

Experiments on Fluid-Structure Coupling under Impinging Shock Wave Loading

Thesis by
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In Partial Fulfillment of the Requirements for the
Degree of
Doctor of Philosophy



CALIFORNIA INSTITUTE OF TECHNOLOGY
Pasadena, California

2026
Defended 15th April, 2026

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ACKNOWLEDGEMENTS

First and foremost, I would like to express my sincere gratitude to my advisor, Professor Joanna Austin, for welcoming me into her research group and cultivating a collaborative and supportive lab environment. Her invaluable guidance and encouragement have been instrumental to my growth as a researcher, and the success of this thesis is a direct result of her mentorship.

I also need to express my thanks to Professor Hans Hornung, whose exceptional physical intuition and insights have been tremendously helpful whenever I encountered challenges in my research. I am profoundly grateful to Professor Joe Shepherd for his wide breadth of experimental knowledge and helpful feedback that he has provided over the years. Additionally, I want to thank Professors Ravi and Sader for their valuable feedback and contributions as members of my thesis and candidacy committees.

I am deeply indebted to all past and current members of the Caltech Hypersonics group: Michael Stramenga, William Feasey, Noel Esparza-Duran, Ying Luo, Wesley Yu, Jorge Rodríguez Gutiérrez, Joel Lawson, and Mallory Neet. I could not have asked for a better lab group. They have not only provided me so much help and technical guidance over the years but have also made working in lab so much more fun and enjoyable. Special thanks goes to Mallory Neet for teaching me how to operate the Caltech Ludwig Tube when I first joined the group, and I also want to acknowledge her accomplishments in designing and conducting fluid-structure interaction experiments in the Ludwig Tube, which served as a foundation for this work. Additionally, I need to thank past and present members of the Explosion Dynamics Laboratory: Branson Davis, Donner Schoeffler, Conor Martin, and Charline Fouchier. I appreciate their technical support as well as the productive discussions and shared exchange of ideas that have contributed to my research.

I am sincerely grateful to our lab administrator, Liza Bradulina, who manages the lab logistics and ensures that our lab runs smoothly. Without her, this work would not have been possible. Thank you to the rest of the GALCIT staff for their support and help in facilitating my research and other department activities. This project was also possible thanks to the machining support from Ali and Brandon at the Aero shop.

I would also like to acknowledge my collaborators at the Ohio State University

Multi-Physics Interactions Research Group and High-Fidelity Computational Multi-Physics Laboratory: Professors Jack McNamara and Datta Gaitonde and Matthew Kronheimer. They conduct amazing fluid-structure interaction simulations and have been a great resource for sharing and discussing my experimental results.

To my GALCIT classmates and friends, thank you for greatly enhancing my graduate school experience. I would not have survived online graduate school during the first year without you, and our weekly board game nights have provided a much needed escape from classes and research.

I also appreciate all the support I received while in undergrad at Rice University. I would like to recognize Professor C. Fred Higgs III and Jordan Wagner at the Particle Flow & Tribology Lab for teaching me about fluid mechanics research and showing me what graduate school is like. Special thanks goes to Professors Matthew Brake and Eleazar Marquez for also inspiring me to attend graduate school. To all my classmates and friends at Rice, I miss having your company working on problem sets and engineering projects together, but I am grateful we have been able to stay in touch!

Thank you to my family for their endless encouragement and support throughout graduate school. I am fortunate to benefit from the wisdom of two Dr. Acostas already in the family, who have provided guidance in helping me navigate graduate school.

Finally, thank you Vina for always being there for me. I greatly appreciate your support as well as for distracting me from work and reminding me there is life outside of school.

This work is supported in part by the Air Force Office of Scientific Research under Award No. FA9550-22-1-0063, subaward from The Ohio State University, with Dr. Sarah Popkin and Dr. Amanda Chou as program officers, and by the National Science Foundation Graduate Research Fellowship under Grant No. DGE-1745301.

ABSTRACT

An impinging shock wave/turbulent boundary layer interaction over a compliant panel was experimentally examined in a Mach 4 Ludwig Tube through simultaneous measurements of both the fluid and structure response. Synchronized measurements were obtained using colinear focused laser differential interferometry and high-speed schlieren together with single-camera stereo-photogrammetry. The static and dynamic behavior of the compliant panel itself was also characterized through response to pressurization, roving hammer, and panel flutter experiments.

When the compliant panel was subjected to shock impingement at the mid-chord and leading-edge locations, the initial panel response was dominated by oscillations at the fundamental vibration mode and was primarily governed by one-way uncoupled fluid-structure behavior. However, under leading-edge impingement, oscillations at the fundamental vibration mode eventually dampened, and a transition to a two-way coupled fluid-structure response was observed. Under two-way coupling, the interaction was characterized by a reduction in oscillation amplitudes and an increase in relative strength of high-order vibration modes.

Phase analysis at the fundamental vibration mode between the separation and reattachment shock motion revealed that the one-way uncoupled mid-chord impingement case exhibited a repeatable phase alignment, where the phase of the separation shock tended to lead that of the reattachment shock by approximately one quarter of a panel oscillation. However, consistent with the behavior in rigid wall shock wave/boundary layer interactions, the instantaneous reattachment motion was anti-correlated and led that of the separation shock in all compliant cases. The transition to a two-way coupled response strengthened the synchronization between the separation shock and panel motion compared to that of the one-way uncoupled response.

Spectral analysis of the mid- and high-frequency content of density fluctuations in the separation bubble and boundary layer showed that the spectral distributions were not significantly modified due to surface compliance. Instead, the magnitude of the density fluctuations inside the separation bubble were attenuated under mid-chord impingement compared to that of leading edge impingement. Moreover, in all compliant cases, surface compliance amplified the spectral energy in the boundary layer downstream of flow reattachment by over a factor two compared to that of the rigid case.

PUBLISHED CONTENT AND CONTRIBUTIONS

Acosta, A. R. and Austin, J. (2026). “Experimental study of shock impingement over a compliant panel with a turbulent boundary layer in Mach 4 flow”. In: *AIAA SCITECH 2026 Forum*. DOI: [10.2514/6.2026-1243](https://doi.org/10.2514/6.2026-1243).

Acosta, A., Luo, Y., and Austin, J. (2025). “Simultaneous fluid and structure measurements of an impinging shock-boundary layer interaction in Mach 4 flow”. In: *35th International Symposium on Shock Waves*.

Luo, Y., Acosta, A., Austin, J., and Hornung, H. (2025). “Co-linear focused laser differential interferometry and high speed schlieren measurements of supersonic cylinder near wake”. In: *35th International Symposium on Shock Waves*.

Acosta, A. R. and Austin, J. M. (2024). “Experimental investigation of an impinging shock-boundary layer interaction on a compliant panel in Mach 4 flow”. In: *AIAA SCITECH 2024 Forum*. DOI: [10.2514/6.2024-1153](https://doi.org/10.2514/6.2024-1153).

The above articles contain material that is adapted for parts of Chapters 2-5 of this thesis. Chapters 2 and 3 include experimental details, diagnostic setups, and data processing techniques from Acosta and Austin (2024), Acosta et al. (2025), Luo et al. (2025), and Acosta and Austin (2026). Model characterization experimental results presented in Acosta and Austin (2024) and Acosta and Austin (2026) are adapted in Chapter 4. Finally, Chapter 5 includes experimental results and analyses from Acosta and Austin (2024), Acosta et al. (2025), and Acosta and Austin (2026).

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Chapter 1

INTRODUCTION

Weight constraints typically necessitate that the outer skin of high-speed vehicles consist of thin, lightweight panels. However, the inherently low flexural rigidity of thin panels means that the outer skins are susceptible to deformation under strong external loading (McNamara and Friedmann, 2011). In supersonic and hypersonic flight environments, aerodynamic surface loads can be significantly amplified by shock waves generated by leading edges, fins, corners, and other flow deflecting features. Shock waves impinging on the vehicle surface produce an adverse pressure gradient on the boundary layer, which may lead to shock wave/boundary layer interactions (SBLIs).

The frequencies associated with unsteadiness in SBLIs are typically orders of magnitude lower than characteristic turbulent boundary layer frequencies, and as such, SBLI flows may particularly excite and induce resonance with the vibration modes of the panel, leading to a coupled fluid-structure interaction (FSI). Feedback between the flow and structure can amplify structural oscillation amplitudes, significantly degrade aerodynamic performance, and enhance thermal loads. In severe cases, such interactions may compromise structural integrity or control authority, ultimately posing a risk of catastrophic failure.

Consequently, the effects of surface compliance on SBLIs often guide design-driving requirements for the structural skin panels on high-speed vehicles. However, uncertainties of fluid-structure coupling mechanisms and complexities of the SBLI unsteadiness have made accurately predicting the coupled aerodynamic and structural response of compliant surfaces subjected to SBLIs emerge as a significant challenge in the development of high-speed vehicles (Zuchowski, 2012). This work presents ground-test studies with time-resolved—and synchronized—full-field measurements of both the flow and structural response to gain insights on the nature of fluid-structure coupling in SBLIs over compliant surfaces at Mach 4 conditions.

1.1 Shock Wave/Turbulent Boundary Layer Interactions

When a shock impinges on a boundary layer in supersonic flow, the boundary layer thickens due to the adverse pressure gradient. If the pressure gradient is

sufficiently strong to overcome the momentum of the incoming flow, the boundary layer separates from the surface. The extent of flow separation is governed by the strength of the impinging shock and nature of the incoming boundary layer.

The present work focuses on shock wave/turbulent boundary layer interactions (STBLIs), which can be encountered in many practical high-speed flight conditions. STBLIs arise in a broad range of internal and external high-speed flow applications, including engine inlets and aerodynamic control surfaces.

Many different canonical SBLI configurations have been investigated to elucidate the fundamental physics of these interactions (Clemens and Narayanaswamy, 2014; Sabnis and Babinsky, 2023). SBLI configurations are commonly classified as either nominally two-dimensional (2D) or three-dimensional (3D), and they are further distinguished by whether the separated flow region is closed or open. Nominally 2D interactions, such as unswept compression ramp and impinging oblique shock interactions, typically produce a closed separation bubble in which the separated flow recirculates upstream. In contrast, configurations such as the blunt fin and swept compression ramp are 3D due to their curved or swept shocks and spanwise non-uniform separation. Although the blunt fin interaction also produces a closed separation region, the swept compression ramp forms an open separation region, where the separated fluid is instead convected downstream in a large-scale vortical motion (Clemens and Narayanaswamy, 2014).

Despite the geometric differences between different SBLI configurations, separated 2D STBLIs exhibit remarkably similar unsteady characteristics of the separation bubble, separated shear layer, and separation shock dynamics (Clemens and Narayanaswamy, 2014). Three characteristic length scales are typically used to describe the interaction: the incoming boundary layer thickness, δ , the streamwise length of the separation bubble, L_{sep} , and the streamwise length of the intermittent region of the separation shock foot, L_i . When combined with the freestream velocity, u_∞ , these scales can be used to non-dimensionalize frequency, f , through Strouhal numbers defined as $St_\delta = f\delta/u_\infty$, $St_{L_{\text{sep}}} = fL_{\text{sep}}/u_\infty$, and $St_{L_i} = fL_i/u_\infty$.

Separated 2D STBLI interactions are known to contain three distinct frequency ranges that span several orders of magnitude. The high-frequency range is associated with the most energetic scales of the turbulent boundary layer. Upstream of the interaction, this energy scales with the boundary layer thickness and is centered around $St_\delta \approx 1$. Downstream of the interaction, the characteristic scale shifts to the separation length, yielding $St_{L_{\text{sep}}} \approx 1$ (Helm et al., 2021; Pasquariello et al., 2017;

Priebe and Martín, 2012). The mid-frequency range is typically associated with separation bubble and shear layer dynamics. This includes Kelvin-Helmholtz vortex shedding along the separated shear layer near $St_{L_{sep}} \approx 0.5$ as well as oscillations of the separation bubble itself around $St_{L_{sep}} \approx 0.1$ (Adler and Gaitonde, 2018; Agostini et al., 2012). The lowest-frequency motions correspond to the large-scale, broadband “breathing” behavior of the separation bubble and the associated motion of the separation shock foot, typically occurring within $St_{L_{sep}} \approx 0.02 - 0.05$ or $St_{L_i} \approx 0.01 - 0.03$ (Clemens and Narayanaswamy, 2014; Dussauge et al., 2006).

The leading theory of the mechanism driving the low-frequency unsteadiness is that for weak interactions with small separation lengths relative to the boundary layer thickness, the low-frequency separation shock motion is highly correlated with the upstream turbulent boundary layer fluctuations (Clemens and Narayanaswamy, 2014; Plotkin, 1975; Toubert and Sandham, 2011). For stronger interactions, the unsteadiness is primarily dominated by downstream mechanisms inherent to the shock and separation bubble system (Clemens and Narayanaswamy, 2014; Piponniau et al., 2009; Wu and Martín, 2008).

1.1.1 Upstream Mechanism

The characteristic time scales associated with turbulent boundary layer fluctuations are typically one to two orders of magnitude smaller than the time scales associated with the low-frequency unsteadiness observed in STBLIs. Plotkin (1975) proposed a mathematical model that demonstrated how high-frequency perturbations originating from the incoming turbulent boundary layer can lead to relatively low-frequency motion of the separation shock foot. In this model, streamwise velocity fluctuations in the upstream turbulent boundary layer perturb the shock as they pass through it, causing the shock to be displaced upstream or downstream. Once displaced, the shock is assumed to be restored back towards its mean position due to the stability of the mean flow field. Based on these assumptions, Plotkin (1975) formulated a first-order ordinary differential equation describing the shock motion. The resulting characteristic response time predicted by the model was found to be consistent with the low-frequency content observed in the spectra of the interaction.

Building on this idea, Toubert and Sandham (2011) derived a similar first-order model describing the coupled dynamics of the shock and upstream boundary layer. When forced with white noise perturbations representing broadband turbulent fluctuations, their model reproduced wall pressure spectra that closely matched those observed

in experiments and numerical simulations. Their analysis demonstrated that the interaction between the shock and upstream boundary layer behaves mathematically as a first-order low-pass filter. Consequently, they argued that the prominent low-frequency unsteadiness observed in STBLIs does not necessarily originate from low-frequency forcing within the incoming turbulence itself, but rather emerges as an intrinsic property of the coupled shock-boundary layer system.

1.1.2 Downstream Mechanism

Piponniau et al. (2009) proposed a model that linked the separated shear layer vortices generated by the Kelvin-Helmholtz instability to the low frequency motion of the separation shock. They identified that eddies are formed in the separated shear layer and grow as they move downstream. Fluid from the separation bubble is entrained by the shear layer, and after some distance, the eddies are shed into the downstream flow, bringing with them a certain amount of mass (Fig. 1.1). The high velocity side of the shear layer is continuously fed by the incoming turbulent boundary layer and, therefore, is unaffected by the mass ejection. However, on the low velocity side in the recirculating region, there is no source of mass to replace the amount that is entrained by the shear layer. Thus, the separation bubble itself must expand to reinject mass into the bubble in order to maintain a mean separated region. The upstream movement of the separation point pushes the separation shock upstream with it. As the shock moves upstream, the pressure gradient in the separation bubble decreases, which reduces the amount of fluid entrained by the shear layer. This causes the separation bubble to become unstable and rapidly shrink, pushing the shock back downstream. The pressure gradient in the separation bubble then increases, and the amount of fluid entrained by the shear layer subsequently increases. As a result, the cycle continues where the separation bubble must then grow in size again to reinject mass into the bubble.

The time scale of the cycle of the separation bubble expansion/contraction was defined by Piponniau et al. (2009) as the ratio of the initial mass in the mean separation bubble divided by the rate of mass entrainment due to the shear layer. They showed by modeling the separated shear layer as a canonical free shear layer, one can derive an analytical expression of this time scale. Their estimation of the characteristic frequency of the separation bubble, defined as the inverse of this time scale, was found to agree with the order of magnitude of the low-frequency separation shock and bubble motions measured experimentally.

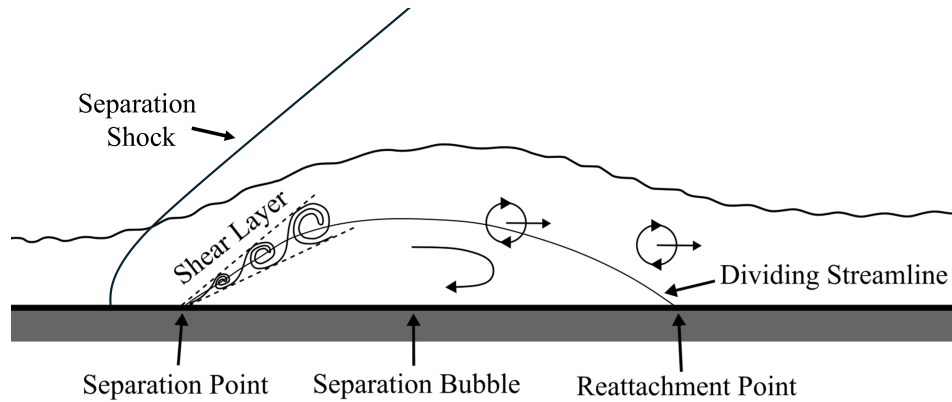


Figure 1.1: Shear layer low frequency unsteadiness mechanism (diagram adapted from Piponniau et al. (2009)).

