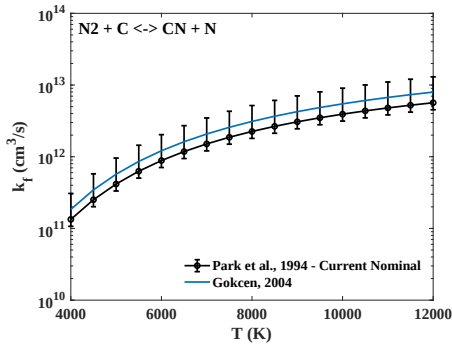


(b)

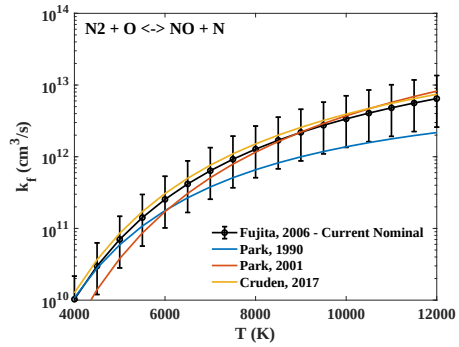
Figure 11: Summary of proposed $\text{CO} + \text{O} \rightleftharpoons \text{O}_2 + \text{C}$ and $\text{CN} + \text{O} \rightleftharpoons \text{NO} + \text{C}$ rates.

Table 8: Summary of flow field ionization reactions and their rate data.

Reaction	$A_{f,i} \left(\frac{\text{cm}^3}{\text{mol-s}} \right)$	$n_{f,i}$	$D_{f,i}(K)$	$T_{f,i}$	U_i	Reference
$\text{C} + \text{e}^- \leftrightarrow \text{C}^+ + \text{e}^- + \text{e}^-$	3.90e+33	-3.78	1.31e+05	T_{ve}	0.32, 3.2	
$\text{C} + \text{N} \leftrightarrow \text{CN}^+ + \text{e}^-$	1.00e+15	1.50	1.64e+05	T_{ve}	0.32, 3.2	
$\text{C} + \text{O} \leftrightarrow \text{CO}^+ + \text{e}^-$	8.80e+08	1.00	3.31e+04	T_{ve}	0.32, 3.2	
$\text{C}^+ + \text{CO} \leftrightarrow \text{CO}^+ + \text{C}$	1.00e+13	0.00	3.14e+04	T_{tr}	0.32, 3.2	
$\text{C}^+ + \text{N}_2 \leftrightarrow \text{N}_2^+ + \text{C}$	1.11e+14	-0.11	5.00e+04	T_{tr}	0.32, 3.2	
$\text{CN}^+ + \text{N} \leftrightarrow \text{CN} + \text{N}^+$	9.80e+12	0.00	4.07e+04	T_{el}	0.32, 3.2	
$\text{H} + \text{e}^- \leftrightarrow \text{H}^+ + \text{e}^- + \text{e}^-$	2.20e+30	-2.80	1.58e+05	T_{ve}	0.32, 3.2	
$\text{N} + \text{N} \leftrightarrow \text{N}_2^+ + \text{e}^-$	4.40e+07	1.50	6.75e+04	T_{ve}	0.3, 1.3	[49, 69–71]
$\text{N} + \text{O} \leftrightarrow \text{NO}^+ + \text{e}^-$	5.30e+12	0.00	3.19e+04	T_{ve}	0.32, 3.2	
$\text{N} + \text{e}^- \leftrightarrow \text{N}^+ + \text{e}^- + \text{e}^-$	2.50e+34	-3.82	1.68e+05	T_{ve}	0.5, 2.0	[49, 72–74], Fig. 14(a)
$\text{N}^+ + \text{N}_2 \leftrightarrow \text{N}_2^+ + \text{N}$	1.00e+12	0.50	1.22e+04	T_{ve}	0.5, 1.7	[70, 71, 75–78]
$\text{N}_2 + \text{O}^+ \leftrightarrow \text{N}_2^+ + \text{O}$	9.10e+11	0.36	2.28e+04	T_{tr}	0.32, 3.2	
$\text{N}_2 + \text{e}^- \leftrightarrow 2\text{N} + \text{e}^-$	6.00e+3	2.60	1.13e+05	T_{ve}	0.32, 3.2	[49, 76, 78]
$\text{NO} + \text{O}^+ \leftrightarrow \text{N}^+ + \text{O}_2$	1.40e+05	1.90	2.66e+04	T_{tr}	0.32, 3.2	
$\text{NO}^+ + \text{C} \leftrightarrow \text{C}^+ + \text{NO}$	1.0e+13	0.00	2.32e+04	T_{tr}	0.32, 3.2	
$\text{NO}^+ + \text{N} \leftrightarrow \text{N}_2^+ + \text{O}$	7.20e+13	0.00	3.55e+04	T_{tr}	0.32, 3.2	
$\text{NO}^+ + \text{N} \leftrightarrow \text{O}^+ + \text{N}_2$	3.40e+13	-1.08	1.28e+04	T_{tr}	0.32, 3.2	
$\text{NO}^+ + \text{O} \leftrightarrow \text{N}^+ + \text{O}_2$	1.00e+12	0.50	7.72e+04	T_{tr}	0.32, 3.2	
$\text{NO}^+ + \text{O} \leftrightarrow \text{O}_2^+ + \text{N}$	7.20e+12	0.29	4.86e+04	T_{tr}	0.32, 3.2	
$\text{NO}^+ + \text{O}_2 \leftrightarrow \text{NO} + \text{O}_2^+$	2.40e+13	0.41	3.26e+04	T_{tr}	0.32, 3.2	
$\text{O} + \text{O} \leftrightarrow \text{O}_2^+ + \text{e}^-$	7.10e+02	2.70	8.06e+04	T_{ve}	0.32, 3.2	
$\text{O} + \text{e}^- \leftrightarrow \text{O}^+ + \text{e}^- + \text{e}^-$	3.90e+33	-3.78	1.59e+05	T_{ve}	0.4, 3.0	[49, 72–74, 79], Fig. 14(b)
$\text{O}_2 + \text{C} \leftrightarrow \text{O}_2^+ + \text{C}$	1.00e+13	0.00	9.40e+03	T_{tr}	0.32, 3.2	
$\text{O}_2^+ + \text{N} \leftrightarrow \text{O}_2 + \text{N}^+$	8.700e+13	0.14	2.86e+04	T_{tr}	0.32, 3.2	
$\text{O}_2^+ + \text{N}_2 \leftrightarrow \text{N}_2^+ + \text{O}_2$	9.90e+12	0.00	4.07e+04	T_{tr}	0.32, 3.2	
$\text{O}_2^+ + \text{O} \leftrightarrow \text{O}^+ + \text{O}_2$	4.00e+12	-0.09	1.80e+04	T_{tr}	0.32, 3.2	



(a)



(b)

Figure 12: Summary of proposed $\text{N}_2 + \text{C} \rightleftharpoons \text{CN} + \text{N}$ and $\text{N}_2 + \text{O} \rightleftharpoons \text{NO} + \text{N}$ rates.

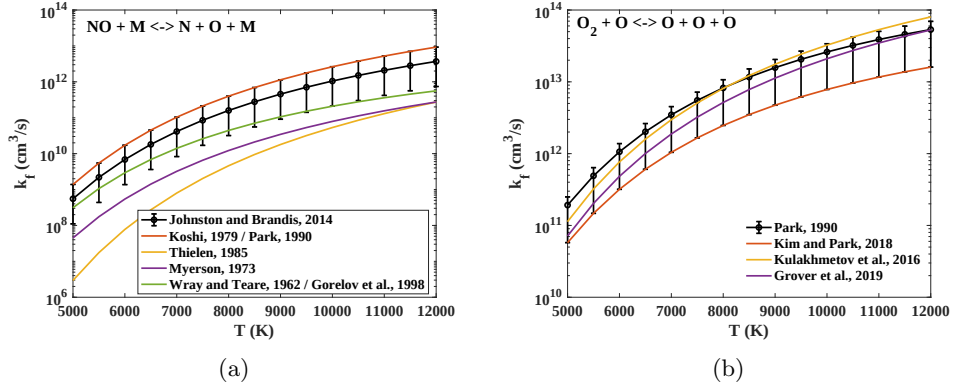


Figure 13: Summary of proposed NO and O₂ dissociation rates.

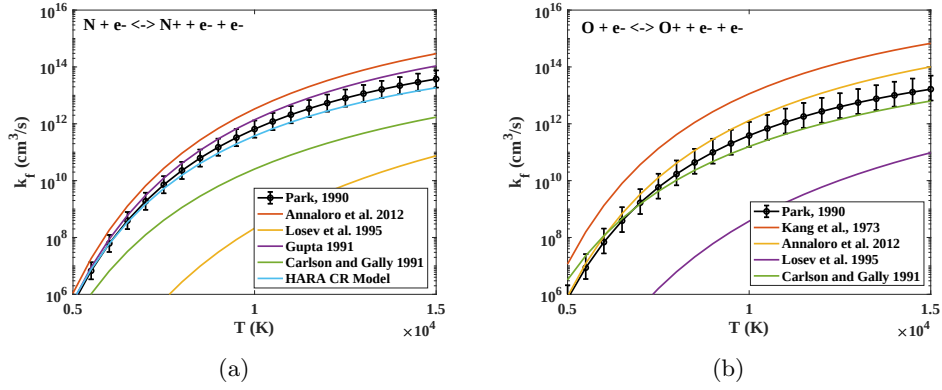


Figure 14: Summary of proposed electron impact ionization rates.

The CO dissociation rate has a significant impact on the radiative heating. Various proposed rates for this process are compared in Fig. 10(a). The error bars chosen for the nominal rate are seen to capture 3 out of the 4 other proposed rates. The rates not captured are considered anomalously low. A smaller range of proposed rates are presented in Fig. 10(b) for the CO₂ dissociation rate.

For the CN and HCN dissociation rates, Fig. 15 compares the nominal rate with other proposed rates [89–92]. Due to the limited information available for these processes, the uncertainty are chosen to capture most of the range implied by the various proposed rates.

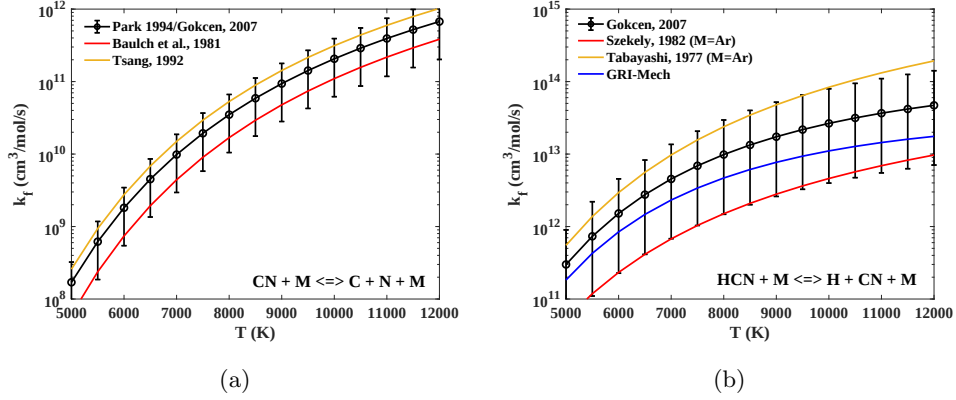


Figure 15: Summary of proposed dissociation rates for CN and HCN.

3 Laminar/Stagnation Heating Environment

The convective and radiative heating discussed in this section are for non-ablating simulations. The final radiative heating recommended for RLVP design includes the impact of coupled ablation, which is presented later in Section 5.4.

3.1 RLVP Laminar Heating

While the recommended RLVP heating values assume fully-turbulent flow, the stagnation region environment remains laminar. Figure 16 presents the laminar heating values at the stagnation point and near shoulder location for the “a” trajectory listed in Table 1, where the near shoulder values are only shown for reference. In addition to not including turbulence or roughness effects, this laminar stagnation region environment is unique from the downstream flow because radiative heating is noticeable relative to the convective heating (once the turbulence and roughness effects are included in the flank heating as discussed in Section 4, the radiative heating component is less than 4% of the convective).

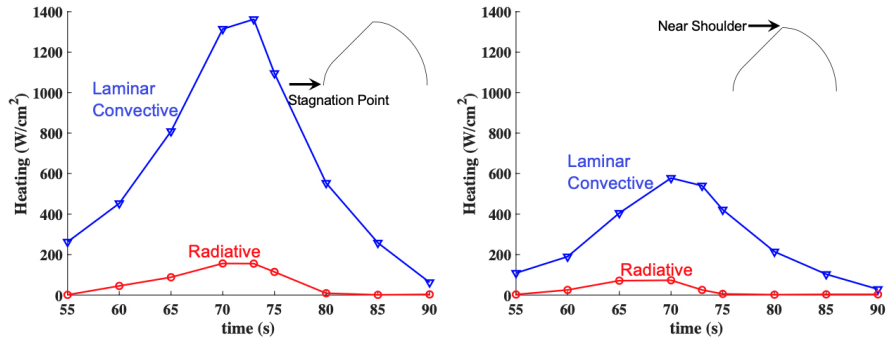


Figure 16: Laminar forebody heating environment.

Further insight into the stagnation region heating is gained by breaking the

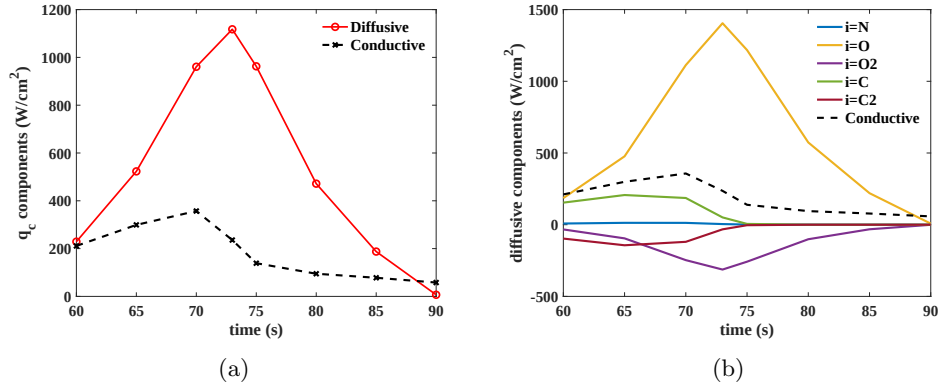


Figure 17: Breakdown of stagnation-point convective heating into diffusive and conductive components.

convective heating into the conductive and diffusive components:

$$q_c = q_{c,cond} + q_{c,diff} \quad (2)$$

where the conductive component is written as

$$q_{c,cond} = k_{ve} \frac{dT_{ve}}{dz} + k_{tr} \frac{dT_{tr}}{dz} \quad (3)$$

and the diffusive component as:

$$q_{c,diff} = \rho_w \sum_i^{N_{species}} J_i h_{w,i} \quad (4)$$

where

$$J_i = D_i \frac{\partial \chi_i}{\partial n} - c_i \sum_j^{N_{species}} D_j \frac{\partial \chi_j}{\partial n} \quad (5)$$

These components are presented in Fig. 17(a) for the RLVP stagnation point along the nominal trajectory. This figure shows that the diffusive component provides the dominant contribution to the stagnation point convective heating. This means that the gradients in the species mole fractions or number densities at the wall are the primary driver to the stagnation-point convective heating. These gradients are driven by wall catalycity, so this diffusive heating component could also be considered a catalytic heating component. Figure 17(b) presents the diffusive heating component from individual species. The dominant contributor is from atomic oxygen, which is a result of its catalytic recombination to O₂. Note that only homogenous recombination is considered in these simulations, so the possibility of CO₂ recombination from CO and O is not considered. Section 5.3 will show the impact of ablation on these convective heating components, where the impact of hydrogen from the pyrolysis gas will be shown to provide the dominant diffusive heating component.

While the stagnation point convective heating is sensitive to the near surface gradients driven by catalycity, the radiative heating is sensitive to the chemistry

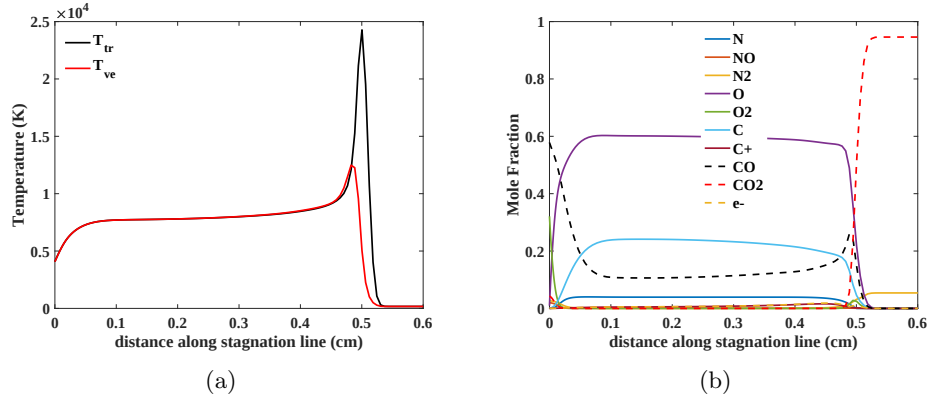


Figure 18: Stagnation line temperatures and mole fractions for peak heating case.

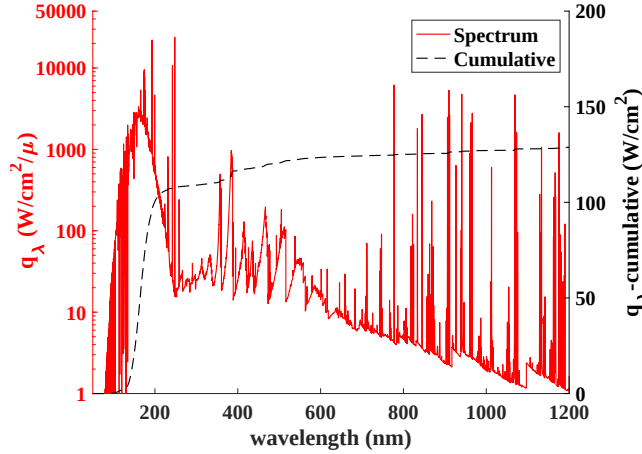


Figure 19: Stagnation point radiative flux for the peak heating case (73 s).

along the entire stagnation line. Figure 18 presents the stagnation line temperatures and mole fractions for the peak radiative heating case ($t=73$ s). The post-shock nonequilibrium region (the shock is near 0.5 cm and the wall is at the origin) is relatively large, as indicated by the gradient in the temperature profile along the entire stagnation line. Figure 19 presents the resulting radiative flux spectrum at the stagnation point. This trajectory point is dominated by CO 4th-Positive emission, which is located below 200 nm. Figure 20 presents the components from individual radiative mechanisms on the stagnation-point radiative heating. This figure shows that at trajectory points earlier than the considered 73 s case, the atomic emission also provides a large contribution to the radiative heating. These trajectory points earlier than 73 s are at velocities greater than 10 km/s, which are required to reach the shock layer temperatures greater than 10,000 K through the equilibrium region of the shock layer that are required for strong atomic emission.

Because of the importance of thermochemistry and radiation on the stagnation region heating environment, relevant experimental data must be near the actual

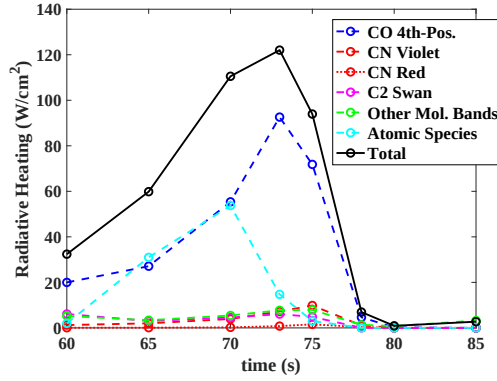


Figure 20: Contributions from various radiative mechanisms on the stagnation point radiative heating.

RLVP flight velocity near peak heating, which is between 8.0–10.2 km/s (this restriction is relaxed for the more fluid mechanics dominated turbulence and roughness modeling on the flank discussed in Section 4). More restrictive, however, is that the free-stream chemistry must be similar to Venus, which is roughly 97% CO₂ and 3% N₂. This limits the potential relevant flight vehicles measurements to the Pioneer-Venus probe (Russian data for Venus missions is not available in the open literature). The Pioneer-Venus measurements were considered recently by Cabrera and West [93]. They showed that the thermocouple measurements provided minimal insight into the aerothermal environment, because of the thermocouple depth and uncertainty in the material properties. Furthermore, the Pioneer-Venus probes entered at steeper entry flight path angle, which resulted in an order-of-magnitude higher density at peak heating. This higher density results in fundamentally different drivers to the stagnation point heating environments.

For ground-based data, shock tube measurements in the NASA Ames EAST facility [94] provide both the relevant velocities and free-stream chemistries. However, this facility measures the radiative emission as a function of distance behind a normal shock, which does not provide insight into convective heating. These measurements can provide direct insight into the radiative heating to actual flight vehicles using the shock-tube informed bias approach [95], which is presented in the following subsection.

3.2 Application of Shock-Tube Informed Bias to RLVP

The shock-tube informed bias (STIB) approach [95] allows differences between shock-tube measurements and their simulations to be converted into differences in the radiative heating to a vehicle surface. The mapping of the shock tube flow to the vehicle flowfield is possible by recognizing that the flow behind an oblique shock is the same as that behind a normal shock with a matching normal shock velocity. Using this relationship, the differences between the shock tube measurements and simulations may be applied to the RLVP flowfield by tracing the streamline of every point in the flowfield back to the shock to determine its normal shock velocity, as illustrated in Fig. 21. This disagreement factor is then applied to the upper level

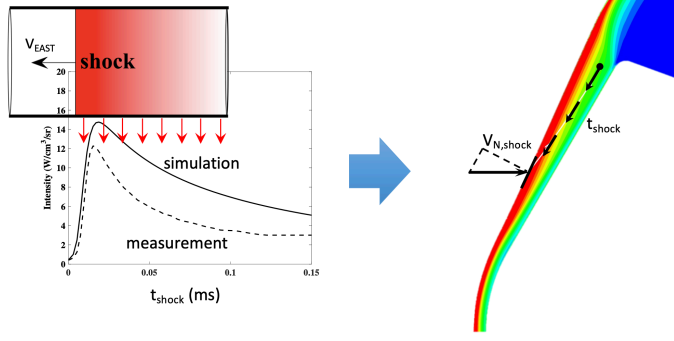


Figure 21: Overview of the shock-tube informed bias approach.

number density during the RLVP radiative heating simulation. The resulting STIB value for the radiative heating represents essentially the heating value implied by the shock-tube measurements. These STIB values will be used in Section 6.2 to provide the experimental, or model form, component of the radiative heating margin.

To evaluate the STIB for RLVP, subsection 3.2.1 presents the reduction of available shock tube measurements to the form used by STIB. The evaluation of the STIB for RLVP cases is then presented in subsection 3.2.2.

3.2.1 Evaluation of f_{adjust} for RLVP-Relevant Shock-Tube Measurements

To apply the STIB approach to RLVP, the disagreement factors (f_{adjust}) must be evaluated for the relevant shock-tube measurements. The measurements considered in this work [96, 97] are listed in Table 9, where these measurements are all for a Mars/Venus relevant free-stream composition of 96% CO₂ and 4% N₂, by mole. These cases are chosen because the measured spectral range captures CO 4th-Positive in the 165–195 nm range, CN Violet in the 340–440 nm range, CN Red in the 580–880 nm range, and C₂ Swan in the 480–530 nm range. These spectral ranges capture only a small fraction of each band system noted. The f_{adjust} values required to adjust the simulations to match the measurements are presented in Figs. 22–24. The f_{adjust} factors are applied as multipliers to the upper electronic level of the band system captured in the given spectral range. Due to the impact of the spatial convolution function, f_{adjust} is fixed to a constant value for the first 0.01 ms from the shock, and then allowed to vary with distance beyond this point. The f_{adjust} value in the first 0.01 ms behind the shock impacts the simulation up to 0.05 ms from the shock once the spatial convolution is applied. An iterative process is therefore applied to determine the constant f_{adjust} within the first 0.01 ms. The downstream values (beyond 0.01 ms) are then determined, which are influenced less by the spatial convolution.

The f_{adjust} values presented in Figs. 22–24 are compiled into tables as a function of normal shock velocity, time from the shock, and pressure. The f_{adjust} values are interpolated first in the pressure space by matching density ($p_{EAST} = \rho_{flight} R T_{EAST}$). Then, a streamline tracer built into LAURA/HARA evaluates the normal shock velocity and time from the shock for each point in the flowfield. An example of this

Table 9: EAST Measurements considered for the shock-tube informed bias analysis of CO, CN, and C₂ bands

Velocity (km/s)	Pressure (Torr)	Test #
6.72	0.1	51-40
6.79	0.1	54-4
6.90	0.1	51-12
7.00	0.1	54-6
7.71	0.1	54-2
7.98	0.1	54-7
6.43	0.25	51-7
6.53	0.25	51-17
6.71	0.25	51-2
7.75	0.25	48-43
7.82	0.25	48-39
7.06	1.0	54-8
7.35	1.0	54-9
8.08	1.0	54-10

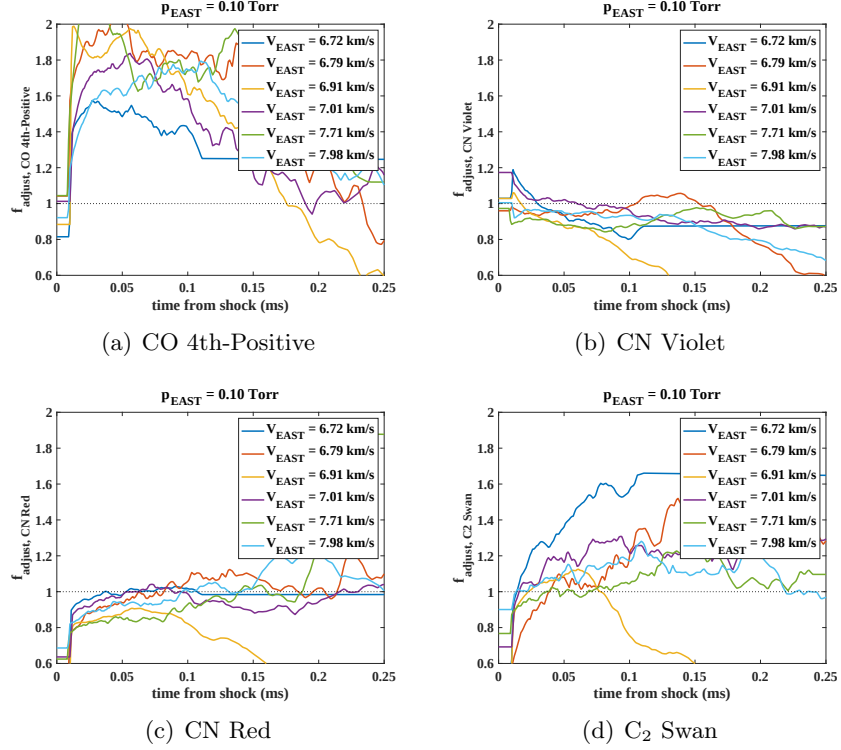


Figure 22: Summary of f_{adjust} values at $p_{\text{EAST}} = 0.10$ Torr.

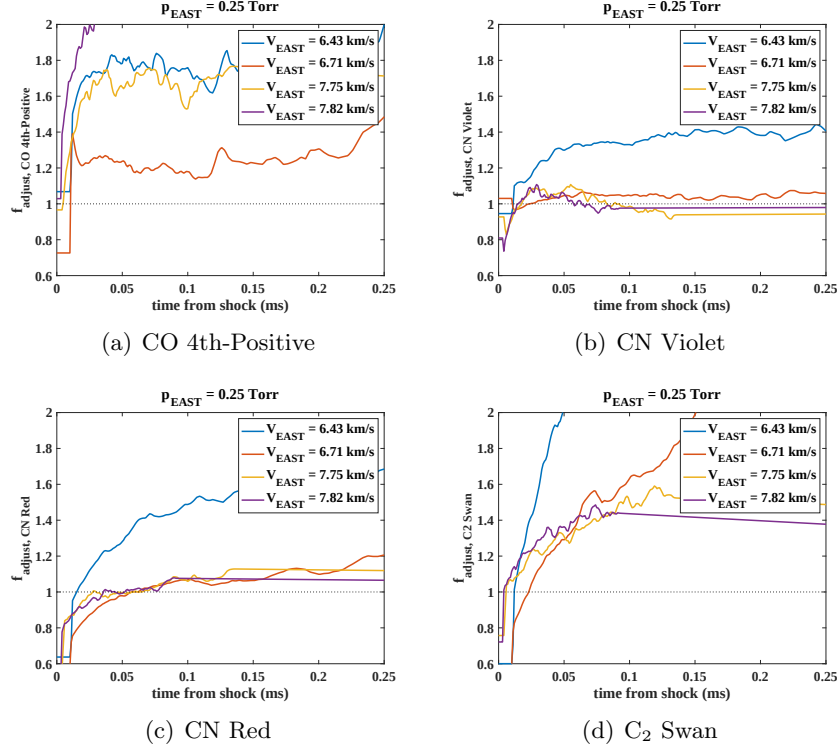


Figure 23: Summary of f_{adjust} values at $p_{EAST} = 0.25$ Torr.

for RLVP is presented in Fig. 25. With these normal shock velocity, time from the shock, and pressure values for each point in the flowfield, the f_{adjust} values are then obtained for each band system by interpolating through the tables representing Figs. 22–24. These f_{adjust} values are then applied as multipliers to the upper electronic level for the respective band systems during the ray-tracing evaluation of the radiative heating, with the resulting radiative heating being the STIB value.

3.2.2 Application of Shock-Tube Informed Bias

Application of the f_{adjust} factors discussed in the previous subsection to the ray-tracing radiative heating simulation for the 73 s case results in the STIB values for the radiative heating presented in Fig. 26. The percent differences between the STIB and nominal values are presented in Fig. 27, which shows up to a 34% increase in the STIB values relative to the nominal. This implies that a model anchored to the shock-tube measurements would result in up to a 34% increase in the radiative heating relative to the current nominal model, for this single trajectory point at 73 s. Section 6.2 will present the STIB values for other trajectory points to provide the experimental, or model form, component of the radiative heating margin, in addition to the parametric component presented in that section.

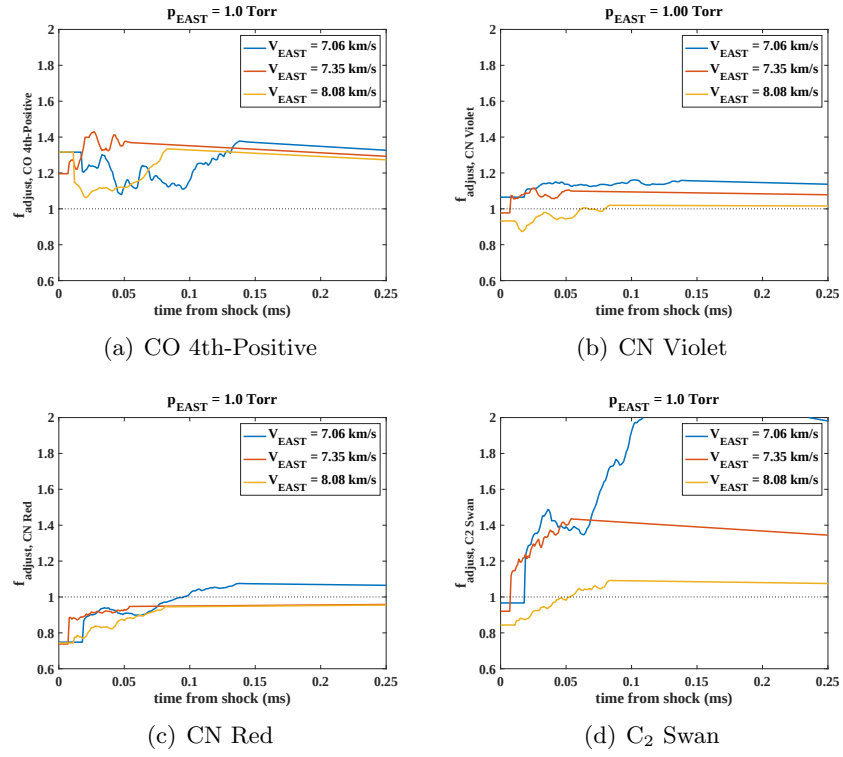


Figure 24: Summary of f_{adjust} values at $p_{\text{EAST}} = 1.00$ Torr.

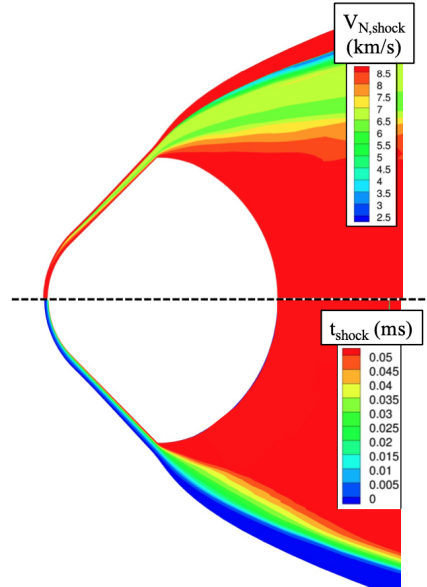


Figure 25: Streamline normal shock velocity and time from the shock for the 73 s case ($p_{\text{EAST}} = 0.52$ Torr).

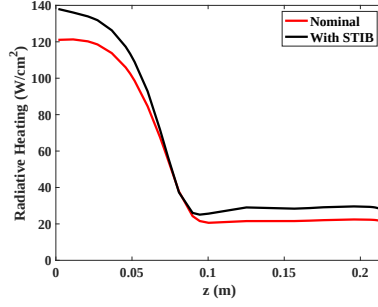


Figure 26: Comparison between the nominal and STIB forebody radiative heating for the 73 s case.

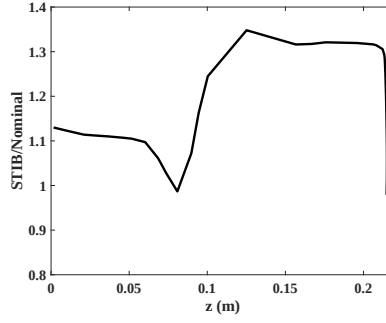


Figure 27: Percent increase in the STIB radiative heating relative to the nominal for the 73 s case.