Later studies investigated the likeliness of separated shear layers to canonical free shear layers and found they share many similarities (Dupont et al., 2019; Helm et al., 2021; Priebe and Martín, 2012). Dupont et al. (2019) and Helm et al. (2021) showed the mean velocity and turbulence stress profiles collapsed when using a redefined coordinate system and similarity variable. The profiles resembled those of the canonical free shear layer case, with the exception that they were slightly non-symmetric and biased towards the high-speed, turbulent boundary layer side. Their results indicated that the separated shear layer grows linearly up to the apex of separation bubble, where the incident shock impinges on the layer. The measured spreading rate was found to be typical of a counterflow shear layer. However, the most significant difference of separated shear layers compared to the canonical case was the greatly increased turbulence production rate. Priebe and Martín (2012) found the largest values of in-plane turbulent kinetic energy occurred approximately in the middle of the separated shear layer and were attributed to the formation and shedding of the vortical shear layer structures. The intensity of these energetic turbulent structures in the shear layer was dependent on the phase of the low-frequency unsteadiness.

1.1.3 Shock Wave/Turbulent Boundary Layer Interaction Dynamics

Consistent with the characteristic breathing motion of the separation bubble, Wu and Martín (2008) observed that the separation and reattachment point locations were negatively correlated. This relationship indicated that the separation and reattachment points move in opposite directions as the separation bubble grows and shrinks. In addition, they found that the motion of the reattachment point led that of the separation point, suggesting that downstream dynamics in the separation bubble

and reattachment region play an important role in governing the unsteady behavior of the interaction.

Further insight into this behavior was provided by Priebe and Martín (2012), who showed that the reattachment point followed a sawtooth-like trajectory. In this motion, the reattachment point convected downstream at an approximately constant speed before rapidly shifting upstream. Their spectral analysis revealed that the separation point motion was dominated by low-frequency fluctuations associated with the large-scale breathing of the separation bubble. In contrast, while the reattachment point also contained some low-frequency energy, a significant portion of its spectral content resided at mid-frequencies associated with large-scale vortical structures forming within the separated shear layer and convecting downstream.

Although nominally 2D STBLIs exhibit predominantly 2D flow features, the interactions remain moderately 3D. In the reattachment region, pairs of counter-rotating streamwise vortices, commonly referred to as Görtler-like vortices, induce spanwise variations in the reattachment region due to vortex-induced upwash and downwash (Pasquariello et al., 2017). In numerical simulations, these vortices typically exhibited no preferred spanwise position and tended to meander randomly over time. However, small experimental model imperfections could introduce a preferred spanwise position for these structures (Pasquariello et al., 2017).

1.1.4 Impinging Shock Wave/Turbulent Boundary Layer Interactions

One type of a nominally 2D interaction is the impinging shock wave/turbulent boundary layer interaction. In an impinging STBLI, as illustrated in Fig. 1.2, an incident oblique shock impinges on a turbulent boundary layer. If the shock strength is great enough, the boundary layer will separate, forming a closed separation bubble. Compression and expansion waves form as a result of the deflection of flow around the separation bubble. The compression waves may coalesce to form separation and reattachment shocks.

1.2 Panel Flutter

Aeroelasticity is the study of how the inertia and elasticity of a structure interact with the aerodynamic forces induced by a surrounding flow field (Dowell, 2022). Since the beginning of powered flight, the effects of aeroelasticity have played an important role in the design and development of aircraft (Garrick and Reed, 1981). One of the most significant types of aeroelastic instabilities in high-speed flight is flutter, which is a self-excited, dynamic instability that results in sustained, large

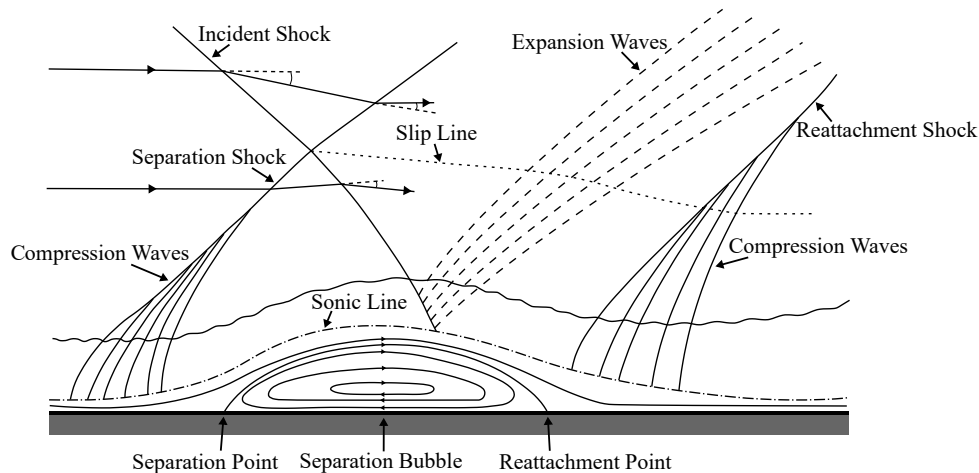


Figure 1.2: Impinging shock-turbulent boundary layer interaction (diagram adapted from Babinsky and Harvey (2011)).

amplitude oscillations that could potentially lead to structural failure (Dowell, 1972). Consequently, flutter is typically an undesirable phenomenon that vehicle designers attempt to avoid or mitigate.

Prior to flutter onset, pressure fluctuations in the turbulent boundary of a supersonic freestream drive random, low-amplitude oscillations of a panel that are significantly less than the panel thickness (Dowell, 1970). When the flutter condition is reached, the oscillations shift to a nearly sinusoidal response with amplitudes on the order of the panel thickness (Dowell, 1974). This post-flutter behavior is known as limit cycle oscillations and is characteristic of a balance between the unsteady flow and structural forces. The boundary at which flutter occurs for a particular panel configuration is highly dependent on both panel structural properties as well as the freestream and boundary layer conditions.

Some of the foundational work on flutter originated at the Graduate Aerospace Laboratories at the California Institute of Technology (GALCIT) in the 1950s and 1960s under the supervision of Prof. Yuan-Cheng Bert Fung. Fung and his students conducted experiments that were among the first to measure and characterize flutter boundaries under specific structural and flow conditions, and their work provided important insights into the validity of structural and aerodynamic modeling assumptions for the flutter of rectangular plates and circular cylindrical shells (Anderson, 1963; Cummings, 1957; Eisley, 1956; Fleming, 1960; Fung, 1963; Lock, 1961; Olson and Fung, 1967; Stearman et al., 1962; Stearman, 1961). Notably, Olson (1967)

extended the application of the finite element method (FEM) to the analysis of linear panel flutter problems for configurations where no exact solutions may be readily available. The flexibility of the FEM framework allows complex panel geometries and configurations to be modeled with relative ease. As a result, a wide range of physical effects could be incorporated into linear panel flutter analyses, including aerodynamic damping, anisotropic panel properties, in-plane stresses, and thermal loading (Mei et al., 1999; Olson, 1967, 1970).

Today, aeroelastic analyses are typically performed using a spectrum of approaches, ranging from simplified analytical models, high-fidelity numerical simulations that couple finite element analysis (FEA) and computational fluid dynamics (CFD) solvers, as well as hybrid numerical-theoretical approaches (McNamara and Friedmann, 2011). However, the applicability and validity of theoretical and reduced-order aeroelastic models for compliant structures experiencing loading from STBLIs remain an active area of investigation.

1.2.1 Coupled Fluid-Structure Model

The coupled fluid-structure governing equations of motion for a thin rectangular plate in supersonic or hypersonic flow, as derived by Freydin and Dowell (2021) in modal coordinates, are presented in Eqs. 1.1 & 1.2. These equations incorporate a nonlinear structural model and linear aerodynamic and cavity acoustic models to capture the coupled dynamics of the plate, fluid, and cavity. The plate is assumed to be clamped along all edges for out-of-plane displacement, but in-plane displacements are allowed and modeled using specified in-plane edge stiffnesses. The effect of thermal loading is incorporated in the structural stiffness tensor, and a generalized force term accounts for the static pressure differential.

$$\underbrace{M_{nk}\ddot{w}_k + C_{nk}\dot{w}_k + K_{nk}w_k}_{\text{linear plate model}} + \underbrace{G_{nkrs}w_kw_rw_s}_{\text{nonlinear structural stiffness}} + \underbrace{C_{PT_{nk}}\dot{w}_k + K_{PT_{nk}}w_k}_{\text{piston theory aerodynamics}} - \underbrace{H_{c_{kn}}p_k}_{\text{cavity coupling}} + \underbrace{Q_{s_n}}_{\text{static pressure differential}} = 0 \quad (1.1)$$

$$\underbrace{\rho_c H_{c_{nk}}\ddot{w}_k}_{\text{plate coupling}} + \underbrace{\frac{1}{a_c^2} M_{c_{nk}}\ddot{P}_k + K_{c_{nk}}P_k}_{\text{acoustic wave equation}} = 0 \quad (1.2)$$

In both Eqs. 1.1 & 1.2, non-repeated indices represent a specific mode, and repeated indices imply summation. Complete definitions of the matrices, M_{nk} , C_{nk} , G_{nkrs} , $C_{PT_{nk}}$, $K_{PT_{nk}}$, $H_{c_{nk}}$, $M_{c_{nk}}$, $K_{c_{nk}}$, and Q_{s_n} , are detailed in Freydin and Dowell (2021). The cavity fluid density and speed of sound are known and denoted as ρ_c and a_c , respectively. The unknown terms in the equations are the modal coordinates, w_n and P_n , which are the time-dependent coefficients of the out-of-plane panel displacement, $z(x, y, t)$, and cavity pressure, $p_c(x, y, z, t)$, as shown in Eqs. 1.3 & 1.4. The j -th basis functions of the out-of-plane displacement and cavity pressure expansions are represented as $\Phi_j(x, y)$ and $\Psi_j(x, y, z)$, respectively .

$$z(x, y, t) = \sum_j^{n_{\text{modes}}} w_j(t) \Phi_j(x, y) \quad (1.3)$$

$$p_c(x, y, z, t) = \sum_j^{n_{\text{modes}}} P_j(t) \Psi_j(x, y, z) \quad (1.4)$$

The second-order structural tensors consist of the panel inertia (M_{nk}), damping (C_{nk}), and linear stiffness (K_{nk}). These tensors characterize the panel response when the vibration amplitudes are small relative to the panel thickness. The fourth-order structural tensor, G_{nkrs} , represents the nonlinear panel stiffness, and its contribution to the panel response becomes significant when the vibration amplitudes are on the order of the panel thickness (Eitner et al., 2023).

The aerodynamic model used in Eq. 1.1 is first-order classical piston theory, which is incorporated through the aerodynamic damping and stiffness tensors, $C_{PT_{nk}}$ and $K_{PT_{nk}}$. Classical piston theory was originally developed by Lighthill (1953) by extending the idea of hypersonic equivalence between n -dimensional steady flow and $(n - 1)$ -dimensional unsteady flow proposed by Hayes (1947) and Tsien (1946). Lighthill considered that when a thin column of fluid, oriented perpendicular to the freestream, travels over a moving surface at a velocity much greater than the surface motion, the fluid element remains approximately normal to the surface, and the surface can be modeled as a piston acting on an inviscid isentropic fluid. The effective velocity component normal to the surface, W_{eff} , is a function of both the local time rate of change of panel displacement and its spatial gradient in the flow direction, as shown in Eq. 1.5.

$$W_{\text{eff}} = \left(\frac{\partial}{\partial t} + u_{\infty} \frac{\partial}{\partial x} \right) z(x, y, t) \quad (1.5)$$

Furthermore, given that the surface motion is analogous to that of a piston moving in a one-dimensional tube, the local pressure induced by the surface motion can be modeled using the isentropic pressure-Mach number relation (Eq. 1.6).

$$\frac{p}{p_\infty} = \left(1 + \frac{\gamma - 1}{2} \left(\frac{W_{\text{eff}}}{a_\infty} \right)^2 \right)^{\frac{\gamma}{\gamma - 1}} \quad (1.6)$$

Lighthill recommended expanding Eq. 1.6 up to a third-order binomial expansion (Eq. 1.7) to minimize errors (Lighthill, 1953). However, the first-order variant (Eq. 1.8) is also generally valid for sufficiently high Mach numbers and when Mach number times the deflection/chord ratio is much smaller than one (Eitner et al., 2023). The first term in Eq. 1.8 forms the aerodynamic damping tensor, $C_{\text{PT}_{nk}}$, and the second is responsible for the aerodynamic stiffness tensor, $K_{\text{PT}_{nk}}$.

$$\frac{p}{p_\infty} = 1 + \gamma \left(\frac{W_{\text{eff}}}{a_\infty} \right) + \frac{\gamma(\gamma + 1)}{4} \left(\frac{W_{\text{eff}}}{a_\infty} \right)^2 + \frac{\gamma(\gamma + 1)}{12} \left(\frac{W_{\text{eff}}}{a_\infty} \right)^3 \quad (1.7)$$

$$\Delta p_{\text{PT}} = p - p_\infty = \frac{\rho_\infty u_\infty}{M_\infty} \left(\frac{\partial z}{\partial t} + u_\infty \frac{\partial z}{\partial x} \right) \quad (1.8)$$

The implication of Eq. 1.8 signifies that the unsteady aerodynamic pressure induced by surface motion is a function of both the time rate of change and streamwise spatial gradient of panel displacement. In addition, the pressure is also scaled by the freestream quantities, ρ_∞ , u_∞ , and $1/M_\infty$.

Although higher-fidelity fluid-structure numerical modeling techniques now exist with the rapid increase in computational power, piston theory remains a valuable analytical tool to understand the contributions of aerodynamic damping and stiffness on the coupled fluid-structure system.

1.3 Compliant Surface Shock Wave/Turbulent Boundary Layer Interactions

As introduced in Section 1.1, a defining characteristic of separated STBLI flows over rigid walls is their broadband, low-frequency unsteadiness inherent to the separation shock and separation bubble motion (Clemens and Narayanaswamy, 2014). When surface compliance is introduced, the unsteady surface pressure loads associated with STBLIs can excite multiple structural vibration modes, which can significantly modify the interaction dynamics through fluid-structure coupling. If the loads and dynamics generated by this fluid-structure interaction are not accurately predicted,

they may pose a significant risk to high-speed vehicles. Structural oscillations enhanced by resonance with the STBLI unsteadiness can adversely affect the vehicle performance and could potentially lead to structural failure due to fatigue.

Although theoretical aeroelastic models have successfully predicted the structural behavior of panels in supersonic flows using simplified aerodynamic models, such as piston theory, these frameworks often fail to capture the complex physics of STBLIs (Eitner et al., 2023). Consequently, characterizing the behavior of STBLIs over compliant surfaces has necessitated a reliance on high-fidelity numerical simulations and experiments, where both of which present significant technical challenges.

1.3.1 Fluid-Structure Coupling Modeling Techniques

From a numerical perspective, the inherent low-frequency unsteadiness of STBLIs requires long integration times to resolve the lowest-order flow time scales. Additionally, these interactions exhibit spanwise variation driven by both intrinsic flow structures and three-dimensional structural deformations. Thus, high-fidelity simulations must be computed over a three-dimensional (3D) volume with a relatively long integration time. While Reynolds-averaged Navier Stokes (RANS) models offer computational efficiency, they generally struggle to predict strong flow separation and the associated low-frequency unsteadiness (Hoy and Bermejo-Moreno, 2022; Sandham et al., 2014). This necessitates the use of Large Eddy Simulations (LES) or Direct Numerical Simulations (DNS). However, strict grid resolution requirements of these higher-fidelity methods often limit the feasible Reynolds number range for such studies. Furthermore, the fidelity and computational cost also depend heavily on the chosen fluid-structure coupling method.

1.3.1.1 Two-Way Coupling

This coupling method is, at present, the most rigorous approach to accurately model the communication between the flow and structure in aeroelastic problems. Two-way coupling involves communication between the CFD and FEA solvers at each time step. Initially, the CFD solver computes the flow solution at the first time step. The wall pressure on the compliant surface is then treated as a load input for the FEA solver. The FEA solver then calculates the resulting displacement, velocity, and acceleration of the compliant panel, which are sent as inputs to the CFD solver to deform the computational mesh. The process is repeated at each time step, forming a two-way coupled feedback framework. Because of the difference in time-scales

between the flow and structure, sub-time steps are frequently employed in the CFD solver to maintain stability and reduce the overall computational cost (Shinde et al., 2022).

1.3.1.2 One-Way Coupling

A less computationally expensive approach to two-way coupling is created by removing one side of the coupling feedback loop. In one-way prescribed motion simulations, the structure follows a predetermined displacement profile, allowing the flow to respond to the surface without the flow response feeding back into the structural dynamics. In contrast, in the one-way flow to structure coupling mechanism, the structural response is calculated using wall pressures obtained from prior flow simulations without allowing the resulting deformation to modify the flow field.

1.3.1.3 Static Deformation

Rather than prescribing structural motion, this coupling approach prescribes a static structural deformation for the duration of the simulation. This effectively isolates the influence of surface curvature while neglecting dynamic effects associated with surface velocity and acceleration.

1.3.1.4 Piston Theory

Piston theory, first developed by Lighthill (1953), has been extended to both STBLI analytical models and numerical simulations. Local piston theory replaces the freestream flow quantities, ρ_∞ , u_∞ , M_∞ , p_∞ , in Eq. 1.8 by the local flow quantities to more accurately model the STBLI aerodynamic loading.

1.3.2 Evaluation of Coupling Methods

Building on the upstream mechanism model developed by Toubert and Sandham (2011) and classical piston theory, Brouwer et al. (2017) extended the original models to incorporate the effect of surface compliance on STBLIs to demonstrate that the separation point streamwise location is directly proportional to panel surface curvature and the spatial gradient of surface velocity. Through one-way prescribed motion RANS numerical simulations, both terms were found to compete against each other in shifting the separation point. Furthermore, they found that the lower the frequency of oscillation, the closer the interaction resembled a statically deformed

panel case, whereas increasing the frequency of panel oscillations led to reduced variation of the separation length.

Brouwer and McNamara (2019) developed a variant of local piston theory that utilizes the static surface deformation in the computation of local flow quantities from steady CFD simulations. Their model effectively captured the unsteady shock induced structural limit-cycle oscillations when compared with Euler and RANS simulations. However, their model assumes a quasi-steady, inviscid flow, which becomes less accurate for large separation length to panel surface length ratio interactions since the unsteady viscous effects are not fully captured.

Recently, Kronheimer et al. (2025) compared the flow response between an uncoupled one-way interaction, coupled two-way interaction, statically deformed panel, and statically deformed panel with local piston theory corrections through implicit Large Eddy Simulations (ILES). When the panel was held fixed at the time-averaged deformation shape as computed through coupled two-way interaction simulations, the resulting mean wall pressure closely matched that of the two-way coupled interaction. However, this case faced less extreme pressure fluctuations than the two-way coupled case on the panel downstream of flow reattachment. The superposition of the statically deformed panel with local piston theory implemented on the panel downstream of flow reattachment resulted in a more accurate level of pressure fluctuations that produced comparable fluctuations to that of the two-way coupled case, both in terms of low-frequency spectral content and RMS value. Though, farther downstream near the panel trailing edge, the local piston theory corrections over a statically deformed model did not account for all discrepancies in flow unsteadiness between the uncoupled and coupled interaction. This indicated that the coupling mechanisms in this region cannot be accounted for with quasi-steady induced pressure superposition with the statically coupled pressure.

Hoy and Bermejo-Moreno (2022) similarly conducted wall-modeled Large Eddy Simulations (WMLES) for an impinging STBLI and compared the two-way coupled solution results to those computed from one-way uncoupled, one-way uncoupled with local piston theory correction, and two-way coupled inviscid flow simulations. As expected, the inviscid two-way coupled simulation resulted in large discrepancies in wall pressure and panel deflection. The piston theory corrected one-way uncoupled case accurately predicted the time-averaged wall pressure upstream of flow separation and downstream of the STBLI compared to that of the two-way coupled case. However, the panel response predicted by the piston theory case

exhibited noticeable discrepancies compared to the two-way coupled case. Though, the predictions were significantly improved compared to that of the strictly one-way uncoupled case. Additionally, the piston theory case overestimated the peak pressure and exhibited a smaller separation bubble compared to that of the two-way coupled case. Therefore, Hoy and Bermejo-Moreno (2022) concluded local piston theory is not accurate in the separation region and suggested additional corrections are needed to improve its accuracy.

Despite the encouraging performance of lower-order coupling methods in predicting the structural and flow responses in STBLIs, each approach has inherent limitations. This motivates further research to better define their range of applicability and to develop new insights into the conditions under which higher-fidelity coupling methods become necessary.

1.3.3 Characteristics of Compliant STBLI

High fidelity experiments and two-way coupled simulations have provided important insights into the interaction dynamics of compliant STBLIs. These studies have revealed that surface compliance can significantly influence the separation bubble size, extent of the separation shock foot motion, and synchronization of the STBLI dynamics. However, the magnitude and direction of these effects remain partially inconsistent across studies, highlighting the strong dependence on the configuration parameters.

For example, while some studies observed that surface compliance increases the separation bubble length (Daub et al., 2016; Hoy and Bermejo-Moreno, 2022), others reported a reduction compared to the rigid wall case (Pham et al., 2018; Shinde et al., 2022; Visbal, 2014). These differing behaviors likely arise from variations in key interaction properties, including the mean panel deformation shape, strength of the shock interaction, panel stiffness, and the particular vibration modes excited during the interaction, among others. As a result, establishing consistent trends in mean STBLI characteristics, such as separation bubble length, across different configurations remains challenging.

Furthermore, the sensitivity to configuration is also reflected in the observed coupling between the flow and structural dynamics. Eitner et al. (2023) reported in their compression ramp experimental study that the introduction of an STBLI had no measurable effect on the frequency, amplitude, or mode shape of the compliant panel fundamental vibration mode when compared to the structural response under

only a supersonic turbulent boundary layer. Instead, the STBLI impact on the structural response was primarily restricted to an increase in the vibration amplitude of higher-order vibration modes. In addition, the flow field structure and dynamics significantly changed when STBLI was introduced, where the shock foot motion was observed to primarily oscillate at the fundamental vibration mode. This result indicated that although there was strong communication from the structural oscillations to the flow field, the interaction lacked a strong coupling mechanism from the flow field to the lowest-order vibration mode of the panel.

By contrast, in their experimental study of an impinging STBLI, Varigonda and Narayanaswamy (2023) observed that the two lowest-order panel vibration modes underwent a reduction in strength in the presence of shock impingement compared to without impingement. A computational study by Shinde et al. (2022) similarly observed a reduction in vibration amplitude of the panel response in the two-way coupled interaction compared to a one-way uncoupled panel response. They suggested that the elevated level of turbulence caused by surface compliance serves to attenuate the panel vibration when the fluid and structure are fully coupled. Though, consistent with the observations of Eitner et al. (2023), Varigonda and Narayanaswamy (2023) also found that the presence of STBLI excited several higher-order vibration modes compared to the case without STBLI. Additionally, surface pressure cross-correlation maps indicated that the breathing motion of the separation bubble may be strengthened by surface compliance relative to the rigid wall case.

Typically, in a rigid wall case, the reattachment shock motion precedes the separation shock motion (Wu and Martín, 2008). Varigonda and Narayanaswamy (2023) found this trend also persisted in the compliant case for frequencies that do not coincide with the panel modes. However, at frequencies corresponding to the panel vibration modes, they observed that the separation and reattachment shock motions became synchronized. They attributed this behavior to increased spanwise coherence of the separation shock at the vibration mode frequencies, which promoted the formation of spanwise coherent shear layer eddies that convected downstream and drove the reattachment shock motions. Interestingly, despite these changes in the interaction dynamics, wall pressure power spectral density measurements did not indicate a global change to the shear layer growth rate or boundary layer dynamics due to surface compliance.

Analogous to the behavior of STBLIs over rigid walls, Musta et al. (2022) observed in a compliant compression ramp experimental study that the separation shock

foot motion was anti-correlated with the reattachment point motion. Their cross-correlation analysis further showed that the reattachment motion led the separation shock foot motion. Other studies examining the lead-lag relationship between the separation shock motion and panel oscillations have reported differing results. For example, D’Aguanno et al. (2023) observed that the separation shock motion lagged the fundamental vibration mode oscillations by approximately a quarter cycle. On the other hand, Peltier et al. (2019) found that the separation shock motion preceded that of the panel oscillation. These differing observations of the lead-lag behavior in compliant STBLI suggest that the coupling mechanisms and interaction dynamics can vary substantially depending on the configuration.

In Ludwig Tube experiments of a clamped-free-clamped-free compliant panel under shock impingement conducted by Vasconcelos et al. (2023), the compliant panel underwent an abrupt transfer of energy from the impulsive loading of flow arrival. This drove the panel to vibrate predominantly at the fundamental vibration mode with minimal feedback from the flow field, indicating that the fluid-structure response was dominated by one-way uncoupled behavior. They observed that thicker, stiffer panels more strongly dampened the initial impact response than less stiff panels did, resulting in the panel dynamics to transition from one-way uncoupled to two-way coupled behavior. They noted that thinner, more compliant panels exhibited lower mechanical impedance, such that the mean steady-state aerodynamic loading produced a stiffening effect that locked the panel oscillations at predominantly the fundamental vibration mode. In contrast, the stiffer panels had higher mechanical impedance and were more resistant to the aerodynamic stiffening effect. This enabled the vibrational response of the thicker panels to oscillate more freely at the higher-order modes, helping to dissipate the original impact response more quickly.

1.3.4 Effect of Impingement Location

In impinging STBLIs, the location of shock impingement relative to the compliant surface plays a critical role in governing the coupled fluid-structure behavior. For mid-chord impingement interactions, the compliant surface is subjected to pressure fluctuations associated with both the separation shock foot and downstream reattachment point. In contrast, leading-edge impingement interactions are primarily influenced by the dynamics of the reattachment region and reattached boundary layer. These differing interaction characteristics modify the spatial distribution and temporal behavior of the pressure loading on the compliant surface, which may

excite different panel vibration modes and affect the strength and nature of the fluid-structure coupling.

Much of the existing work on compliant surface impinging STBLIs has focused on the mid-chord shock impingement configuration (D'Aguanno et al., 2023; Daub et al., 2016; Hoy and Bermejo-Moreno, 2022; Varigonda and Narayanaswamy, 2023). However, several studies have demonstrated that variations in impingement location can substantially modify the structural response. In particular, Deshmukh et al. (2023) conducted a computational study investigating the effect of five different impingement locations on a compliant panel. They found that the time-averaged panel deformation shape varied with impingement location. Under mid-chord impingement, the mean deformation resembled mode (2,1), whereas for the other impingement locations, it more closely resembled mode (1,1). The impingement location also influenced the relative contributions of higher-order panel modes to the structure response. Specifically, when the shock impinged near the trailing edge of the panel, the modal order changed such that mode (1,3) became the second most energetic POD mode, replacing mode (2,1) observed in the other impingement cases. Experimental studies have also reported a similar sensitivity of the structural response to impingement location. In Mach 2 wind tunnel experiments, Spottswood et al. (2012) observed notable differences in the panel response between one-third and mid-chord shock impingement cases. Likewise, Peltier et al. (2019) found that the change in shock impingement location affected both the order and structure of the panel mode shapes. Furthermore, Tripathi et al. (2024) observed from surface oil flow visualization measurements that the separation bubble increased in size for a three-quarter chord shock impingement configuration compared to the mid-chord impingement case. They largely attributed this behavior to differences in the mean panel deformation shape, which is governed by the static pressure loading acting on both the top and bottom surfaces of the panel. Collectively, these findings demonstrate that the shock impingement location modifies both the static deformation and the dynamic loading environment experienced by the compliant surface. These changes can significantly alter the structural response, and, consequently, the mechanisms governing the fluid-structure coupling in impinging STBLIs.

1.4 Thesis Scope and Outline

This work is an experimental and comparative investigation into the role of surface compliance in impinging STBLIs. In particular, the study seeks to address how the interaction may differ from both the rigid wall STBLI case and compliant case

without STBLI loading and to assess how changes to the interaction parameters influence the coupled flow and structural responses. Shock impingement location and cavity pressure are varied across three experimental configurations. Specifically, leading-edge and mid-chord shock impingement cases are examined, and two cavity pressures are considered for the leading-edge impingement configuration. One cavity pressure for the leading-edge impingement case is matched to the mid-chord impingement case to isolate the effect impingement location while the second is set equal to the average pressure loading over the panel to reduce the impact of the mean static panel deformation shape.

Simultaneous, synchronized flow and structure diagnostics are used to measure shock and boundary layer dynamics together with full-field panel surface deformations. Time-resolved point measurements in the flow are utilized to resolve variations in spectral content within the boundary layer and separation bubble. A combination of time- and frequency-domain analysis techniques is employed to compare and contrast the flow and structural responses between rigid and compliant cases. Cross-correlation and phase analyses are also used to quantify the relationship between the shock motion and panel dynamics to elucidate how surface compliance, shock impingement location, and cavity pressure influence the coupling between the flow field and compliant panel.

This work is structured as follows: Chapter 2 introduces the experimental wind tunnel facility and flow test conditions and details the design of the experimental model. Chapter 3 describes the diagnostic setups implemented to obtain synchronized full-field and point flow and structure measurements. The post-processing techniques utilized to analyze the acquired data are also summarized. Chapter 4 contains the characterization of both the facility and experimental model. The startup and freestream noise inherent to the Ludwig Tube facility are measured, and the static and dynamic properties of the compliant panel and cavity are experimentally investigated and compared against theoretical values. In Chapter 5, the rigid and compliant panel STBLI experimental results are presented and discussed. Similarities and differences between the three compliant cases investigated are evaluated, and cross-correlation and phase analysis techniques are applied to examine degree and extent that fluid-structure coupling modifies the flow and structural behavior. Finally, Chapter 6 summarizes the key insights obtained from this work and proposes directions for further research into compliant surface STBLIs.

Chapter 2

EXPERIMENTAL FACILITY AND MODEL

This chapter details the experimental facility, flow test conditions, and experimental model designs. Complete engineering drawings are provided in Appendix [E](#).

2.1 Ludwig Tube

In response to a competition in 1955 for developing an economical, high Reynolds number transonic or supersonic wind tunnel, Hubert Ludwig proposed the idea of a Ludwig tube (Ludwig et al., 1969; El-Naggar et al., 2004). This blowdown-type facility comprises a long, high pressure reservoir tube that accelerates pressurized gas through a nozzle when a diaphragm or valve is opened. Steady supersonic, cold-flow freestream conditions are generated at the exit of the nozzle for durations on the order of 100 ms. Today, Ludwig tubes are widely used in research laboratories due to their reliability, efficiency, and inexpensive operating costs.

Ludwig tubes generally have four main sections: a driver (or reservoir) tube, a converging-diverging nozzle, a test section, and a dump tank. A diaphragm or valve initially separates the driver tube from the test section and dump tank at the throat of the nozzle. The test section and dump tank are evacuated to a low pressure while the driver tube is pressurized with the test gas until either the diaphragm bursts or valve is opened. At this time, a weak shock wave travels through the nozzle and test section while a non-steady expansion wave travels in the opposite direction through the driver tube. The test gas is accelerated through the nozzle and reaches steady, supersonic flow at the exit of the nozzle in the test section. The large dump tank volume ensures the ambient pressure at the nozzle exit does not significantly increase during run time. The duration of steady flow conditions is limited by the time it takes the non-steady expansion wave to travel to the end of the driver tube, reflect off the driver tube end wall, and then travel back to the nozzle entrance.

Multiple steady runs can be achieved by only rupturing the diaphragm or opening the valve once as long as the pressure ratio remains high enough for the nozzle to be underexpanded. After the reflected non-steady expansion wave reflects off the nozzle entrance, the process of generating steady flow is repeated until equilibrium is reached. After each run cycle, the stagnation pressure is reduced.

All experiments for this work were carried out in the Caltech Ludwig Tube, depicted in Fig. 2.1. This facility consists of a 17 m long, 300 mm diameter driver tube that can be pressurized between 100 to 600 kPa ($Re_\infty = 4.1$ to 24.8 million/m, where $Re_\infty = \frac{\rho_\infty u_\infty}{\mu_\infty}$). It is connected to an axisymmetric, converging-diverging Mach 4 nozzle that has a 91 mm throat diameter and 315 mm exit diameter. A pneumatic piston valve is supported inside the driver tube just upstream of the nozzle. A polyurethane plug connected to the pneumatic piston forms a seal at the throat of the nozzle when the valve is closed. The dump tank and test section are evacuated to approximately 200 Pa via a Leybold SV630 vacuum pump. Flow begins when the piston valve is pneumatically retracted towards the driver tube side. Air is used as the test gas in all experiments. Note that this Ludwig tube is operated such that one reflected expansion wave arrival is the termination of the test time, rather than multiple wave reflections; as such, the test time is roughly 60 ms for each experiment. Additional descriptions of the operation of the Caltech Ludwig Tube facility are discussed by Mouton (2007) and Schmidt (2016).

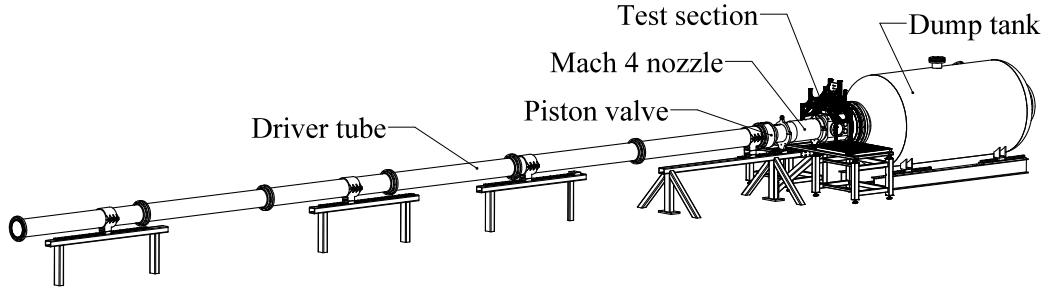


Figure 2.1: Schematic of Caltech Ludwig Tube.

2.1.1 Freestream Conditions

Nominal freestream flow conditions are computed for the Caltech Ludwig Tube assuming quasi-one-dimensional isentropic flow of a perfect gas. Given a specified driver tube fill pressure, p_{res} , the stagnation and freestream flow conditions can be computed using Riemann invariants and isentropic flow calculations. For this work, all experiments were performed at one set of Mach 4 flow conditions, listed in Table 2.1, corresponding to a unit Reynolds number of 10.3 million per meter.

2.2 Experimental Model

The experimental model for this work is designed to produce a static oblique shock wave that impinges on a turbulent boundary layer over a flat plate. The model assembly consists of a wedge shock generator and a rigid flat plate with interchangeable

Table 2.1: Flow conditions.

Driver/reservoir pressure, p_{res}	250 kPa
Ratio of specific heats, γ	1.4
Stagnation pressure, p_0	233 kPa
Freestream Mach number, M_∞	4
Freestream static pressure, p_∞	1.53 kPa
Freestream velocity, u_∞	667 m/s
Freestream static temperature, T_∞	69 K
Stagnation temperature, T_0	297 K
Freestream static density, ρ_∞	0.0770 kg/m ³
Freestream speed of sound, a_∞	167 m/s
Freestream unit Reynolds number, Re_∞	$10.3 \times 10^6 \text{ m}^{-1}$

rigid or compliant panel inserts. Detailed schematics are displayed in Figs. 2.2 & 2.3, and complete engineering drawings of the parts are provided in Appendix E.