4 Roughness Augmented Turbulent Heating Environment

Because of the uncertainty in modeling transition on the rough ablating surface anticipated for RLVP, and to ensure conservatism, the heating environment is assumed fully turbulent for the entire trajectory. To provide further justification for this fully turbulent assumption, Fig. 28 presents the Re_θ values from laminar solutions along the surface for the nominal trajectory. Applying the transition criteria suggested by Hollis [98], which is written as $Re_{\theta,TR} = 215[(k/\theta)(T_e/T_w)]^{-0.7}$, results in the transition location variation through the trajectory shown in Fig. 29. This figure shows that transition should occur near the shoulder prior to the peak heating point in the trajectory. Because the near shoulder has the highest turbulent heating, as shown in Fig. 30 (which is a repeat of Fig. 1), this suggests that the fully turbulent assumption is not overly conservative.

4.1 Smooth Wall Turbulent Heating

The increase in convective heating due to turbulence was shown in Fig. 30. This figure demonstrates the importance of the turbulence model, which provides nearly a 100% increase in the convective heating due to smooth wall turbulence (note the present subsection focuses on the smooth-wall turbulence, while the next subsection is devoted to the roughness augmentation). The forebody surface distribution of

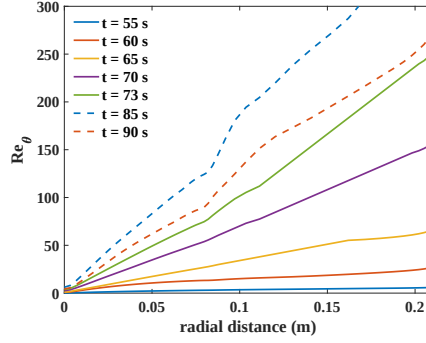


Figure 28: Values of Re_θ resulting from laminar simulations.

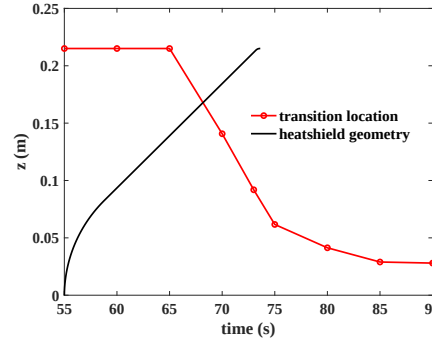


Figure 29: Transition location through the trajectory based on $Re_{\theta,TR} = 215[(k/\theta)(T_e/T_w)]^{-0.7}$

the smooth wall turbulent convective heating is presented in Fig. 31. These results apply the Cebeci-Smith algebraic turbulence model, where details of its implementation in LAURA are detailed by Cheatwood and Thompson [6]. The Cebeci-Smith turbulence model was chosen as the baseline model for the RLVP database based on the comparisons with measurements provided later in this section. A comparison between the results of the Cebeci-Smith and SA-Catris model for smooth wall turbulent heating at the near-shoulder locations is presented in Fig. 32. The two models agree within 5% at peak heating, while the SA-Catris model prediction is up to 20% larger than the Cebeci-Smith prediction later in the trajectory. This lower value from the Cebeci-Smith model will be captured by the convective heating margin presented in Section 6.1.

Before discussing the impact of roughness on heating, comparisons between the Cebeci-Smith model and Orion-sponsored measurements made in the CUBRC and AEDC facilities are presented in the following paragraphs. These measurements represent the best available smooth wall turbulent heating data, and support the application of the Cebeci-Smith model. Note that all convective heating measurements considered for validating the turbulence or roughness models are for air or N_2 free-stream gases. These measurements contain negligible chemistry, which results in negligible diffusive components to the convective heating. This isolates the resulting convective heating to the conductive component, which is driven more by

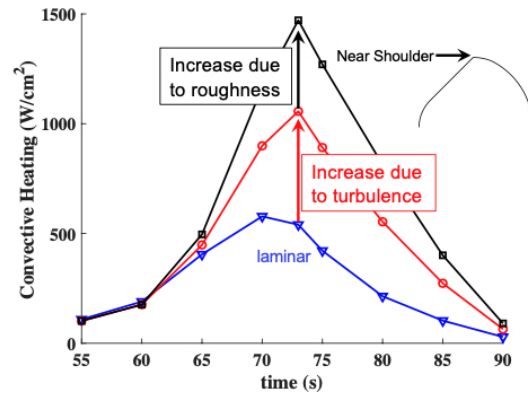


Figure 30: Impact of turbulence and roughness augmentation on the near-shoulder heating.

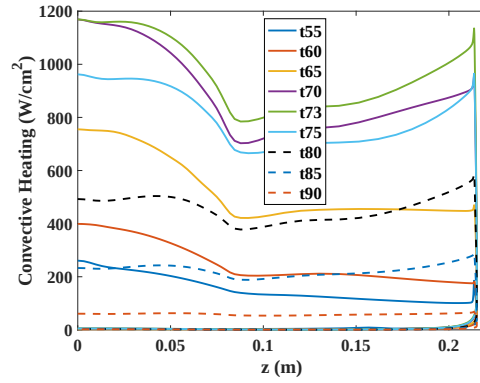


Figure 31: Smooth wall turbulent convective heating distributions for the nominal trajectory cases.

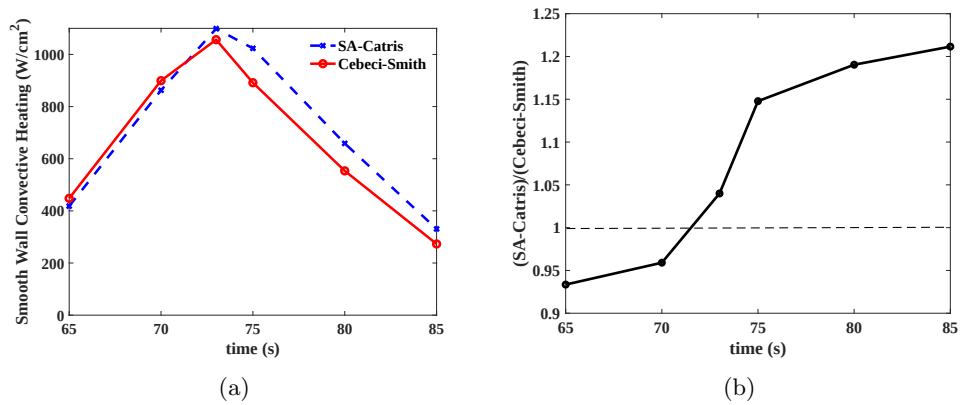


Figure 32: Comparison of Cebeci-Smith and SA-Catris smooth wall convective heating at the near shoulder locations.

the fluid dynamics of the flow than the chemistry, which makes the resulting air or N_2 measurements relevant to a Venus entry.

4.1.1 Orion AEDC Measurements

The Orion program performed tests in the AEDC Tunnel 9 facility on a scaled Orion heatshield at 28 degrees angle of attack [99,100]. These test obtained both smooth wall and rough wall turbulent heating measurements, where the smooth wall turbulence was ensured through a boundary layer trip. Only the smooth wall values are considered here since no RLVP-relevant patterned roughness measurements were obtained, with only sand-grain roughness measurements made. Figure 33 compares the scaled AEDC model at angle of attack with the RLVP heatshield. The free-stream conditions considered in this analysis are listed in Table 10. Note that the free-stream composition is 100% N_2 , unlike the other measurements considered in this analysis which are performed in air. Figure 34 compares the boundary layer parameters for the experimental cases with RLVP near peak heating. Except for Re_θ , large differences are seen between the experimental and flight cases, which is not surprising considering the geometry differences and because these measurements were made to support a specific mission that differs noticeably from RLVP. Regardless of these differences, the presence of smooth turbulence measurements makes these experiments worth considering in this analysis.

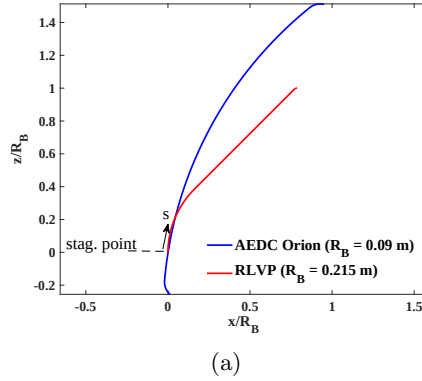
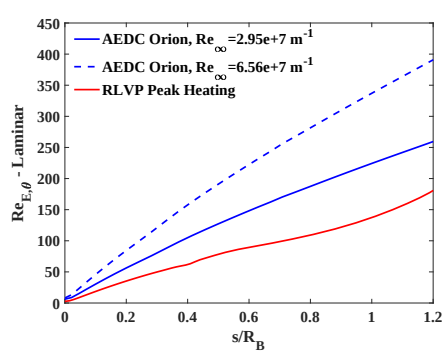


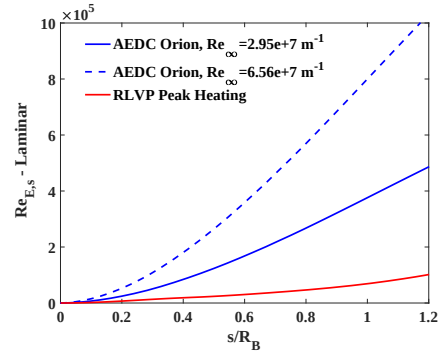
Figure 33: Comparison of scaled RLVP and the AEDC geometry at 28 degrees angle of attack.

Table 10: Free-stream conditions for Orion AEDC measurements.					
k^+ at $x = 0.05$ m	Re_∞ (1/m)	Velocity (m/s)	Density (kg/m ³)	Temperature (K)	Wall temperature at stag. point (K)
49.5	2.95e+7	1466.0	7.41e-2	51.0	300
76.7	4.59e+7	1486.1	1.16e-1	50.0	300
98.4	6.56e+7	1482.0	1.54e-1	48.9	300

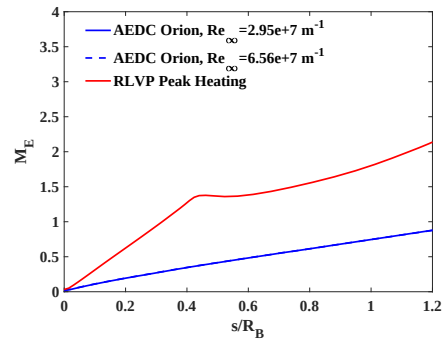
Figure 35 compares the smooth wall turbulent measurements with a range of simulations. The difference between the Baldwin-Lomax and Cebeci-Smith turbulence models is relatively small. In the fully-turbulent region near $x = 0.05$ m,



(a)



(b)



(c)

Figure 34: Comparison of boundary layer parameters between RLVP peak heating ($t = 73 \text{ s}$) and AEDC cases.

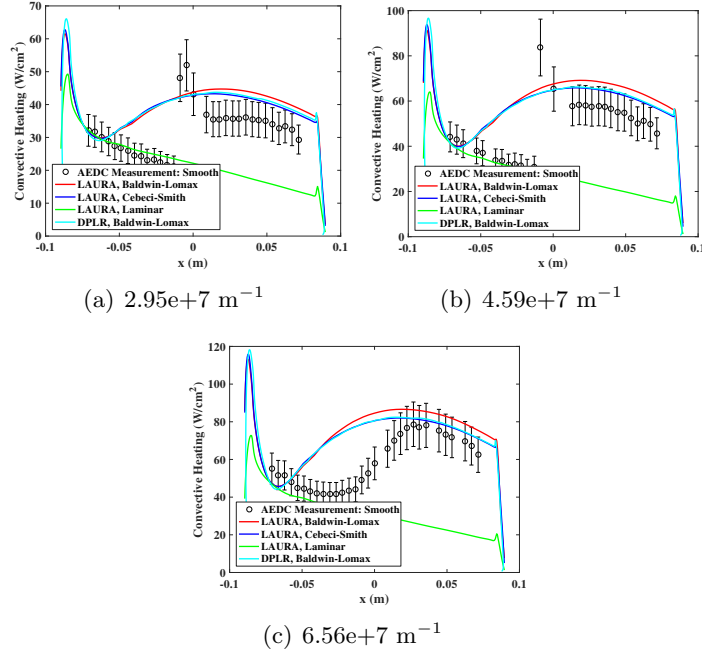


Figure 35: Comparison of smooth wall heating between AEDC measurements and simulations.

the simulations from all models are on the higher end of, or slightly above, the measurement uncertainty bounds.

4.1.2 Orion CUBRC Measurements

Similar to the AEDC measurements, the CUBRC measurements in the LENS facility were performed on an Orion geometry at 28 degrees angle of attack [101, 102]. The CUBRC model, however, is over 6 times larger than the AEDC model. A comparison of the scaled model and RLVP geometries is presented in Fig. 36. While the geometries are scaled to be consistent with figures presented for the previous test campaigns, the R_B value for the CUBRC measurement is three times larger than the RLVP. The free-stream conditions for the CUBRC cases are listed in Table 11. The resulting boundary layer parameters compared to RLVP are presented in Fig. 37. Even though the CUBRC geometry is larger than the AEDC geometry, the comparison of the boundary layer parameters with RLVP is similar because of the free-stream conditions differences.

Table 11: Free-stream conditions for Orion CUBRC measurements.

k^+ at $x = 0.3$ m	Re_∞ (1/m)	Velocity (m/s)	Density (kg/m ³)	Temperature (K)	Wall temperature at stag. point (K)
18.6	6.22e+6	1521.6	1.42e-2	55.3	300
37.0	1.19e+7	1554.8	2.89e-2	57.5	300
61.0	1.77e+7	1598.9	4.72e-2	60.4	300

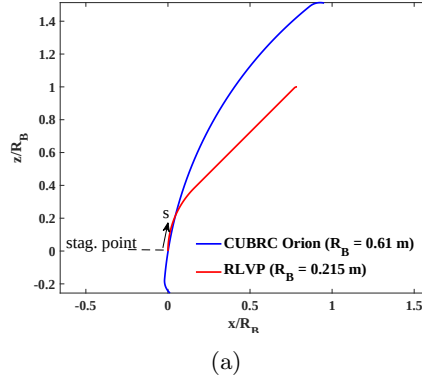


Figure 36: Comparison of scaled RLVP and the CUBRC geometry at 28 degrees angle of attack.

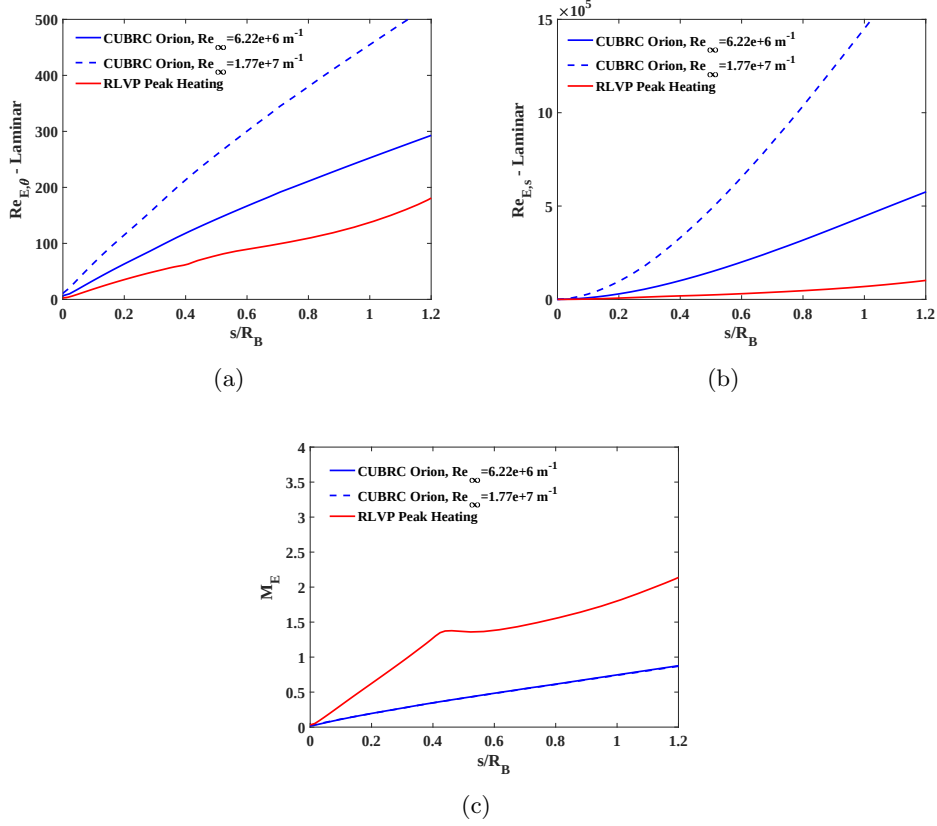


Figure 37: Comparison of boundary layer parameters between RLVP peak heating (73 s) and CUBRC cases.

Comparisons between the simulated and measured smooth wall turbulent heating are presented in Fig. 38. The Baldwin-Lomax and Cebeci-Smith models are both shown to provide excellent agreement with the measurements. This good agreement for the Cebeci-Smith motivates the use of this model for the nominal RLVP environments. Note that these comparisons do not inform the model form uncertainty,

however, because they do not capture the impact of roughness, which is considered in the following subsections.

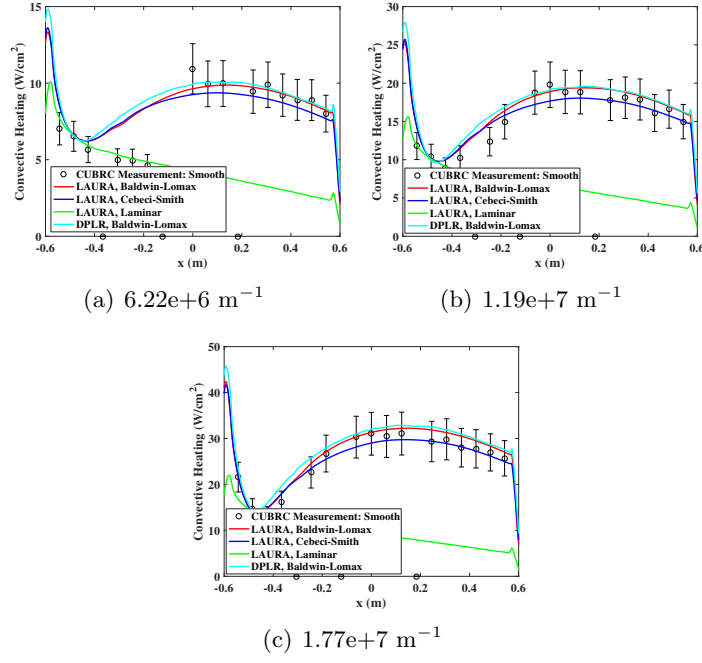


Figure 38: Comparison of smooth wall heating between CUBRC measurements and simulations.

4.2 Roughness-Induced Turbulent Convective Heating Augmentation

The physical roughness height for RLVP's 3MDCP heatshield is assumed equal to 0.7 mm, which represents the peak to valley height of the weave-based roughness pattern. This height was chosen based on the post test analysis of arc-jet samples, as shown in Fig. 4. This roughness height (k) divided by the boundary layer thickness (δ) and the roughness Reynold's number (k^+) are presented in Fig. 39 for the near shoulder location. The roughness Reynold's number is defined as [103]:

$$k^+ = \left(\frac{\tau_w}{\rho_w} \right)^{1/2} \frac{\rho_w}{\mu_w} k. \quad (6)$$

Figure 39 shows that the roughness extends over 20% of boundary layer height, which is significant. This figure also shows that k^+ reaches values over 70, while the peak heating value is around 55. The impact of these roughness values on the convective heating is assessed through the following four approaches:

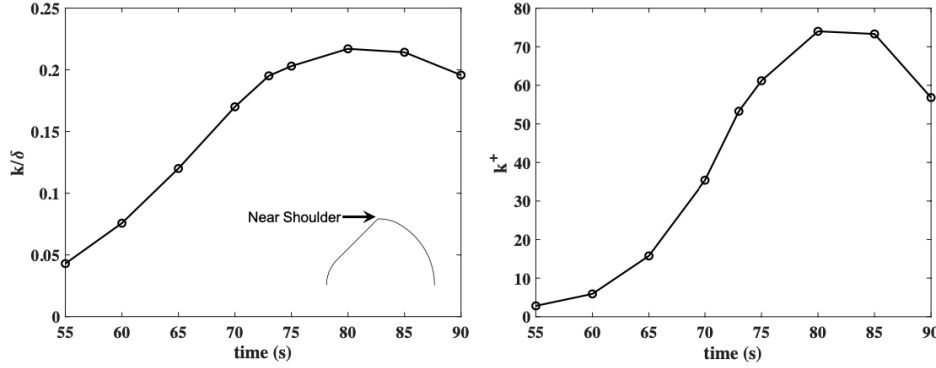


Figure 39: Near shoulder roughness properties assuming a 0.7 mm roughness heights.

1. Dahm Model:

The roughness model applied by Edquist [104] for MSL and Mars 2020 was developed by Dahm [16] and is written as:

$$\frac{q_{c,rough}}{q_{c,smooth}} \approx \frac{C_{H,rough}}{C_{H,smooth}} = 0.46 + 0.54(\log_{10} k^+), 10 < k^+ < 70 \quad (7)$$

This model is based on measurements from the 1970s era PANT II program [16]. For MSL and Mars 2020 a roughness height of 0.6 mm was applied for the MSL PICA heatshield, which resulted in peak k^+ values of 30 and augmentation ratios of 1.25, which are noticeably lower than the present RLVP values. Note that this model was developed for sand-grain roughness, while the current 3MDCP roughness is patterned.

2. Approach Used by Orion:

The roughness model applied for Orion [105] is written as:

$$\frac{q_{c,rough}}{q_{c,smooth}} \approx \frac{C_{H,rough}}{C_{H,smooth}} = 1 + 0.39 \log_{10} \left(\frac{k^+}{2.6} \right), 2.6 < k^+ \quad (8)$$

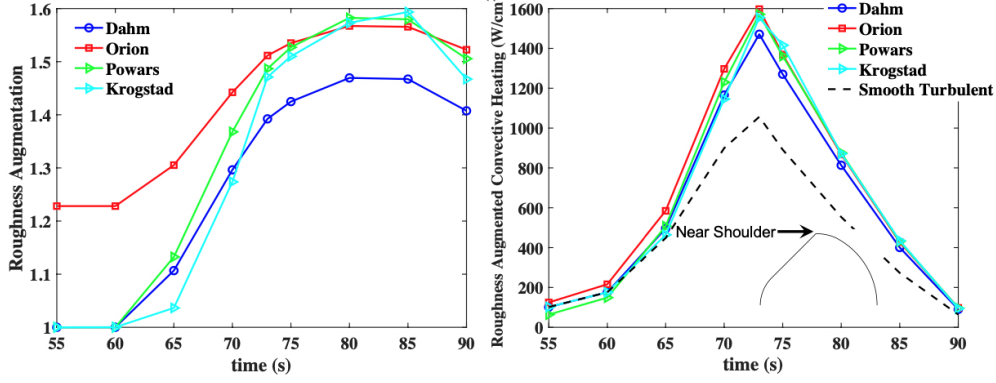


Figure 40: Impact of various roughness augmentation models on the heating.

This model is based on AEDC and CUBRC wind tunnel measurements on the Orion geometry. Many of these measurements are re-considered in the present analysis, which is presented later in this section. Note that, as with the Dahm model, this model was developed for sand-grain roughness, while the current 3MDCP roughness is patterned.

3. Powars Model:

A widely used roughness augmentation model developed by Powars [106] based on PANT data is written as:

$$\frac{q_{c,rough}}{q_{c,smooth}} \approx \frac{C_{H,rough}}{C_{H,smooth}} = 0.67 \log_{10}(k^+) + 0.33 \quad (9)$$

This model is also based on the correlation of sand grain roughness measurements.

4. Krogstad Modification to Turbulence Model:

While the previously listed roughness augmentation models are applied as a post-processing step to the flowfield solution, based on the simulated k^+ values, this model is coupled to the flowfield solution. Krogstad proposed [107] an alteration to the Van Driest damping function. This new function, which is dependent on the roughness height, is applied within the Cebeci-Smith turbulence model in LAURA. This model will be utilized in Section 5 to capture roughness effects for simulations with coupled ablation.

A comparison of these various roughness augmentation models on the near-shoulder heating along the nominal RLVP trajectory is presented in Figure 40. While the augmentation ratios vary noticeably between the models, the difference in dimensional heating rates is relatively small, since the best agreement in the augmentation ratios occurs near peak heating.

To provide experimental justification for choosing one of these roughness augmentation models, the following subsections consider the available RLVP-relevant measurements. These subsections will justify the choice of the Dahm model as the recommended model for RLVP design.

4.3 Langley Mach 6 Aerothermal Measurements for 3MDCP Patterned Roughness

Due to the potential large impact of patterned roughness on the turbulent heating augmentation for vehicles with a 3MDCP heatshield, an aeroheating test campaign was recently performed in the Langley Mach 6 facility [108]. This test aimed to collect roughness augmentation measurements on a patterned roughness 52.5 degree sphere-cone model. This test utilized the approach developed recently by Hollis for building models with a patterned roughness surface that are appropriate for the Langley Mach 6 facility. This test also utilized the nominally axisymmetric flow expected for entry vehicles likely to experience over 1500 W/cm^2 heating, and therefore likely to use a 3MDCP heatshield. These factors led to the model configuration presented in Fig. 41, which includes a patterned roughness region, a smooth region (laminar) and a tripped region (turbulent). An example of the phosphor thermography measurement of the heat transfer coefficient is presented in Fig. 42, where the line cuts through the various regions are compared with simulations. Although the smooth wall laminar and turbulent heating are measured, the focus of the present analysis is on the patterned roughness section. As identified in Fig. 42, the heating at a radial distance of 6 cm will be considered throughout this study.

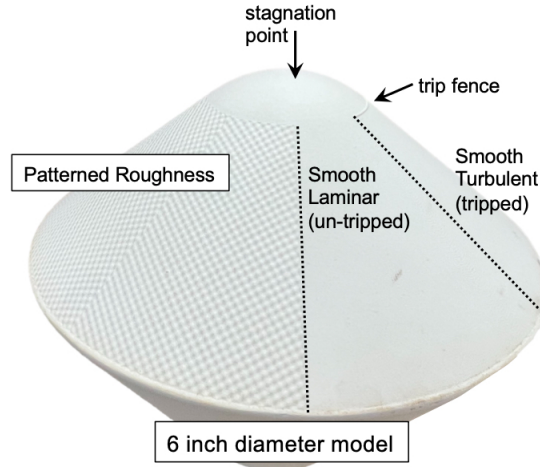


Figure 41: Model layout for the Langley Mach 6 3MDCP patterned roughness measurements.

To define the patterned roughness surface topology, scans of the charred surface of post AEDC arc-jet test samples were used. This represents the most RLVP-relevant information on the charred 3MDCP surface topology that RLVP may experience near peak heating. These arc-jet measurements tested 3MDCP at RLVP-relevant heat flux levels in the presence of turbulent flow, as discussed with Fig. 4. Figure 43 presents another example of the post-test surface measurements. The underlying weave of the 3MDCP material results in the patterned surface seen in this figure. To reproduce the RLVP roughness pattern presented in Fig. 43, an idealized sinusoidal distribution is used. For the orientation shown in Fig. 44, the pattern is

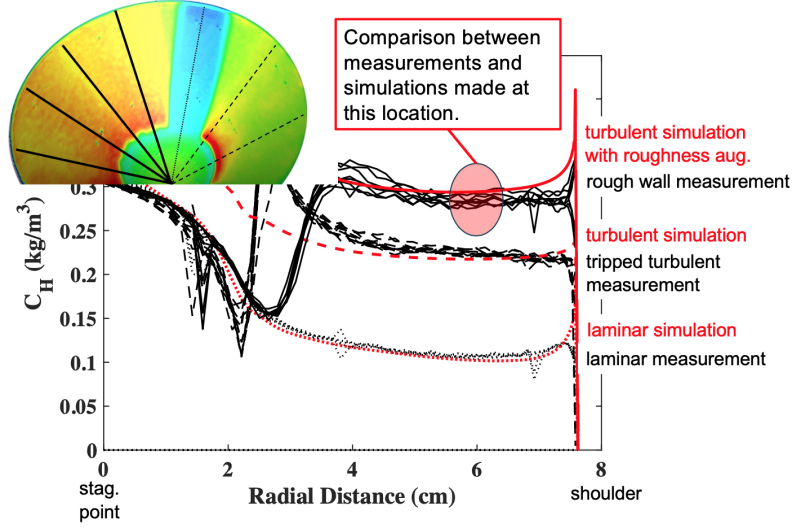


Figure 42: Example comparison between simulations and the Langley Mach 6 3MDCP patterned roughness measurements.

defined by the following equation:

$$z = \frac{k}{4} \sin\left(\frac{2\pi x}{P}\right) + \frac{k}{4} \sin\left(\frac{2\pi y}{P}\right), \quad (10)$$

where $P = D/\cos(\pi/4)$ and D is defined as an average peak-to-valley distance, while k is defined as an average peak-to-valley height (see Fig. 43). This configuration is referred to as “Orientation 1”. Because streamlines for RLVP will flow over all orientations of this pattern, this idealized pattern is also rotated 45 degrees to obtain the following:

$$z = \frac{k}{2} \sin\left(\frac{\pi x}{D}\right) \sin\left(\frac{\pi y}{D}\right) \quad (11)$$

This configuration is referred to as “Orientation 2”. These two patterns are presented in Fig. 44, where in the figure on the right the intersection of red lines are the peaks and the middle of the gray boxes are the valleys. Both pattern orientations are equally relevant for RLVP, as demonstrated in Fig. 45, which presents the simplified pattern orientation for RLVP relative to the boundary layer edge streamlines. The actual orientation will be distorted from forming the weave in three-dimensions, but this figure shows the basic orientation prior to forming. Regardless of the distortion due to forming, this figure shows that the streamlines emanating from the stagnation region will encounter the pattern at both orientation 1 and 2, as well as every orientation in-between.

The k value in these equations is chosen so that the measurements match the targeted RLVP k^+ values between 35 - 65. The k^+ values for the measurements depend on the free-stream conditions. For the Langley Mach 6 facility, the velocity is essentially fixed while the free-stream density varies to change the free-stream Reynolds number. Table 12 lists the free-stream conditions considered in these tests. These conditions result in the k^+ values presented in Fig. 46 for k values of

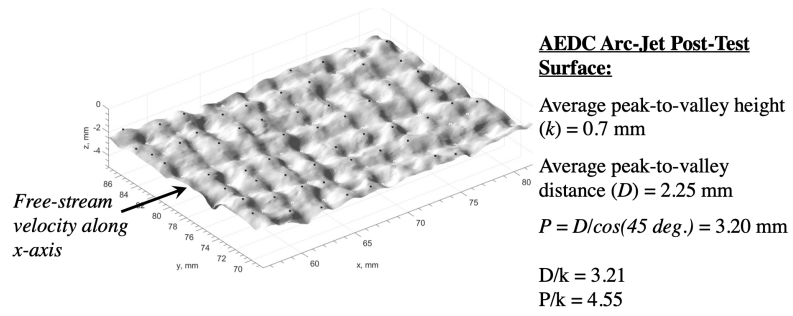


Figure 43: Post arc-jet surface from AEDC wedge test (from Wilder).

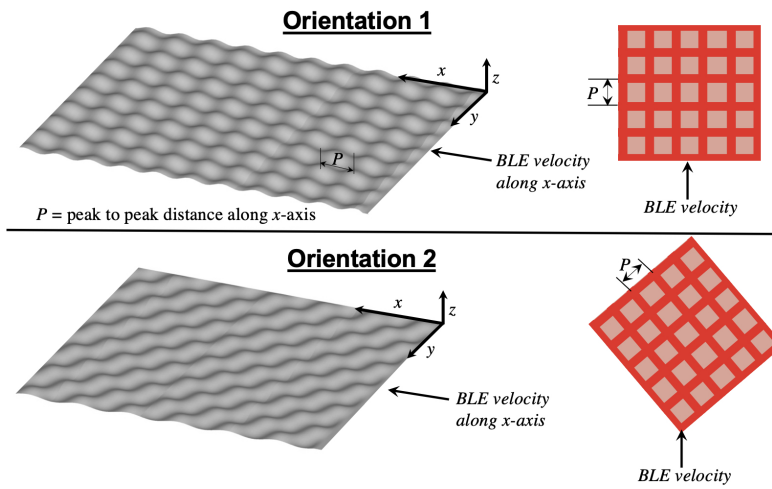


Figure 44: Idealized patterned roughness surface topology for two orientations.

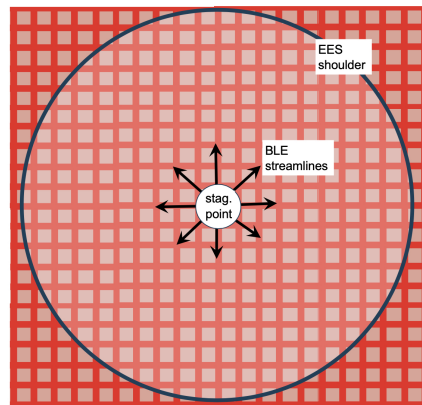


Figure 45: Simplified pattern orientation for RLVP relative to boundary layer edge streamlines.

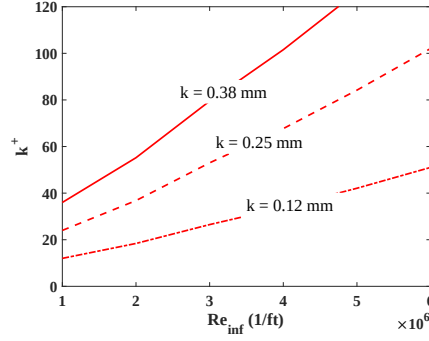


Figure 46: Dependence of k^+ at the model flank on Re_{∞} for the Langley Mach 6 3MDCP cases.

Table 12: Free-stream conditions for the Langley Mach 6 3MDCP measurements.