The conceptualization and original design of this impinging STBLI experimental model is by Mallory Neet. Neet performed Euler simulations to inform shock generator geometry and placement relative to the panel, and the same shock generator, flat plate, and panel insert designs are used in this study. The original support structure for the shock generator featured a crankshaft connected to a servomotor that could mechanically oscillate the shock generator at a constant frequency or hold the shock generator fixed at a constant rotation angle. Neet conducted experiments characterizing surface pressure, visual boundary layer height, and separation bubble size through pressure sensitive paint, pressure transducer, and high-speed schlieren measurements at the maximum and minimum deflection angles attainable by the variable angle shock generator. In addition, she examined the effect of statically deformed rigid panels on surface pressure and boundary layer thickness. Further details on the design and her experimental results are reported in Neet and Austin (2020).

The shock generator is a 35 degree aluminum wedge supported by two steel struts bolted to the aft surface of the wedge. A custom U-shaped bracket rigidly connects the steel struts to the top window flange. The bracket has two sets of bolt holes for two mounting configurations: $\theta_{\text{SG}} = 0^\circ$ or 10° wedge rotation. Under the freestream conditions, the wedge produces a spanwise-uniform oblique shock that slightly bends upward due to the Prandtl-Meyer expansion fan generated by the trapezoidal

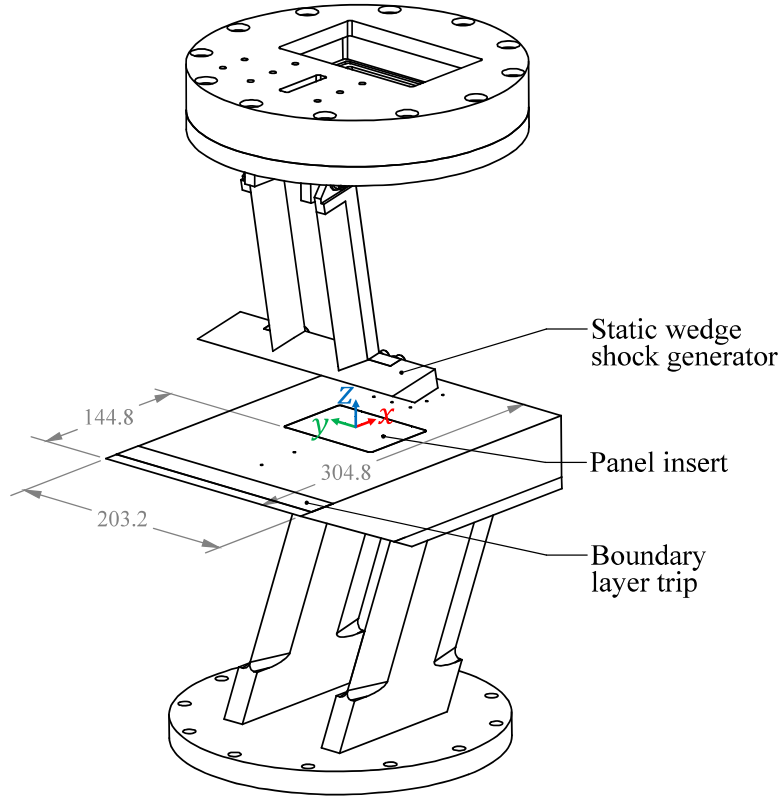


Figure 2.2: Isometric view of STBLI experimental model (dimensions in mm).

shape of the wedge. Prior to impingement on the plate surface, the oblique shock is approximately planar at a shock angle of $\beta_s \approx 40^\circ$ for the $\theta_{SG} = 0^\circ$ case and $\beta_s \approx 30^\circ$ for the $\theta_{SG} = 10^\circ$ case.

The aluminum flat plate features a rectangular cavity, where rigid or compliant panel inserts can be mounted. The compliant panel insert is composed of two parts: an aluminum base (Fig. 2.4b) and a thin compliant panel (Fig. 2.4a). The compliant panel is made from a 0.254 mm thick sheet of 316 stainless steel shim stock, and it is rigidly clamped to the shoulder of the aluminum base via an epoxy structural adhesive (Loctite E-60HP). The aspect ratio of the compliant region of the panel is $b/a = 1.476$, where a and b are the length and width of the compliant region. Additional dimensions of the compliant panel are listed in Table 2.2. The rigid panel insert shares the same design as the compliant panel insert with the exception that the entire insert is made from a single aluminum piece, where the top surface is 20.32 mm thick and non-compliant (Fig. 2.5).

The streamwise position of the shock generator relative to the flat plate can be adjusted by swapping the bottom mounting plate underneath the flat plate model.

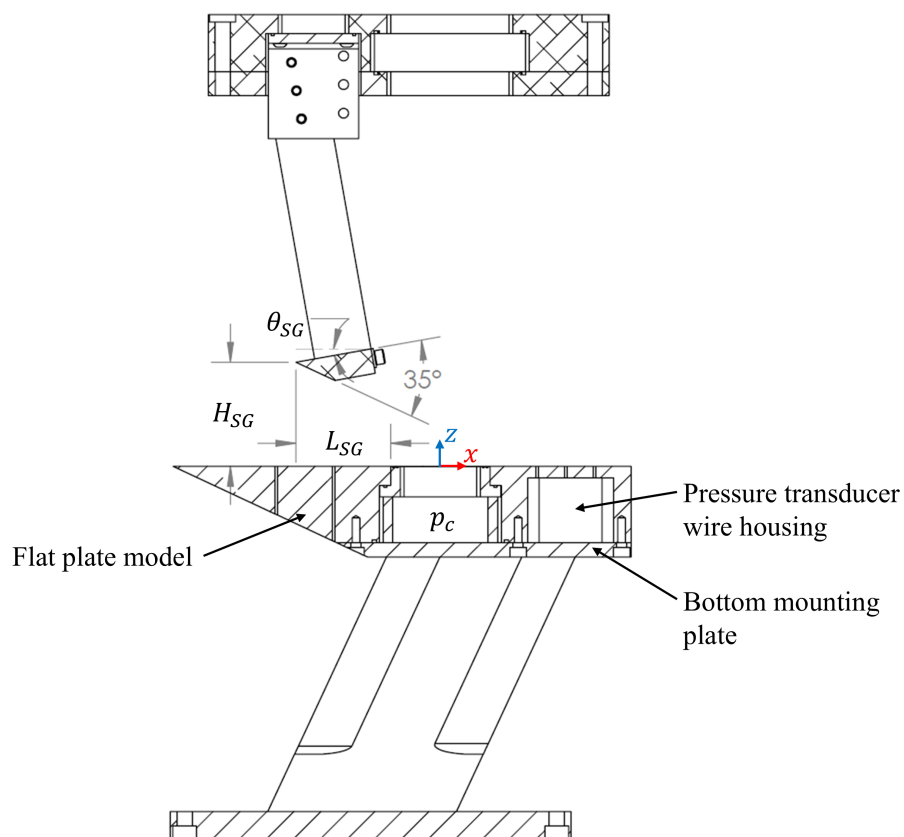


Figure 2.3: Cross-section view of STBLI experimental model.

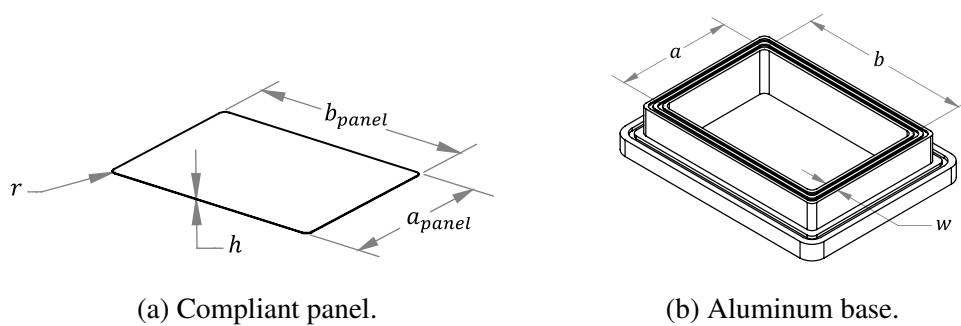


Figure 2.4: Compliant panel insert.

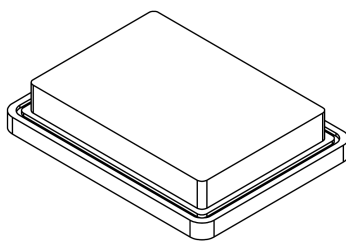


Figure 2.5: Rigid panel insert.

Table 2.2: Compliant panel dimensions (in mm).

Thickness, h	0.254
Compliant panel streamwise length, a	53.3
Compliant panel spanwise width, b	78.7
Panel insert streamwise length, a_{panel}	65.9
Panel insert spanwise width, b_{panel}	91.3
Corner radius, r	2.5
Shoulder width, w	6.4

Two bottom mounting plates were manufactured, each with their bolt holes offset by $a/2$ compared to the other. When the shock generator is rotated by $\theta_{\text{SG}} = 10^\circ$, the distance from the shock generator to panel insert cavity leading edge, denoted L_{SG} , can be set to either 61.9 or 110.6 mm. This results in the 30° incident oblique shock to impinge on the panel surface at either the mid-chord ($x_{\text{imp}}/a = 0$) or leading edge ($x_{\text{imp}}/a = -0.5$).

A boundary layer trip is mounted near the leading edge of the flat plate. The boundary layer trip consists of $190 \mu\text{m}$ roughness height (80 grit) sandpaper positioned 12.7 mm downstream of the flat plate leading-edge. The trip spans 25.4 mm in the streamwise direction and the entire model width in the spanwise direction. A trip roughness sensitivity analysis was previously performed by Neet and Austin (2020) using visual boundary layer height measurements from schlieren images to ensure the $190 \mu\text{m}$ roughness height does not under- or over-trip the boundary layer. Characterization of the boundary layer is presented in Section 4.2.

Underneath the panel is a 50.6 mm deep cavity that is pressurized to a constant value, p_c , prior to test time. Two cavity pressures, $p_c = 6.5$ and 9 kPa, were chosen to correspond to approximately the average STBLI pressure load over the panel for the mid-chord and leading-edge shock impingement cases at $\theta_{\text{SG}} = 10^\circ$, respectively.

A summary of the three compliant panel STBLI and three baseline cases studied in this work are listed in Table 2.3. Case A corresponds to mid-chord shock impingement with the cavity pressure set to ensure close to zero mean panel deflection. Case B corresponds to leading-edge shock impingement with the same cavity pressure as Case A. This case isolates the effect of shock impingement on the panel response compared to Case A. Case C corresponds to leading-edge shock impingement with a revised cavity pressure to ensure close to zero mean panel deflection. This case changes both impingement location and cavity pressure to reduce the impact that

mean panel deflection may have on the fluid and structure response compared to Case A. A rigid panel STBLI case was also tested to compare the compliant cases against a baseline rigid wall STBLI. Finally, the ‘No-STBLI’ and ‘No-STBLI (rigid)’ cases were tested with the shock generator removed to compare the compliant panel and rigid wall response without a STBLI. For the No-STBLI case, the cavity pressure was set to the nominal freestream static pressure, p_∞ , to ensure the panel exhibited close to zero mean deflection.

Table 2.3: Model test conditions.

Case	Impingement Location	Panel Insert	p_c [kPa]	θ_{SG}	L_{SG} [mm]	H_{SG} [mm]
A	Mid-chord	Compliant	6.5	10°	61.9	69.3
B	Leading-edge	Compliant	6.5	10°	110.6	69.3
C	Leading-edge	Compliant	9.0	10°	110.6	69.3
Rigid	N/A	Rigid	N/A	10°	N/A	69.3
No-STBLI	No Impingement	Compliant	1.53	N/A	N/A	N/A
No-STBLI (Rigid)	No Impingement	Rigid	N/A	N/A	N/A	N/A

Chapter 3

DIAGNOSTICS AND DATA PROCESSING

Obtaining temporally- and spatially-resolved measurements is vital for deciphering the complex fluid-structure coupling in STBLI. In this work, particular emphasis is placed on simultaneous and synchronized experimental measurements of both flow and structural responses. This chapter details the diagnostic setups and post-processing techniques implemented in this study. Specifically, Section 3.1 covers the steady and unsteady static surface pressure measurements obtained over the flat plate model. Section 3.2 describes the optical setup, fundamental principles, and data processing techniques for the single-camera stereo-photogrammetry measurements. Section 3.3 details the two types of schlieren optical setups used and the image processing and feature tracking routines implemented for enhancing image quality and detecting shock and boundary layer motion. Section 3.4 covers the design and post-processing of focused laser differential interferometry (FLDI) measurements carried out in a colinear FLDI and schlieren optical setup. Finally, Section 3.5 details proper orthogonal decomposition, which is modal decomposition technique applied to both the flow and structural measurements.

3.1 Pressure Transducers

Surface-mounted pressure transducers are instrumented on the flat plate model to characterize the rigid wall STBLI loading and examine the spectral content downstream of the interaction. Five Kulite XCS-062-5A pressure transducers (labeled K3-K5 in Fig. 3.1) equipped with a “B”-type screen are mounted flush to the flat plate model surface downstream of the panel. Each sensor is spaced 19.05 mm apart, and the most upstream row of sensors (K3, K6, K7) are 31.53 mm downstream from the panel insert cavity. In addition, nine NovaSensor NPP-301A-100A piezoresistive pressure sensors (NPP11-19) are mounted on a rigid panel insert. Each sensor is mounted to a custom printed circuit board secured on the underside of the panel, where the sensor is recessed 1 mm below the panel top surface. A Buna-N gasket seals the sensor port from the panel cavity, and a 0.6 mm diameter through hole provides the sensor access to the flow field above the panel. Sensors NPP11-17 are spaced 6.00 mm apart, and the most upstream sensors (NPP11 and NPP18) are 12.70 mm from the leading edge of the panel insert cavity. Repeat

experiments are also performed with the rigid panel insert rotated 180 degrees such that NPP11 and NPP18 become the most downstream sensors on the panel. In this rotated configuration, the sensor labeling scheme is switched from NPP1X to NPP2X for distinction.

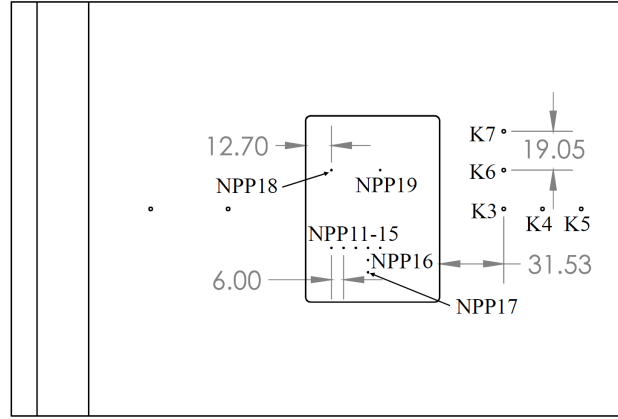


Figure 3.1: Pressure transducer locations on flat plate and rigid panel insert (flow direction is left to right, dimensions in mm).

The signal from each sensor is amplified using a Micro-Measurements 2310B signal conditioner and recorded by a Yokogawa DL810E oscilloscope. Both types of sensors exhibit a linear relationship between output voltage and pressure and are calibrated by performing a linear fit of sensor voltage recorded at known pressures. For calibration, the test section and dump tank are first evacuated to 0.5 kPa before turning off the vacuum pump and allowing the pressure to slowly increase. The voltage of each sensor is recorded in intervals of 0.5 kPa between dump tank pressures of 0.5 to 9.5 kPa, which are measured using an MKS 626C13TDE pressure gauge connected to the dump tank.

The natural frequency of “B” screen Kulite XCS-062 sensors is typically on the order of 100 kHz, and a previous study by Hurst et al. (2014) reported that the sensors exhibit a flat frequency response up to 16 kHz. Although the NovaSensor NPP-301A-100A sensors have been used in previous hypersonic wind tunnel experiments (Camillo et al., 2020; Stern et al., 2018), the frequency response is not well characterized. Therefore, all measurements obtained from the NovaSensor pressure transducers are time-averaged over the entire length of test time.

A Kulite pressure transducer is also mounted inside the panel cavity to monitor the cavity pressure underneath the compliant panel. The sensor, labeled KC in Fig. 3.2,

is mounted on the aft wall of the cavity 31.5 mm below the compliant panel and 25.4 mm from the model centerline.

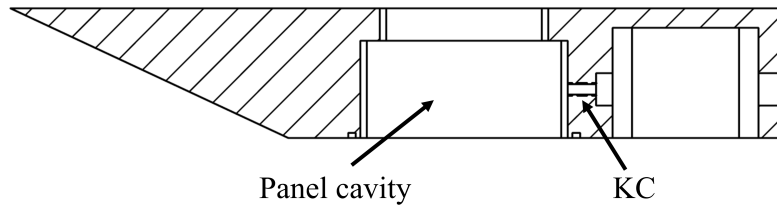


Figure 3.2: Cavity pressure transducer, KC, location in flat plate model cross-section diagram.

3.2 Photogrammetry

Time-resolved full-field displacement measurements are essential for characterizing the panel response induced by STBLI. Photogrammetry offers a non-intrusive approach to obtain such measurements by imaging a series of points from multiple perspectives to reconstruct their absolute, 3D positions. This technique employs a mathematical framework based on projective geometry to model how 3D objects are projected onto 2D planes. At least two independent views are required to resolve the 3D coordinates, and when a sufficiently dense grid of points across a surface is imaged, the full-field deformation of that surface can be resolved.

One of the earliest demonstrations of photogrammetry was Finsterwalder's use of aerial photographs for topographic mapping in 1900 (Doyle, 1964). Subsequent applications were primarily restricted to tracking rigid-body displacements of discrete objects rather than capturing full-field behavior (Baqersad et al., 2017). The first application of photogrammetry to measure mechanical stress and strain in deforming structures was introduced by Peters and Ranson (1982), and today, stereo- and multi-camera systems are widely used for structural deformation and vibration measurements (Baqersad et al., 2017).

3.2.1 Experimental Setup

Based on a suggestion from Marcia Cooper, Mallory Neet implemented an initial photogrammetry setup to measure the surface deformations of the compliant panel for her STBLI experiments in the Caltech Ludwig Tube. The design of the photogrammetry system and post-processing routine in this work is built on Neet's original setup and post-processing scripts.

A single-camera stereo-photogrammetry setup based on the works of Walker et al.

(2010), Yu and Pan (2016), and Whalen et al. (2020) is implemented to image the full compliant panel surface and resolve displacements on the order of tens of micron. Because the side test section windows are reserved for other optical diagnostics, the photogrammetry system views the panel surface from the top test section window. Viewing angle restrictions and camera budget constraints limit the system to a single-camera setup. To overcome this, a compact stereo configuration is implemented to split the field of view of a single camera into two. Following the approach of Yu and Pan (2016), this arrangement employs a right-angle prism mirror, labeled M3 in Fig. 3.3, to capture two distinct views of the panel surface. A high-speed camera is positioned directly above M3 so that the prism divides the image along the vertical centerline of the camera sensor. Two flat mirrors, M1 and M2, are placed on either side of M3 at an angle of $\theta_M \approx 50^\circ$ to redirect the panel surface view into the prism. This results in the camera obtaining two views of the panel surface, which are represented by the left and right virtual images in Fig. 3.3.

The panel is illuminated by four continuous white light emitting diodes (Thorlabs MCWHL7 and MCWHL1) and condenser lenses through the side windows of the test section. A 532 nm notch filter is mounted on the front of the camera lens to block any laser light originating from the other diagnostics from being imaged by the camera. A Phantom v1612 high-speed camera is used to image the panel surface, and it features a maximum resolution of 1280 by 800 pixels at up to 16,600 frames per second (fps). This setup results in a camera magnification of approximately 8 pixels per millimeter, or roughly 10 pixels per marker. This magnification optimizes the number of pixels representing each marker and the number of markers able to be captured within the field of view of the camera. Further details of the specific optics components used in this setup are listed in Table 3.1.

Table 3.1: Photogrammetry component list.

Component	Type	Details
M1, M2	Flat mirror	ThorLabs PF30-03-P01, $\varnothing = 76$ mm
M3	Right angle prism mirror	ThorLabs MRAK25-G01
NF	Notch filter	Edmund 67-128, 532 nm, OD 4.0
FL	Focusing lens	Nikon 105 mm f/2.8

The compliant panel inserts are spray painted with a grid of 29 by 21 black circular markers 1.25 mm in diameter and spaced 3 mm apart over a coat of white paint (Fig. 3.4). The spray paint is uniformly applied in order to reduce effects caused by

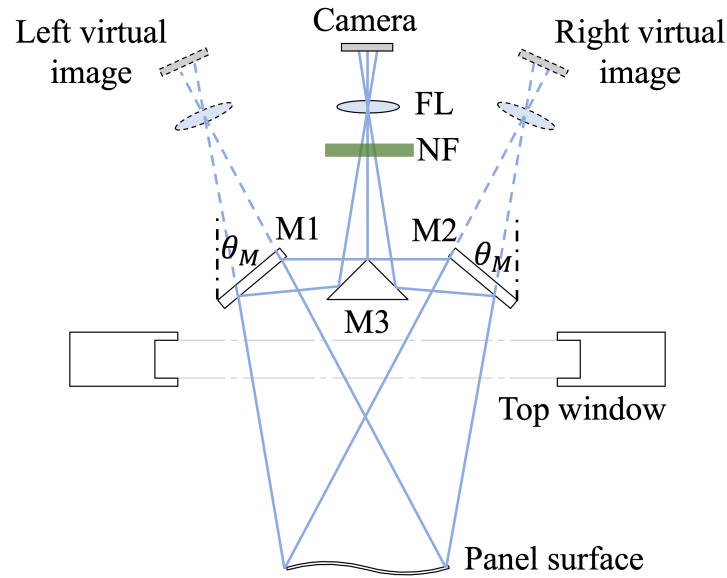


Figure 3.3: Photogrammetry optics diagram.

uneven paint thickness. The white base coat is approximately $64 \mu\text{m}$ in thickness and the markers are approximately $25 \mu\text{m}$ in thickness. A vinyl cut stencil is used to accurately paint the circular markers. Two additional 29 by 5 grids of circular markers are painted upstream and downstream of the panel on the flat plate model to track the rigid body movement of the model and facility relative to the stationary camera. These markers are denoted “tracker markers”, while the markers painted on the compliant panel are denoted “panel markers”.

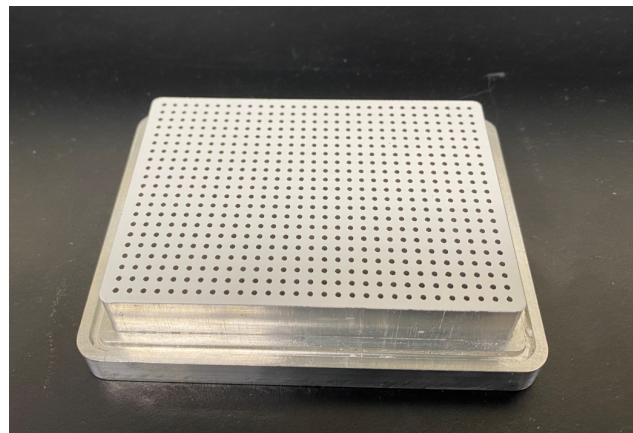


Figure 3.4: Painted panel.

3.2.2 Camera Calibration

The central perspective projection model describes the geometrical relationship between a point in 3D space with its image point on a 2D projection plane (Fig. 3.5). In this model, the point, X_A , is an object in 3D space that is projected through the perspective center, X_O , onto the projection plane, or camera sensor, where it creates the image point, x_a . The distance between the principal point and perspective center is known as the principal distance, c_p . The object coordinate system is arbitrary and represented by $\{X, Y, Z\}$, while the image coordinate system is centered at X_O and represented by $\{x', y', z'\}$.

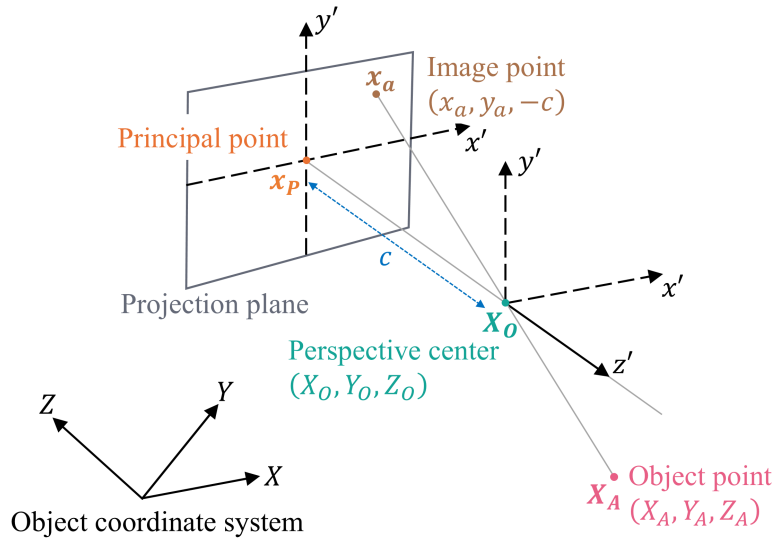


Figure 3.5: Central perspective projection model (diagram adapted from Walker et al. (2010)).

The collinearity equations (Eqs. 3.1 & 3.2) describe the linear relationship between any point in 3D space, the perspective center, and the corresponding point on the projection plane (Walker et al., 2010). In these equations, the terms, r_{ij} , are the (i, j) element of a 3 by 3 rotation matrix, $\mathbf{R}$. A system of collinearity equations is created based on the number of cameras and target points. At least two camera views are required to solve this system of collinearity equations, and calibration of the optical setup must be performed to obtain the fixed relationships between the object and image coordinate systems, c_p , X_O , and $\mathbf{R}$.

$$x_a = \frac{-c_p[r_{11}(X_O - X_A) + r_{12}(Y_O - Y_A) + r_{13}(Z_O - Z_A)]}{[r_{31}(X_O - X_A) + r_{32}(Y_O - Y_A) + r_{33}(Z_O - Z_A)]} \quad (3.1)$$

$$y_a = \frac{-c_p[r_{21}(X_O - X_A) + r_{22}(Y_O - Y_A) + r_{23}(Z_O - Z_A)]}{[r_{31}(X_O - X_A) + r_{32}(Y_O - Y_A) + r_{33}(Z_O - Z_A)]} \quad (3.2)$$

A bundle adjustment MATLAB script provided by Walker et al. (2010) is used to obtain optimal estimates of the camera parameters. Approximately thirty images are captured of a rigid calibration target positioned at various orientations. The calibration target consists of a 6.35 mm thick aluminum plate patterned with a 20 by 11 grid of circular markers, each 1.25 mm in diameter and spaced 3 mm apart. These images are processed with the bundle adjustment routine, where the marker centroids are extracted using a procedure similar to that described in Section 3.2.3. Given the known grid spacing, the algorithm computes the optimal estimates of the camera parameters using a nonlinear least-squares fit of Eqs. 3.1 & 3.2. The camera parameters are stored in a matrix for later use in converting the test-time footage into 3D panel coordinates.

3.2.3 Post-Processing

A custom MATLAB script is developed to accurately measure the out-of-plane surface deflection of the compliant panel by post-processing the high-speed photogrammetry footage. The script is organized into three main functions: (1) photogrammetry image processing and centroid tracking, (2) 2D-to-3D coordinate conversion, and (3) out-of-plane z -coordinate post-processing. A detailed step-by-step description of the complete post-processing procedure is provided below.

1. Select Region of Analysis

User-defined three point rectangles, defined by the top left, top right, and bottom right points, specify the centroids within the regions of interest for analysis. The bounding points are carefully selected to ensure the selected markers match in both the left and right images.

2. Initial Guess of Centroid Location

An initial estimate of the centroid locations of the markers is calculated using a binary image centroid tracking scheme.

a) Split Images

Each frame is divided into left and right images by cropping along the vertical centerline. An example left image is shown in Fig. 3.6a.

b) Reduce Image Noise

A Gaussian filter is applied to slightly blur each image. This smooths the background texture and helps to reduce camera sensor noise. Zero padding is applied around the entire image to reduce tracking errors when a centroid is too close to the image edge (Fig. 3.6b).

c) Convert to Binary Images

The grayscale image is binarized such that the pixel intensities are converted from grayscale (0-255) to black and white (0 or 1) (Fig. 3.6c).

d) Invert Pixel Intensities

The complement of the binary image is obtained by inverting its pixel intensities (Fig. 3.6d).

e) Calculate Binary Centroids

The built-in MATLAB centroid finding function, `regionprops(BW, 'centroid')`, calculates the centroid of each cluster of white pixels in the binary image (Fig. 3.6e).

f) Sort Binary Centroids

Based on the selected bounding markers, the computed centroid locations are compared against the predicted marker locations. For the wind-off and first frame images, the predicted marker locations are determined from the estimated marker spacing defined by the bounding points. For all subsequent frames, the predicted locations are taken from the marker locations in the previous frame. Any erroneously detected centroids that do not correspond to an actual marker are rejected by applying a search criterion within a specified radius of each predicted location. The remaining centroid coordinates are organized into a r_c by c_c by n_{images} by 2 matrix, where r_c and c_c denote the number of rows and columns of centroids in the region of interest, n_{images} is the total number of frames in the footage, and the last index distinguishes between the left and right images. In Fig. 3.6f, panel marker centroids are shown in cyan and upstream/downstream tracker centroids are shown in magenta.

3. Calculate Optimized Centroid Positions

Although the built-in centroid finding function in MATLAB provides a reliable estimate of the centroid locations of the markers, it does not provide a high enough level of accuracy required to measure the small displacements experienced by the compliant panel that are on the order of tens of micron,

or about 8% of a pixel width. A higher fidelity centroid locating algorithm is implemented, where the binary centroid locations are used as an initial guess to the actual centroid location.

a) **Crop Marker Region**

A square region of size $2d_{\text{marker}} + 1$ is cropped around each detected marker, where d_{marker} represents the typical marker diameter in pixels rounded to the nearest integer. An example cropped marker image and surface plot are shown in Figs. 3.7a & 3.8a.

b) **Smooth Background and Marker Shape**

A Gaussian filter is applied to reduce background noise and smooth the marker shape (Figs. 3.7b & 3.8b).

c) **Normalize Pixel Intensities**

The pixel intensity range is rescaled from 0-255 to 0-1, with the brightest background pixel intensity set to 1 (Figs. 3.7c & 3.8c).

d) **Invert Pixel Intensities**

The complement of the normalized images is obtained by inverting the pixel intensities (Figs. 3.7d & 3.8d).

e) **Increase Image Resolution**

The image resolution is increased by subdividing each pixel into 100 linearly interpolated subpixels (Figs. 3.7e & 3.8e).

f) **Nonlinear Least-Squares Curve Fit**

Because the markers are viewed at an angle offset from the vertical z -axis, their circular shape appears slightly elliptical on the camera sensor. Therefore, the distribution of pixel intensities in the resolution-enhanced image is fit to a 2D function that models the marker as a blurred ellipse. The fitting function, shown in Eq. 3.3, employs a hyperbolic tangent approximation to Gaussian edge blurring (Cantatore et al., 2009).

$$I(x_p^*, y_p^*) = I_b - \left(\frac{I_b - I_m}{2} \right) \left\{ \tanh \left[\zeta \left(\sqrt{\left(\frac{x_p^*}{a_m} \right)^2 + \left(\frac{y_p^*}{b_m} \right)^2} + 1 \right) \right] - \tanh \left[\zeta \left(\sqrt{\left(\frac{x_p^*}{a_m} \right)^2 + \left(\frac{y_p^*}{b_m} \right)^2} - 1 \right) \right] \right\} \quad (3.3)$$

$$\begin{bmatrix} x_p^* \\ y_p^* \end{bmatrix} = R_{\phi_{\text{ellipse}}} \begin{bmatrix} x_p - x_{p,0} \\ y_p - y_{p,0} \end{bmatrix} \quad (3.4)$$

In this model, I_b represents the average background pixel intensity, and I_m is the maximum marker pixel intensity. Both values are approximated prior to curve fitting. The remaining parameters, $x_{p,0}$, $y_{p,0}$, a_m , b_m , ϕ , and ζ , are obtained from the curve fitting process. The coordinate, $(x_{p,0}, y_{p,0})$, denotes the marker centroid location, a_m and b_m are the semi-minor/semi-major axes of the marker ellipse, and x_p^* and y_p^* are the transformed coordinates defined in Eq. 3.4. The rotation matrix, $R_{\phi_{\text{ellipse}}}$, is based on the ellipse angle, ϕ_{ellipse} .

A nonlinear least-squares curve fit is then performed using the built-in MATLAB function, `lsqcurvefit()`, to solve for the optimal parameter values. The curve fit tolerance is set to 10^{-12} and maximum number of iterations to 10^5 . This fitting routine follows the approach described by Whalen et al. (2020).

An example of the fitted intensity distribution using the optimal parameter values is shown in Figs. 3.7f & 3.8f, and the locations of all optimized centroid coordinates of a representative frame are plotted in Fig. 3.9. The optimized centroid locations for each marker across all frames are stored in a matrix with the same structure as the binary centroid matrix.

4. Convert 2D Centroid Coordinates to 3D

Using the camera parameters calculated in Section 3.2.2 and inputting the optimized centroid coordinates, the collinearity equations (Eqs. 3.1 & 3.2) are solved using the bundle adjustment routine provided by Walker et al. (2010) to determine the 3D marker coordinates (Fig. 3.10a).

5. Post-Process 3D Coordinates

The 3D centroid coordinates are transformed from an arbitrary coordinate system, $\{X, Y, Z\}$, to the model coordinate system, $\{x, y, z\}$. Additionally, the rigid body motion due to facility movement relative to the camera is reduced, and the coordinates are spatially filtered in the z -direction to reduce measurement noise.

a) Fit Plane to Tracker Markers in Wind-off Image

Since the 3D coordinates are defined in an arbitrary coordinate system,

the position of the upstream and downstream tracker points are used to redefine a new coordinate system. The tracker points are fit to a plane that defines the orientation of the vertical, z -axis (Fig. 3.10b).

b) Spatially Filter z -coordinates

The z -coordinates are spatially filtered using a 2D Gaussian smoothing kernel with a full-width half-maximum of 4.71 mm, or 3.77 marker diameters. This spatial filter reduces tracking errors at the expense of attenuating higher order vibration modes.

c) Align Tracker Points to Wind-off Position

Due to facility vibrations, the camera and experimental model move relative to each other in primarily the streamwise direction. The panel coordinates are converted from a lab-stationary frame to a model-stationary frame by aligning the tracker points in each frame to the tracker point position in the wind-off frame using an optimal translation and rotation.

d) Redefine x - and y -axes

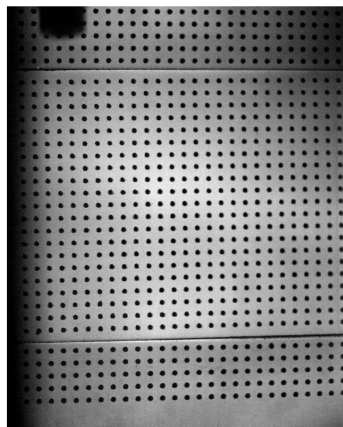
The distance between the painted panel markers and panel edges are measured from a calibration image. The x - and y -axes are defined based on the directions parallel to the streamwise and spanwise panel edges. The panel coordinates are then transformed to this new model coordinate system, $\{x, y, z\}$, where the origin is located at the panel center in the streamwise and spanwise direction.

e) Subtract Wind-off z -coordinates

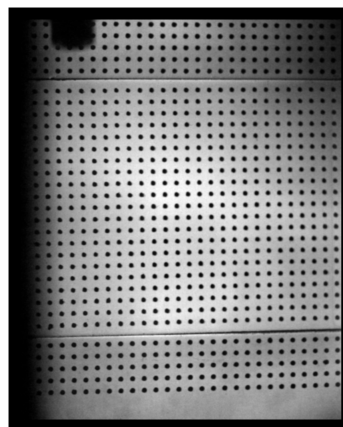
While wind-off z -coordinate subtraction is not strictly necessary, this step is performed to compare the panel displacement from the undeformed state. Wind-off coordinate subtraction improves tracking accuracy due to optical distortions and camera calibration inaccuracies, and it accounts for differences in paint thickness between markers. An example of the final processed 3D coordinates are plotted in Fig. 3.10c.

f) Interpolate Panel Coordinates

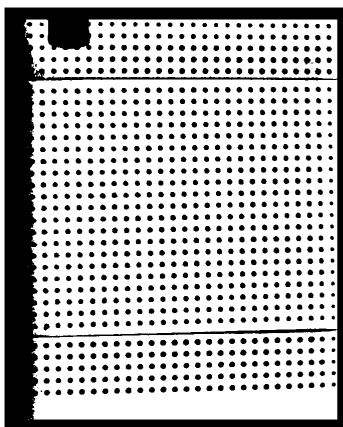
The z -coordinates are linearly interpolated on a uniform grid of x - and y -coordinates, and a colormap plot of an example frame of the interpolated coordinates is shown in Fig. 3.10d.