Re_{∞} (1/ft)	Velocity (m/s)	Density (kg/m ³)	Temp. (K)
1.0e+6	930.6	1.66e-2	61.9
2.1e+6	944.9	3.16e-2	62.4
2.5e+6	944.9	3.96e-2	62.4
3.0e+6	951.0	4.66e-2	62.6
3.6e+6	951.0	5.54e-2	62.6
4.1e+6	951.0	6.29e-2	62.6
5.0e+6	961.5	7.79e-2	63.5
5.8e+6	958.6	8.96e-2	63.5
6.8e+6	957.6	1.06e-1	63.5

0.126, 0.254, and 0.381 mm, which are shown to result in RLVP-relevant k^+ values at the measurement conditions. Using these k values and the D/k or P/k from Fig. 43 result in the D or P values used in Eqs. (19) or (20), respectively.

To establish the relevance of the test conditions and geometry with the RLVP flight cases, Fig. 47 compares the simulated measurement and flight case values for the boundary layer edge Reynold's number based on momentum thickness $Re_{E,\theta}$, boundary layer edge Reynold's number based on running length distance from the stagnation point $Re_{E,s}$, boundary layer edge Mach number M_E , roughness height divided by boundary layer thickness k/δ , and previously defined k^+ . Because the roughness augmentation is highest for RLVP after peak heating as shown in Fig. 40, a peak heating and later case are included in the comparison with the measurement case values. For the $Re_{E,\theta}$ and $Re_{E,s}$ values, the 80 s RLVP case nearly reaches the lower bound of the measurement values. This establishes the relevance between the fluid dynamics of the boundary layer between the measurement and flight cases, even if the thermodynamics and chemistry are significantly different. The M_E values in Fig. 47(c) are seen to differ noticeably, which is the result of the measurement case being at a much lower free-stream Mach number than the flight cases. For the k/δ comparison in Fig. 47(d), note that the presented measurement values are for a roughness height of 0.254 mm, which Fig. 46 shows provides flight relevant k^+ values for Re_{∞} values around $3e+6$ 1/ft. These k/δ values agree well with the RLVP cases, which further establishes the relevance of the measurements to RLVP.

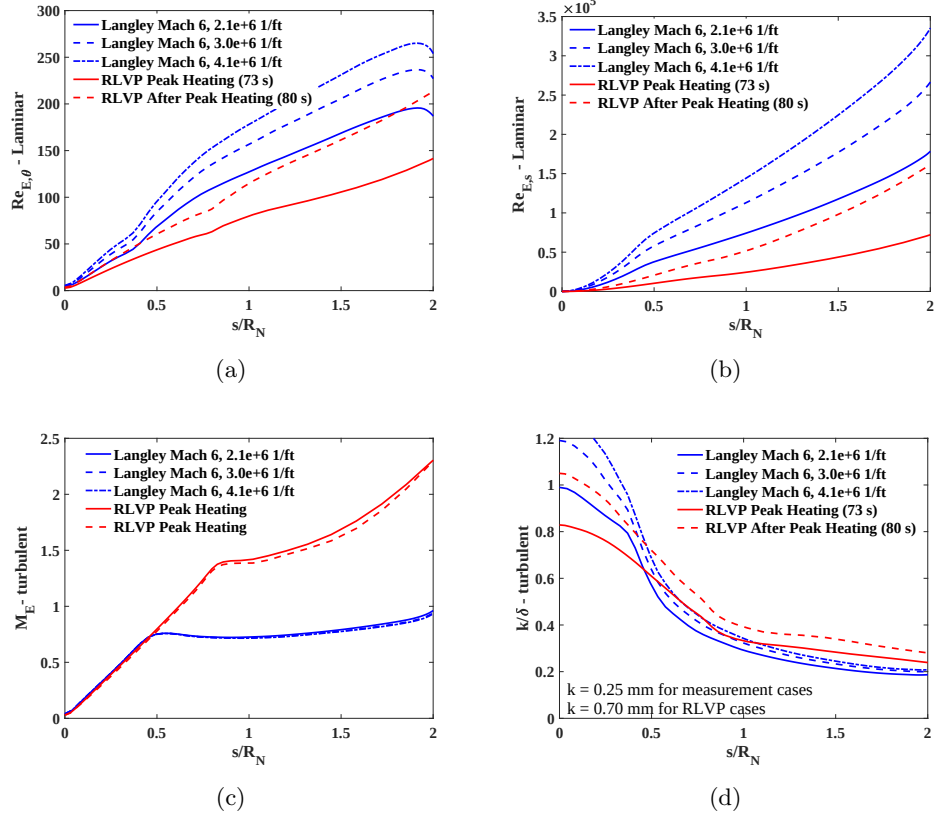


Figure 47: Comparison of boundary layer parameters between RLVP peak heating and Langley Mach 6 3MDCP cases.

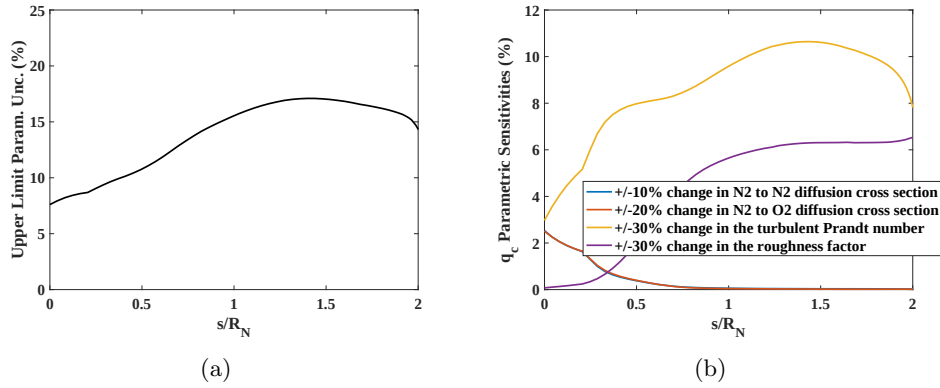


Figure 48: Parametric uncertainty results the Langley Mach 6 3MDCP case at $3e+6 \text{ ft}^{-1}$.

Measurements from five different models are considered in the present analysis. Each model included two different roughness patterns. The first three models varied the roughness height, with values of 0.126, 0.254, and 0.381 mm. Each model with a different roughness height included both the orientation 1 and 2 patterns. The fourth and fifth model included smaller ($P/k = 3.0$) and larger ($P/k = 7.0$) pattern wavelengths for the 0.254 mm roughness height. For a given test condition and model, two measurements were made, one on each side of the model (due to the camera only capturing one side of the model), which captures the two different roughness patterns. Combining the 5 models, 2 roughness patterns on each model, and 9 free-stream conditions, results in 90 individual measurements considered in the present analysis.

The primary application of the present measurements is to help evaluate the model form uncertainty used for the RLVP margin. As shown in Fig. 8, this requires evaluating the difference between the upper limit of the measurement error bars and the upper limit of the simulated parametric uncertainty. The measurement error bars for the Langley Mach 6 measurements are reported as $\pm 15\%$ [98]. For the parametric uncertainty of the simulations, the approach described in Section 2.2.2 was applied to the $3e+6 \text{ 1/ft}$ case. The resulting parametric uncertainty is presented in Fig. 48(a), with the dominant uncertainty contributors shown in Fig. 48(b). These figures show that the parametric uncertainty on the turbulent flank (between s/R_N of 1.0 and 2.0) is around 15%, with the dominant parameter being the turbulent Prandtl number. Because the roughness augmentation on this flank region is the quantity of interest in the measurements, the parametric uncertainty for the simulations may be conveniently set to 15%. This is especially convenient because this 15% is the same as the measurement uncertainty, so the uncertainty contributions cancel when evaluating the model form uncertainty. This allows the model form uncertainty to be evaluated as the difference between the nominal measurements and simulations.

An example comparison between the measurement and simulations was presented in Fig. 42, which included laminar, smooth wall turbulent, and rough wall turbulent comparisons. For the present assessment of the model form uncertainty,

only the rough wall turbulent comparisons are considered. While the smooth wall turbulent measurements are also of interest to validate the turbulence model, these measurements are complicated by the influence of the trip ring, which caused a spike in heating and then decay to the smooth wall turbulent value. For higher Reynolds numbers, this decay lasted for the entire length of the model, therefore resulting in anomalously high measurements.

The ratios of the rough wall turbulent measurements at a radial distance of 6 cm and the smooth wall turbulent simulations, which represent the roughness augmentation, are compared with the roughness augmentation correlations in Figure 49. This figure shows that the Dahm model provides the closest agreement with the measurements, which motivates the choice of this model for RLVP design. At the k^+ values expected near peak heating of roughly 50 - 60, the Dahm model provides an excellent prediction of the upper-bound of the measurements.

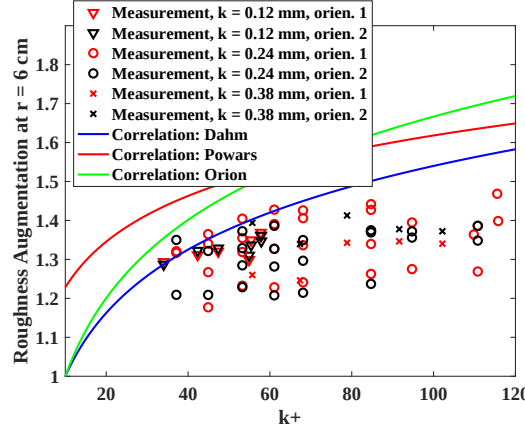


Figure 49: Summary comparison for all Langley Mach 6 3MDCP measurements and simulations for roughness augmentation.

To examine the spread of the values shown in Fig. 49 for a given k^+ , Fig. 50 shows the impact of orientation for three different P/k values. The measured values presented in this figure are normalized by the rough wall simulated values. This figure shows that, except for a few outliers, the heating augmentation for orientation 1 is larger than for orientation 2, with a peak difference of 8%. The variation due to changing P/k is apparent in this figure, but more clearly shown in Fig. 51 which presents the values for only orientation 1. This figure shows that the heating augmentation is reduced with increasing P/k . Both this reduced augmentation with increasing pattern wavelength and the increased augmentation for orientation 1 relative to orientation 2 may be explained by the peak-to-peak spacing in the stream wise direction, where smaller spacing results in higher augmentation. Obviously smaller P/k values result in smaller peak-to-peak spacing, while the impact of orientation on the peak-to-peak spacing is shown in Fig. 52, where the peaks are located at the intersection of the red lines. This figure shows that orientation 2 has a 1.41 times larger spacing for the same P/k as orientation 1. This dependence of the roughness augmentation on the peak-to-peak spacing is consistent with the study of Finson and Clarke [109], which showed that the larger spacing of hemispherical

roughness elements resulted in lower augmentation.

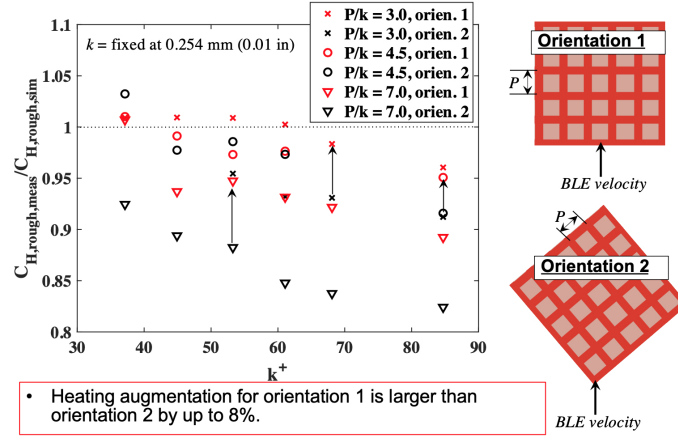


Figure 50: Impact of pattern orientation on the Langley Mach 6 3MDCP measurements.

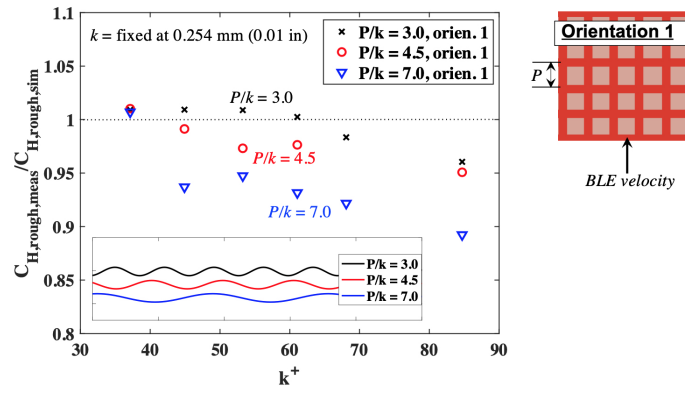


Figure 51: Impact of pattern wavelength (P/k) on the Langley Mach 6 3MDCP measurements.

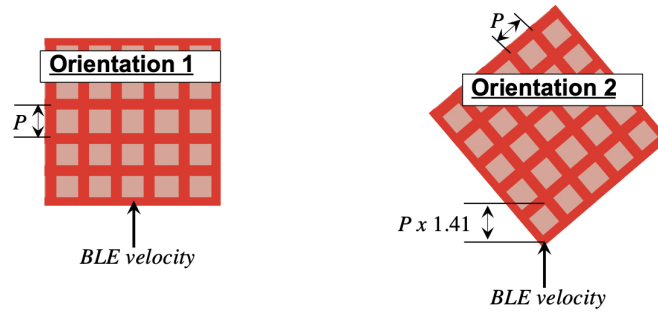


Figure 52: Impact of pattern orientation on peak-to-peak spacing along a streamline.

To evaluate the model form uncertainty implied by these measurements, the ratio of the measurements and simulations are presented in Fig. 53 (the nominal values are

used because the measurement error bars and simulation's parametric uncertainty are the same). This figure shows that the maximum under-prediction is 4%, which is the model form uncertainty resulting from these measurements. This small value confirms the adequacy of the turbulence and roughness models recommended for RLVP.

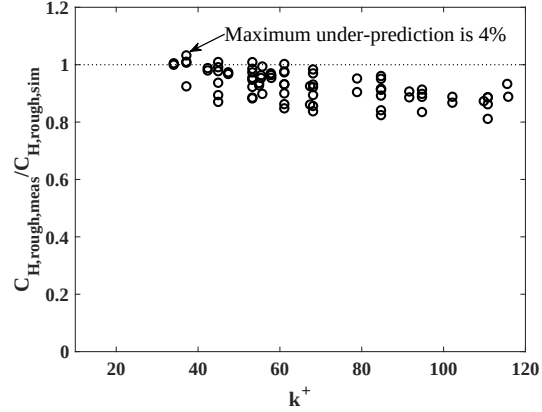


Figure 53: Summary comparison for all Langley Mach 6 3MDCP measurements and simulations (using the Dahm roughness model) for model form uncertainty.

4.4 Wilder and Prabhu Ballistic Range Measurements

The second most relevant experimental validation data for the turbulence and roughness models used for RLVP, behind only the 3MDCP Langley Mach 6 test presented in the previous subsection, are the ballistic range measurements made by Wilder and Prabhu [110]. This campaign obtained smooth wall turbulence and roughness augmentation convective heating measurements on the model geometry presented in Fig. 54(a). Because the angle of attack is zero, the distinct smooth and rough regions on the flank allow for simultaneous measurements of smooth wall heating and roughness augmentation for both sand-grain and 3MDCP roughness patterns. The rough stagnation region trips the boundary layer to ensure the flow is turbulent even for the smooth wall flank. The free stream conditions for the measurements are listed in Table 13, where the “2020” measurements were made by Wilder after the 2019 Wilder and Prabhu [110] paper. The cases listed in Table 13 are simulated using the same models and assumptions applied for the RLVP simulations, with the exception that the wall temperatures are fixed to the values specified in Table 13. The simulations are performed at the mid-range velocity specified in this table, with the resulting heating scaled back to the launch velocity, through the multiplication factor $(V_{launch}/V_{mid})^3$, to be consistent with Wilder and Prabhu’s conversion from temperature measurements at multiple range locations to convective heat rates.

The relevance of these measurements to RLVP is due to both geometric and boundary layer similarity with RLVP. The geometric similarity is shown in Fig. 54(b), which shows that the two scaled 45 degree sphere cones are nearly identical. To establish the boundary layer similarity, Fig. 55 compares the experimental and RLVP flight values for the boundary layer edge Reynold’s number based on momentum thickness $Re_{E,\theta}$, boundary layer edge Reynold’s number based on running length distance from the stagnation point $Re_{E,s}$, boundary layer edge Mach number M_E , roughness height divided by boundary layer thickness k/δ , and previously defined k^+ . These figures show a relatively good comparison between RLVP and the measurements. The largest difference is in M_E , which is due to the free-stream Mach number for the RLVP case being over three times higher than the ballistic range cases. These lower M_E values result in subsonic flow near the roughness elements, unlike the RLVP flight case where supersonic flow extends down to near the roughness elements. The varying roughness heights for the measurements are seen to provide a range of RLVP relevant k^+ values.

The first question these measurements provide insight towards is the quality of the smooth wall turbulent simulations. These smooth wall comparisons are presented in Fig. 56 for each measurement. Note that the stagnation region comparison is omitted because the measurements contain roughness in this region to trip the flow to turbulent. For each case, the Cebeci-Smith result is just below the 15% measurement uncertainty bounds, with agreement within 20%. The better agreement by the Baldwin-Lomax model is fortuitous, since its deviation from the Cebeci-Smith result is due to deficiencies in the Baldwin-Lomax model. The better agreement provided by the Baldwin-Lomax deficiencies cannot be expected to extend flight conditions. These comparisons help further justify the choice of the Cebeci-Smith turbulence model for RLVP, which is consistent with the earlier comparisons shown

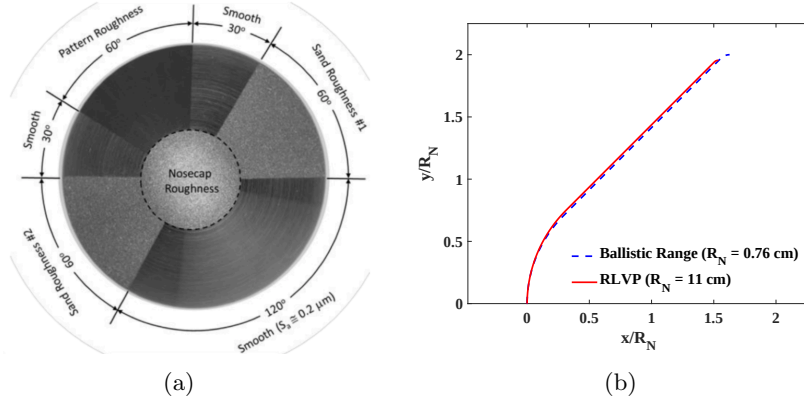


Figure 54: Comparison of similitude parameters between EES peak heating and Wilder and Prabhu cases.

Table 13: Free-stream conditions for the Wilder and Prabhu measurements.

$k_{pattern}^+$ at $s/R_N = 1.7$	Test #	Velocity Launch (m/s)	Velocity Mid (m/s)	Density (kg/m ³)	Temp. (K)	Wall Temp. (K)	Roughness Height (m)
2019 Test:							
34.4	2812	3117	2870	1.80e-1	295	640	1.05e-5
61.6	2813	3112	2861	1.80e-1	295	641	1.88e-5
70.9	2810	3247	2984	1.80e-1	295	690	1.97e-5
2020 Test:							
72.0	2834	3243	2980	1.80e-1	295	678	1.89e-5
83.0	2851	3547	3211	2.37e-1	295	785	1.76e-5
87.0	2852	3546	3211	2.37e-1	295	782	1.84e-5
90.0	2850	3542	3209	2.37e-1	295	785	1.92e-5

in Sections 4.1.2 and 4.1.1.

The remainder of this analysis of the Wilder and Prabhu measurements focuses on the roughness augmentation. Only the 3MDCP patterned roughness measurements are considered because they capture the roughness pattern expected for RLVP, therefore ignoring the sand-grain roughness measurements.

The measurement uncertainty and parametric uncertainty are required to evaluate the model form uncertainty. According to Wilder and Prabhu [110], the measurement uncertainty is $\pm 15\%$. The parametric uncertainty was computed for the 2813 case using the approach discussed in Section 2.2.2. The resulting upper limit uncertainty and the dominant parameters contributing to this uncertainty are presented in Fig. 57. This figure shows that the parametric uncertainty peaks at 21%, with the turbulent Prandtl number being the dominant contributor on the flank. This parametric uncertainty will be used to evaluate the model form uncertainty.

To evaluate the model form uncertainty implied by these measurements, Fig. 58 compares the rough wall heating for the 2019 cases, where the Cebeci-Smith turbulence model and Dahm roughness model are applied for the simulations. The nominal simulations are near the lower bound of the measurement uncertainty bars for each case. Adding the parametric uncertainty to the simulations brings them to within 5% of the upper measurement bound for s/R_N values below 2.0. For the

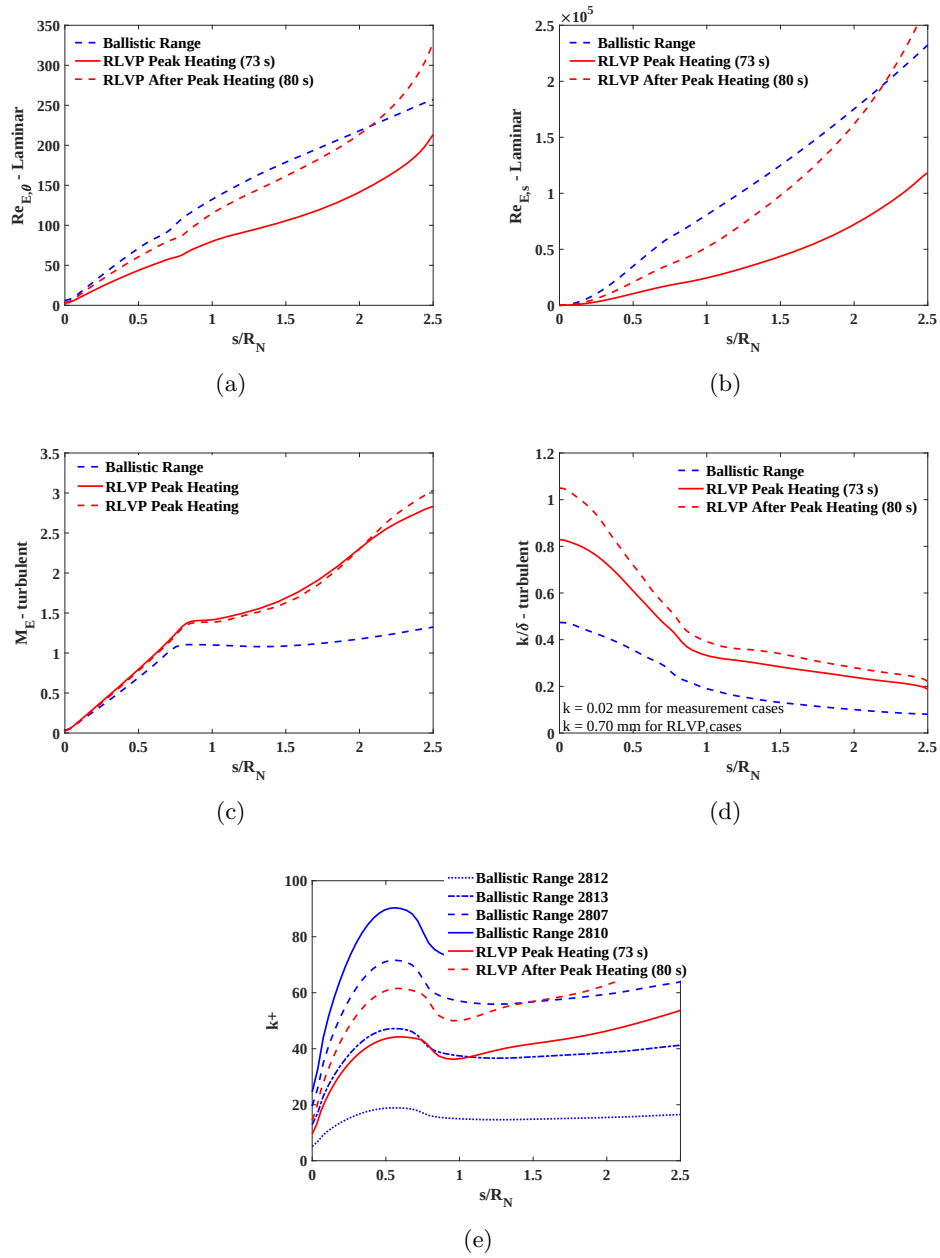
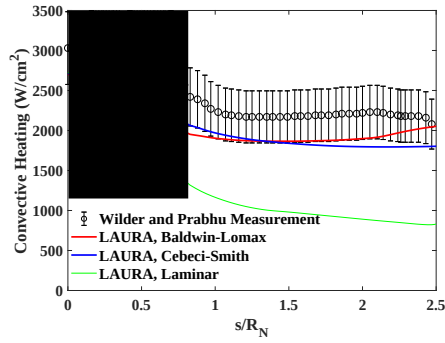
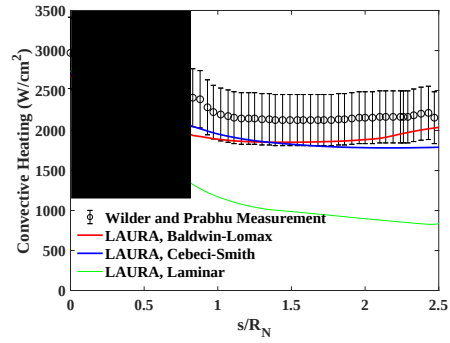


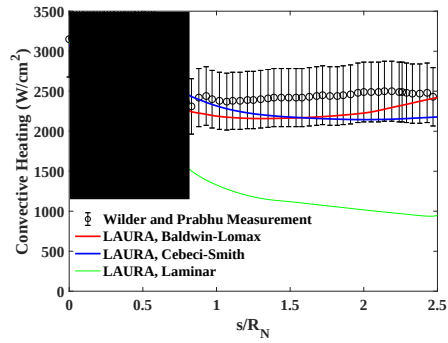
Figure 55: Comparison of boundary layer parameters between RLVP and Wilder and Prabhu cases.



(a) 2812

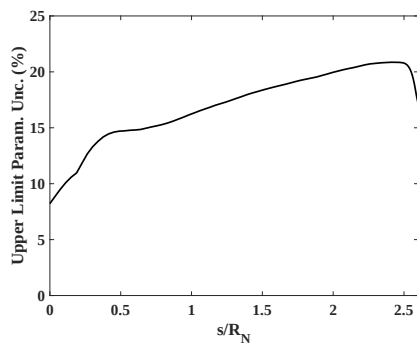


(b) 2813

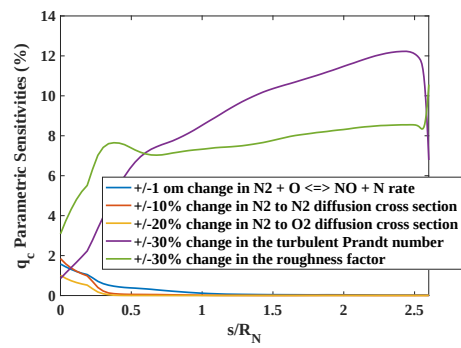


(c) 2810

Figure 56: Comparison of smooth wall convective heating between Wilder and Prabhu measurements and simulations.



(a)



(b)

Figure 57: Parametric uncertainty results for the Wilder and Prabhu 2813 case.

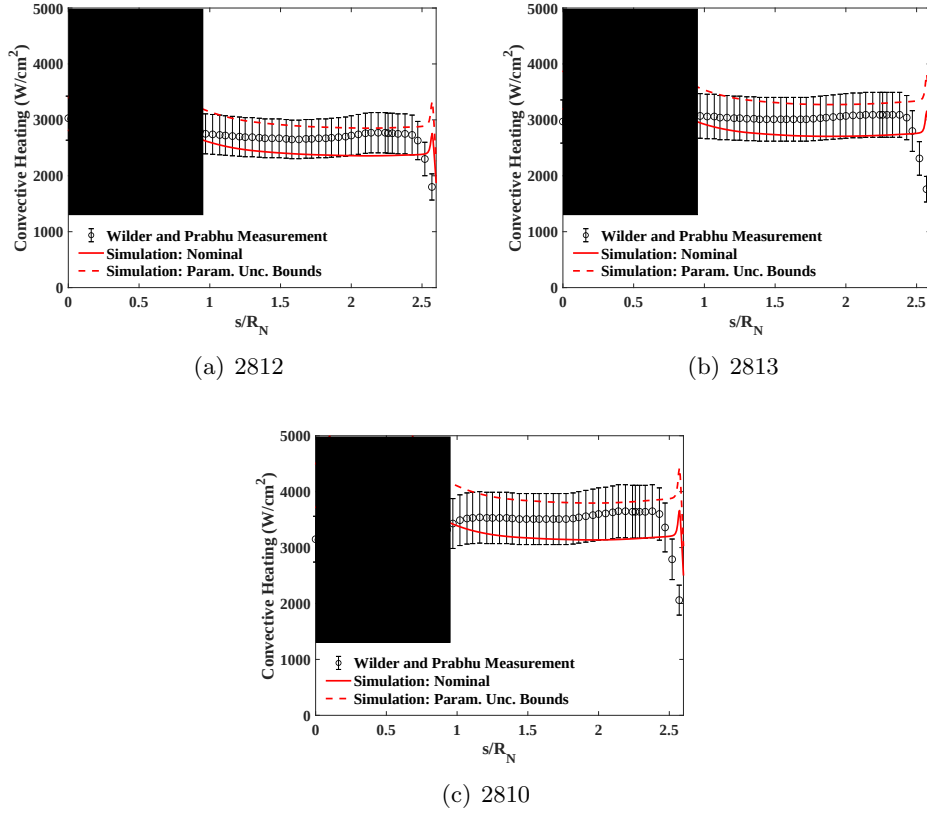


Figure 58: Comparison of rough wall heating between Wilder and Prabhu measurements and simulations.

2812 and 2810 cases, this difference increases to 9% above s/R_N values below 2.0. However, the slight bump in the measured heating above an s/R_N of 2.0 is considered anomalous, so the 5% value is used for the model form uncertainty resulting from these measurements.

Figure 59 summarizes the pattern roughness comparison for $s/R_N=1.7$ for the 2019 and 2020 cases, and includes the various roughness augmentation models. The four higher k^+ values are the 2020 cases, for which surface distributions are not available, which is why they were not included in Fig. 58. These 2020 measurements all compare better with simulations than the 2019 measurements, and therefore did not contribute to the model form uncertainty. Note that the under-prediction of the Dahm model for the three lower k^+ values is inconsistent with the the Langley Mach 6 measurements presented in Fig. 49, where the Dahm model provided essentially an upper bound on the measurements.

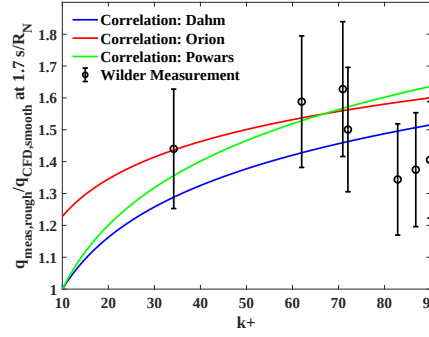


Figure 59: Comparison of rough wall heating ratios at $s/R_N=1.7$ between Wilder and Prabhu measurements and correlations.

4.5 Hollis Langley Mach 6 Measurements

Hollis (unpublished) obtained convective heating measurements on 70-degree sphere cones at 16 degrees angle of attack with a patterned roughness surface. An example of this patterned roughness surface is shown in Fig. 60. These measurements were made in the NASA Langley Mach 6 facility [98], and are therefore similar to the 3MDCP Mach 6 test presented in Section 4.3, except for the different model geometry and surface pattern. Even if they are less relevant to RLVP than the 3MDCP test, they still provide valuable insight into the impact of a pattern roughness surface on the heating augmentation at RLVP-relevant k^+ values.



Figure 60: Example of the patterned roughness surface for the Hollis measurements.

Figure 61 compares the scaled model geometry with the RLVP heatshield. Although the measurements are made on a 70 degree sphere cone at 16 degrees angle of attack, the leeside of the model is at a similar angle relative to the free-stream as the RLVP 45 degree sphere cone flank. Table 14 presents the free-stream conditions for the range of cases considered in this test campaign. This table also lists the peak-to-valley roughness height (k) for the patterned roughness, and the corre-

sponding k^+ values based on the simulations. Note that the simulations apply the same models and assumptions as the RLVP simulations, including the application of the Cebeci-Smith turbulence model and Dahm roughness model. The boundary layer is not tripped for these measurements, so that transition occurs sooner for the larger roughness heights and higher Reynolds numbers, and no smooth wall turbulent measurements are obtained.

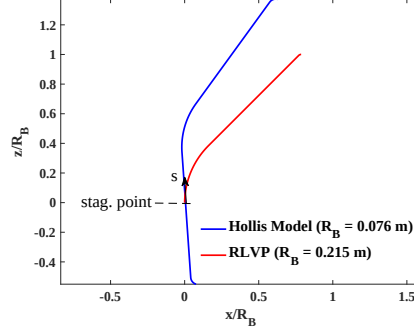


Figure 61: Comparison of scaled RLVP and Hollis geometry at 16 degrees angle of attack.

Table 14: Free-stream conditions for the Hollis measurements.

k^+ at $z = 0.05$ m	Re_∞ (1/ft)	Velocity (m/s)	Density (kg/m ³)	Temp. (K)	Wall Temp. (K)	Roughness Height (m)
72	2.0e+6	939.5	3.25e-2	61.9	333	5.1e-4
50	3.0e+6	939.5	4.88e-2	61.9	333	2.5e-4
80	5.0e+6	939.5	8.13e-2	61.9	333	2.5e-4
50	6.5e+6	939.5	1.06e-1	61.9	333	1.3e-4
55	7.2e+6	939.5	1.17e-1	61.9	333	1.3e-4

The relevance of these measurements to RLVP is assessed based on the same boundary layer parameters considered in the previous subsections. This comparison of the boundary layer parameters with RLVP is presented in Fig. 62. While the obvious difference in geometry between the model and RLVP heatshield lead to expected differences in the distributions of the various boundary layer parameters, the magnitude of these parameters agree within a factor of two in the downstream region (near $s/R_B=1.0$), except for $Re_{E,s}$ and M_E . This downstream region is where the comparison between the turbulent simulations and measurements are made, since the transition to turbulence has occurred by this point.