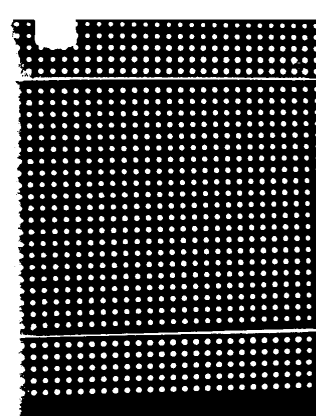
(a) Original.



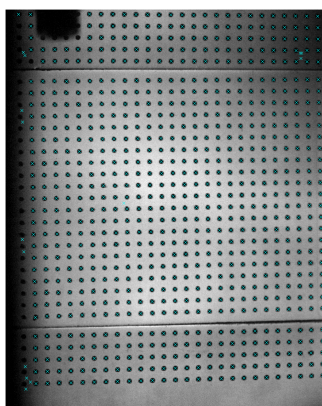
(b) Gaussian filter.



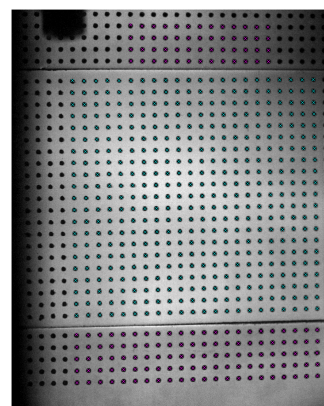
(c) Binarize.



(d) Complement.

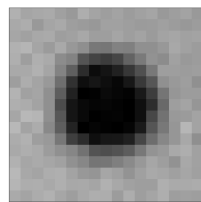


(e) Binary centroids.

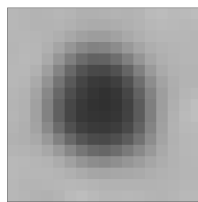


(f) Sorted binary centroids.

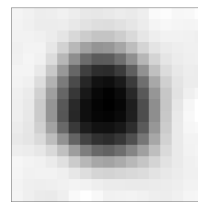
Figure 3.6: Binary centroids image processing.



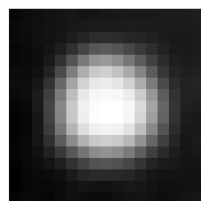
(a) Original.



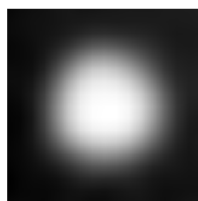
(b) Gaussian filter.



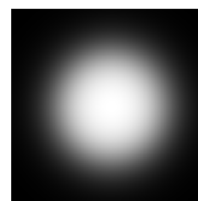
(c) Normalized.



(d) Complement.

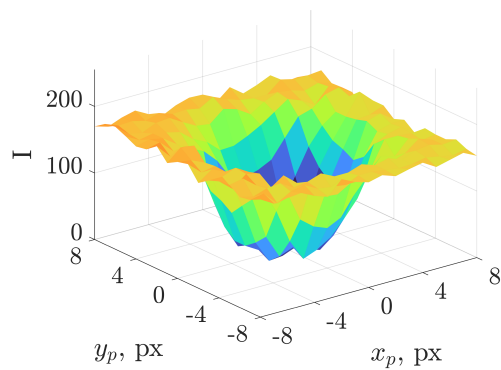


(e) Interpolated.

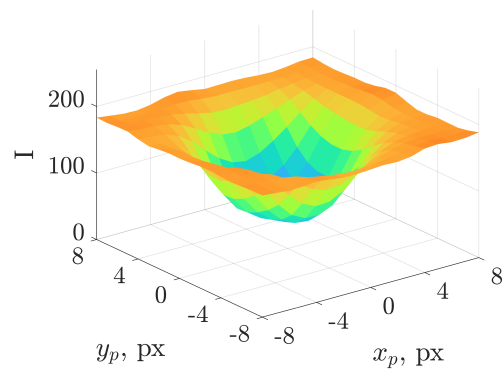


(f) Optimization result.

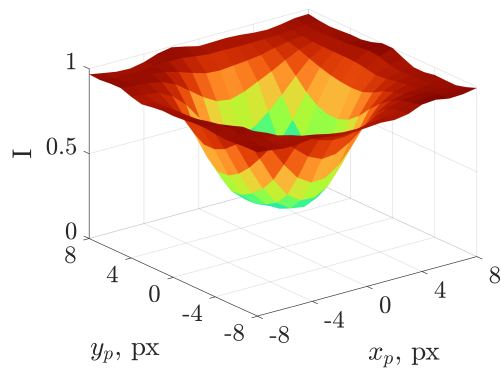
Figure 3.7: Marker image processing and optimization result.



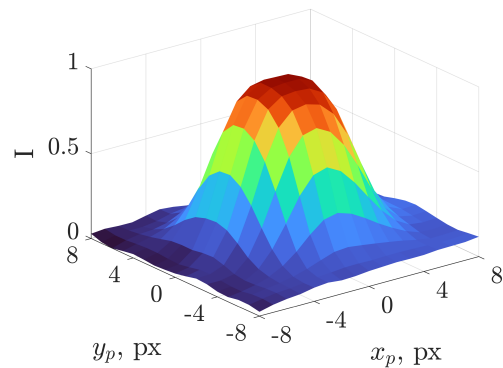
(a) Original.



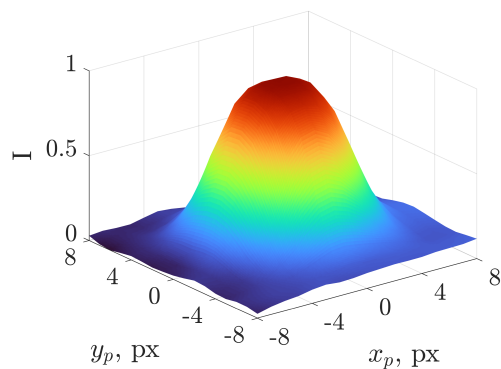
(b) Gaussian filter.



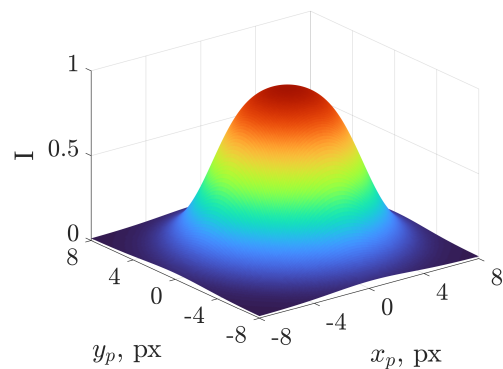
(c) Normalized.



(d) Complement.

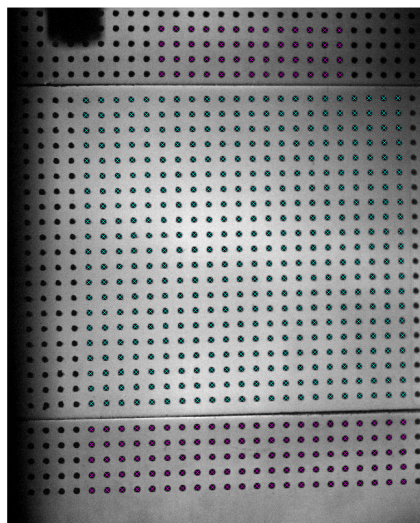


(e) Interpolated.

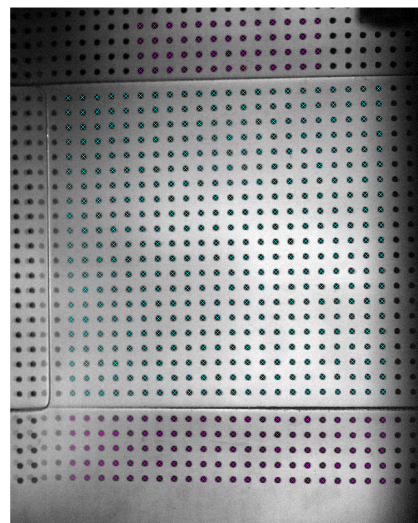


(f) Optimization result.

Figure 3.8: Surface plots of marker image processing and optimization result.

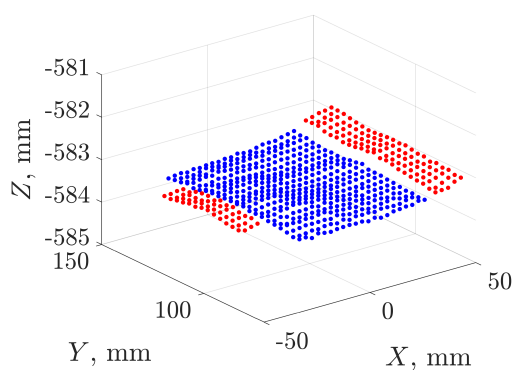


(a) Left image.

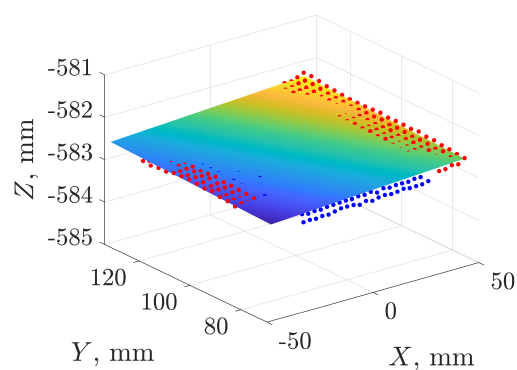


(b) Right image.

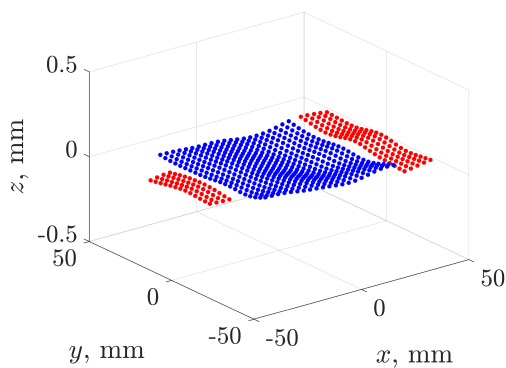
Figure 3.9: Optimized centroid locations.



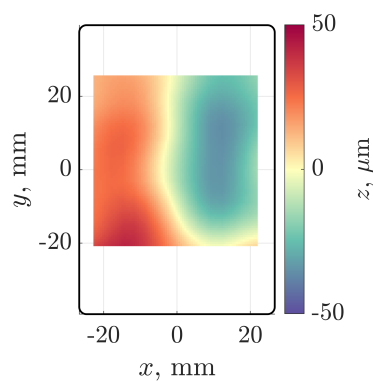
(a) Initial 3D centroid coordinates (blue = panel, red = tracker points).



(b) Plane fit to wind-off tracker points.



(c) Processed 3D centroid coordinates.



(d) Colormap of interpolated coordinates.

Figure 3.10: 3D centroid coordinate post-processing.

3.2.4 Measurement Uncertainty

The measurement uncertainty of this photogrammetry system is quantified by examining the apparent out-of-plane deflections of the tracker markers during test time. These markers are painted on rigid areas of the flat plate model, where the true physical displacement is expected to be zero. Any non-zero displacement measured at these rigid locations can therefore be attributed to experimental error arising from factors such as camera sensor noise, lens distortions, centroid tracking inaccuracies, and facility vibrations.

The time-averaged deflection of each tracker marker, denoted $\bar{z}_{\text{tracker}}$, is computed and visualized using swarm and box plots, as shown in Fig. 3.11a. The distributions are centered near zero and approximately Gaussian, indicating that the measurements are not systematically biased. The spatial- and trial-averaged time-mean deflection is $\langle \bar{z}_{\text{tracker}} \rangle \approx 0 \mu\text{m}$, where $\langle \cdot \rangle$ represents averaging over all markers and all trials. This result confirms that the photogrammetry system, on average, does not introduce a consistent offset error.

In addition to the mean values, the variability of the displacement of each marker is characterized by computing its standard deviation, $\sigma_{z_{\text{tracker}}}$. These results are also presented as swarm and box plots in Fig. 3.11b. The standard deviations are narrowly spread, with a spatial- and trial-averaged value of $\langle \sigma_{z_{\text{tracker}}} \rangle = 4.9 \mu\text{m}$. This demonstrates that the instantaneous displacement measurements remain tightly clustered around the mean values.

To capture both the potential bias and variability in a single metric, the root mean square (RMS) of the displacement of the marker is computed. The distribution of RMS values is shown in Fig. 3.11c, and the spatial- and trial-averaged root mean square value is $\langle \text{RMS}\{z_{\text{tracker}}\} \rangle = 9.2 \mu\text{m}$. The overall photogrammetry measurement uncertainty is conservatively defined as twice the spatial- and trial-averaged RMS value, $\varepsilon_{\text{photogrammetry}} = 2\langle \text{RMS}\{z_{\text{tracker}}\} \rangle = 18.4 \mu\text{m}$. This uncertainty encompasses more than 93% of all observed tracker marker displacements.

Whalen et al. (2020) noted that their photogrammetry measurement errors were more substantial in their higher Reynolds number freestream condition compared to their lower Reynolds number condition. They conclude this is due to the higher gas refractive index in the higher Reynolds number condition, but they do not observe the scaling factor of the error to be equal to scaling factor of the refractive index between the two conditions. For this study, the out-of-plane deflections of the panel markers painted on the rigid panel are measured during test time without the shock generator

mounted to assess the effect of refractive index variation due to viewing through oblique shocks. Swarm and box plots of the time-average, standard deviation, and root mean square deflection values for each marker are shown in Fig. 3.12.

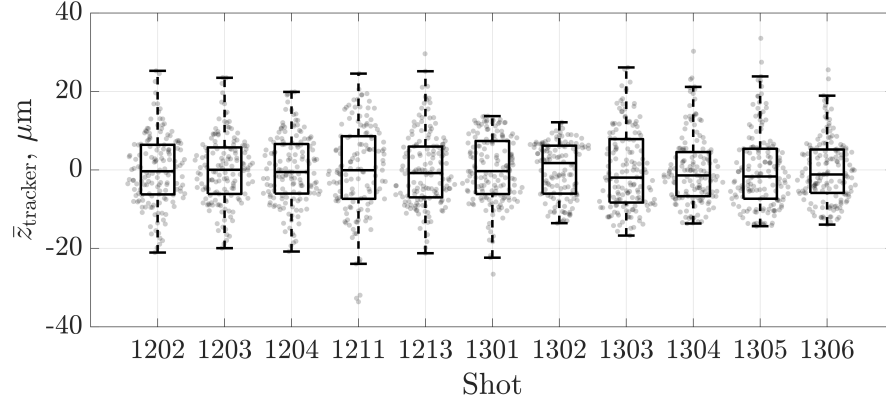
Similar to that of the tracker marker displacements measured under shock impingement, the time-average deflection of the rigid panel markers under no shock impingement is centered about zero displacement. Furthermore, the distribution of standard deviation and root mean square values closely match those of the tracker markers in Figs. 3.11b & 3.11c. Because the spatially-averaged root mean square value of the rigid panel measurements is approximately equal to that of the tracker marker measurements ($\langle \text{RMS}\{z_{\text{tracker}}\} \rangle \approx \langle \text{RMS}\{z_{\text{rigid}}\} \rangle$), the rigid panel deflections are within the expected measurement uncertainty. Therefore, these experiments demonstrate that no noticeable effects within the measurement uncertainty were observed due to viewing the panel through the shock waves and other flow features.

Frequency-domain analysis of the measurement error is also performed by computing the power spectral density (PSD) of the tracker displacements for a representative shot (Fig. 3.13). The PSD for each individual marker is shown in gray, with the spatially-averaged spectrum overlaid in black. The response is relatively flat across the frequency range of interest, suggesting that the error behaves approximately as a white noise process without pronounced frequency bias.