Comparisons between the measured and simulated C_H values are presented in Fig. 63 for the Hollis measurements. The shaded region represents the fully turbulent region that is appropriate to compare with the simulations. The Dahm roughness model is applied for the roughness augmentation in the simulations. These comparisons show excellent agreement between the measurements and simulations with roughness augmentation. Figure 64 presents a summary of the roughness augmentation factors resulting from these measurements, which again shows good agreement with the Dahm roughness model.

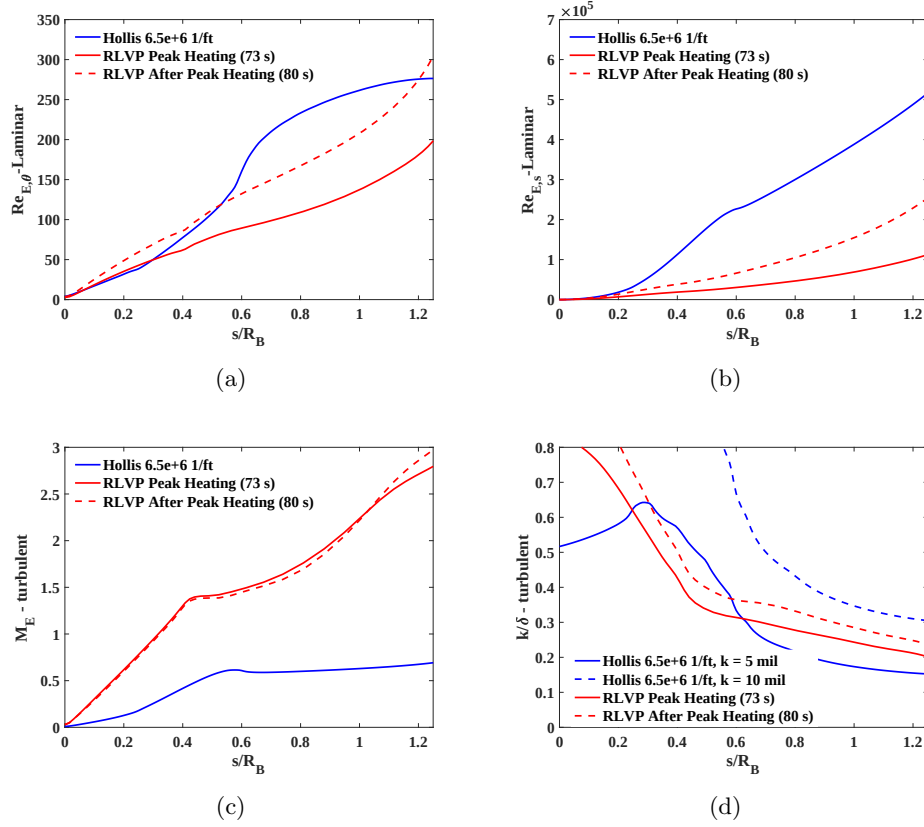


Figure 62: Comparison of boundary layer parameters between RLVP and Hollis cases.

To assess the model form uncertainty implied by these measurements and simulations, the measurement uncertainty and parametric uncertainty are required. The measurement uncertainty is $\pm 15\%$, as reported by Hollis [98]. The parametric uncertainty was computed for the $Re_\infty = 2e+6$ 1/ft case, which showed the largest disagreement between measurements and simulations in Fig. 63, using the approach discussed in Section 2.2.2. The resulting parametric uncertainty along the centerline is presented in Fig. 65, with a peak value of 12%. This parametric uncertainty is applied to simulations in Fig. 66, which also applies the measurement error bars. The upper limit of the measurement error bars are 5% higher than the upper limit of the parametric uncertainty simulation. This 5% represents the model form uncertainty implied by these measurements.

4.6 Holden Measurements

A drawback of the measurements considered so far in this section is that the Mach number at the roughness height (based on the smooth body simulation) is less than 1.0. This is unfortunate because for the RLVP case this Mach number at the roughness height is greater than 1.0 along over half of the conical flank region, which is a result of the 45 degree sphere cone geometry. This supersonic Mach number is demonstrated for the 73 s RLVP case in Fig. 67, which presents the Mach number

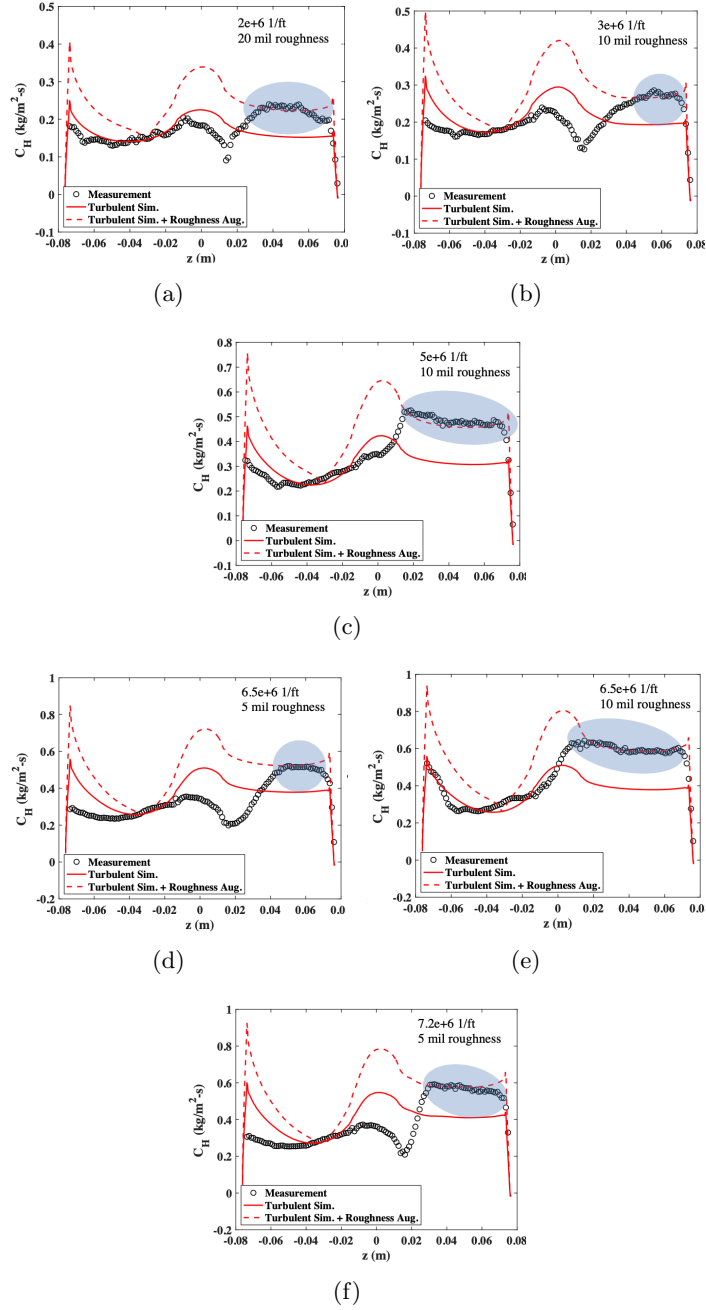


Figure 63: Comparison of roughness heating ratios between Hollis measurements and correlations.

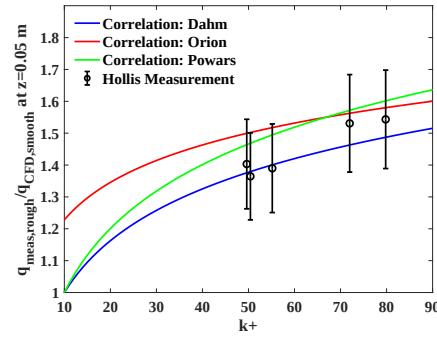


Figure 64: Comparison of roughness heating ratios between Hollis measurements and correlations.

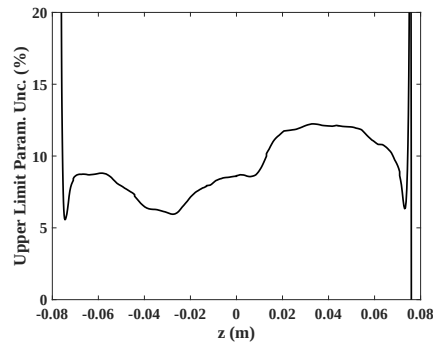


Figure 65: Parametric uncertainty along the centerline for the Hollis $Re_\infty=2e+6$ 1/ft case.

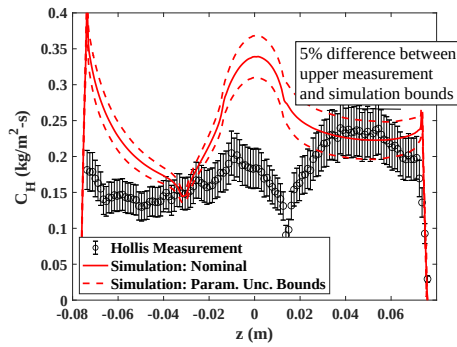


Figure 66: Assessment of the model form uncertainty from the Hollis $Re_\infty=2e+6$ 1/ft measurements and simulations.

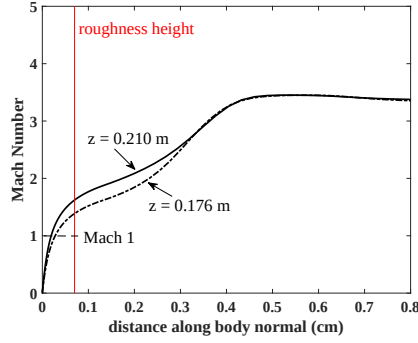


Figure 67: Mach number along the near shoulder body normal line.

Table 15: Free-stream conditions for the Holden measurements.

k^+ at $x = 0.3$ m	Re_∞ (1/m)	Velocity (m/s)	Density (kg/m ³)	Temperature (K)	Wall temperature at stag. point (K)	Roughness height (m)
120	3.2e+7	1014	1.59e-1	60.0	300	1.0e-4
220	2.9e+7	1758	8.45e-2	60.0	300	1.0e-4

for a body normal line at the near shoulder location. Because of the impact of shocks forming potentially on roughness elements, the presence of supersonic flow at the roughness height could have a significant impact on the convective heating augmentation [111], and therefore experimental data that contain this behavior is required. Fortunately, the measurements made by Holden [112, 113] actually do contain supersonic Mach numbers at the roughness heights, and therefore provide valuable validation data for RLVP. In fact, Holden presents the Schlieren shown in Fig. 68, which demonstrates the small shocks formed off of individual roughness elements.

Measurements to Holden’s sharp cone model are considered in this analysis (blunt cone models were also considered by Holden, however the sharp cone models result in more RLVP-relevant boundary layers and higher Mach numbers near the surface). Figure 69 shows that, beyond the sharp nose, the scaled model geometry is similar to RLVP. The two free-stream conditions considered in this analysis are presented in Table 15. Figure 70 compares the resulting boundary layer parameters for the $3.2e+7$ m⁻¹ case with the RLVP cases before and after peak heating. The agreement between the Holden and RLVP parameters is not good, except for the k/δ . Note that, as shown in Table 15, these similar k/δ values correspond to significantly larger k^+ values for the Holden case, with values of 120 compared to 40 - 65 for the RLVP case. Regardless of the lack of boundary layer similarity between these Holden measurements and RLVP, the presence of supersonic flow near the roughness elements makes these measurements uniquely relevant to RLVP, and therefore require consideration.

Figure 71 compares the laminar, smooth turbulent, and rough turbulent simulations with their respective measurements. For both cases, the smooth and rough turbulent simulations are accurately simulated. This comparison provides the only

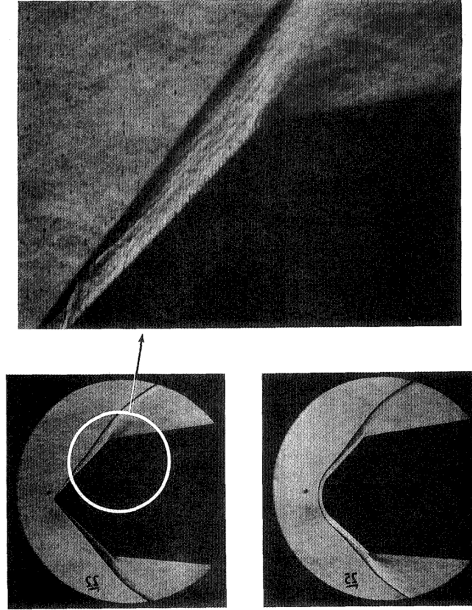


Figure 68: Example of shocks forming on roughness elements extending into the supersonic region of the boundary layer. Taken from Holden [113].

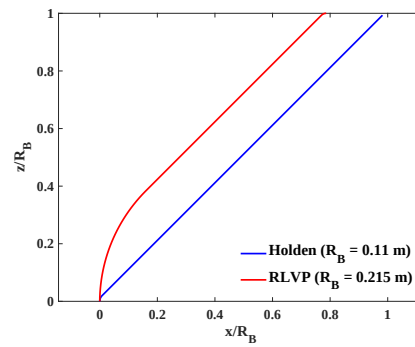
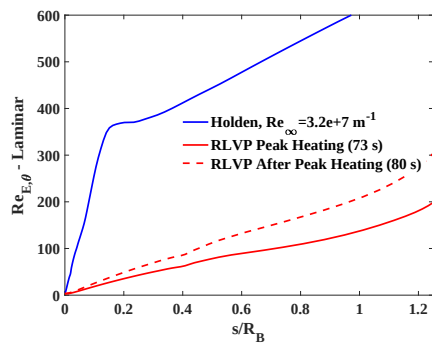
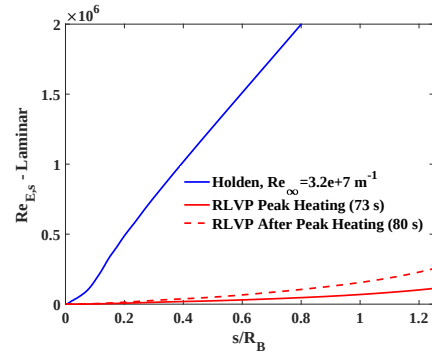


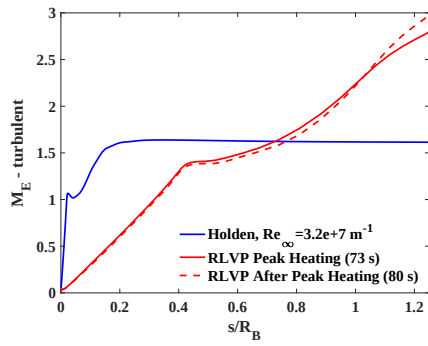
Figure 69: Comparison of scaled RLVP and the Holden geometry.



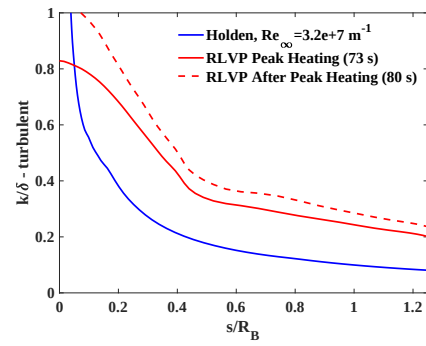
(a)



(b)



(c)



(d)

Figure 70: Comparison of boundary layer parameters between RLVP and Holden cases.

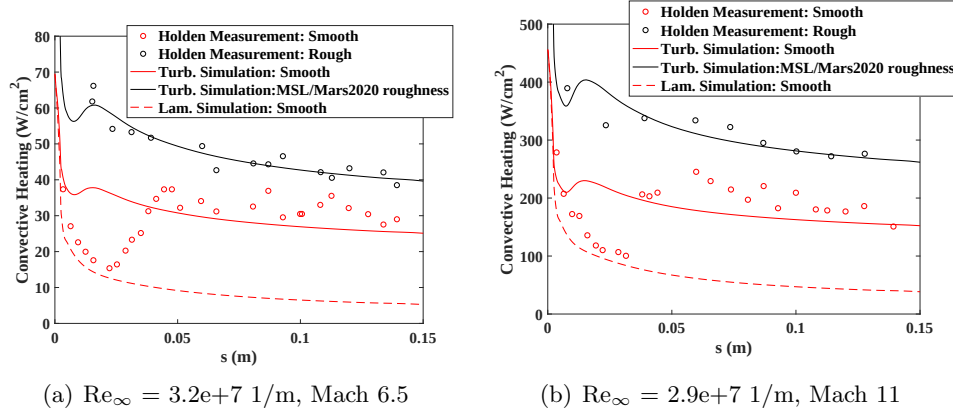


Figure 71: Comparison between Holden measurements and simulations.

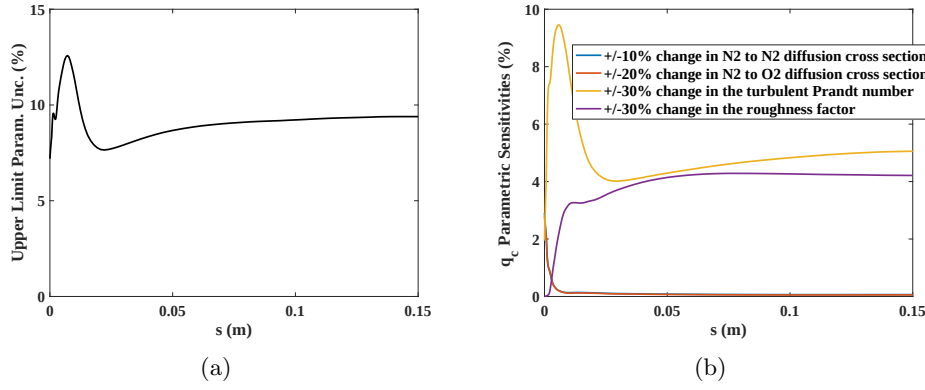


Figure 72: Parametric uncertainty results for the Holden $Re_\infty = 3.2 \times 10^7$ 1/m, Mach 6.5 case.

available confirmation that the roughness height extending into supersonic flow does not noticeably change the roughness heating augmentation, meaning that the present heating augmentation model remains valid for this type of flow.

To assess the model form uncertainty implied by these measurements and simulations, the measurement uncertainty and parametric uncertainty are required. The measurement uncertainty is $\pm 5\%$, as reported by Holden [113]. The parametric uncertainty was computed for the $Re_\infty = 3.2 \times 10^6$ 1/m case, which has the more RLVP-relevant k^+ values, using the approach discussed in Section 2.2.2. Figure 72 presents the resulting parametric uncertainty and sensitivities. As with the other turbulent cases presented throughout the previous subsections, the turbulent Prandtl number uncertainty is the largest uncertainty contributor. Applying this parametric uncertainty to the simulated values and measurement error bars results in the comparison shown in Fig. 73, which allows the model form uncertainty to be assessed at 3%.

Note that supersonic flow at the roughness height has not been present for any NASA entry vehicle, except for perhaps Pioneer-Venus. Therefore, this issue demands further investigation beyond considering the limited Holden measurements

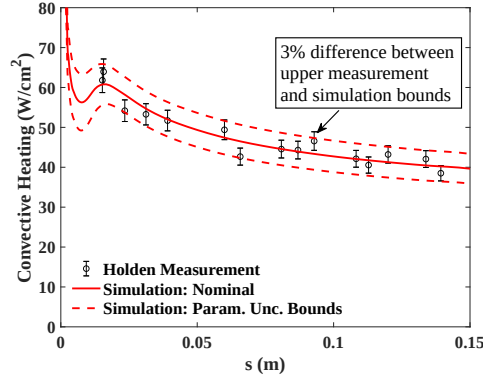


Figure 73: Comparison of rough wall heating between Holden $Re_\infty = 3.2e+7$ 1/m, Mach 6.5 measurements and simulations, including uncertainties.

mentioned in the previous paragraph. To accomplish this investigation, both a computational and experimental path are being pursued. The computational effort is presented in the next subsection (Section 4.7), presents roughness-resolved CFD simulations. The patterned roughness and axisymmetric smooth body flow for RLVP make this task feasible, along with recent advances made in the flowfield solver numerics.

4.7 Grid-Resolved Roughness (GRR) Simulations

The analysis of experimental measurements relevant to the impact of patterned roughness on the turbulent convective heating presented in the previous subsections are limited to ground test conditions that do not capture the thermodynamic, finite-rate chemistry, or radiation effects that are present for RLVP near peak heating. To provide a link between the ground test measurements, which are used to establish the validity and model form uncertainty, and the RLVP flight environments, the grid-resolved roughness (GRR) simulations developed in this section were pursued. The developed simulation approach is presented in subsection 4.7.1. Validation of this approach with the 3MDCP Mach 6 measurements is presented in subsection 4.7.2, and the application to RLVP is presented in subsection 4.7.3.

4.7.1 GRR Simulation Approach

The combination of the 3MDCP's patterned roughness and RLVP's axisymmetric nominal forebody flowfield make the grid-resolved roughness simulations pursued in this section feasible. As discussed in Section 4.3, the patterned roughness for RLVP may be approximated by Eqs. (19) and (20), with the roughness height and peak-to-valley spacing listed in Fig. 43. This roughness pattern allows a periodic boundary condition to be applied along the edges of the axisymmetric flowfield slice shown in Fig. 74. For the simulations presented in this section, a single roughness element is resolved with a 15×15 surface grid. This periodic boundary condition prevents the simulation of the full, half or quarter body grid, which reduces the number of cells

from 1.65B to 4.5M for a typical case. The simulations presented in this section were performed with a roughness patch significantly larger than this.

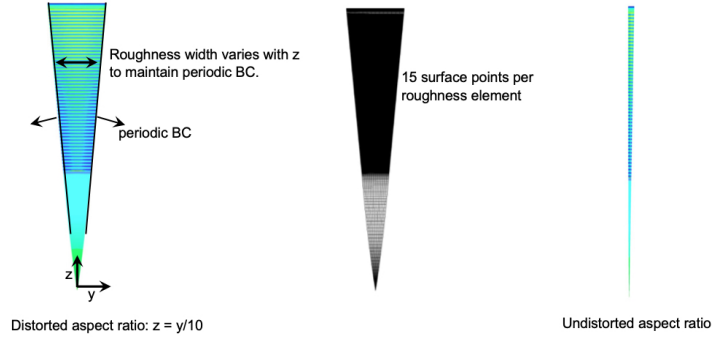


Figure 74: Overview of the periodic roughness pattern applied to an axisymmetric flowfield slice.

To enable the flowfield simulation on this tightly spaced three-dimensional grid with periodic boundary conditions, a novel solution methodology was developed. The roughness resolved grid (one-degree axisymmetric slice) is first reconstructed from the coarse-grid half body surface solution. This serves as an initial solution for the axisymmetric slice simulation as well as a boundary condition for the nose region. Imposing the stagnation region as an upstream boundary condition bypasses numerical stability issues that would arise with extremely fine grid resolutions around the stagnation point in the nose region that are inevitable in typical block-structured meshes. Then a three-dimensional periodic boundary condition is applied along the two sides of the axisymmetric slice in azimuthal direction until a converged solution is reached. The flow is assumed to be fully turbulent using the Spalart-Allmaras (SA) model [114], which is used here instead of the Cebeci-Smith model to better capture the vortical flow in the roughness valleys. When evaluating the roughness augmentation factor, the grid-resolved roughness simulation obtained with the SA model is divided by the smooth wall simulation also computed with the SA model (instead of Cebeci-Smith), therefore capturing the impact of only roughness and not the turbulence model. Note that the larger surface area of the GRR simulation is accounted for in the roughness augmentation factor. This results in an increase of roughly 1.05x in the augmentation factor.

4.7.2 Establishing GRR Validity With 3MDCP Mach 6 Measurements

The present application of a Reynolds Averaged Navier-Stokes (RANS) flowfield solver with a one equation turbulence model is questionable for capturing the impact of roughness on the heating in a turbulent boundary layer. While limited parallel efforts to reproduce the present simulations with both Direct Numerical Simulations (DNS) and Large Eddy Simulations (LES) are being pursued, their applicability for a 19 species reacting gas simulation at RLVP peak heating conditions has yet to be established. In the absence of these higher fidelity simulations, some level of validation for the present RANS approach may be achievable through comparisons with experimental data. This validation is pursued in the present subsection using

the 3MDCP Mach 6 measurements presented in Section 4.3.

The GRR approach discussed in the previous subsection was applied to the $2\text{e}+6$ 1/ft case with a roughness height of 0.38 mm (0.015 in) and the nominal P/k of 3.0, which results in RLVP-relevant k^+ values around 55. This represents the largest roughness height considered, which for the same P/k , results in the largest peak to valley spacing. Figure 75 compares the resulting averaged heat transfer coefficient obtained from the GRR simulation with the measured values for both pattern orientations 1 and 2. The smooth wall simulation and result of the baseline Dahm roughness model are also presented in this figure for reference. Note that the measured values for orientation 1 show that the flow remains transitional along the flank, which degrades the comparison with the simulations slightly. However, considering only the measurement and GRR results beyond a radial distance of 5.5 cm, where the flow is closer to fully turbulent, shows agreement within 5% for both orientations.

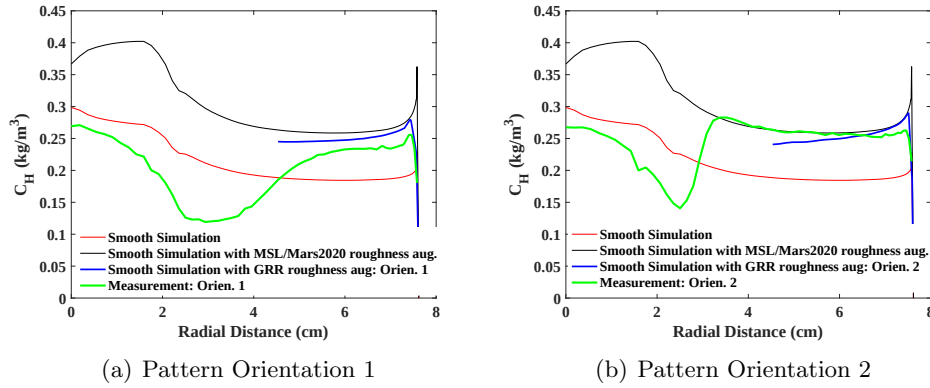


Figure 75: Comparison between 3MDCP Mach 6 measurements and grid-resolved roughness simulations at $2\text{e}+6$ 1/ft and $k=0.38$ mm (0.015 in).

Considering now a higher Reynolds number case at $3\text{e}+6$ 1/ft but with a smaller roughness height of 0.24 mm (0.010 in) (P/k of 3.0) to maintain an RLVP-relevant k^+ value of around 55. Figure 76 presents the resulting comparison between the measurements and GRR simulations. For both orientations, the GRR simulations provide excellent agreement with the measurements.

The excellent agreement between the GRR simulations and measurements presented in this subsection provides a level of validation for the present GRR approach, which will be applied in the next subsection to RLVP flight cases. The apparent success of the present RANS modeling is perhaps a result of the relative smooth topology of the current patterned roughness, where the flow remains attached except for a small region in the valley of each roughness element (this will be shown in the following discussion for the RLVP flight cases). The attached flow that produces the high heating on the peak of each element, which dominates the averaged heating, is appropriate for the RANS modeling. This would not be the case, for example, for spherical roughness elements that produce vortices off the peak of each element that interact with the downstream flow.

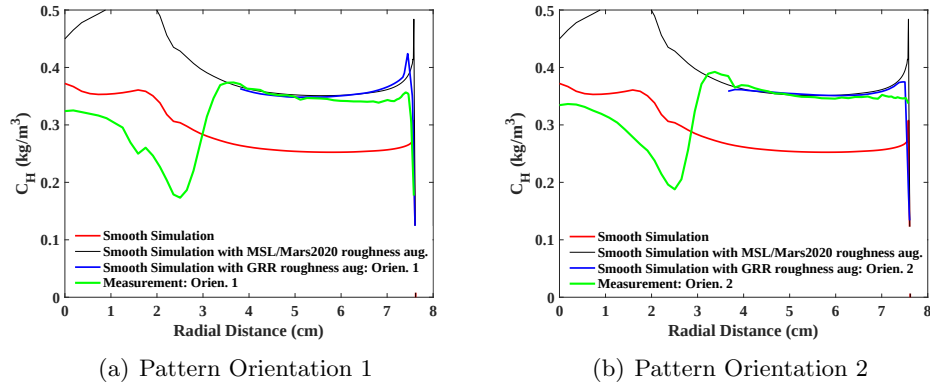


Figure 76: Comparison between EES Mach 6 measurements and grid-resolved roughness simulations at $3e+6$ $1/ft$ and $k=0.24$ mm (0.010 in).

4.7.3 GRR Simulations for RLVP

The velocities of over 8 km/s for these RLVP flight cases are beyond the limits of turbulent convective heating from ground test facilities, meaning the GRR results provide the best available insight into the impact of patterned roughness on the convective heating at these conditions.

The first case considered is the RLVP at 73 s, with the resulting Mach number and pressure contours along a slice through the center of the roughness elements presented in Figs. 77 and 78. Figure 77 shows that the supersonic region is pushed above the roughness elements, so that the supersonic region does not directly interact with the roughness. However, Fig. 78 does show noticeable pressure waves emanating from each roughness element into the supersonic region of the flow.

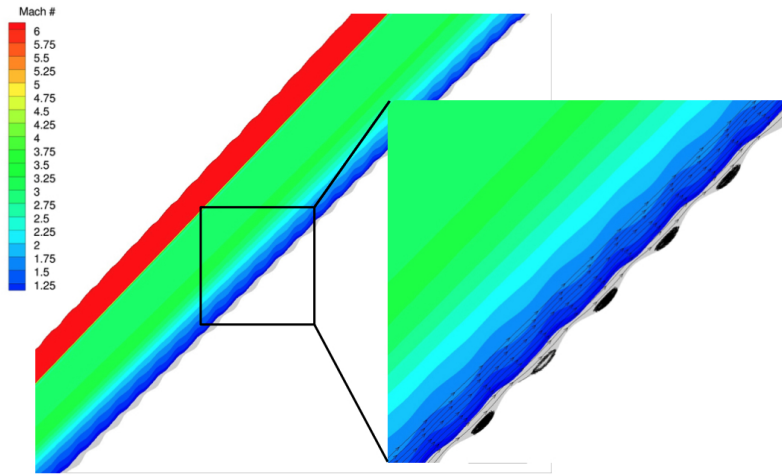


Figure 77: Mach contours resulting from grid-resolved roughness simulations (color contour is cut off below Mach 1).

The convective heating resulting from this simulation is presented in Fig. 79, which shows the higher heating on the peaks of each roughness element and the re-

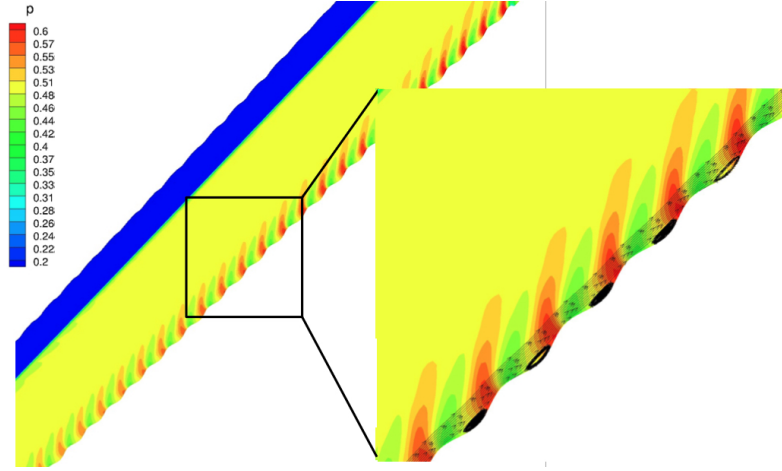


Figure 78: Pressure contours resulting from grid-resolved roughness simulations.

duced heating in the valleys. The resulting surface averaged heating, which accounts for the increased surface area for the rough surface, is presented as the red dashed line in Fig. 80. This figure compares this result with the simple Dahm correlation used to provide the nominal RLVP environments. Surprisingly, unlike the Mach 6 cases, the GRR simulation predicts a negligible level of roughness augmentation. This negligible roughness augmentation is assumed to be a result of the relatively low Reynolds number for this 73 s case, which leads to a more laminar-like flow, where laminar heating has been shown to have similarly low roughness augmentation factors. To check this assumption, the higher Reynolds number case at 80 s was simulated with the GRR approach. The resulting heating values are presented in Fig. 81, which shows 8 to 20% augmentation, although this is significantly lower than the nearly 40% augmentation resulting from the Dahm model. These GRR simulations suggest that the Dahm model is conservative at RLVP conditions. Although the Dahm model is still recommended for RLVP until further validation of the simulations is available, this valuable insight prevents further margin to be added to cover uncertainties in the roughness augmentation model.

4.8 Summary of Turbulence and Roughness Measurement Comparisons

The previous subsections compared the smooth and rough wall turbulent simulations with the best available measurements. The Cebeci-Smith model is shown to provide good agreement with smooth wall measurements for the Wilder and Prabhu, CUBRC, and AEDC measurements. This provides the justification for using the Cebeci-Smith turbulence model as the standard for the RLVP database. The smooth wall comparisons, however, do not feed into the margin analysis since the rough wall turbulent environments are ultimately what will be encountered by RLVP and used for design. Figure 82 presents a summary of these roughness augmentation factors for all the cases considered in this analysis. These measurements all consider patterned roughness surfaces similar to that expected for RLVP. The

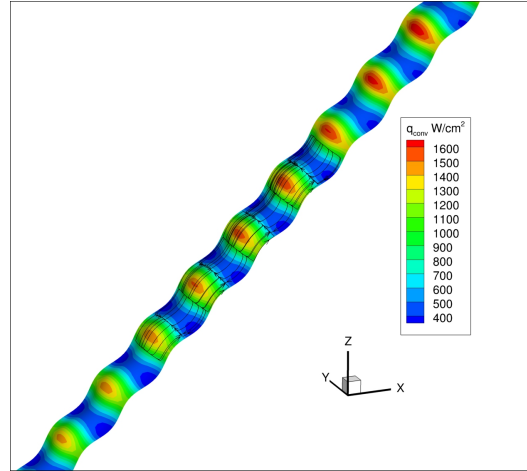


Figure 79: Convective heating contours resulting from grid-resolved roughness simulations.

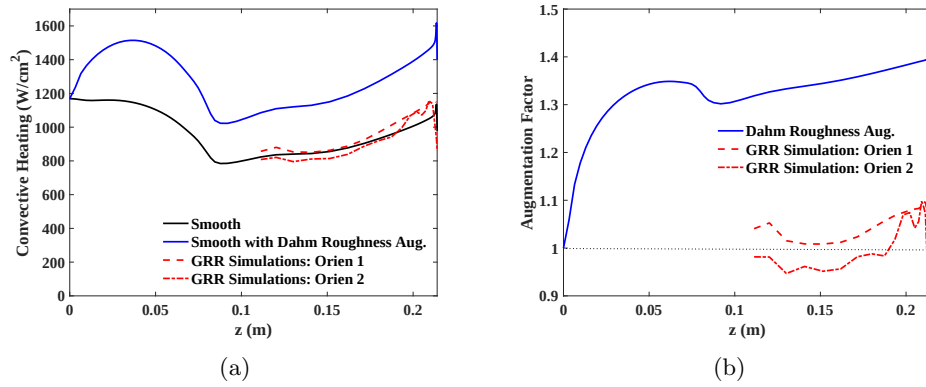


Figure 80: Surface averaged convective heating values for the 73 s case.

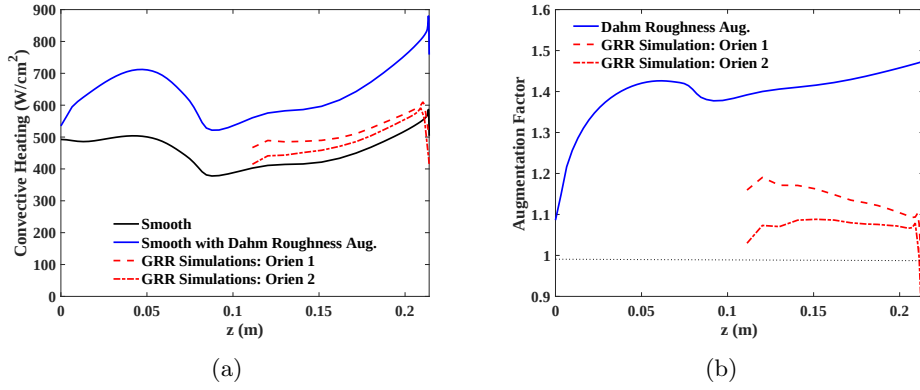


Figure 81: Surface averaged convective heating values for the 80 s case.

Dahm correlation, which is used as the baseline for RLVP, provides the best comparison with the measurements, especially for the RLVP-specific Mach 6 test, which is the most RLVP relevant. The data scatter above the Dahm correlation is captured in the margin analysis presented in Section 6, and discussed through the preceding sections in the assessment of the model form uncertainty.

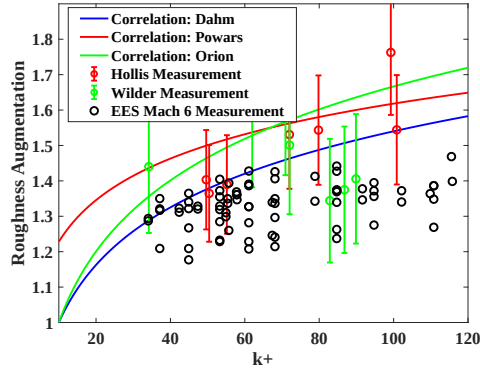


Figure 82: Summary of roughness heating ratios from all considered measurements.

5 Impact of Ablation on Heating

The aerothermal analysis provided so far in this report focuses on the radiative and non-ablating convective heating. These heating values are used by the material response code to determine the thickness of the ablator required to maintain the bondline temperature below a specified limit. (These heating values may also be used to check that they are below the tested heat flux limit of the heatshield material.) Although the material response analysis and ablator sizing is beyond the scope of the aerothermal analysis documented in this report, two major assumptions are made within the material response code to account for the impact of ablation on the aerothermal environment and heating values. The impacts of these assumptions, which will be defined below, are assessed in this section. Note that the convective and radiative heating parametric uncertainties are computed for the coupled ablation simulations (with finite-rate surface properties) presented in this section.

This section is organized as follows. In subsection 5.1, the approximations applied within the material response code to account for the impact of ablation on the convective heating and surface energy balance are presented. Note that in this discussion, the term ablation is used to indicate the injection of both char and pyrolysis gases from the surface into the flowfield. Subsection 5.2 then applies the so-called coupled ablation approach, which accounts for the blowing wall and ablation species in the flowfield simulation, and compares it with the approximate, or uncoupled, approach described in subsection 5.1,. Subsection 5.3 pushes this coupled ablation simulation further by removing the chemical equilibrium assumption at the surface, which provides a higher fidelity simulation of the char rate. Subsection 5.4 presents the impact of ablation on the radiative heating. To provide experimental validation of the uncoupled design approach and these coupled simulations, subsection 5.5 considers the Orion-sponsored CUBRC measurements that include both blowing and roughness. Finally, subsection 5.6 discusses the potential for cross-hatching patterns to form on the heatshield surface due to differential recession.

5.1 Approximations for Modeling Impact of Ablation on Aerothermal Environment

The aerothermal assumptions made within the material response code are based on writing the non-ablating convective heating ($q_{c,0}$) in terms of the heat transfer coefficient:

$$C_{H,0} = q_{c,0}/(H_T - h_{w,0}) \quad (12)$$

where H_T is the total enthalpy and $h_{w,0}$ is the wall enthalpy for the non-ablating simulation, which is provided by the aerothermal database. The impact of a given char mass flux, $\dot{m}_c$, and pyrolysis gas mass flux, $\dot{m}_g$, on $C_{H,0}$ and the elemental composition at the wall (which defines the wall species assuming chemical equilibrium) is assessed through the following two approximations:

1. *Approximation #1*: The material response code accounts for the influence of

$\dot{m}_c$ and $\dot{m}_g$ on the heat-transfer coefficient as follows:

$$C_H = C_{H,0} \frac{2\lambda B'_0}{\exp(2\lambda B'_0) - 1} \quad (13)$$

where $C_{H,0}$ is the non-abating heat-transfer coefficient, λ is assumed equal to 0.3 for RLVP, and $B'_0 = (\dot{m}_c + \dot{m}_g)/C_{H,0}$. This approximation has been assessed by Thompson and Gnoffo [115] and Martinelli et al. [116] for perfect gas flows. To obtaining the convective heating with ablation from this C_H , the wall enthalpy that accounts for ablation species is required, which requires the next approximation.

2. Approximation #2:

To determine the wall enthalpy that accounts for the chemical species introduced by ablation, the mixing between the ablation species and boundary layer edge species must be approximated. Assuming equilibrium chemistry at the wall, only the mixing of the elements can be considered. The elemental diffusion mass flux at the surface is approximated as [117]

$$\tilde{J}_k = C_M(\tilde{c}_{w,k} - \tilde{c}_{e,k}) \quad (14)$$

where it is assumed that $C_M = C_H$. This approximation allows the elemental mass balance at the wall to be solved algebraically for the elemental mass fractions at the wall: [117]

$$\tilde{c}_{w,k} = \frac{\tilde{c}_{e,k} + B'_c \tilde{c}_{c,k} + B'_g \tilde{c}_{g,k}}{1 + B'_c + B'_g} \quad (15)$$

where $B'_g = \dot{m}_g/C_H$, $B'_c = \dot{m}_c/C_H$, $\tilde{c}_{e,k}$ is the elemental mass fraction of the boundary layer edge gas (for Venus: 0.263 C, 0.702 O and 0.035 N), $\tilde{c}_{c,k}$ is the elemental mass fraction of the char (1.0 C), and $\tilde{c}_{g,k}$ is the elemental mass fraction of the pyrolysis gas (0.44 C, 0.14 H, 0.41 O, and 0.01 N for 3MDCP). Note that the C_H values in the denominator of the B' definition apply Approximation #1 to convert $C_{H,0}$ to C_H . A discussion of this approximation is presented by Bianchi et al. [118] for graphite ablation.

After applying these approximations, the elemental mass fractions at the wall are converted to species mass fractions through an equilibrium computation at the wall temperature and pressure. More importantly, this equilibrium computation provides the ablating wall enthalpy h_w . The ablating convective heating can then be written using C_H from Approximation #1 and this value of h_w (from Approximation #2) as follows:

$$q_c = C_H(H_T - h_w) \quad (16)$$

This ablating convective heating value is applied within the material response code to solve the surface energy balance, which is written as:

$$q_c + \alpha q_{rad} - (\dot{m}_c + \dot{m}_g) h_w - \epsilon \sigma T_w^4 + \dot{m}_g h_g + \dot{m}_c h_c - q_{cond} = 0 \quad (17)$$

The terms in red are the quantities of interest in the present work, as they are obtained from either the aerothermal simulations or Approximations #1 and #2. As just discussed, q_c and h_w are obtained from $q_{c,0}$ and $h_{w,0}$ and the two approximations, while the radiative heating, q_{rad} is discussed in subsection 5.4. The $-\epsilon\sigma T_w^4$ term is the re-radiation from the ablator surface. The $\dot{m}_g h_g + \dot{m}_c h_c$ term is the enthalpy of solid char (h_c) and pyrolysis gas (h_g) entering the surface control volume, while the $-(\dot{m}_c + \dot{m}_g)h_w$ term is the enthalpy of material leaving the control volume. Finally, the heat conducted into the surface, q_{cond} , represents the inability of the previous terms from relieving the incoming convective and radiative heat fluxes. In summary, the material response code computes q_c , h_w , T_w , $\dot{m}_g$, h_g , $\dot{m}_c$, h_c , and q_{cond} for a given time history of q_{rad} , $q_{c,0}$, and $h_{w,0}$. For the RLVP nominal trajectory, these outputs from a material response code are presented in Fig. 83 for the stagnation point and near shoulder location. The input values $q_{c,0}$ and q_{rad} are also included in these figures for completeness. As required by Eq. (17), the sum of all the terms, excluding $q_{c,0}$, equals zero at each time point.

Figure 83 shows that the reduction in the convective heating due to ablation and the $-(\dot{m}_c + \dot{m}_g)h_w$ terms, which both depend on Approximations #1 and #2, are of the same order-of-magnitude as the re-radiation term, and are therefore significant components of the surface energy balance. The goal of this section is to assess the accuracy of Approximations #1 and #2 for converting $q_{c,0}$ and $h_{w,0}$ to q_c and h_w . This assessment is made by modeling the specified $\dot{m}_c$ and $\dot{m}_g$ rates within the Navier-Stokes flowfield model, as discussed in the next subsection.

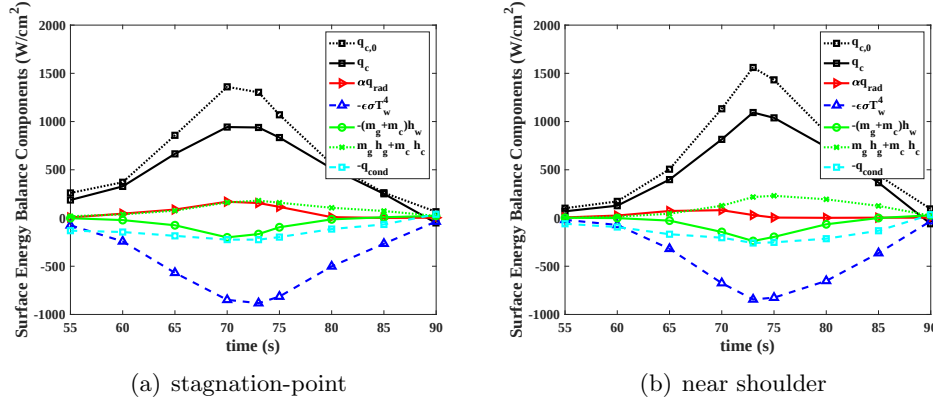


Figure 83: Comparison of surface energy balance terms for RLVP.

5.2 Coupled Ablation RLVP Results for an Equilibrium Surface

Details of the coupled ablation approach used in this discussion are presented by Johnston et al. [9]. Given a T_w , $\dot{m}_g$, $\dot{m}_c$, and elemental composition of the pyrolysis gas, the coupled ablation capability in LAURA applies these surface boundary conditions within the flowfield simulation, therefore allowing Approximations #1 and #2 discussed in the previous subsection to be removed. For the present coupled simulations, the following 11 species were included in addition to 19 species presented

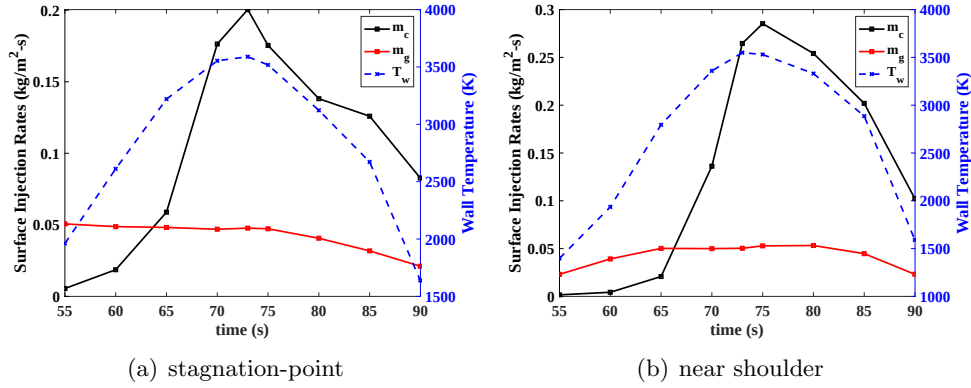


Figure 84: Specified injection rates and wall temperatures applied in coupled ablation analysis.

in Section 2.1: C_3 , H , H^+ , H_2 , CH , C_2H , C_2H_2 , CH_4 , CH_3 , CH_2 and HCN . This results in a total of 30 species in the flowfield, which are modeled using the rates presented in Section 2.2.2. The Krogstad modification to the turbulence model to account for roughness is applied by these coupled ablation simulations, which is required to capture the impact of applying the Dahm roughness augmentation factor to $C_{H,0}$ in the uncoupled approach. The Krogstad model was shown in Section 4 to provide good agreement with the Dahm roughness augmentation approach.

The coupled ablation analysis discussed in the previous paragraph was applied to cases between 60 and 90 s of the nominal RLVP trajectory presented in Table 1. The T_w , $\dot{m}_g$, and, $\dot{m}_c$ values from the uncoupled material response computation [119] are applied as fixed values throughout this analysis. Examples of these values at the stagnation point and near shoulder location are presented in Fig. 84. Note that the q_c , h_w , and wall species composition resulting from the coupled simulations no longer enforce the surface energy balance or char equilibrium constraint. A self-consistent coupled ablation analysis would therefore require further iterations with the material response code. However, this is beyond the scope of the present analysis, which seeks to confirm that Approximations #1 and #2, which represent the “uncoupled” approach, result in conservative q_c and h_w values relative to the coupled simulations.

5.2.1 Impact of Approximation #2

The first aspect of the uncoupled approach that may be evaluated from the coupled solutions is Approximation #2. The impact of this approximation can be isolated from #1 by evaluating Eq. (15) for the elemental composition at the wall using the C_H and B' values from the coupled simulation, and then comparing these with the actual elemental composition from the coupled simulation. The result of this analysis for the $t=73$ s is presented in Fig. 85, which compares the elemental composition at the wall between the coupled solution and the result of Eq. (15) using the B' values from this same solution. Agreement within 5% is seen along the entire forebody surface, which is considered favorable. This same comparison is made through

the trajectory at the near shoulder location in Fig 86, which also provides the resulting B' values applied for the uncoupled results. Agreement within 15% is seen throughout the trajectory, suggesting that Approximation #2 is adequate for RLVP. These results confirm that the elemental diffusion approximation of Eq. (15) is accurate for RLVP cases. The next step is to confirm the accuracy of the blowing correction of Eq. (13), which was bypassed in the current results by applying the C_H from the coupled simulation.

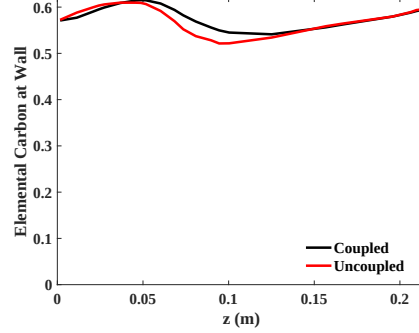


Figure 85: Impact of Approximation #2 on the elemental carbon at the wall for $t=73$ s.

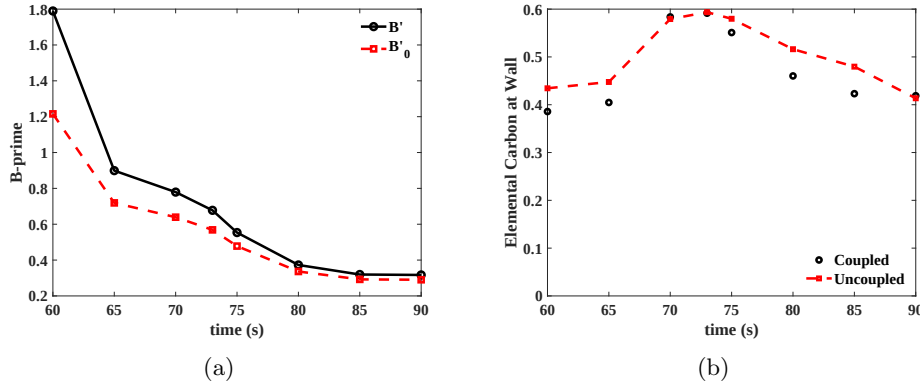


Figure 86: Impact of Approximation #2 on near-shoulder elemental carbon.

For reference, the species mass fractions through the boundary layer at the stagnation point and near shoulder location are presented in Figs. 87–89 for a range of cases. These figures demonstrate the complex chemistry and diffusion processes occurring in the near wall region, which Approximation #2 was shown to capture surprisingly well. Note that the nominal roughness height of 0.7 mm extends beyond the bounds of the horizontal axis on these figures, which focuses on the near wall region. Recall that these simulations include the Krogstad addition to the turbulence model to account for roughness.

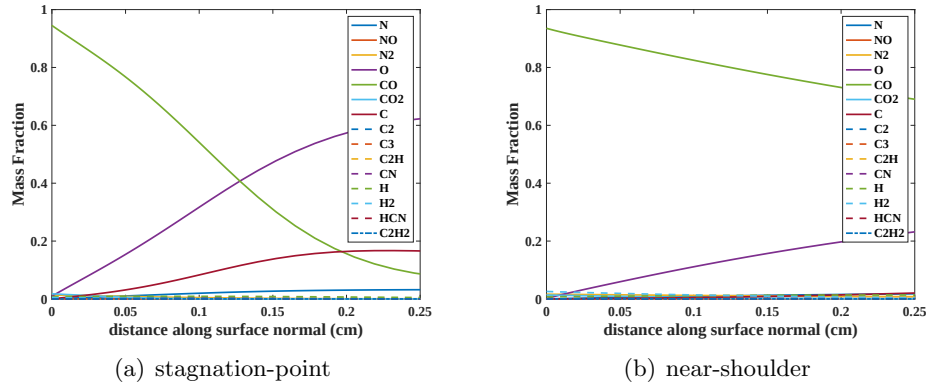


Figure 87: Boundary layer species for $t = 65$ s.

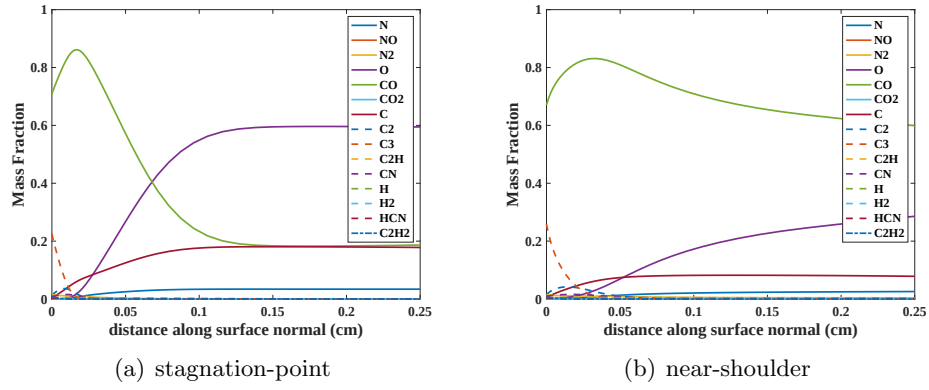


Figure 88: Boundary layer species for $t = 73$ s.

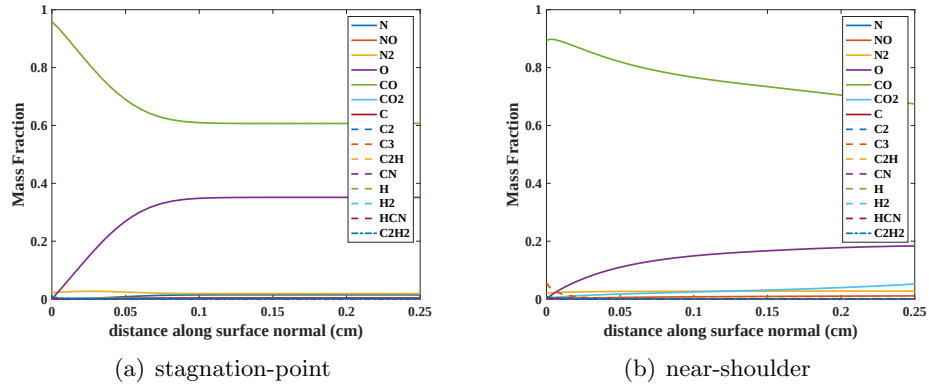


Figure 89: Boundary layer species for $t = 80$ s.

5.2.2 Impact of Approximation #1

Approximation #1 determines the decrease in the non-ablating heat transfer coefficient ($C_{H,0}$) due to ablation through Eq. (13). The present uncoupled results apply this equation using the same T_w , $\dot{m}_g$, and, $\dot{m}_c$ applied for the coupled analysis. Note that $C_{H,0}$ includes the impact of roughness augmentation, which is significant because it reduces the resulting B' value applied in Eq. (13). For consistency both the non-ablating and ablating simulations apply the Krogstad roughness augmentation model in these comparisons.

The resulting comparison between the coupled simulation and the uncoupled value resulting from Approximation #1 is shown in Fig. 90 for the $t=73$ s case. The non-ablating values are also shown for reference. This figure shows that the uncoupled approach does not predict the same level of heating reduction due to ablation as the coupled approach. This difference is seen throughout the trajectory for both the stagnation point and near shoulder location in Fig. 91. Note that some of the difference between the coupled and uncoupled values is likely due to the λ of 0.3 being applied for the uncoupled approach. Figure 92 presents the λ values required for the uncoupled C_H values to match the coupled results. This figure shows that mostly values over 0.8 are needed to reproduce the coupled results.

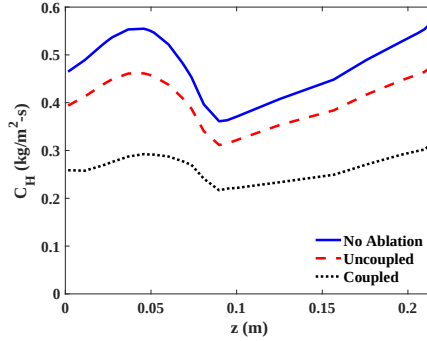


Figure 90: Impact of Approximation #1 on C_H for $t=73$ s (case 5a).

5.2.3 Combined Impact of Both Approximations

Combining the C_H resulting from Approximation #1 with the elemental mass balance of Approximation #2, allows the uncoupled values for the elemental composition at the wall and h_w to be computed. Fig. 93 compares the uncoupled values for the elemental mass fraction of carbon with the result of the coupled simulations. Differences of up to 20% in the elemental mass fraction of carbon are seen at the stagnation point and near shoulder locations considered in these figures. These differences are slightly larger than those shown in Fig. 86, because that comparison applied the C_H value from the coupled solution to isolate the impact of Approximation #2, while this comparison applies the C_H from Approximation #1 (these differences would be larger except for the fortuitous cancellation of errors between the two approximations away from peak heating). This confirms that the inaccuracies

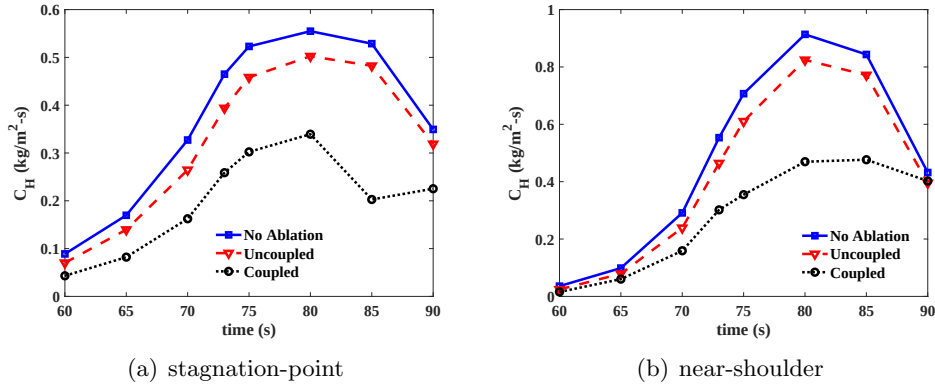


Figure 91: Impact of Approximation #1 on C_H along the trajectory.

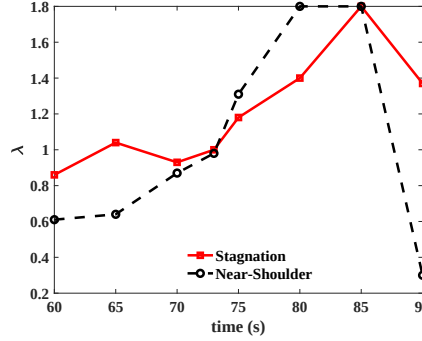
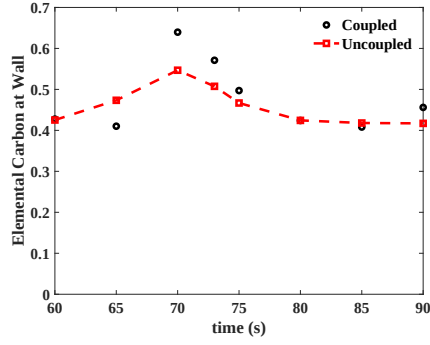


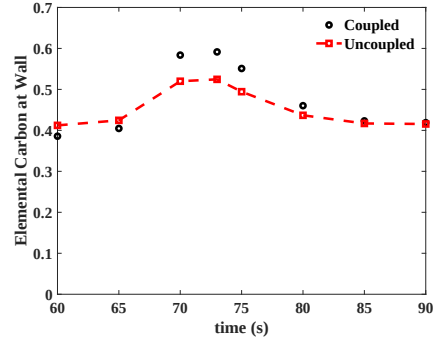
Figure 92: Values of λ required for coupled simulations to match uncoupled.

in Approximation #1 lead to the disagreement shown in Fig. 93. The consequence of this difference in the elemental composition is a difference in the resulting h_w , which is based on an equilibrium chemistry composition at the wall elemental composition, temperature, and pressure. Figure 94 compares the h_w resulting from the uncoupled approach with the results of the coupled analysis. Note that the same pressures and temperatures are applied to the uncoupled and coupled analysis, with the only difference being the elemental composition. Note that h_w influences the surface energy balance (Eq. (17)) through both q_c and the $-(\dot{m}_c + \dot{m}_g)h_w$ term. The impact on this latter term is presented in Fig. 95, which results in differences of less than 100 W/cm².

Combining Fig. 91 with Fig. 96 results in the convective heating comparison presented in Fig. 97. This comparison shows that the uncoupled approach is conservative for both the stagnation-point and near shoulder throughout the trajectory. Figure 98 presents this comparison along the surface for a single case near peak heating at $t=73$ s, which shows similar disagreement over the entire surface. Comparison of the integrated heat load along the surface is presented in Fig. 99, which presents the coupled convective heat load divided by the uncoupled convective heat load. The coupled convective heat load is seen to be around 40% lower than the

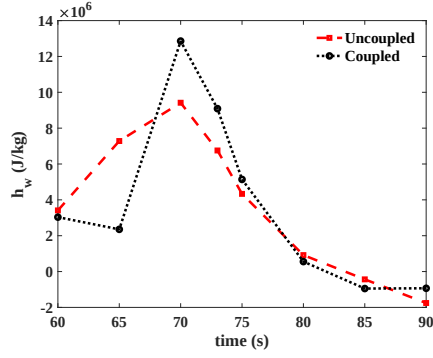


(a) stagnation-point

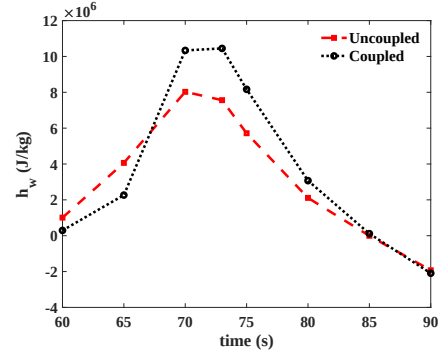


(b) near-shoulder

Figure 93: Impact of both approximations on the elemental carbon at the wall.

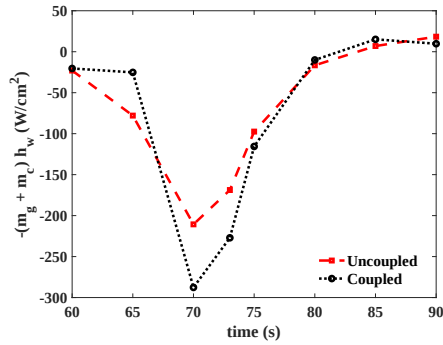


(a) stagnation-point

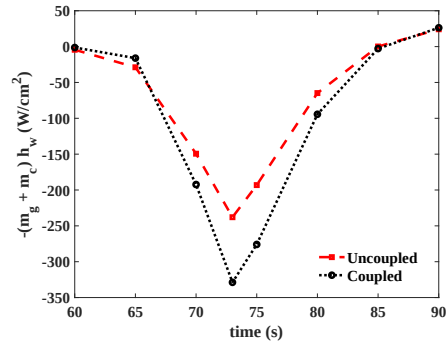


(b) near-shoulder

Figure 94: Impact of both approximations on h_w through the nominal RLVP trajectory.



(a) stagnation-point



(b) near-shoulder

Figure 95: Impact of both approximations on the $-(\dot{m}_c + \dot{m}_g)h_w$ term through the nominal RLVP trajectory.

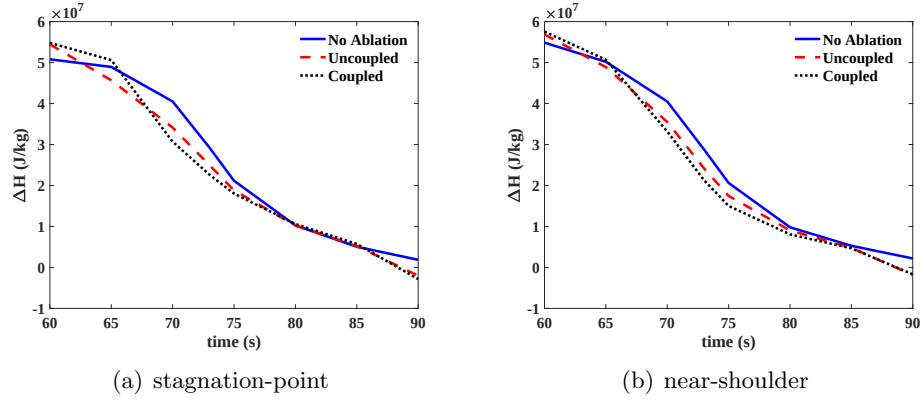


Figure 96: Impact of both approximations on $H_T - h_w$ through the nominal RLVP trajectory.

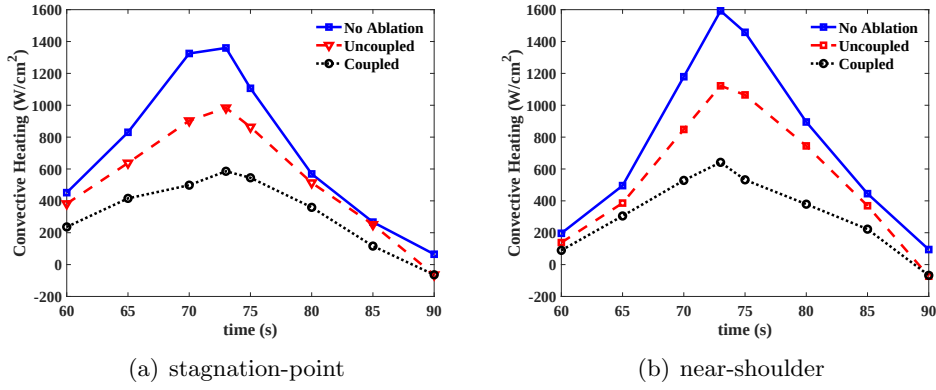


Figure 97: Comparison between uncoupled and coupled convective heating along the nominal RLVP trajectory.

uncoupled value. This suggests that the uncoupled approach contains a significant level of conservatism for the TPS sizing for RLVP, assuming equilibrium ablation. The next subsection will show that this equilibrium ablation model, even when coupled to the flowfield, misses the potential impact of hydrogen catalysis, which may drive the heating above the uncoupled predictions.

5.3 Coupled Ablation Approach for a Finite-Rate Surface

For equilibrium ablation, the solution of the species mass balance at the surface reduces to solving for the elemental mass balance, which was discussed in the previous subsection. The present subsection removes the equilibrium chemistry constraint at the surface by treating the individual finite-rate surface reactions, which provide the individual species mass fluxes due to surface reactions ($\dot{m}_{c,i}$). Expressions for $\dot{m}_{c,i}$ are provided in the following discussion. With these expressions, the species mass

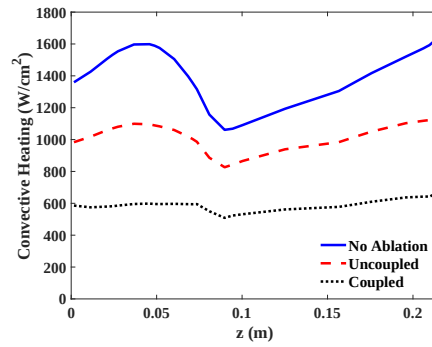


Figure 98: Comparison between uncoupled and coupled convective heating along the surface for $t=73$ s.