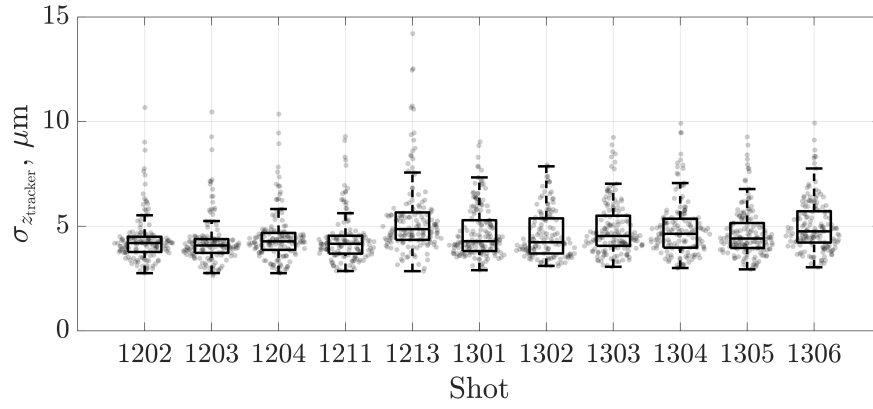
Overall, these results demonstrate that the photogrammetry system introduces only small, unbiased measurement uncertainties that remain well within $\pm 19 \mu\text{m}$. This level of accuracy provides confidence that the technique is sufficiently precise for resolving the dynamic response of the panel.

3.3 Schlieren Flow Visualization

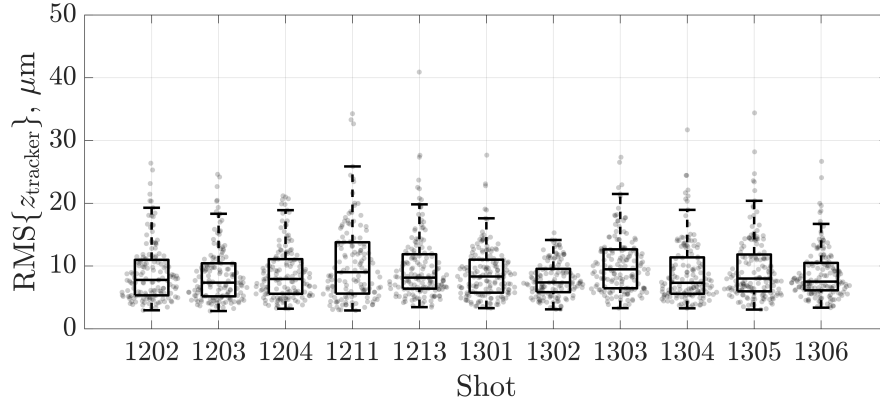
In STBLI flow fields, time-resolved full-field flow measurements are essential in characterizing key flow features such as shock waves, separation bubbles, and boundary layers. A widely used technique to visualize these features is high-speed schlieren imaging. Schlieren visualization makes density gradients in a flow field visible through the deflection of light rays. This is possible due to the variation of the refractive index of a gas with its density, as described by the Gladstone-Dale law in Eq. 3.5 (Settles, 2001). In this equation, ρ is the fluid density, κ is a constant specific to the fluid and wavelength of light, and n is the refractive index defined as the ratio of the speed of light in a vacuum, c_0 to that in the medium, c (Eq. 3.6). Consequently, regions of varying density within a compressible flow refract light by



(a) Time-average.



(b) Standard deviation.



(c) Root mean square.

Figure 3.11: Swarm and box plots of out-of-plane tracker marker displacements.

different amounts.

$$n - 1 = \kappa \rho \quad (3.5)$$

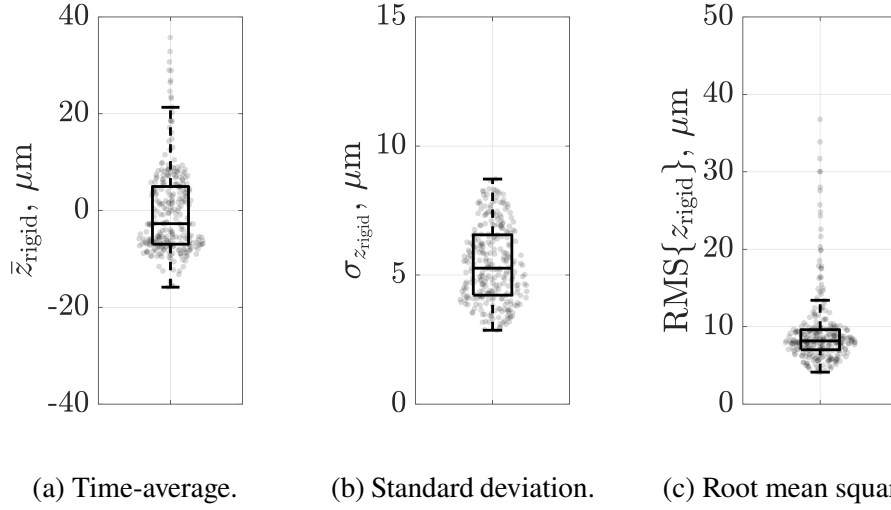


Figure 3.12: Swarm and box plots of out-of-plane rigid panel displacements with no STBLI loading (shot 1337).

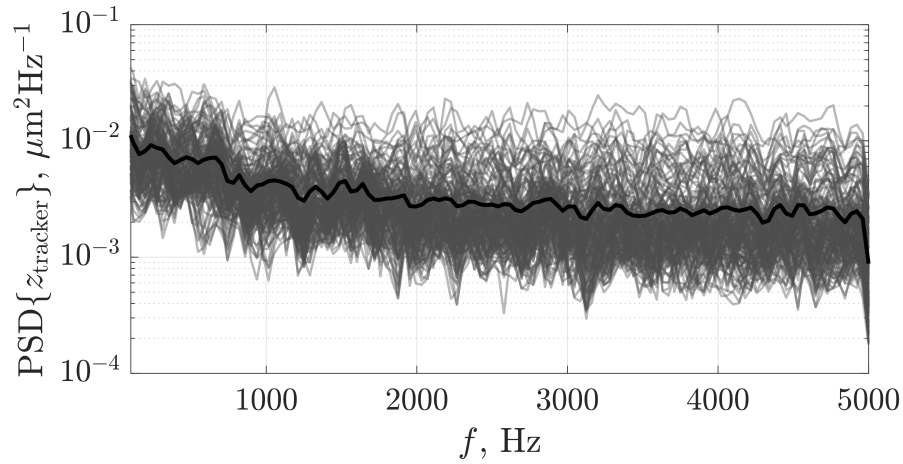


Figure 3.13: Power spectra of out-of-plane tracker marker displacements (black line represents spatial average power spectrum, gray lines represent each tracker marker power spectrum).

$$n = \frac{c_0}{c} \quad (3.6)$$

When a light ray traveling in the y -direction intersects a region of varying refractive index in an x, z -plane, the resulting ray curvature is given by Eqs. 3.7 & 3.8 (Settles, 2001).

$$\frac{\partial^2 x}{\partial y^2} = \frac{1}{n} \frac{\partial n}{\partial x} \quad (3.7)$$

$$\frac{\partial^2 z}{\partial y^2} = \frac{1}{n} \frac{\partial n}{\partial z} \quad (3.8)$$

Therefore, the components of the angular ray deflection in the x - and z -directions, ϵ_x and ϵ_z , are computed by integrating Eqs. 3.7 & 3.8 once (Eqs. 3.9 & 3.10). These equations demonstrate that the ray deflection is dependent on the gradient of refractive index orthogonal to the ray propagation direction. Thus, when the light rays are projected onto a viewing plane, the deflected rays result in brighter or darker regions of light intensity that enables visualization of the nonuniform density flow field.

$$\epsilon_x = \frac{1}{n} \int \frac{\partial n}{\partial x} \partial y \quad (3.9)$$

$$\epsilon_z = \frac{1}{n} \int \frac{\partial n}{\partial z} \partial y \quad (3.10)$$

3.3.1 Optical Design

Standard schlieren systems consist of a point light source that is collimated either through a lens or mirror. The collimated light passes through the varying density field, specifically the test section in wind tunnel experiments, and a second lens or mirror refocuses the collimated beam to an image of the point source at the focal point. Past the focal point, the beam forms a real inverted image of the test region that is imaged by a high-speed camera. A knife-edge is oriented at the focal point to cut off half of the refracted light rays. The knife-edge affects only ray refractions with components perpendicular to it, such that the schlieren image projected on the camera sensor is sensitive to the first derivative of density in the perpendicular direction to the knife-edge. A complete discussion on the theory and principles of schlieren systems is provided by Settles (2001).

Two separate schlieren systems were used in this study: a z -type mirror system used for large field of view visualizations and a compact lens-type system used for narrow field of view visualizations in conjunction with simultaneous focused laser differential interferometry (FLDI) measurements.

3.3.1.1 Z-type Mirror Setup

Z -type mirror schlieren systems are widely popular due to the large field of view and inexpensive cost of mirrors compared to large diameter lenses (Settles, 2001). In this

setup, depicted in Fig. 3.14, two 1500 mm focal length, 255 mm diameter concave mirrors are tilted at equal and opposite angles from their center-lines ($2\theta \approx 9^\circ$). A light source is focused using an aspheric focusing lens, and an iris is positioned at the focal point of the lens to create a point light source. The iris is positioned one focal length away from the first focusing mirror so that the light rays are collimated by the mirror. Note that a periscope is used between the iris and collimating mirror to raise the height of the light to the height of the collimating mirror and test section. The collimated light passes through the test section and flow field and are then focused by the second focusing mirror. A second periscope lowers the light down to height of the optics table. A knife-edge is positioned at the subsequent focal point, and the remaining light passes through a relay lens to focus the image on the camera sensor. Additional details on the optics components are listed in Table 3.2.

Two types of light sources are used in the z-type schlieren system depending on the desired measurement: a continuous white light-emitting diode (Cree X-Lamp MC-E LED) for time-averaged flow visualization and a pulsed laser diode (ThorLabs L637G1 or Cavilux HF 640 nm) for unsteady, time-resolved flow visualization. High-speed images are captured with either the Phantom v711, v1612, v2512, or TMX 7510 depending on camera availability. All four cameras feature a maximum resolution of 1280 by 800 pixels at various frame rates ranging between 7.5 to 75 kHz. Images captured using the continuous light source are recorded at 16 kHz at the full sensor resolution with camera exposures between 20 to 60 μ s. Images captured using the pulsed laser diode are recorded at reduced resolutions to achieve higher frame rates. The laser diode is pulsed at a 140 ns pulse width for the ThorLabs light source and between 10 to 40 ns pulse width for the Cavilux light source to effectively freeze the fluid motion in each frame. Due to the strong spatial coherence of the ThorLabs laser diode, a liquid light guide is used to reduce the spatial coherence followed by a fiber optic cable to diffuse the light into a suitable background for schlieren imaging.

3.3.1.2 Lens-type Setup

A lens-type schlieren system is implemented for use in a colinear FLDI and schlieren optical system, which is discussed in more detail in Section 3.4.1. In this setup, the schlieren images are illuminated by a Cavilux HF 640 nm pulse diode laser pulsed at 100 kHz. The light is focused to a point using a 15 mm focal length aspheric lens (F_3) and an iris. The schlieren light is introduced into the FLDI system using

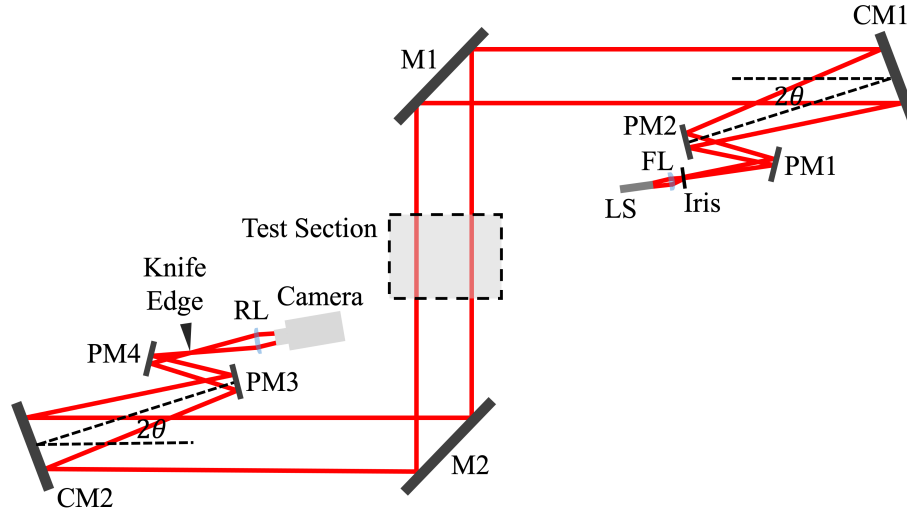


Figure 3.14: Facility Z-type schlieren diagram.

Table 3.2: Z-type schlieren component list.

Component	Type	Details
LS	Light source	ThorLabs L637G1 637 pulsed laser diode Cavilux HF 640 nm pulsed laser diode Cree X-Lamp MC-E continuous white LED
FL	Aspheric focusing lens	$\varnothing = 25.4$ mm, $FL = 20$ mm
PM1, PM4	Periscope flat mirror	$\varnothing = 50.8$ mm
PM2, PM3	Periscope flat mirror	$\varnothing = 100$ mm
CM1, CM2	Collimating mirror	$\varnothing = 255$ mm, $FL = 1500$ mm
M1, M2	Flat mirror	$\varnothing = 38.1$ cm
RL	Relay lens	$\varnothing = 50.8$ mm, $FL = 200$ or 300 mm

a 50.8 mm cube non-polarizing 50:50 beam splitter (BS_1) placed between focusing lenses, $F_{1,2}$, and Wollaston prisms, $WP_{1,2}$. The 76.2 mm diameter, 250 mm focal length achromatic doublet collimates the schlieren light through the test section. The second beam splitter, BS_2 , redirects the schlieren light through a 550 nm long pass dichroic mirror (DM), a knife-edge, a 0.5 OD neutral density filter (ND), and a 250 mm focal length imaging lens (F_4). The image is captured by either a Phantom v2512 or TMX 7510 high-speed camera at a sampling rate of 100 kHz. A diagram of the optical setup is shown in Fig. 3.15, a list of optical components is detailed in Table 3.3, and a detailed schematic of the setup with dimensions of optical components is provided in Appendix A.

Simultaneous flow and structure measurements are obtained using the colinear FLDI

Table 3.3: Colinear FLDI and schlieren component list.

Component	Type	Details
Laser	FLDI laser	532 nm Photonics Cobolt 05-01 Samba laser
D	Diverging lens	Thorlabs LC2067-A plano-concave lens, $FL = -9$ mm
QWP	Quarter wave plate	Thorlabs WPQ20ME-532, $\varnothing = 50.8$ mm
WP ₁ , WP ₂	Wollaston prism	Quartz, 2 arc-min splitting angle, 50 x 50 mm
BS ₁ , BS ₂	Cube beam-splitter	Non-polarizing, 50:50, 50.8 x 50.8 mm
F ₁ , F ₂	Focusing lens	Newport PAC095AR achromatic doublet lens, $FL = 250$ mm
LP	Linear polarizer	Thorlabs LPVIS100-MP
BP	Bandpass filter	Thorlabs FLH532-10, FWHM = 10 nm
PD	Photodetector	Thorlabs PDA36A2 Si switchable gain detector
Cavilux	Schlieren light source	Cavilux HF 640 nm pulsed laser diode
F ₃	Focusing lens	aspheric lens, $FL = 15$ mm
DM	Dichroic mirror	550 nm long pass dichroic mirror
F ₄	Relay lens	$FL = 250$ mm
ND	Neutral density filter	OD = 0.5

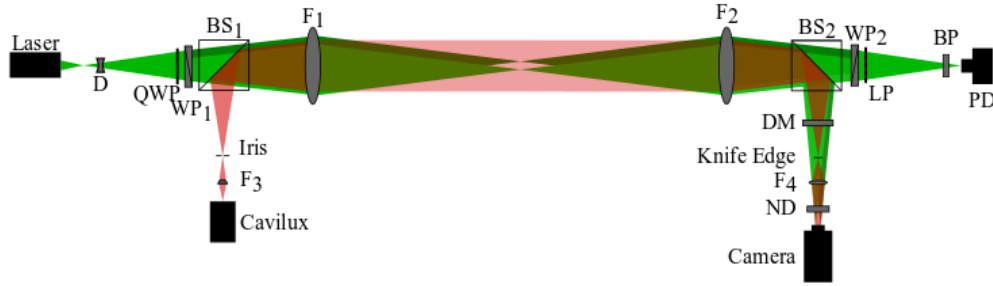


Figure 3.15: Colinear FLDI and schlieren diagram (Luo et al., 2025).

and schlieren setup in conjunction with the photogrammetry setup, as shown in the experimental model and diagnostic setup diagram in Fig. 3.16. The photogrammetry and schlieren camera exposures are synchronized by a Berkeley Nucleonics 577 pulse generator, where the photogrammetry camera captures a frame for every tenth frame captured by the schlieren camera.

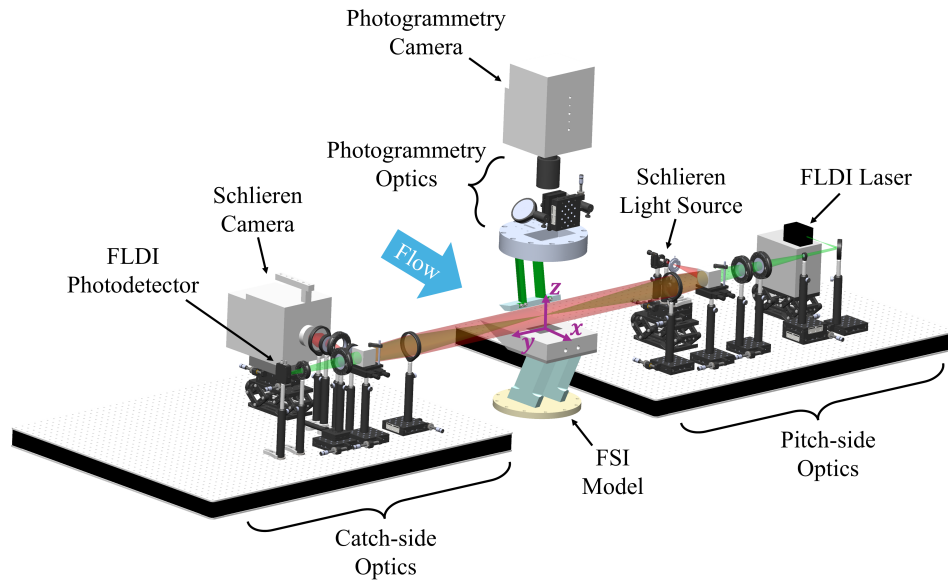


Figure 3.16: Schematic of experimental model and combined photogrammetry and colinear FLDI and schlieren diagnostic setup.