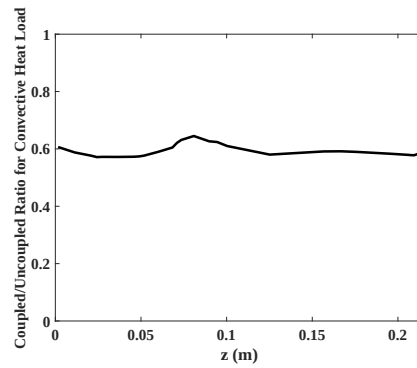


Figure 99: Ratio of coupled to uncoupled convective heat load along the surface assuming equilibrium ablation.

balance at the surface is written as:

$$J_i + \left(\sum_j \dot{m}_{c,j} + \dot{m}_g \right) c_{w,i} - \dot{m}_{c,i} - \dot{m}_g c_{g,i} = 0 \quad (18)$$

where $\dot{m}_g$ represents the pyrolysis rate and $c_{g,i}$ are the species composition of the pyrolysis gases. The $c_{g,i}$ values are computed assuming chemical equilibrium, for the given elemental composition of the pyrolysis gas, at the surface temperature and pressure.

Recent advancements in finite-rate surface recession models provide the opportunity for enhanced simulation fidelity of an ablating heatshield's aerothermodynamic environment. Application of a finite-rate surface recession model, instead of an equilibrium ablation model, allows processes such as oxidation, sublimation, and surface catalysis to be modeled in detail, instead of relying on the assumption of chemical equilibrium at the surface elemental composition, pressure, and temperature. The rate model compiled for this work, and presented in the following subsections, is based on values proposed by Park et al. [68] and Driver and Maclean [19], with additional sublimation reactions proposed by Keenan and Candler [120] and the addition of H catalysis. The choice of these rates is intended to provide a model that is applicable over a wide range of conditions, ranging from the low-temperature rate-limited oxidation regime to the high-temperature sublimation regime.

5.3.1 Oxidation

The forward rate for the primary carbon oxidation reaction, $O + C(s) \rightarrow CO$, is written as:

$$k_{f,O} = \gamma_O \sqrt{\frac{RT}{2\pi M_O}} \quad (19)$$

where $\gamma_O = 0.63e^{-1160/T}$ as suggested by Park et al. [68]. For the secondary oxidation reaction, $O_2 + 2C(s) \rightarrow 2CO$, the forward rate is written as:

$$k_{f,O_2} = \gamma_{O_2} \sqrt{\frac{RT}{2\pi M_{O_2}}} \quad (20)$$

where $\gamma_O = (1.43 \times 10^{-3} + 0.01e^{-1450/T}) / (1 + 2.0 \times 10^{-4}e^{13000/T})$ as proposed by Park et al [68]. Note that the temperature dependence of this term results in rate-limited oxidation at low temperatures, whereas if a constant is applied, such as the value of 0.01 suggested by Driver and Maclean [19], significantly larger oxidation values are obtained at low temperatures. The resulting species mass fluxes ($\text{kg}/\text{m}^2/\text{s}$) resulting from these processes are written as follows:

$$\dot{m}_{CO} = \frac{M_{CO}}{M_O} k_{f,O} \rho_O + 2 \frac{M_{CO}}{M_{O_2}} k_{f,O_2} \rho_{O_2} \quad (21)$$

$$\dot{m}_O = -k_{f,O} \rho_O \quad (22)$$

$$\dot{m}_{O_2} = -k_{f,O_2} \rho_{O_2} \quad (23)$$

where ρ_i are the species densities (kg/m^3) and M_i are the species molecular weights (kg/mol).

Table 16: Coefficients for sublimation rate equations.

C_x	α_x	A_x	n_x	E_x
C	0.14	1.486e+12	-1.487	87,110
C ₂	0.26	1.8399e+17	-2.318	101,715
C ₃	0.03	4.3197e+22	-3.459	103,339
C ₅	0.015	2.6559e+15	-1.266	117,049

5.3.2 Sublimation

To accommodate the surface temperatures of up to 4000 K that may be encountered at Venus conditions, sublimation of C, C₂, C₃, and C₅ are treated similarly to the work of Keenan and Candler [120] that was done for the Mars-return condition.. The rate of sublimation for these molecules, expressed as $x\text{C(s)} \rightarrow \text{C}_x$, are written as

$$\dot{m}_{C_x} = \alpha_{C_x} (\rho_{E,C_x} - \rho_{C_x}) \sqrt{\frac{RT}{2\pi M_{C_x}}} \quad (24)$$

where $\rho_{E,C_x} = A_x T^{n_x} \exp(-E_x/T)$ is the equilibrium vapor pressure (N/m²). The coefficients for ρ_{E,C_x} are computed from Gordon and McBride [121] data and are listed in Table 16. The α_{C_x} values are taken from Keenan and Candler [120].

5.3.3 Nitridation and N₂ Recombination

The rate of nitridation, $\text{N} + \text{C(s)} \rightarrow \text{CN}$, is written as

$$k_{f,N} = \gamma_N \sqrt{\frac{RT}{2\pi M_N}} \quad (25)$$

where the value of γ_N is set to 0.001 as derived by Driver and Maclean [19], which is significantly smaller than the 0.3 value proposed by Park et al [68]. For the N₂ recombination process, $\text{N} + \text{N(s)} \rightarrow \text{N}_2$, the forward rate is written as:

$$k_{f,Nr} = \gamma_{Nr} \sqrt{\frac{RT}{2\pi M_N}} \quad (26)$$

where the value of γ_{Nr} is set to 0.05 as derived by Driver and Maclean [19]. The mass fluxes resulting from the nitridation and recombination processes are written as follow:

$$\dot{m}_{CN} = \frac{M_{CN}}{M_N} k_{f,N} \rho_N \quad (27)$$

$$\dot{m}_N = -(k_{f,N} + k_{f,Nr}) \rho_N \quad (28)$$

$$\dot{m}_{N2} = k_{f,Nr} \rho_N \quad (29)$$

5.3.4 H₂ Recombination

The presence of elemental H in the pyrolysis gas requires that the potential for homogeneous recombination of H to H₂ occurs at the surface, analogously to N recombining to N₂. Note that C and O recombination are not modeled because these species participate in the sublimation and oxidation processes, respectively, which makes the impact of homogeneous recombination negligible. Past studies have not considered the impact of H recombination on the aerothermodynamic environment. This is due mainly to most past finite rate analyses considering a non-charring ablator, such as graphite, which avoids the presence of elemental H. The present analysis shows that, based on the best estimate for the H recombination efficiency, the recombination of H provides a notable increase in the convective heating.

Analogously to the N₂ recombination process, the H₂ recombination rate is written as:

$$k_{f,H} = \gamma_H \sqrt{\frac{RT}{2\pi M_H}} \quad (30)$$

which is related to the mass flux of H and H₂ as follows:

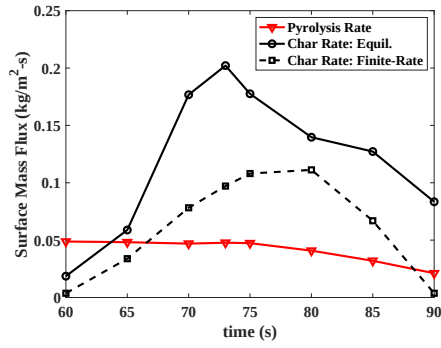
$$\dot{m}_H = -k_{f,H}\rho_H \quad (31)$$

$$\dot{m}_{H_2} = k_{f,H}\rho_H \quad (32)$$

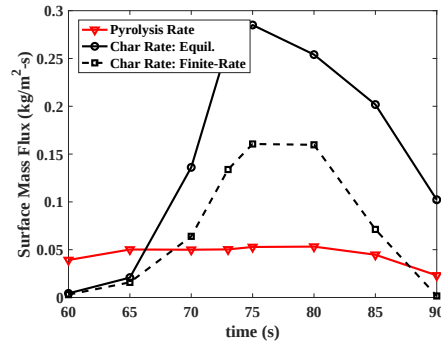
The primary challenge in modeling this recombination process is the estimation of the H recombination efficiency, γ_H . The assessment of this value for wall temperatures above 2500 K and a carbon surface, which would make it appropriate for RLVP, is not available. The most relevant values are presented by Kim and Boudart [20] and Mackay et al. [21], which estimate a value of roughly 0.2 at temperatures above 2000 K, although a silica surface is assumed. This value is consistent with the previous measurements by Wood and Wise [122] on a range of materials, such as copper, aluminum, titanium, and nickel, which implied values greater than 0.1 at temperatures greater than 1000 K. Based on these past studies, a value of γ_H equal to 0.2 is assumed for the present work.

5.3.5 Application of the Finite-Rate Surface Model to RLVP

Application of this char rate model to the RLVP case, with the pyrolysis rate and wall temperatures fixed to the values used in the equilibrium analysis, results in the char rates presented in Fig. 100. These finite-rate char values are seen to be significantly lower than the previously considered equilibrium char rates. The contribution of each finite rate process to the char rate is presented in Fig. 101. This figure shows that the peak heating region of the trajectory is dominated by oxidation and C₃ sublimation, while later in the trajectory oxidation alone dominates. The slight negative contribution resulting from C sublimation implies deposition of C back to the char surface. This negative rate is due to the abundance of C at the wall, which causes Eq. (24) to become negative. Note that the analysis outlined by Candler [123] for determining the equivalent surface mass flux contributions for equilibrium ablation shows the same negative mass flux for C as the finite rate model. Therefore, this behavior is not specific to the finite rate model.

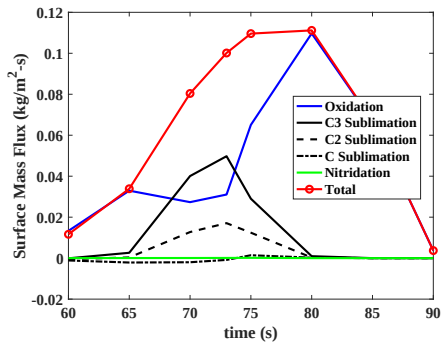


(a) stagnation-point

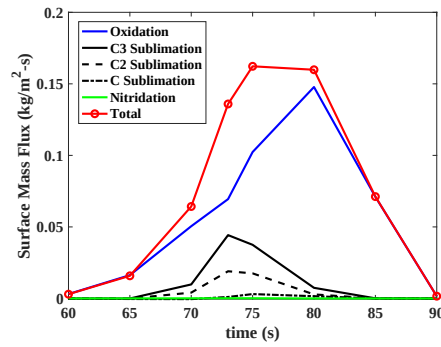


(b) near-shoulder

Figure 100: Comparison between char rates resulting from equilibrium and finite-rate surface models.



(a) stagnation-point



(b) near-shoulder

Figure 101: Finite-rate char rate contributions from individual processes.

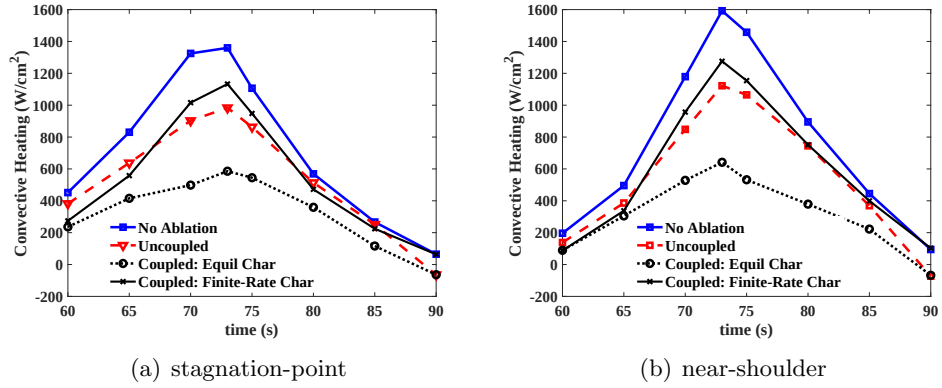


Figure 102: Comparison between convective heating resulting from equilibrium and finite-rate surface models.

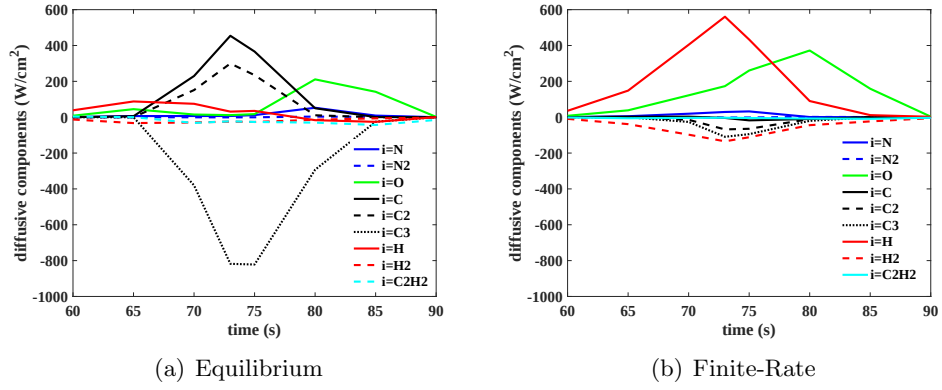


Figure 103: Comparison between equilibrium and finite rate char models on diffusive convective heating components at the near shoulder.

The resulting heating for the finite-rate char case is compared in Fig. 102 with the equilibrium char result. The lower char rates for the finite rate model result in higher heating values, especially at the near shoulder. The significant change in the diffusive component of the near-shoulder heating between the equilibrium and finite-rate char results are presented in Fig. 103. In addition to a weaker negative contribution from C_3 , the finite rate result has strong contributions from H , which is a result of its catalytic recombination rate to H_2 . The equivalent catalycity is only modeled in the equilibrium result by the mass fraction of H resulting from the equilibrium chemistry computation at the given wall elemental composition.

A significant result of Fig. 102 is that the finite-rate char simulations result in a 10% increase in the heating above the uncoupled values, where the uncoupled approach applies the higher equilibrium char rates. This under-prediction of the uncoupled approach, assuming a λ of 0.3, will be captured in the margin assessment.

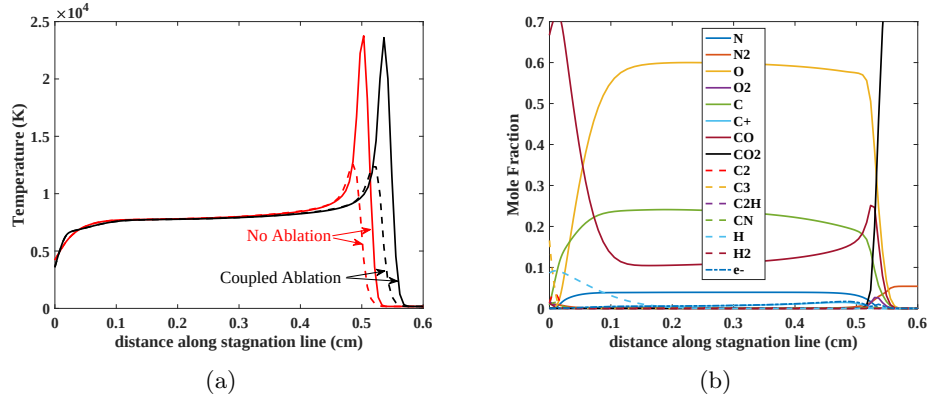


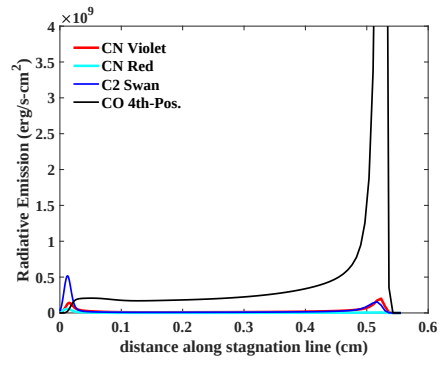
Figure 104: Impact of ablation on the RLVP stagnation line flowfield at 73 s.

5.4 Impact of Ablation on Forebody Radiative Heating

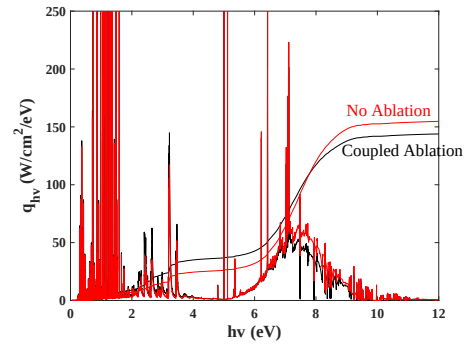
Both the equilibrium and finite-rate char simulations performed in the previous subsections included coupled radiation in addition to coupled ablation. These coupled radiation simulations included the radiative emission and absorption from ablation products. The ablation product radiation models used for these simulations have been presented by Johnston et al. [124,125]. These include molecular band systems from CN, CO, H₂, C₂, C₂H₂, among others, as well as atomic line and photoionization contributions from C and H. This subsection investigates the impact of these ablation products on the radiative heating. The curve fits for radiative heating presented in Section 6.4 include the impact of ablation products.

Figure 104 presents the stagnation line temperatures and dominant species mole fractions for the peak radiative heating case at 73 s. The impact of coupled ablation is seen to noticeably increase the shock standoff distance, which is the result of the mass flux from the wall due to ablation. The dominant ablation products at the wall are C₃ and H (CO is also a significant ablation product, although it is also a large component for the non-ablating cases). While CN and C₂ are limited to peaks below 0.05, this abundance is large enough for the strongly emitting CN Red and Violet and C₂ Swan bands to provide noticeable boundary layer emission. This is shown in Fig. 105(a), which presents the emission coefficient along the stagnation line from the strongest molecular band systems. The first three band systems listed in the legend emit below 6 eV, while the CO 4th-Positive band emits mostly above 6 eV. Figure 105(b) shows that ablation products increase the radiation below 6 eV, but then the increased absorption above 6 eV cancels this increase. Figure 105(c) shows this difference on the wall-directed radiative flux profile (these are tangent-slab values, so are roughly 15% larger than the ray-tracing radiative heating presented elsewhere). Overall, this shows that the impact of ablation on the stagnation region radiative heating is less than 10%.

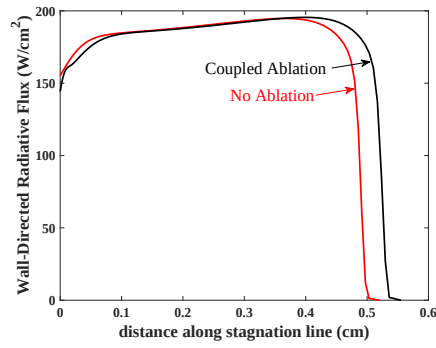
The same figures presented for the stagnation line are presented for the near shoulder location in Figs. 106 and 107 (again, these figures present tangent-slab radiation). While the impact of ablation on the stagnation point radiative heating



(a)



(b) tangent-slab



(c) tangent-slab

Figure 105: Impact of ablation on the RLVP stagnation line radiation at 73 s.

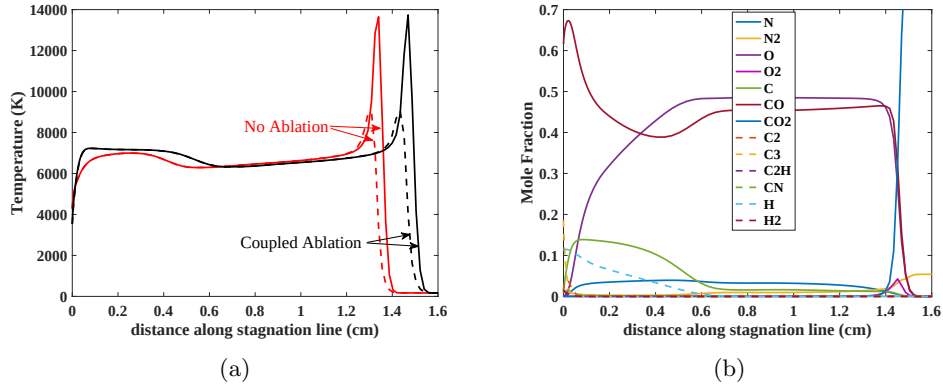


Figure 106: Impact of ablation on the RLVP near-shoulder flowfield at 73 s.

was shown to be small, these figures show that this conclusion does not hold in the near shoulder region of the vehicle. This is the result of both a thicker ablation layer (due to the impact of stagnation region ablation products flowing downstream), and due to the weaker emission from the inviscid and post-shock regions, which makes the relative impact of the ablation product emission larger. Figure 107(b) shows that CN Violet and Red emission provide over a factor of two increase in the radiative heating. This would be an extremely important result if the magnitude of the radiation was larger relative to the convective heating. However, the coupled ablation radiative heating of 63 W/cm^2 is less than 5% of the 1600 W/cm^2 non-ablating (or 1300 W/cm^2 for coupled finite-rate ablation) convective heating at the near-shoulder for this case.

The impact of ablation on the radiative heating (these are ray-tracing values) through the trajectory is summarized for the stagnation point and near shoulder location in Fig. 108. The significant impact of ablation on the radiative heating at the shoulder indicates that the impact of ablation on the radiation must be accounted for in the nominal aerothermal environments. Therefore, the aerothermal indicators presented in Section 6.4 combine the convective heating from non-ablating solutions (which are corrected for ablation in the material response code) and radiative heating from coupled ablation solutions (because the material response code does not include a correction for radiative heating).

5.5 Comparison with CUBRC Measurements with Roughness and Blowing

The only available ground test that captures the impact of blowing and roughness on a turbulent flow heating environment is the Orion-sponsored 69CH test in the CUBRC facility [126]. This test campaign utilized the experience of the 39CH test, which obtained turbulent heating measurements on a rough surface with no blowing. These 39CH measurements are considered in Section 4.1.2. In addition to including blowing, the 69CH measurements differ from the 39CH measurements in that the model diameter is 2 ft for the 69 CH test instead of 4 ft.

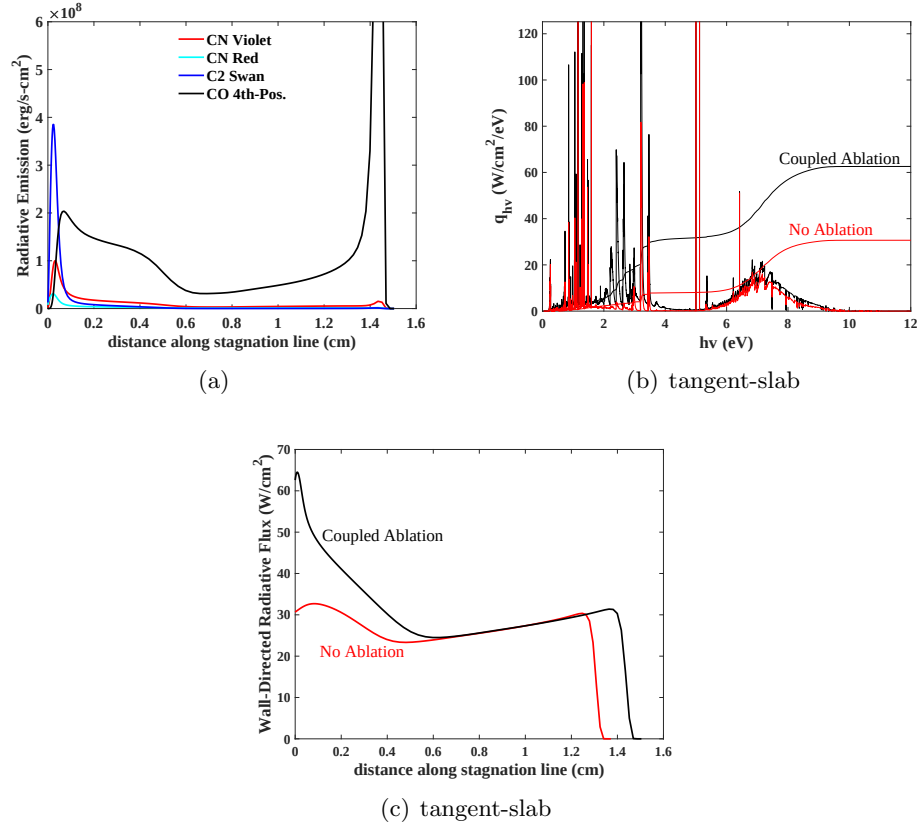


Figure 107: Impact of ablation on the RLVP near-shoulder radiation at 73 s.

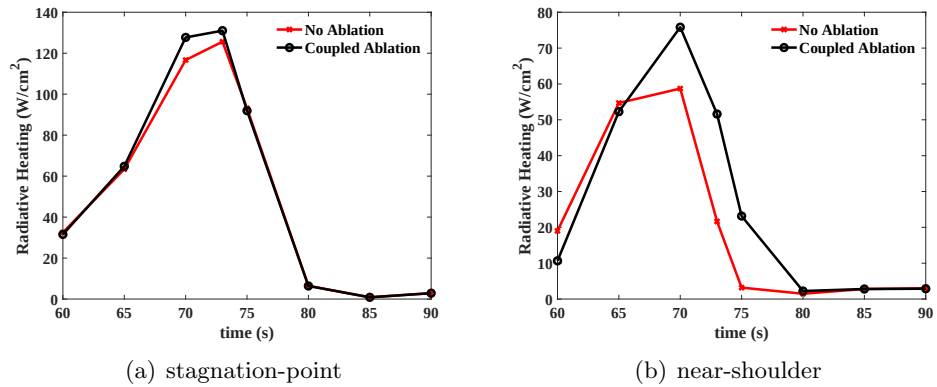


Figure 108: Impact of ablation on the RLVP radiative heating through the trajectory.

The present comparison focuses on a single free-stream condition, with a velocity of 1.59 km/s, density of 3.54×10^{-2} kg/m³, and temperature of 58.3 K. This results in a diameter-based Reynold's number of 9 million, which is used in the CUBRC reports. The angle of attack is 28 degrees, and the geometry is the Orion capsule.

The first comparison made with these measurements implements the nominal approach for accounting for roughness and blowing. In this approach, the non-blowing, smooth-wall convective heat-transfer coefficient is modified to account for roughness using Eq. (7), which is then modified to account for blowing using Eq. (13) at the specified blowing rate. The roughness height for these cases is 0.1 mm, which consists of closely spaced hemispherical elements. The blowing rate is essentially constant across the surface, which results in a spatially-varying B' based on the spatially-varying C_H . Cases with blowing rates of 0.10 and 0.26 kg/m³ are considered in this work. These blowing rates produce B' values between 0.5 and 2.0. These B' values capture the range relevant to RLVP near peak heating, which Fig. 86 shows range from 0.3 to 1.8 at the near shoulder location, and are roughly half these values at the stagnation point.

The resulting uncoupled ablation simulations are compared in Fig. 109(a) for the smooth wall measurements and Fig. 109(b) for the rough wall measurements. Considering first the smooth wall case, the non-blowing $B'=0$ comparison shows good agreement between measurements and simulations above 0.1 m, where the measurement is turbulent. The agreement in the stagnation region, which is not impacted by turbulence, is also excellent. As the blowing rate increases above zero, the smooth wall comparison above 0.1 m remains excellent, while the stagnation region comparison becomes worse. For the RLVP-relevant blowing rates of $B' = 0.6$ and 0.3 at $x = -0.2$ and 0.1 m, respectively, the stagnation region simulation is 20% below the measurement. Turning to the rough wall case in Fig. 109(b), the stagnation region simulations are similarly below the measurement by roughly 20%. For the downstream turbulent regions, however, the rough wall simulations over-predict the measurements. As the blowing rate increases, the level of over-prediction increases. As will be shown in the next paragraph, this increased disagreement with increased blowing is partially the result of applying $\lambda=0.3$ in the blowing correction (Eq. (13)).

The impact of λ on comparisons with measurements for the rough wall cases is presented in Fig. 110, which compares λ values of 0.3 and 0.5 for both blowing rate cases. While the $\lambda=0.5$ results compare better in the downstream region, they make the stagnation-point under-prediction even worse than the $\lambda=0.3$ results. This worse agreement is also seen for the coupled results presented in this figure, which model the blowing at the surface in the flowfield solution, and therefore remove the blowing correction and need to choose λ . Ignoring the stagnation region, both the measurements and coupled analysis support the choice of $\lambda=0.5$. However, the stagnation region comparison shows that the choice of even $\lambda=0.3$ is not conservative. This comparison requires that both $\lambda=0.3$ is applied for design, and that a margin of at least 20% is applied at the stagnation point to account for this under-prediction.

The 20% difference at the stagnation region between the nominal measurements and simulations is converted to a model form uncertainty component by considering the measurement uncertainty and parametric uncertainty. The measurement uncer-

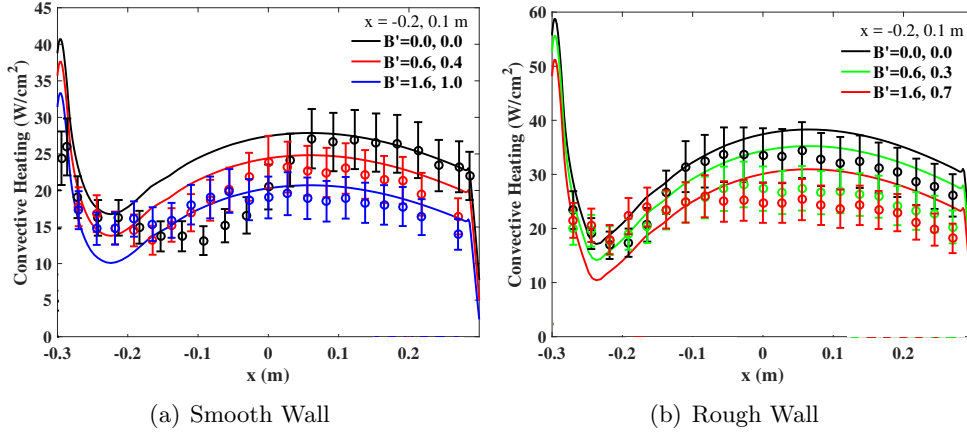


Figure 109: Comparison between CUBRC measurements and uncoupled blowing simulations.

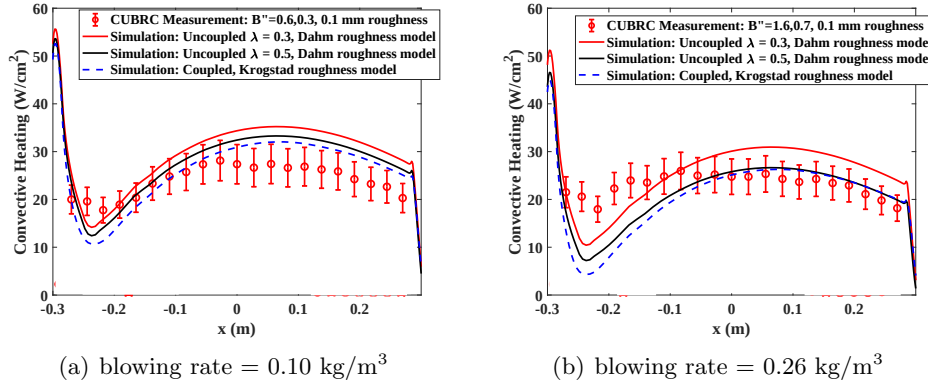


Figure 110: Impact of various blowing models for the rough wall case.

tainty reported by Holden et al. [126] is $\pm 10\%$. The parametric uncertainty for this case was obtained using the analysis presented in Section 2.2.2, which resulted in a parametric uncertainty of roughly 10% at the stagnation point. The measurement uncertainty and parametric uncertainty therefore cancel, resulting in a model form uncertainty component of 20% for the stagnation region. For the downstream turbulent region, the model over-predicts significantly, resulting in a negative model form uncertainty that will not impact the margin.

While the 20% stagnation point uncertainty resulting from these CUBRC comparisons would be overly-conservative if these measurements were not accurate in the stagnation region, note that the only other ground-based stagnation region heating measurements with blowing show similar under-prediction as blowing increases. These measurements made by Feldhuhn [127] on a sphere and Kaattari [128] on blunt nose are summarized by Thompson [115], who identifies this under-prediction at the nose. Park [129] argues the increased stagnation region heating seen in the measurements is due to the blowing producing a form of stagnation-region turbulence, which increases the heating. However, the model proposed by Park fails to

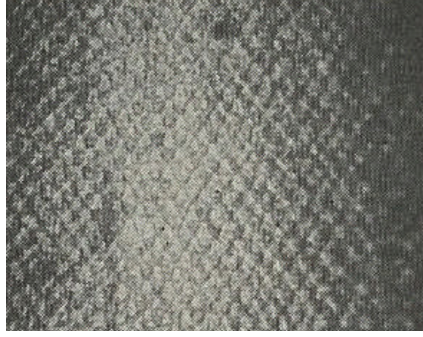


Figure 111: Example of a cross-hatching pattern from Canning et al. [131].

reproduce the Feldhuhn or Kaattari measurements, and degrades the comparisons away from the stagnation point.

5.6 Assessing the Potential of Cross-Hatching Patterns Forming on the RLVP Heatshield

This subsection assesses whether the formation of cross-hatching patterns should be anticipated to form on RLVP's 3MDCP surface. The consequences of such patterns are increased heating due to enhanced roughness augmentation (beyond that already considered) and changes to the aerodynamic forces, particularly increased roll torques [130]. If cross-hatching is expected, these consequences must be addressed in both the aerothermal and aerodynamic predictions used for design.

The formation of cross-hatching patterns has been observed on recovered ablating slender flight vehicles and on post-test ablating ground test models. An example of this cross-hatching is shown in Fig. 111, which was taken from Canning et al. [131]. This pattern was obtained in ballistic range tests for a model with a Lexan surface. As reviewed by White and Grabow [132], most ground-test measurements of cross-hatching were obtained on low temperature ablators such as Lexan and Teflon. For more RLVP-relevant graphite or carbon-phenolic materials, White and Grabow list a total of 3 tests, which were performed in arc-jet and rocket facilities. An example of a flight test that experienced cross-hatching is reported by Hochcrein and Wright [133] for a graphite vehicle. Note that cross-hatching has not been seen for any recovered NASA or JAXA vehicles, which will be discussed further in the following paragraphs.

A definitive explanation for when and how cross-hatching patterns form is not available in the open literature, although theories have been proposed [134–136]. Figure 112 presents Powars's explanation for cross-hatching. In step 1 of this process, multiple initial disturbances result in turbulent vortex pairs or a turbulent wedge that propagate downstream at the local Mach angle. These initial disturbance may result from surface imperfections or small discontinuities in recession resulting from non-uniform transition. In step 2, the adjacent vortices collide, which causes a new disturbance with another set of trailing vortices. The trailing vortices from each of these disturbances result in increased localized heating and recession. Although

standard RANS flowfield simulations with turbulence modeling are unable to capture these trailing vortices in the presence of turbulence, recent simulations [137] using direct numerical simulation (DNS), coupled to a material response code, have shown promise in simulating this phenomenon. However, these simulations remain infeasible for RLVP-relevant cases.

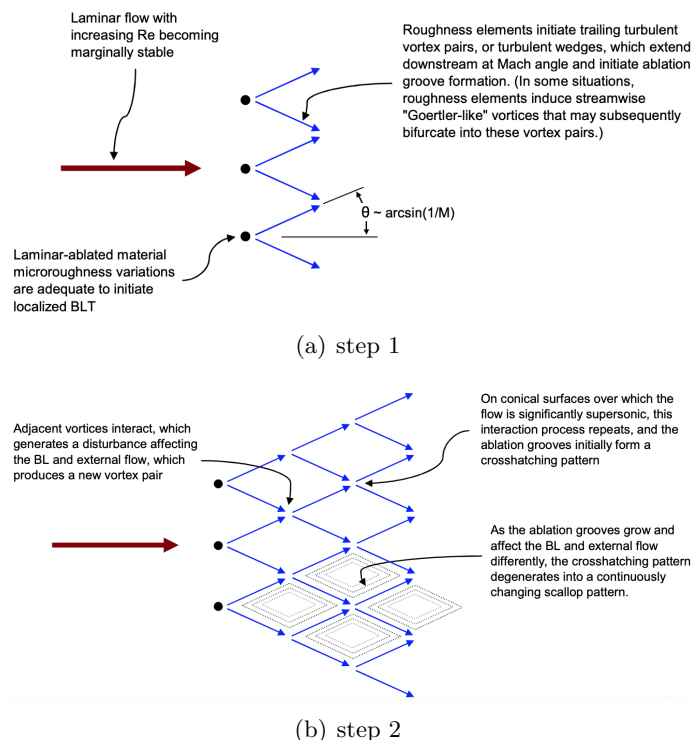


Figure 112: Mechanism for formation of cross-hatching presented by Powars [136].

Using the available measurements that experience cross-hatching, past researchers [138, 139] have identified three primary criteria required for the onset of cross-hatching. The first is that the boundary layer edge must be supersonic, the second is that the boundary layer must be transitional or turbulent, and the third is that the ablation rate or recession rate must be relatively significant (this last criterion is admittedly vague). Using these criteria, the lack of cross-hatching for Stardust [140, 141] or Hayabusa [142, 143] is explained by the laminar flow expected for both vehicles through the heat pulse. For Genesis [144, 145], the forebody TPS was not an ablator, so the last criterion was not met, and the flow remained laminar except behind 3 discrete cavities.

For RLVP, however, the boundary layer edge is both supersonic and turbulent. This leaves the vague last criteria of whether the ablation rate or recession is large enough as the deciding factor of whether cross-hatching should be expected for RLVP. Another complicating factor for RLVP is the woven structure of the 3MDCP heatshield, which results in a patterned roughness surface that is similar to a cross-hatched surface. Regarding the level of recession, the predicted nominal recession for RLVP at the shoulder is less than 0.8 cm. Instead of making the arbitrary

assessment of whether this recession is too small to enable cross-hatching, other factors are considered instead. The first is that the AEDC arc-jet measurements on a 3MDCP wedge model, discussed in Section 2, exposed 3MDCP to RLVP-relevant aerothermal conditions. These tests captured the turbulence and supersonic boundary layer edge, as well as RLVP-relevant levels of recession. Post-test scans of the 3MDCP surface did not show evidence of cross-hatching. This observation provides valuable evidence that cross-hatching will not occur for RLVP. Note that a similar measurement in the Langley 8 ft High-Temperature Tunnel was considered specifically to address this cross-hatching issue, since this facility has demonstrated cross-hatching in a past test [146]. However, the resulting heat flux in this facility is less than 30 W/cm^2 , which results in fundamentally different recession processes than the flight values of over 1500 W/cm^2 . This test was therefore not pursued.

Another factor that supports the conclusion that cross-hatching will be negligible for RLVP is the potential wavelength of the cross-hatching pattern. White and Grabow [132] provide a correlation for this wavelength as a function of the surface pressure, where higher pressures result in smaller wavelengths. Evaluating this correlation for carbon-phenolic at the peak RLVP surface pressure results in a wavelength of 6 mm. This value is close to the wavelength of the surface roughness pattern resulting from the weave, which has a wavelength of roughly 5 mm. This suggests that the cross-hatching mechanism described in Fig. 112 will be disturbed by the existing weave roughness pattern. Perhaps this does not allow the cross-hatching pattern to form, in which case it can be ignored, or the resulting cross-hatching pattern is indistinguishable from the weave roughness. If the latter is the case, the currently assumed roughness height of 0.7 mm is assessed from the post-arc jet samples, which would therefore include this cross-hatching influence. Finally, note that cross-hatching has never been measured on a sphere-cone geometry with a cone angle larger than 36 degrees, which is likely a result of the decreasing boundary layer edge Mach number with increased angle. This would make its presence on RLVP's 45 degree sphere-cone geometry unprecedented.

Based on these considerations, the impact of cross-hatching on the aerothermal and aerodynamic models for RLVP is assumed negligible.

6 Heatshield Margins

The approach adopted for the RLVP heatshield convective and radiative heating margin was presented in Section 2.2. This approach converts the margin evaluation into determining the model form uncertainty (based of experimental comparisons), f_{exp} , and parametric uncertainty component, f_{param} , which are described in Sections 2.2.1 and 2.2.2, respectively. As shown in Fig. 7, these components are summed to obtain the final margin (f_{total}):

$$f_{total} = f_{exp} + f_{param} \quad (33)$$

Recall from Section 2.2 that f_{exp} is evaluated as the difference between the upper measurement error bar and upper limit of the simulation's parametric uncertainty for the measurement case. This process removes the impact of parametric uncertainties, which may be different between the measurement and RLVP, from the measurement comparison, which isolates the model form uncertainty. This model form uncertainty is due to inadequacies in the model formulation and governing equations, whereas the parametric uncertainty is due to lack of knowledge of the modeling parameters that go into these equations.

Because the dominant heating mechanisms in the stagnation and flank regions are so different, a spatial variation of both f_{exp} and f_{param} are included in the analysis. While the spatial variation of f_{param} comes directly from the parametric uncertainty analysis, the spatial variation of f_{exp} is captured by evaluating the disagreement between measurements and simulations at the stagnation point (f_{stag}) and downstream of the stagnation point (f_{flank}), and then applying the distribution function presented in Fig. 113. This distribution function blends the stagnation point margin along the spherical nose cap to the constant value on the flank.

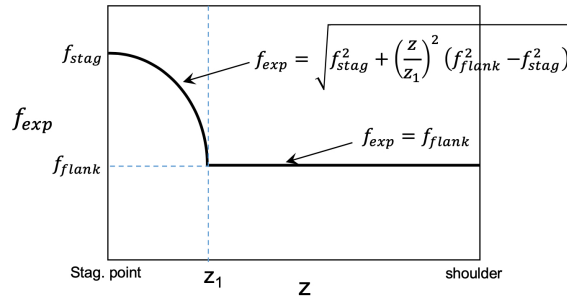


Figure 113: Surface distribution function for the measurement-based margin factor, f_{exp} .

In addition to being spatially-varying, this margin may vary between trajectory points because f_{param} is reevaluated for each trajectory point (while f_{exp} remains constant). This spatial and trajectory point variation of the margin is captured by including this varying margin in the margined heating indicators presented in Section 6.4. The following subsections provide details of these f_{param} and f_{exp} components for convective and radiative heating.

Table 17: Summary of model form uncertainties (f_{exp}) resulting from convective heating measurements and their relevant physics. Values in parentheses are for cases with blowing, while those without parentheses are non-blowing.

	CUBRC Orion	Wilder Ballistic Range	Hollis LAL	Holden	LAL EES Test	
Laminar/Stagnation q_{conv}						
Rough Turbulent q_{conv}						
Chemistry Impact on q_{conv}						
Blowing Impact on q_{conv}						
Model form uncertainty resulting from comparison between measurements and simulations:						Max
q_{conv} : stagnation non-blowing (blowing)	5 (20)	13 -	0 -	- -	5 -	20%
q_{conv} : near-shoulder non-blowing (blowing)	- 0	5 -	5 -	3 -	5 -	5%

6.1 Convective Heating

The dominant physical processes impacting the convective heating at the stagnation point and on the flank are discussed in Sections 3 and 4, and are condensed into four categories in Table 17. The ability of the experimental campaigns considered in those previous sections to engage these physics is noted with a green box in this table. The more green boxes for a given measurement, the more it represents the RLVP flight environment. Note that the chemistry impact for Venus entry is not captured by any of these measurements.

The lower half of Table 17 presents a summary of the model form uncertainties assessed for each measurement. This assessment, which is presented in the section discussing each measurement in Sections 3 and 4, is made separately for the stagnation point and a representative downstream location. For the limited cases where a blowing wall is included, the resulting values are listed in parenthesis. A value of zero in this table indicates that the model form uncertainty was less than zero. The last column at the right of Table 17 lists the largest model form uncertainty for that row, which are used to define f_{stag} and f_{flank} . These values of 20% and 5% for f_{stag} and f_{flank} , respectively, are applied to the distribution function defined in Fig. 113 to obtain f_{exp} .

A meaningful assessment of the convective heating parametric uncertainty requires simulations that include turbulence, roughness, and ablation, which allows the uncertainties in modeling parameters associated with these phenomena to impact the parametric uncertainty. This analysis is challenging because of the computational expense of coupled ablation simulations and the large number of uncertain parameters, which are both due to the large number of species required for a coupled ablation simulation. To address this challenge, West [12] developed an efficient parametric uncertainty approach that minimizes the number of simulations required for the uncertainty assessment. This approach is integrated into the LAURA coupled analysis and applied to the coupled ablation simulations with finite-rate char

presented in Section 5.3. Uncertainties in the flowfield chemical rates, roughness modeling parameters, turbulent Schmidt number, diffusion cross sections, as well as many other modeling parameters are included in the analysis were presented in Section 2.2.2. Full details of the application of this approach are presented by West [12].

The RLVP TPS design and sizing will likely use the uncoupled ablation approach described in Section 5, which corrects the non-ablating convective heating (or heat transfer coefficient) with the blowing correction. Therefore, the upper parametric uncertainty bound resulting from the present coupled ablation parametric uncertainty analysis must be assessed relative to the uncoupled convective heating (instead of the nominal coupled ablation heating). This parametric uncertainty component is defined as the multiplier required for the non-ablating heat transfer coefficient ($f_{param}C_{H,0}$) so that when it is applied to the material response code for TPS sizing, the resulting C_H obtained within the material response code (using the blowing correction) is equal to the upper parametric uncertainty limit ($C_{H,upper}$). Note that $C_{H,upper}$ is obtained for a finite-rate char coupled ablation simulation. Therefore, some of the resulting f_{param} captured in this process is due to the parametric uncertainty in the coupled ablation simulation and some is due to the different surface model (equilibrium vs. finite-rate) between the coupled and uncoupled simulations.

The process for evaluating f_{param} described in the previous paragraph leads to the following equation, which is solved iteratively for f_{param} :

$$C_{H,upper} = f_{param}C_{H,0} \frac{2\lambda(\dot{m}_c f_{param} + \dot{m}_g)/[C_{H,0} f_{param}]}{\exp(2\lambda(\dot{m}_c f_{param} + \dot{m}_g)/[C_{H,0} f_{param}]) - 1} \quad (34)$$

Solving this equation requires that $\dot{m}_c$ and $\dot{m}_g$ are assigned values that are as close to the values anticipated within the material response code as possible (obtaining exact values is not possible without knowing the exact trajectory). This is done by obtaining the uncoupled equilibrium ablation values for $\dot{m}_c$ and $\dot{m}_g$ on the nominal trajectory using the nominal $C_{H,0}$, and then recognizing that $\dot{m}_g$ is relatively insensitive to small changes in $C_{H,0}$ and $\dot{m}_c$ is proportional to $C_{H,0}$ because B'_c is relatively insensitive to small changes in $C_{H,0}$. Therefore, $\dot{m}_g$ is fixed to the nominal equilibrium ablation result and $\dot{m}_c$ is varied linearly with f_{param} , which results in the f_{param} multiplying $\dot{m}_c$ in Eq. (34). This equation is solved separately for each point on the surface to obtain a surface-dependent distribution for f_{param} , which is also obtained for various trajectory points to make f_{param} time-dependent.

Figures 114-116 present the nominal coupled ablation C_H values and the upper limit coupled ablation C_H values resulting from the parametric uncertainty analysis. Also presented in these figures is the nominal uncoupled ablation result, which is the result likely used for TPS design. Recall that the coupled ablation simulations apply the finite-rate char model, while the uncoupled result applies the equilibrium char model. As discussed in the previous paragraph, the change in $C_{H,0}$ required for the nominal uncoupled ablation result to match the parametric uncertainty is defined as the parametric uncertainty, which is presented in (b) of these figures. For cases where the nominal uncoupled ablation convective heating is higher than the upper limit, this difference is set to zero, such as in Fig. 114.

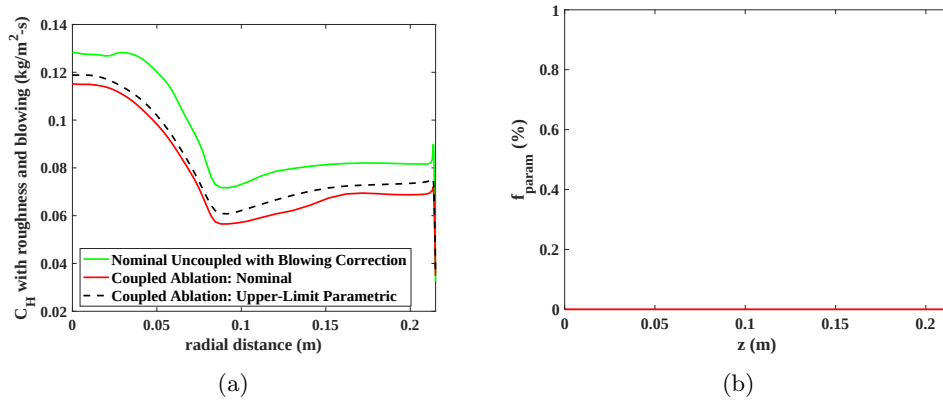


Figure 114: Parametric uncertainty result for the 65 s case.

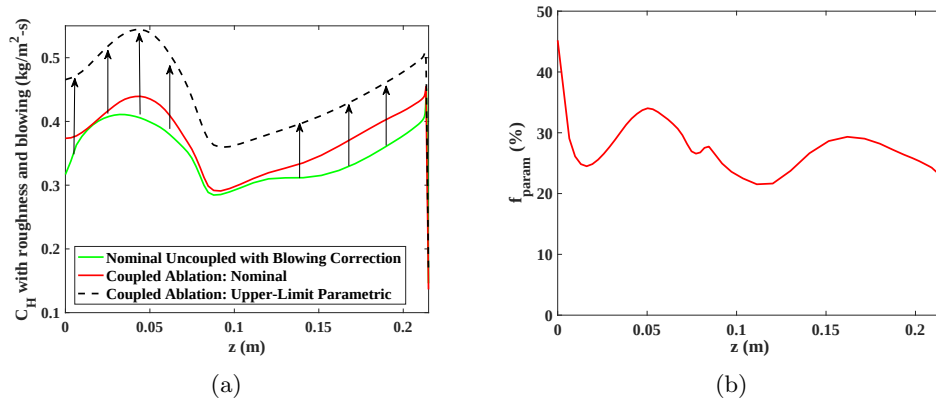


Figure 115: Parametric uncertainty result for case near peak heating at 73 s.

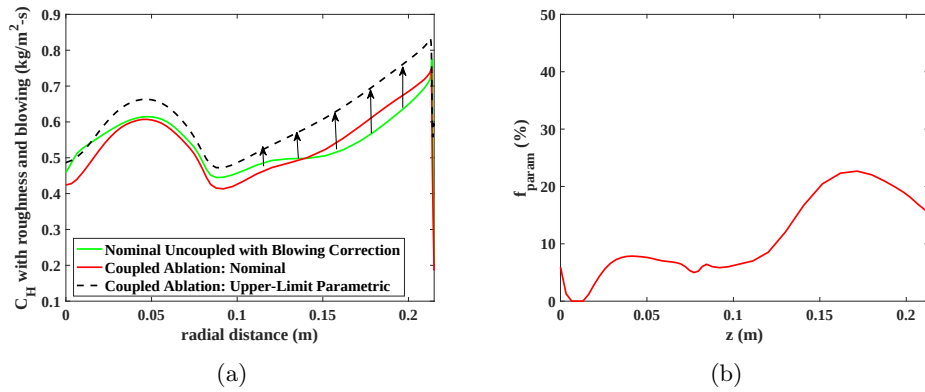


Figure 116: Parametric uncertainty result for case after peak heating at 80 s.

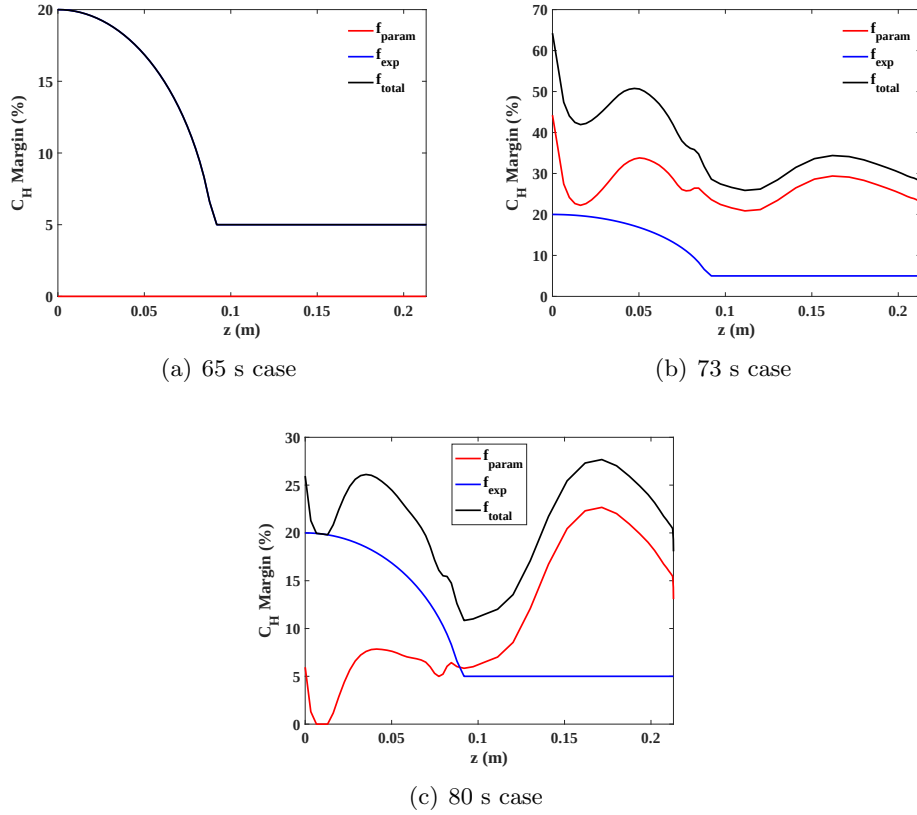


Figure 117: Final convective heating margin compilation for three cases.

The convective heating or C_H margin is evaluated using Eq. (33), where the f_{exp} values are defined by Fig. 113 and Table 17 (and are constant through the trajectory), and the f_{param} values are presented in Figs. 114(b)–116(b). These two components and the resulting margin are presented for three cases in Fig. 117. These surface dependent margins (defined by the “ f_{total} ”) curve in each figure, are seen to vary from the 5 to 65% depending on the surface location and case. These final convective heating margin distributions are computed for a range of database cases and interpolated to other cases based on free-stream velocity. The resulting surface dependent margins are included in the margined heating indicators presented in Section 6.4.

6.2 Radiative Heating

This subsection discusses the evaluation of the model form uncertainty (f_{exp}) and parametric uncertainty (f_{param}) for radiative heating, which are combined using Eq. (33) to obtain the radiative heating margin.

The f_{exp} values assessed for the radiative heating are based exclusively on EAST measurements utilizing the STIB approach discussed in Section 3.2.2.

In addition to the f_{exp} values for radiative heating obtained from the STIB approach, the current margin framework represented by Eq. (33) requires the ra-

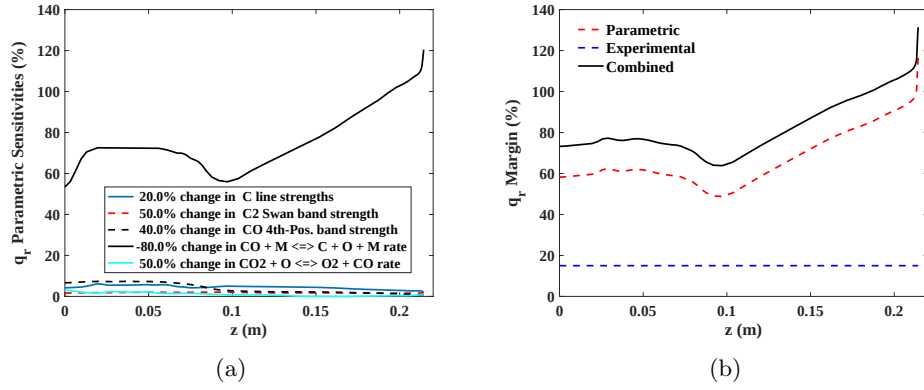


Figure 118: Radiative heating parametric uncertainty and combined margin components for the 65 s case (10.95 km/s, $1.24\text{e-}4 \text{ kg/m}^3$).

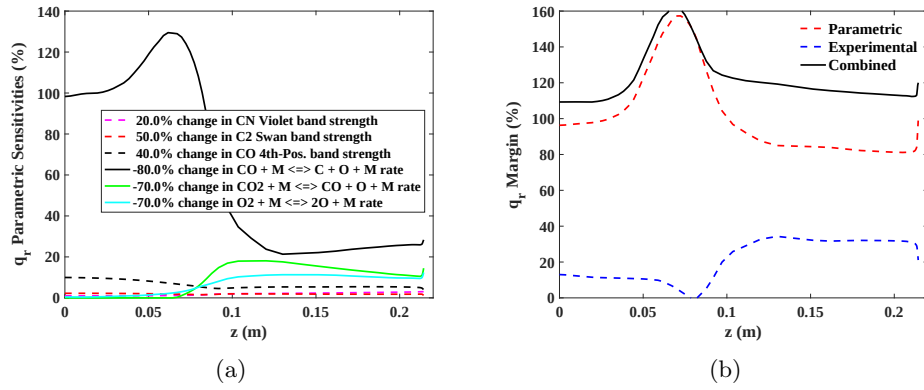


Figure 119: Radiative heating parametric uncertainty and combined margin components for the 73 s case (9.07 km/s, $1.22\text{e-}3 \text{ kg/m}^3$).

diative heating parametric uncertainty. This uncertainty is computed using the approach and uncertainty values for various parameters presented in Section 2.2.2. These parametric uncertainty analyses were performed on the coupled ablation simulations, so that uncertainties in the ablation product radiation are captured. Figures 118(a)–120(a) present the dominant uncertainty contributors for two cases near peak radiative heating. The large uncertainty contribution from the CO dissociation rate is the result of its strong impact on post-shock nonequilibrium emission. The resulting radiative heating parametric uncertainty is presented as the red dashed line in Figs. 118(b)–120(b). This is seen to vary from 60–128% in both the stagnation and flank regions. Also shown in these figures is f_{exp} , which is the STIB result discussed in Section 3.2.2, as well as the combined parametric and experimental components obtained using Eq. (33).

Note the final margined radiative heating is less than 250 W/cm^2 , while the convective heating is near 1600 W/cm^2 . The radiative heating margin approach presented here is evaluated (meaning the parametric component) at a range of trajectory points and interpolated to others based on velocity. This results in a margin

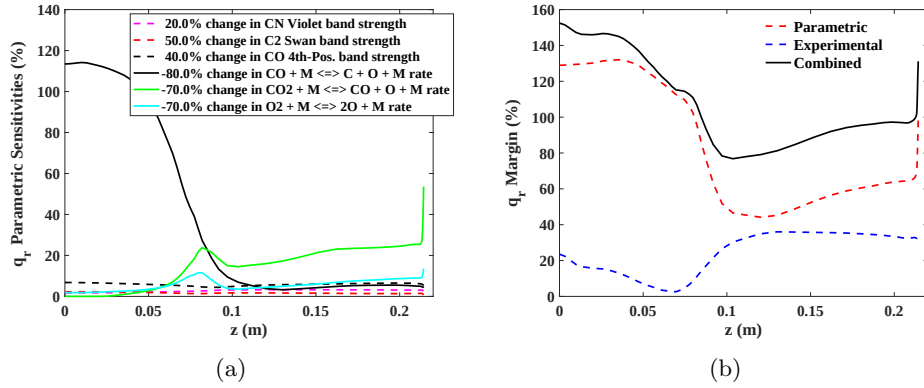


Figure 120: Radiative heating parametric uncertainty and combined margin components for the 75 s case (8.07 km/s, $1.92\text{e-}3 \text{ kg/m}^3$).

that varies through the trajectory, analogously to the convective heating margin. This variation allows the change in the dominant physics through the radiative heat pulse to be captured.

6.3 Final Margined Heating

The final margined heating values for the nominal (“a”) trajectory defined in Table 1 are presented in Fig. 121 for the stagnation-point and the maximum heating value near shoulder location. The final margined heating values are evaluated as

$$q_{final, margined} = (1 + f_{total}) q_{nominal}, \quad (35)$$

where f_{total} is the final margin defined in Section 6. These convective values include the Dahm roughness augmentation model, but do not include the impact of ablation, whereas the radiative heating includes the impact of ablation. The peak margined total heat rate (convective plus radiative) is 2203 W/cm^2 at the stagnation point and 2088 W/cm^2 at the near-shoulder location. The margined total heat load between 55 and 85 s is 29.3 kJ/cm^2 at the stagnation point and 24.8 kJ/cm^2 at the near-shoulder location. Assuming a linear decrease in heating to zero within 10 s of the last considered trajectory point (55 and 85 s) brings these total heat load values to 31.5 and 26.0 kJ/cm^2 at the stagnation point and near shoulder location, respectively. Table 18 lists the margined results of the present aerothermal analysis. The margined values include the margins developed previously in this section, which vary along the surface and through the trajectory for the convective and radiative heating. For shear and pressure the f_{total} is assumed to be 10%.

6.4 Aerothermal Indicators

To reduce the aerothermal simulations into a convenient format for application within trajectory codes, the aerothermal results presented in Table 18 were correlated at two locations on the vehicle surface, the stagnation point and the maximum heating point near shoulder location. This correlation, which allows the aerothermal

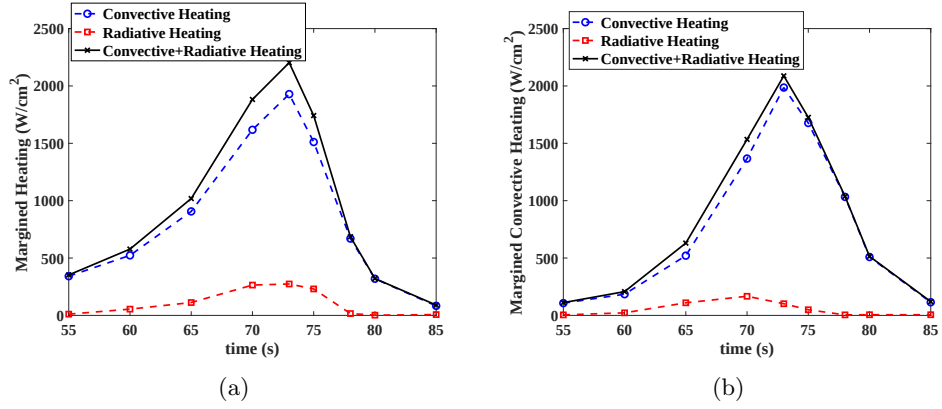


Figure 121: Margined heating, including roughness augmentation, at the stagnation point and near-shoulder locations.

Table 18: Margined aerothermal values for the database cases defined in Table 1. Trajectories (b) and (c) are, respectively, the lower and higher heating trajectories relative to the nominal trajectory (a).

Time (s)	Case #	Velocity (m/s)	Density (kg/m ³)	q_c (W/cm ²)	q_r (W/cm ²)	C_H (kg/m ² -s)	p_w (kPa)	τ_w (kPa)
55	1a	1.117E+04	4.16E-06	3.42E+02	1.09E+01	6.58E-02	1.00E+00	9.47E-03
60	2a	1.117E+04	2.52E-05	5.23E+02	5.46E+01	1.11E-01	3.31E+00	8.07E-03
65	4a	1.095E+04	1.24E-04	9.06E+02	1.12E+02	1.90E-01	1.58E+01	1.16E-02
70	5a	1.018E+04	5.21E-04	1.62E+03	2.64E+02	4.00E-01	5.76E+01	2.34E-02
73	6a	9.066E+03	1.22E-03	1.93E+03	2.74E+02	6.38E-01	1.07E+02	2.60E-02
75	7a	8.035E+03	1.92E-03	1.51E+03	2.31E+02	6.63E-01	1.32E+02	3.30E-02
78	8a	6.310E+03	3.24E-03	6.70E+02	1.60E+01	5.89E-01	1.37E+02	3.02E-02
80	9a	5.213E+03	4.19E-03	3.19E+02	2.14E+00	5.55E-01	1.22E+02	2.38E-02
85	10a	3.153E+03	6.78E-03	8.29E+01	7.16E+00	3.95E-01	7.13E+01	1.28E-02
-	1b	1.102E+04	4.16E-06	4.79E+02	7.80E-08	1.03E-01	1.18E+00	2.87E-02
-	2b	1.102E+04	2.52E-05	6.49E+02	3.77E+01	1.39E-01	3.63E+00	1.51E-02
-	3b	1.087E+04	6.15E-05	7.30E+02	8.91E+01	1.60E-01	7.70E+00	1.03E-02
-	5b	1.018E+04	2.64E-04	1.34E+03	2.08E+02	3.35E-01	2.91E+01	1.77E-02
-	6b	9.066E+03	5.66E-04	1.57E+03	2.30E+02	5.24E-01	4.97E+01	2.18E-02
-	7b	8.035E+03	9.31E-04	1.19E+03	2.21E+02	5.46E-01	6.41E+01	2.29E-02
-	8b	6.310E+03	1.62E-03	5.01E+02	1.67E+01	4.84E-01	6.87E+01	1.97E-02
-	9b	5.213E+03	2.19E-03	2.37E+02	1.77E+00	4.63E-01	6.36E+01	1.48E-02
-	10b	3.153E+03	3.56E-03	6.76E+01	4.22E+00	3.00E-01	3.74E+01	8.24E-03
-	1c	1.132E+04	4.16E-06	5.27E+02	8.46E-08	1.06E-01	1.06E+00	1.92E-02
-	2c	1.132E+04	2.52E-05	6.61E+02	6.78E+01	1.37E-01	3.43E+00	9.53E-03
-	3c	1.102E+04	2.64E-04	1.48E+03	2.23E+02	3.06E-01	3.42E+01	1.94E-02
-	5c	1.018E+04	1.17E-03	2.70E+03	3.96E+02	6.75E-01	1.29E+02	4.07E-02
-	6c	9.066E+03	2.45E-03	3.38E+03	3.61E+02	1.05E+00	2.15E+02	5.43E-02
-	7c	8.035E+03	3.71E-03	2.53E+03	3.29E+02	1.06E+00	2.56E+02	5.64E-02
-	8c	6.310E+03	6.06E-03	1.10E+03	2.59E+01	9.21E-01	2.57E+02	4.97E-02
-	9c	5.213E+03	7.66E-03	5.26E+02	3.13E+00	8.50E-01	2.23E+02	4.36E-02
-	10c	3.153E+03	1.22E-02	1.04E+02	1.01E+01	5.43E-01	1.28E+02	2.00E-02

indicators to be estimated for trajectories and points that do not match the ones presented in Table 18, applies the following functional form:

$$F = A\rho_\infty^B U_\infty^C \quad (36)$$

where F represents either the convective heating (W/cm^2), radiative heating (W/cm^2), C_H ($\text{kg}/\text{m}^2 - s$), wall pressure (N/m^2), or shear (N/m^2). The free-stream density, ρ_∞ , assumes units of kg/m^3 , and free-stream velocity, U_∞ , assumes units of m/s . The coefficients for this correlation are listed in Table 19 for the un-margined values and Table 20 for the margined values. These correlations capture the tabulated values within $\pm 20\%$ near peak heating, with the percent error increasing at the wings of the heat pulse.

Table 19: Coefficients for the un-margined aerothermal indicators defined by Eq. (36).

(a) Stagnation Point				(b) Near Shoulder			
<u>Stagnation Point</u>	A	B	C	<u>Near Shoulder</u>	A	B	C
Convective Heating	2.4458E-11	5.9214E-01	3.8984E+00	Convective Heating	4.0691E-10	7.8501E-01	3.7513E+00
C_H	4.0973E-03	4.8044E-01	8.5473E-01	C_H	4.7116E-02	5.8986E-01	6.5094E-01
p_w	8.7580E-01	1.0035E+00	2.0138E+00	p_w	4.4153E-01	1.0000E+00	2.0148E+00
Radiative Heating	2.1799E-18	7.5434E-01	5.5505E+00	Radiative Heating	1.6422E-37	9.6735E-01	1.0440E+01
Shear	8.9112E-05	5.9628E-01	1.8302E+00	Shear	3.2146E-02	5.4246E-01	1.6120E+00

Table 20: Coefficients for the margined aerothermal indicators defined by Eq. (36).

(a) Stagnation Point				(b) Near Shoulder			
<u>Stagnation Point</u>	A	B	C	<u>Near Shoulder</u>	A	B	C
Convective Heating	5.6602E-16	9.1889E-01	5.3580E+00	Convective Heating	6.9430E-12	9.4414E-01	4.3481E+00
C_H	1.7597E-05	6.8669E-01	1.6495E+00	C_H	5.6966E-03	7.4897E-01	1.0275E+00
p_w	9.6338E-01	1.0035E+00	2.0138E+00	p_w	4.8568E-01	1.0000E+00	2.0148E+00
Radiative Heating	5.1993E-17	8.7075E-01	5.3810E+00	Radiative Heating	1.7228E-42	1.0247E+00	1.1815E+01
Shear	9.8023E-05	5.9628E-01	1.8302E+00	Shear	3.5361E-02	5.4246E-01	1.6120E+00

6.5 Future Improvements

This report presents the most rigorous possible with current aerothermal analysis tools, with the coupled ablation and margin assessment approach representing advances in the state-of-the-art relative to any past NASA mission. Nevertheless, areas of possible improvement include refining the flowfield chemical rates uncertainties, obtaining better justification for the turbulent Schmidt number, capturing transition instead of assuming a fully turbulent entry, and increasing the fidelity of the finite-rate surface chemistry model. It is not clear whether the combined effect of these improvements would increase or decrease the peak heating or total heat load.

While this report was mainly focused on the aerothermal analysis, surface pressure and shear were also reported for completeness. For these, a flat 10% margin was considered. Detailed aerodynamic analysis is needed to better estimate the aerodynamic uncertainty.

References

1. Gnoffo, P. A.: An Upwind-Biased, Point-Implicit Relaxation Algorithm for Viscous, Compressible Perfect-Gas Flows. NASA TP 2953, Feb. 1990.
2. Thompson, K.; Hollis, B. R.; Johnston, C. O.; Kleb, B.; Lessard, V.; and Mazaheri, A.: LAURA Users Manual. NASA TM 2020-220566, 2020.
3. Johnston, C. O.; Hollis, B. R.; and Sutton, K.: Spectrum Modeling for Air Shock-Layer Radiation at Lunar-Return Conditions. *Journal of Spacecraft & Rockets*, vol. 45, Sep.-Oct. 2008, pp. 865–878.
4. Johnston, C. O.; Hollis, B.; and Sutton, K.: Non-Boltzmann Modeling for Air Shock Layers at Lunar Return Conditions. *Journal of Spacecraft & Rockets*, vol. 45, Sep.-Oct. 2008, pp. 879–890.
5. Johnston, C. O.; and Brandis, A. M.: Modeling of Nonequilibrium CO Fourth-Positive and CN Violet Emission in CO₂-N₂ Gases. *Journal of Quantitative Spectroscopy and Radiative Transfer*, vol. 149, 2014, pp. 303–317.
6. Cheatwood, F. M.; and Thompson, R. A.: The Addition of Algebraic Turbulence Modeling to Program LAURA. NASA TM 107758, 1993.
7. Baldwin, B.; and Lomax, H.: Thin Layer Approximation and Algebraic Model for Separated Turbulent Flows. AIAA Paper 78–157, 1978.
8. Mazaheri, A.; Johnston, C.; and Sefidbakht, S.: Three-Dimensional Radiation Ray-Tracing for Shock Layer Radiative Heating Simulations. *Journal of Spacecraft & Rockets*, vol. 50, no. 3, 2013.
9. Johnston, C. O.; Gnoffo, P. A.; and Mazaheri, A.: Influence of Coupled Radiation and Ablation on the Aerothermodynamic Environment of Planetary Entry Vehicles. VKI Lecture STO-AVT-218, 2013.
10. French, R.; Mandy, C.; Hunter, R.; Mosleh, E.; Sinclair, D.; Beck, P.; Seager, S.; Petkowski, J. J.; Carr, C. E.; Grinspoon, D. H.; Baumgardner, D.; and on behalf of the Rocket Lab Venus Team: Rocket Lab Mission to Venus. *Aerospace*, vol. 9, no. 8, 2022. URL <https://www.mdpi.com/2226-4310/9/8/445>.
11. West, T. K.; and Bynum, M. D.: HIFIRE Flight 2 Unstart Reliability Analysis. AIAA Paper 2021–4139, 2021.
12. West, T. K.; and Johnston, C. O.: Efficient Parametric Uncertainty Analysis of an Earth Entry Vehicle Concept using Least Angle Regression. AIAA Paper 2023–2430, 2023.
13. Duan, L.; and Martin, M.: Direct Numerical Simulation of Hypersonic Turbulent Boundary Layers. Part 4. Effect of High Enthalpy. *Journal of Fluid Mechanics*, vol. 684, 2011, pp. 25–59.

14. Di Renzo, M.; and Urzay, J.: Direct Numerical Simulation of a Hypersonic Transitional Boundary Layer at Suborbital Enthalpies. *Journal of Fluid Mechanics*, vol. 912, 2021, p. A29.
15. Cogo, M.; Baù, U.; Chinappi, M.; Bernardini, M.; and Picano, F.: Assessment of Heat Transfer and Mach Number Effects on High-Speed Turbulent Boundary Layers. 2023.
16. Dahm, T. J.: Passive Nosedip Technology (PANT II) Program. SAMSO-TR 77-11, 1976.
17. Park, C.: Effects of Atomic Oxygen on Graphite Ablation. *AIAA Journal*, vol. 14, no. 11, 1976, pp. 1640–1642.
18. Palmer, H. B.; and Shelef, M.: Vaporization of Carbon. *Chemistry and Physics of Carbon*, vol. 4, 1968, pp. 85–135.
19. Driver, D.; and MacLean, M.: Improved Predictions of PICA Recession in Arc Jet Shear Tests. AIAA Paper 2011–0141, 2011.
20. Kim, Y. C.; and Boudart, M.: Recombination of O, N, and H Atoms on Silica: Kinetics and Mechanisms. *Lagmuir*, vol. 7, no. 1, 1991, pp. 2999–3005.
21. Mackay, K. K.; Freund, J. B.; and Johnson, H. T.: Hydrogen recombination rates on silica from atomic-scale calculations. *The Journal of Physical Chemistry C*, vol. 120, no. 42, 2016, pp. 24137–24147.
22. Gupta, R. N.; and Thompson, R. A.: A Review of Reaction Rates and Thermodynamic and Transport Properties for an 11-Species Air Model for Chemical and Thermal Nonequilibrium Calculations to 30,000 K. NASA RP 1222, Aug. 1990.
23. Wright, M. J.; Bose, D.; Palmer, G.; and Levin, E.: Recommended Collision Integrals for Transport Property Computations 1: Air Species. *AIAA Journal*, vol. 43, no. 12, 2005, pp. 2558–2564.
24. Wright, M. J.; Hwang, H.; and Schwenke, D. W.: Recommended Collision Integrals for Transport Property Computations Part 2: Mars and Venus Entries. *AIAA Journal*, vol. 45, no. 1, 2007, pp. 281–288.
25. Babou, Y.; Riviere, P.; Perrin, M.-Y.; and Soufiani, A.: Spectroscopic Data for the Prediction of Radiative Transfer in CO₂-N₂ Plasmas. *Journal of Quantitative Spectroscopy and Radiative Transfer*, vol. 110, 2009, pp. 89–108.
26. da Silva, M. L.; and Dudeck, M.: Arrays of Radiative Transition Probabilities for CO₂-N₂ Plasmas. *Journal of Quantitative Spectroscopy and Radiative Transfer*, vol. 102, 2006, pp. 348–386.
27. Rodio, J.; and Hassan, H.: Spectroscopic Data and Model Predictions for Venus and Mars Atmospheres. AIAA Paper 2011–3949, 2011.

28. Babou, Y.; Riviere, P.; Perrin, M.-Y.; and Soufiani, A.: High Temperature and Nonequilibrium Partition Function and Thermodynamic Data of Diatomic Molecules. *International Journal of Thermophysics*, vol. 30, 2009, pp. 416–438.
29. Kokkin, D. L.; Bacskay, G. B.; and Schmidt, T. W.: Oscillator Strengths and Radiative Lifetimes for C₂: Swan, Ballik-Ramsay, Phillips, and d³Π_g-c³Σ_u. *Journal of Chemical Physics*, vol. 126, no. 084302, 2007.
30. Schmidt, T. W.; and Bacskay, G. B.: Oscillator Strengths of the Mulliken, Swan, Ballik-Ramsay, Phillips, and d³Π_g-c³Σ_u Systems of C₂ Calculated by MRCI Methods Utilizing a Biorthogonal Transformation of CASSCF Orbitals. *Journal of Chemical Physics*, vol. 127, no. 234310, 2007.
31. Johnston, C. O.; Brandis, A. M.; and Sutton, K.: Shock Layer Radiation Modeling and Uncertainty for Mars Entry. AIAA Paper 2012–2866, 2012.
32. Park, C.: Rate Parameters for Electronic Excitation of Diatomic Molecules I. Electron-Impact Processes. AIAA Paper 2008-1206, 2008.
33. Park, C.: Rate Parameters for Electronic Excitation of Diatomic Molecules II. Heavy Particle-Impact Processes. AIAA Paper 2008-1446, 2008.
34. Johnston, C. O.; West, T. K.; and Brandis, A. M.: Features of Afterbody Radiative Heating for Titan Entry. AIAA paper 2019-3010, 2019.
35. Schofield, K.: Critically Evaluated Rate Constants for Gaseous Reactions of Several Electronically Excited Species. *Journal of Physical and Chemical Reference Data*, vol. 8, no. 3, 1979, pp. 723–798.
36. Tereshchenko, E. N.; Golovkova, S.; and Dodonova, N.: Study of the Collisional Deactivation Processes of the A State of the CN Radical by the Fluorescence Quenching Method. *Optics and Spectroscopy*, vol. 42, no. 3, 1977, pp. 315–316.
37. Tereshchenko, E. N.; and Dodonova, N.: Collisional Deactivation Processes for Electronically Excited CN (B) Radicals. *Optics and Spectroscopy*, vol. 39, no. 4, 1975, pp. 435–436.
38. Harrison, S.; and Tennyson, J.: Electron Collisions with the CN Radical: Bound States and Resonances. *Journal of Physics B: Atomic, Molecular and Optical Physics*, vol. 45, 2012, pp. 1–7.
39. Gokcen, T.: N₂-CH₄-Ar Chemical Kinetic Model for Simulations of Atmospheric Entry to Titan. *Journal of Thermophysics and Heat Transfer*, vol. 21, no. 1, 2007, pp. 9–18.
40. Hanson, R. K.: Shock-Tube Study of Carbon Monoxide Dissociation Kinetics. *Journal of Chemical Physics*, vol. 60, no. 12, 1974, pp. 4970–4976.

41. Appleton, J. P.; Steinberg, M.; and Liquornik, D. J.: Shock-Tube Study of Carbon Monoxide Dissociation Using Vacuum-Ultraviolet Absorption. *Journal of Chemical Physics*, vol. 52, no. 5, 1970, pp. 2205–2221.
42. Chakerian, C.: The Dissociation of Shock Heated Carbon Monoxide Studied by Two Wavelength Infrared Emission. *Shock Tube Research*, Imperial College, London, 1971.
43. Mick, H.-J.; Burmeister, M.; and Roth, P.: Atomic Resonance Absorption Spectroscopy Measurements on High-Temperature CO Dissociation Kinetics. *AIAA Journal*, vol. 31, no. 4, 1993, pp. 671–676.
44. Park, C.; Howe, J. T.; Jaffe, R. L.; and Candler, G. V.: Review of Chemical-Kinetic Problems for Future NASA Missions, II: Mars Entries. *Journal of Thermophysics and Heat Transfer*, vol. 8, no. 1, 1994, pp. 9–23.
45. Ebrahim, N. A.; and Sandeman, R. J.: Interferometric studies of carbon dioxide dissociation in a free-piston shock tube. *Journal of Chemical Physics*, vol. 65, no. 9, 1976, p. 3446.
46. Oehlschlaeger, M. A.; Davidson, D. F.; Jeffries, J. B.; and Hanson, R. K.: Carbon Dioxide Thermal Decomposition: Observation of Incubation. *Z. Phys. Chem.*, vol. 219, 2005, pp. 555–567.
47. Burmeister, M.; and Roth, P.: ARAS Measurements on the Thermal Decomposition of CO₂ Behind Shock Waves. *AIAA Journal*, vol. 28, no. 3, 1990, pp. 402–405.
48. Saxena, S.; Kiefer, J. H.; and Tranter, R. S.: Relaxation, Incubation, and Dissociation in CO₂. *J. Phys. Chem. A*, vol. 111, 2007, pp. 3884–3890.
49. Park, C.: *Nonequilibrium Hypersonic Aerothermodynamics*, Wiley, New York. 1 ed., 1990.
50. Grover, M. S.; Suchyta, C. J.; and Josyula, E.: Ab Initio Based Rate Coefficients For Two-Temperature Nonequilibrium Models In Hypersonic Blunt Body Flow. AIAA paper 2019-0790, 2019.
51. Hanson, R. K.; and Baganoff, D.: Shock-Tube Study of Nitrogen Dissociation Rates Using Pressure Measurements. *AIAA Journal*, vol. 10, no. 2, 1972, pp. 211–215.
52. Jaffe, R.; Schwenke, D.; and Chaban, G.: Theoretical Analysis of N₂ Collisional Dissociation and Rotation-Vibration Energy Transfer. AIAA paper 2009-1569, 2019.
53. Wray, K. L.; and Teare, J. D.: Shock-Tube Study of the Kinetics of Nitric Oxide at High Temperatures. *Journal of Chemical Physics*, vol. 36, no. 10, 1962, pp. 2582–2596.

54. Thielen, K.; and Roth, P.: Resonance absorption measurements of N and O atoms in high temperature NO dissociation and formation kinetics. *Symposium (International) on Combustion*, Elsevier, vol. 20, 1985, pp. 685–693.
55. Myerson, A. L.: Shock-tube atom kinetics of nitric oxide decomposition. *Symposium (International) on Combustion*, Elsevier, vol. 14, 1973, pp. 219–228.
56. Koshi, M.; Bando, S.; Saito, M.; and Asaba, T.: Dissociation of nitric oxide in shock waves. *Symposium (International) on Combustion*, Elsevier, vol. 17, 1979, pp. 553–562.
57. Park, C.: *Nonequilibrium Hypersonic Aerothermodynamics*. Wiley, 1st ed., 1990.
58. Kulakhmetov, M.; Gallis, M.; and Alexeenko, A.: Ab Initio-Informed Maximum Entropy Modeling of Rovibrational Relaxation and State-Specific Dissociation with Application to the $\text{O}_2 + \text{O}$ System. *The Journal of Chemical Physics*, vol. 144, no. 17, 2016, p. 174302.
59. Kim, J. G.; and Park, G.: Thermochemical Nonequilibrium Parameter Modification of Oxygen for a Two-Temperature Model. *Physics of Fluids*, vol. 30, no. 1, 2018, p. 016101.
60. Dean, A. J.; Hanson, R. K.; and Bowman, C. G.: A Shock Tube Study of Reactions of C Atoms and CH with NO Including Product Channel Measurements. *The Journal of Physical Chemistry*, vol. 95, no. 8, 1991, pp. 3180–3189.
61. Andersson, S.; Markovic, N.; and Nyman, G.: Computational Studies of the Kinetics of the C + NO and O + CN Reactions. *The Journal of Physical Chemistry*, vol. 107, no. 28, 2003, pp. 5439–5447.
62. Dean, A. J.; Davidson, D. F.; and Hanson, R. K.: A Shock Tube Study of Reactions of C Atoms with H_2 and O_2 Using Excimer Photolysis of C_3O_2 and C Atom Atomic Resonance Absorption Spectroscopy. *The Journal of Physical Chemistry*, vol. 95, no. 1, 1991, pp. 183–191.
63. Becker, K. H.; Brockman, K. J.; and Wiesen, P.: Spectroscopic identification of C(3P) atoms in halogenomethane + H flame systems and measurements of C(3P) reaction rate constants by two-photon laser-induced fluorescence. *J. Chem. Soc. Faraday Trans.*, vol. 84, 1988, pp. 455–466.
64. Gokcen, T.: $\text{N}_2\text{-CH}_4\text{-Ar}$ Chemical Kinetic Model for Simulations of Atmospheric Entry to Titan. AIAA Paper 2004-2469, July 2004.
65. Le, Q. N.; and Vanpee, M.: Free radical concentration measurements in nitric oxide acetylene flames. *Combustion and flame*, vol. 62, no. 2, 1985, pp. 193–210.
66. Thielen, K.; and Roth, P.: Resonance Absorption Measurements of N and O Atoms in High Temperature NO Dissociation and Formation Kinetics. *20th*

Symposium (International) on Combustion, The Combustion Institute, Pittsburgh, Pa, 1984.

67. Fujita, K.; Yamada, T.; and Ishii, N.: Impacts of Ablation Gas Kinetics on Hyperbolic Entry Radiative Heating. AIAA Paper 2006-1185, Jan. 2006.
68. Park, C.; Jaffe, R. L.; and Partridge, H.: Chemical-Kinetic Parameters of Hyperbolic Earth Entry. *Journal of Thermophysics and Heat Transfer*, vol. 15, no. 1, 2001, pp. 76-90.
69. Park, C.: Assessment of a Two-Temperature Kinetic Model for Dissociating and Weakly Ionizing Nitrogen. *Journal of Thermophysics and Heat Transfer*, vol. 2, no. 1, 1988, pp. 8-16.
70. Dunn, M. G.; and Kang, S.: Theoretical and Experimental Studies of Reentry Plasmas. NASA CR 2232, Apr. 1973.
71. Dunn, M. G.; and Lordi, J. A.: Measurement of O_2^+ Plus e^- Dissociative Recombination in Expanding Oxygen Flows. *AIAA Journal*, vol. 8, no. 4, 1970, pp. 614-618.
72. Annaloro, J.; Morel, V.; Bultel, A.; and Omaly, P.: Global Rate Coefficients for Ionization and Recombination of Carbon, Nitrogen, Oxygen, and Argon. *Physics of Plasmas*, vol. 19, no. 7, 2012, p. 073515.
73. Losev, S. A.; Makarov, V. N.; and Pogosbekyan, M. Y.: Model of the Physico-Chemical Kinetics Behind the Front of a Very Intense Shock Wave in Air. *Fluid Dynamics*, vol. 30, 1995, pp. 299-309.
74. Carlson, L. A.; and Gally, T. A.: Effect of Electron Temperature and Impact Ionization on Martian Return AOTV Flowfields. *Journal of Thermophysics and Heat Transfer*, vol. 5, no. 1, 1991, pp. 9-20.
75. Park, C.: Review of Chemical-Kinetic Problems of Future NASA Missions. I - Earth Entries. *Journal of Thermophysics and Heat Transfer*, vol. 7, no. 3, 1993, pp. 385-398.
76. Bourdon, A.; and Vervisch, P.: Study of a Low-Pressure Nitrogen Plasma Boundary Layer Over a Metallic Plate. *Physics of Plasmas*, vol. 4, no. 11, 1997, pp. 4144-4157.
77. Teulet, P.; Gonzalez, J. J.; Mercado-Cabrera, A.; Cressault, Y.; and Gleizes, A.: One-Dimensional Hydro-Kinetic Modelling of the Decaying Arc in AIR-PA66-Copper Mixtures: I. Chemical Kinetics, Thermodynamics, Transport and Radiative Properties. *Journal of Physics D: Applied Physics*, vol. 42, no. 17, aug 2009, p. 175201.
78. Cruden, B. A.; and Brandis, A. M.: Measurements and Prediction of Radiative Non-equilibrium for Air Shocks Between 7-9 km/s. AIAA paper 2017-4535, 2017.

79. Kang, S.-W.; Jones, W. L.; and Dunn, M. G.: Theoretical and Measured Electron-Density Distributions at High Altitudes. *AIAA Journal*, vol. 11, no. 2, 1973, pp. 141–149.
80. Jaffe, R.; Schwenke, D.; and Chaban, G.: Theoretical Analysis of N₂ Collisional Dissociation and Rotation-Vibration Energy Transfer. AIAA Paper 2009–1569, 2009.
81. Hanson, R. K.; and Baganoff, D.: Shock-Tube Study of Nitrogen Dissociation Rates Using Pressure Measurements. *AIAA Journal*, vol. 10, no. 2, 1972, pp. 211–215.
82. Jaffe, R.; Schwenke, D.; and Chaban, G.: Vibrational and Rotational Excitation and Dissociation in N₂-N₂ Collisions from Accurate Theoretical Calculations. AIAA Paper 2010–4517, 2010.
83. Panesi, M.; Jaffe, R.; Schwenke, D.; and Magin, T.: Rovibrational Internal Energy Transfer and Dissociation of N₂-N Systems in Hypersonic Flow. *Journal of Chemical Physics*, vol. 138, no. 044312, 2013.
84. Macdonald, R. L.; Jaffe, R.; Schwenke, D.; and Panesi, M.: Construction of a Coarse-Grain Quasi-Classical Trajectory Method. I. Theory and Application to N₂-N₂ System. *Journal of Chemical Physics*, vol. 148, no. 054309, 2018.
85. Chaudhry, R. S.; and Candler, G. V.: Statistical Analyses of Quasiclassical Trajectory Data for Air Dissociation. AIAA Paper 2019–0789, 2019.
86. Bender, J. D.; Valentini, P.; Nompelis, I.; Paukku, Y.; Varga, Z.; Truhlar, D.; Schwartzentruber, T.; and Candler, G.: An Improved Potential Energy Surface and Multi-Temperature QuasiClassical Trajectory Calculations of N₂+N₂ Dissociation Reactions. *Journal of Chemical Physics*, vol. 143, no. 054304, 2015.
87. Valentini, P.; Schwartzentruber, T.; Bender, J.; and Candler, G.: Dynamics of Nitrogen Dissociation from Direct Molecular Simulation. *Physical Review Fluids*, vol. 1, no. 043402, 2016.
88. Bose, D.; Wright, M. J.; and Gocken, T.: Uncertainty and Sensitivity Analysis of Thermochemical Modeling for Titan Atmospheric Entry. AIAA Paper 2004–2455, 2004.
89. Baulch, D. L.; Bowman, C. T.; and Cobos, C. J.: Evaluated Kinetic Data for High Temperature Reactions. Volume 4. Homogeneous Gas Phase Reactions of Halogen- and Cyanide-Containing Species. *The Journal of Physical and Chemical Reference Data*, vol. 10, 1981.
90. Tsang, W.; and Herron, J. T.: Chemical Kinetic Data Base for Propellant Combustion. II. Reactions Involving CN, NCO, and HNCO. *The Journal of Physical and Chemical Reference Data*, vol. 21, 1992.

91. Szekely, A.; Hanson, R.; and Bowman, C.: High-Temperature Determination of the Rate Coefficient for the Reaction $\text{H}_2 + \text{CN} \rightleftharpoons \text{H} + \text{HCN}$. *International Journal of Chemical Kinetics*, vol. 15, no. 9, 1983, pp. 915–923.
92. Tabayashi, K.; Takayuki; and Takasa, K.: Thermal Decomposition of Hydrogen Cyanide in Shock Waves. *Bulletin of the Chemical Society of Japan*, vol. 50, no. 7, 1977, pp. 1754–1757.
93. Cabrera, J. V.; and West, T. K.: Assessment of Pioneer-Venus Entry Heating with Coupled Ablation. AIAA Paper 2024–3560, 2024.
94. Bose, D.; Brown, J. L.; Prabhu, D. K.; Gnoffo, P. A.; Johnston, C. O.; and Hollis, B. R.: Uncertainty Assessment of Hypersonic Aerothermodynamic Prediction Capability. *Journal of Spacecraft & Rockets*, vol. 50, no. 1, 2013, pp. 12–18.
95. Johnston, C. O.: Evaluating Shock-Tube Informed Biases for Shock-Layer Radiative Heating Simulations. *Journal of Thermophysics and Heat Transfer*, vol. 35, no. 2, 2021, pp. 349–361.
96. Cruden, B. A.; Prabhu, D.; Martinez, R.; Lee, H.; Bose, D.; and Grinstead, J. H.: Absolute Radiation Measurements in Venus and Mars Entry Conditions. AIAA Paper 2010–4508, 2010.
97. Brandis, A. M.; Johnston, C. O.; and Cruden, B.: Investigation of Nonequilibrium Radiation for Mars Entry. AIAA Paper 2013–1055, 2013.
98. Hollis, B. R.; Berry, S. A.; Hollingsworth, K. E.; and Wright, S. A.: Experimental Investigation of Roughness Effects on Transition Onset and Turbulent Heating Augmentation on a Hemisphere at Mach 6 and Mach 10. NASA TP 2017-219613, 2017.
99. Lewis, D. R.: NASA Orion Crew Exploration Vehicle Roughness Test in AEDC Tunnel 9. AEDC Report AEDC-TSR-09-H1, 2009.
100. Liechty, D.: Experimental Investigation of Project Orion Crew Exploration Vehicle Roughness Augmentation Aeroheating in AEDC. NASA Report EG-CAP-09-51, 2009.
101. Holden, M. S.; Mundy, E. P.; MacLean, M.; and Wadhams, T. P.: Experimental Studies to Examine the Effects of Boundary Layer Transition and Surface Roughness on the Distribution of Heating over a CEV Configuration in Hypersonic Flow. CUBRC Report A-09-RP-001, 2009.
102. Wilder, M. C.; Brown, J. L.; Amar, A. J.; Holden, M. S.; Mundy, E. P.; and MacLean, M.: The Effects of Distributed Roughness on Turbulent Heating on the Orion Crew Module (CEV Aerosciences Project Test 39-CH in the CUBRC LENS I Facility). CUBRC Report EG-CAP-09-56, 2009.
103. Dirling, R. B.: A Method for Computing Rough Wall Heat Transfer Rates on Reentry Nosetips. AIAA Paper 1973-763, 1973.

104. Edquist, K.: MSL Entry Aerothermodynamic Environments: Revision B. JPL Report, 2010.
105. Orion Aerothermal Team: Orion Aerothermal Substantiation Report. Nasa report, 2019.
106. Powars, C. A.: Surface Roughness Effects on Reentry Heating. Aerotherm Tech Memo 71-10, 1971.
107. Krogstad, P. A.: Modification of the van Driest Damping Function to Include the Effects of Surface Roughness. *AIAA Journal*, vol. 29, no. 6, 1991, pp. 888–894.
108. Cheatwood, J.; Johnston, C. O.; and Hollis, B. R.: Mars Sample Return Earth Entry System Woven Roughness Mach 6 Aeroheating Test. AIAA Paper 2024–0443, Jan. 2024.
109. Finson, M. L.; and Clarke, A. S.: The Effect of Surface Roughness Character on Turbulent Reentry Heating. AIAA Paper 1980–1459, 1980.
110. Wilder, M. C.; and Prabhu, D. K.: Rough Wall Turbulent Heat Transfer Experiments in Hypersonic Free Flight. AIAA Paper 2019-3009, 2019.
111. Bowersox, R.: Survey of High-Speed Rough Wall Boundary Layers: Invited Presentation. AIAA Paper 2007–3998, 2007.
112. Holden, M. S.: Experimental Studies of Surface Roughness, Entropy Swallowing, and Boundary Layer Transition Effects on the Skin Friction and Heat Transfer Distribution in High Speed Flows. AIAA Paper 1982–0034, 1982.
113. Holden, M. S.: Experimental Studies of Surface Roughness Shape and Spacing Effects on Heat Transfer and Skin Friction in Supersonic and Hypersonic Flows. AIAA Paper 1984–0016, 1984.
114. Catris, S.; and Aupoix, B.: Density Corrections for Turbulence Models. *Aerospace Science and Technology*, vol. 4, 2000, pp. 1–11.
115. Thompson, R. A.; and Gnoffo, P. A.: Implementation of a Blowing Boundary Condition in the LAURA Code. AIAA Paper 2008–1243, 2008.
116. Martinelli, S.; Ruffin, S.; McDaniel, R.; Brown, J.; Wright, M.; and Hash, D.: Validation Process for Blowing and Transpiration Cooling in DPLR. AIAA Paper 2007–4255, 2007.
117. Lees, L.: Convective Heat Transfer with Mass Addition and Chemical Reactions. Combustion and propulsion third agard colloquium, Mar. 1958.
118. Bianchi, D.; Nasuti, F.; Martelli, E.; and Onofri, M.: A Numerical Approach for High-Temperature Flows over Ablating Surfaces. AIAA Paper 2007–4537, June 2007.

119. Amar, A.; Blackwell, B.; and Edwards, J.: Development and Verification of a One-Dimensional Ablation Code Including Pyrolysis Gas Flow. *Journal of Thermophysics and Heat Transfer*, vol. 23, no. 1, 2009, pp. 59–71.
120. Keenan, J. A.; and Candler, G.: Simulation of Graphite Sublimation and Oxidation under Re-Entry Conditions. AIAA Paper 1994–2083, 1994.
121. McBride, B. J.; Zehe, M. J.; and Gordon, S.: NASA Glenn Coefficients for Calculating Coefficients for Calculating Thermodynamic Properties of Individual Species. NASA TP 2002–211556, 2002.
122. Wood, B. J.; and Wise, H.: Kinetics of Hydrogen Atom Recombination on Surfaces. *Journal of Physical Chemistry*, vol. 65, 1961, pp. 1976–1983.
123. Candler, G.: Nonequilibrium Processes in Hypervelocity Flows: An Analysis of Carbon Ablation Models. AIAA Paper 2012–0724, 2012.
124. Johnston, C. O.; Gnoffo, P. A.; and Sutton, K.: Influence of Ablation on Radiative Heating for Earth Entry. *Journal of Spacecraft & Rockets*, vol. 46, no. 3, 2009, pp. 481–491.
125. Johnston, C. O.; Mazaheri, A.; Gnoffo, P.; Kleb, B.; and Bose, D.: Radiative Heating Uncertainty for Hyperbolic Earth Entry, Part 1: Flight Simulation Modeling and Uncertainty. *Journal of Spacecraft & Rockets*, vol. 50, no. 1, 2013, pp. 19–38.
126. Holden, M. S.; Mundy, E. P.; MacLean, M.; and Wadhams, T. P.: Experimental Studies of Surface Roughness and Blowing Effects on a CEV Capsule. CUBRC Report 7670.02-F1, 2011.
127. Feldhuhn, R. H.: Heat Transfer from a Turbulent Boundary Layer on a Porous Hemisphere. AIAA Paper 1976–119, 1976.
128. Kaattari, G.: Effects of Mass Addition on Blunt-Body Boundary-Layer Transition and Heat Transfer. NASA TP 1139, 1978.
129. Park, C.: Injection-Induced Turbulence in Stagnation-Point Boundary Layers. *AIAA Journal*, vol. 22, no. 2, 1984, pp. 219–2225.
130. Williams, E. P.: Experimental Studies of Ablation Surface Patterns and Resulting Roll Torques. *AIAA Journal*, vol. 7, no. 9, 1971, pp. 1315–1321.
131. Canning, T. N.; Wilkins, M. E.; and Tauber, M. E.: Ablation Patterns on Cones Having Laminar and Turbulent Flows. *AIAA Journal*, vol. 6, no. 1, 1968, pp. 174–175.
132. White, C. O.; and Grabow, R. M.: Surface Patterns - Comparison of Experiment with Theory. AIAA Paper 1972–0313, 1972.
133. Hochrein, G.; and Wright, G.: Analysis of the Tater Nosetip Boundary Layer Transition and Ablation Experiment. AIAA Paper 1976–0167, 1976.

134. Tobak, M.: Hypothesis for the Origin of Cross-Hatching. *AIAA Journal*, vol. 8, no. 2, 1970, pp. 330–334.
135. Inger, G. R.: Three-Dimensional Heat-Transfer and Ablation Disturbance in High-Speed Flows. *AIAA Journal*, vol. 10, no. 12, 1972, pp. 1641–1646.
136. Powars, C. A.: Heatshield Ablation Pattern Roughness Onset Effects. Final Report for NASA Contract NNX09CC61P, 2009.
137. Dungan, S. D.; McQuaid, J. A.; Zibitsker, A. L.; Martin, A.; and Brehm, C.: Numerical Investigation of Fluid-Ablation Interactions for a Mach 5.3 Transitional Boundary Layer Flow Over a 13 Degree Cone. AIAA Paper 2023–0476, 2023.
138. Swigart, R. J.: Cross-Hatching Studies - A Critical Review. *AIAA Journal*, vol. 12, no. 10, 1974, pp. 1301–1318.
139. Lagenelli, A. L.; and Nestler, D. E.: Surface Ablation Patterns: A Phenomenology Study. *AIAA Journal*, vol. 7, no. 7, 1969, pp. 1319–1325.
140. Stackpoole, D. K. M.: Post-Flight Analysis of the Stardust Sample Return Capsule Earth Entry. AIAA Paper 2008–1197, 2008.
141. Trumble, K. A.: Post-Flight Aerothermal Analysis of the Stardust Sample Return Capsule. *Journal of Spacecraft & Rockets*, vol. 47, no. 5, 2010, pp. 765–774.
142. Yamada, T.; Inatani, Y.; Ishii, N.; Hirai, K.; and Morita, S.: Reentry of Hayabusa Sample Return Capsule and Post-flight Analysis of Recovered Heatshield. AIAA Paper 2011–3322, 2011.
143. Suzuki, T.; Fujita, K.; Yamada, T.; Inatani, I.; and Ishii, N.: Post-Flight TPS Analysis of Hayabusa Reentry Capsule. AIAA Paper 2011–3759, 2011.
144. Cheatwood, F. M.; Merski, N. R.; Riley, C. J.; and Mitcheltree, R. A.: Aerothermodynamic Environment Definition for the Genesis Sample Return Capsule. AIAA Paper 2001–2889, 2001.
145. Tang, C.; and Wright, M.: Analysis of the Forebody Aeroheating Environment During Genesis Sample Return Capsule Reentry. AIAA Paper 2007–1207, 2007.
146. Hunt, L. R.; and Bruce, W. E.: Experimental Study of Groove Patterns on Ablated Surfaces in the Langley 8-Foot High-Temperature Structures Tunnel. NASA TMX 2030, 1970.