3.3.2 Image Processing Routine

An image processing routine is employed to enhance the image quality of the captured flow features. The approach is similar to that of Schmidt (2016), and a detailed step-by-step procedure of the image processing routine is described below. An example raw schlieren snapshot, wind-off image, and final processed schlieren snapshot are shown in Fig. 3.18.

1. **Transform to Model Stationary Frame**

A custom motion reduction scheme is applied to all recorded schlieren images to eliminate facility structural vibrations observed in the laboratory frame by transforming the footage to a model-stationary frame. Preliminary experiments are conducted to quantify facility motion by both camera- and laser-based measurements. For the camera-based measurements, a vinyl crosshair sticker placed on a test-section window is recorded with a high-speed camera, and the streamwise and vertical positions are tracked using a Canny edge-detection filter. Complementary measurements of streamwise motion are obtained with a Keyence LK-G407 laser displacement sensor (LDS). As shown in Fig. 3.17, the facility undergoes streamwise displacements of up to -0.15 mm, while the vertical motion remains negligible. The LDS and camera-based measurements agree well with each other within their respective uncertainties. In subsequent schlieren experiments, the LDS signal is used to correct for streamwise motion by repositioning each frame relative to the wind-off reference position. Subpixel accuracy is achieved through linear interpolation between pixels.

2. **Background Subtraction**

A wind-off image is subtracted from each frame to enhance the image quality by eliminating background artifacts.

3. **Detect Model Boundary**

A Canny edge detector identifies the horizontal wall boundary, and any pixel below this boundary is set to 0.

4. **Enhance Image Contrast**

The pixel intensities of each frame are rescaled from 0-255 to 0-1, where 0 is the darkest and 1 the brightest intensity.

5. **Set Average Image Brightness**

To enhance the image brightness and reduce potential flickering due to inconsistent brightness between frames, each frame is adjusted so that the average pixel intensity, $\langle I(t_k) \rangle$, is 0.5. The average pixel intensity in each frame is calculated according to Eq. 3.11, where $I(i, j, t_k)$ is the pixel intensity of the image at the i -th row and j -th column pixel coordinates at time t_k , and n_r and n_c are the number of rows and columns of the pixels in the image. Each pixel intensity is recomputed following Eq. 3.12.

$$\langle I(t_k) \rangle = \frac{1}{n_r n_c} \sum_{i=1}^{n_c} \sum_{j=1}^{n_r} I(i, j, t_k) \quad (3.11)$$

$$I'(i, j, t_k) = I(i, j, t_k) - \langle I(t_k) \rangle + 0.5 \quad (3.12)$$

6. Rescale Pixel Intensities

The darkest pixel across all frames is assigned an intensity of 0, and the brightest is assigned 1. All remaining pixels are linearly scaled to preserve the mean intensity of each frame.

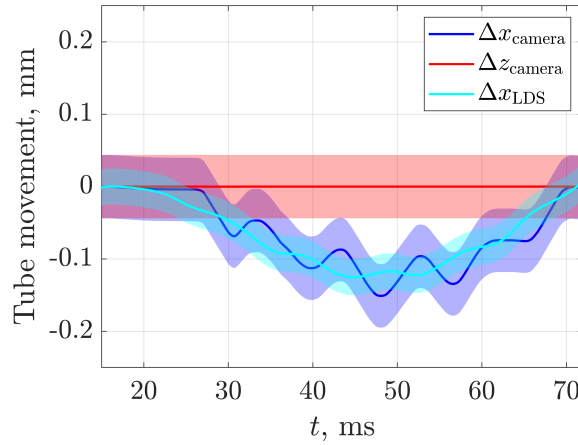
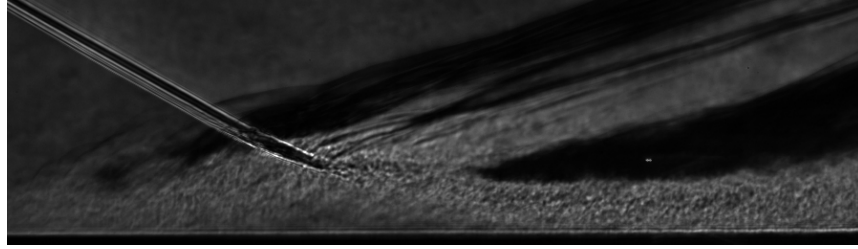


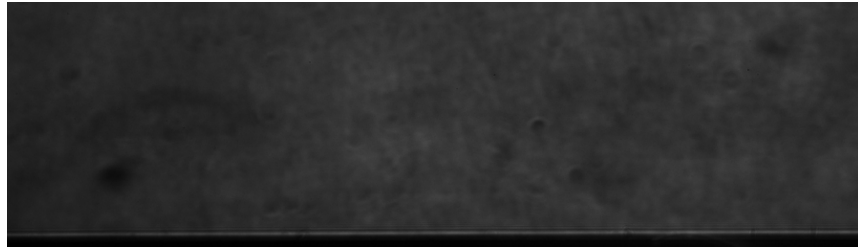
Figure 3.17: Facility movement during test time.

3.3.3 Schlieren Feature Tracking

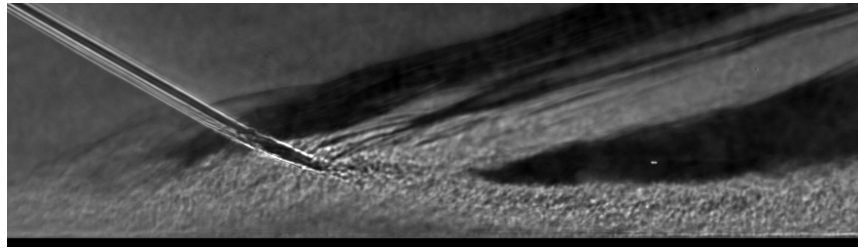
Quantitative flow field data are extracted from the high-speed schlieren footage using a custom schlieren feature tracking image processing routine. The streamwise positions of the separation and reattachment shock fronts, $x_{SS}(z, t)$ and $x_{RS}(z, t)$, and visual boundary layer height, $z_{BL}(x, t)$, are tracked in each schlieren frame using a threshold edge detection method. The shock front and boundary layer edge pixel intensity thresholds are manually chosen for each shot due to shot-to-shot variations in schlieren sensitivity. For the separation and reattachment shocks, the first pixel along the streamwise direction within the region of interest whose pixel intensity value is below the user-specified threshold is identified as the shock front position at a specific z -coordinate. Similarly, the first z -coordinate in the boundary layer region of interest whose intensity is above the user-specified threshold is identified as the visual boundary layer height at a specified x -coordinate. An example schlieren snapshot with the shock and boundary layer positions overlaid is shown in Fig. 3.19.



(a) Original.



(b) Wind-off.



(c) Processed.

Figure 3.18: Example schlieren snapshot.

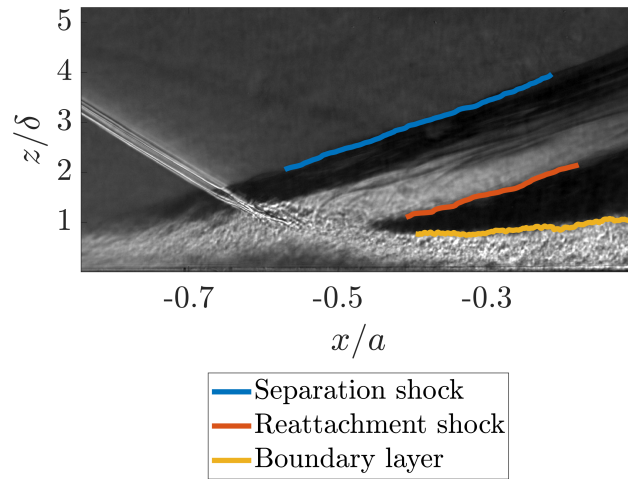


Figure 3.19: Example schlieren snapshot with tracked schlieren features.

3.4 Focused Laser Differential Interferometry

FLDI is an optical technique that responds to variations in the refractive index of a gas, which are linearly related to density by the Gladstone-Dale relation (Eq. 3.5).

Unlike schlieren, FLDI is a point measurement that is most sensitive to refractive index changes at the foci of the FLDI beam pair. This interferometric technique works by splitting a single laser beam into two beams offset by a small distance. As the laser beams travel through a flow field and are refracted by the change in density, the path lengths differ. When the beams are recombined, they interfere with each other depending on the difference in path length, and the resulting intensity is measured. FLDI has gained significant popularity in high-speed wind tunnel experiments, and a more in-depth history and characterization of FLDI is provided in Lawson (2021).

3.4.1 Colinear FLDI and Schlieren

Flow measurements are simultaneously captured using a colinear FLDI and high-speed schlieren system (Fig. 3.15). Conventional schlieren visualization is line of sight integrated, which means that spanwise features are obscured due to the lack of spatial filtering. However, FLDI spatially filters density fluctuations farther away from the beam pair foci resulting in a highly sensitive measurement. Furthermore, camera and light source limitations restrict the resolution and frame rate capable for schlieren imaging. Because the FLDI beam pairs are separated on the order of $100\ \mu\text{m}$ at their foci and the recombined beam is measured by a photodetector capable of sampling rates over 10 MHz, FLDI can better temporally and spatially resolve high frequency flow features, which makes it especially useful for turbulent boundary layer and separation bubble dynamics in STBLIs. The combination of simultaneous FLDI and schlieren measurements enables full-field flow visualization to locate the FLDI beam pair location within the flow while also capturing the structure and unsteady dynamics of the STBLI.

Colinear FLDI and schlieren systems have previously been developed by Bathel et al. (2021), Cobourn and Schmisser (2021), and Berger and Borg (2023). The setup used in this study is developed and characterized by Luo et al. (2025), and the FLDI design is based on the work of Lawson (2021) and Yu (2024). The FLDI system uses a 532 nm Hübner Photonics Cobolt 05-01 Samba laser that is expanded through a -9 mm focal length plano-concave lens, labeled D in Fig. 3.15. The beam is circularly polarized using a quarter wave plate (QWP) before being split by a Wollaston prism (WP_1) with a two arc-minute separation. The two beams are focused using the same focusing lens as in the schlieren light path (F_1). The catch side is symmetric to the pitch side, with the exception that a linear polarizer (LP) replaces the quarter wave plate. The beam passes through a 532 nm band-pass filter

(BP) to filter out the schlieren light before being focused on a Thorlabs PDA36A2 photodetector (PD).

The optical distances for this system are determined using an optimization process to minimize the Gaussian radius of the beam foci at the centerline of the model. This is performed using ray transfer matrix analysis and Gaussian optics code provided by Lawson (2021). Using the optic components discussed above, the optimization routine produced a FLDI system with nominal dimensions listed in Table 3.4. A detailed schematic with all optical component distances is provided in Appendix A.

Table 3.4: FLDI setup dimensions.

Dimension	Value
Diverging lens (D) to focusing lens (F_1) length, l_1	461.5 mm
Focal plane to focusing lens (F_1) length, l_2	533.4 mm
Beam diameter at focal plane, d_B	10.5 μm
Beam separation distance, Δx_B	145.4 μm

Custom 3D printed alignment tools are used to initially position the beam at the desired flow location based on previous schlieren footage. After the beam is aligned perpendicular to the nozzle exit plane and positioned in the correct streamwise and vertical position, both beam splitters, BS_1 and BS_2 , are then aligned by checking back reflections. Prior to any additional FLDI optics alignment, the schlieren catch side optics (DM, F_4 , and ND) are similarly aligned. The remainder of the FLDI optics alignment procedure follows that of Lawson (2021) and Yu (2024).

A Thorlabs BP209-VIS beam profiler is used to aid in the alignment process and measure the beam separation distance, inclination, and Gaussian beam diameter at the focus. Due to the large size of the beam profiler, beam positions near the surface of the model are unable to be measured with the beam profiler while the model is mounted in the test section. Therefore, the flat plate model is removed and remounted in between each shot to accommodate the beam profiler. Precise positions of the Wollaston prism and diverging lens are achieved using the beam profiler and micrometer translation stages to ensure beam parallelism and minimize beam foci diameter at the model centerline.

The catch side Wollaston prism and linear polarizer (WP_2 and LP) are translated and rotated until an infinite-fringe configuration is achieved (Lawson, 2021). The photodetector is positioned such that the recombined beam fills the photodetector sensor and maximizes the voltage output. Lastly, calibration is performed prior to

each shot to correlate the raw photodetector voltage to the beam phase difference, $\Delta\phi$. As performed in Lawson (2021) and Yu (2024), the catch side Wollaston prism is translated by a mechanized translation stage (Thorlabs Z812B controlled by a KDC101 controller) at a constant small velocity through the entirety of the infinite fringe. This process results in a constant change in beam phase difference that corresponds to a smooth sinusoid when plotted as voltage over time. The voltage to phase difference relationship is given by Eq. 3.13, where $V(t)$ is photodetector voltage, and A , B , and $\Delta\phi_0$ are constants determined through curve fitting (Lawson, 2021). The resting photodetector voltage, V_0 , is set to the middle of the dynamic range to avoid phase wrapping.

$$V(t) = A \sin(\Delta\phi(t) + \Delta\phi_0) + B \quad (3.13)$$

3.4.2 FLDI Signal Processing

After converting the FLDI signal from photodetector voltage to $\Delta\phi$, it is useful to quantitatively compare signals in the frequency domain. The power spectral density is defined in Eq. 3.14, where the windowed function $g_T(t) = g(t)$ for $-\frac{T}{2} \leq t < \frac{T}{2}$ and $g_T(t) = 0$ otherwise.

$$\text{PSD}\{g(t)\} = \lim_{T \rightarrow \infty} \frac{1}{T} |\hat{g}_T(f)|^2 \quad (3.14)$$

Welch's method is used to estimate the power spectral density of the finite length $\Delta\phi$ signal using the MATLAB implementation, `pwelch()`, with a Hann window with 50% window overlap. The window length is set to encompass two periods of the lowest frequency of interest to be resolved. Following the method outlined by Lawson (2021), a PSD of the background noise subtracted FLDI signal, $\widetilde{\Delta\phi}(t)$, is expressed by Eq. 3.15, where $V^*(t)$ is the measured photodetector voltage, $\alpha(t)$ is the background noise obtained from wind-off measurements, and β_1 is a constant calculated from the linearization of $\log[V(t)]$ about $\Delta\phi = \Delta\phi_0$ (Eq. 3.16).

$$\text{PSD}\{\widetilde{\Delta\phi}(t)\} \approx \left| \frac{1}{\beta_1} \right|^2 \left(\text{PSD}\{\log[V^*(t)]\} - \text{PSD}\{\log[\alpha(t)]\} \right) \quad (3.15)$$

$$\begin{aligned}
\log[V(t)] &= \log[A \sin(\Delta\phi(t) + \Delta\phi_0) + B] \\
&\approx \log[A \sin(\Delta\phi_0) + B] + \frac{A \cos(\Delta\phi_0)}{A \sin(\Delta\phi_0) + B} \Delta\phi(t) + O(\Delta\phi^2(t)) \\
&\approx \beta_0 + \beta_1 \Delta\phi(t)
\end{aligned} \tag{3.16}$$

3.5 Modal Decompositions

Proper orthogonal decomposition (POD) analysis is a modal decomposition technique that calculates a set of optimal basis vectors that best represents the data of interest (Taira et al., 2017). POD has been extensively used in both the fluid dynamics and FSI community for reduced order modeling and extracting coherent flow structures and dominant structural modes in many types of spatially and temporally resolved data sets (Shinde et al., 2022; Taira et al., 2020; Weiss, 2019).

In this study, for example, the fluctuations of the measured panel deformations, $z(x, y, t)$, are decomposed using POD into a sum of temporal coefficients, $\psi_i(t)$, and spatial modes, $\Phi_i(x, y)$, as shown in Eq. 3.17. The spatial modes are arranged in order of the magnitude of their eigenvalue, which corresponds to how energetic each POD mode is relative to the other POD modes. POD modes with the largest magnitude eigenvalues can inform which features are most prevalent in the original data set. The temporal coefficients represent the relative strength of a particular spatial mode over time. For a given spatial mode at a specific instance in time, a temporal coefficient with a large magnitude indicates the structure is strongly present, while a magnitude near zero indicates weak contribution of the structure.

$$\frac{z(x, y, t) - \bar{z}(x, y)}{h} = \frac{z(x, y, t)'}{h} = \sum_i \psi_i(t) \Phi_i(x, y) \tag{3.17}$$

Chapter 4

FACILITY AND MODEL CHARACTERIZATION

This chapter covers the facility and model characterization of the experiments performed in the Caltech Ludwig Tube. The chapter begins with a characterization of the freestream flow field and facility structural vibrations in Section 4.1. In Section 4.2, the incoming turbulent boundary layer is characterized through quantitative schlieren and pressure transducer measurements. In Sections 4.3.1 & 4.3.2, the static and dynamic properties of the compliant panel are investigated. In Section 4.3.3, the cavity acoustics underneath the compliant panel are examined. Linear flutter analysis is performed in Section 4.3.4 to evaluate whether the panel response exhibits a flutter response under the freestream conditions. In Section 4.3.5, experiments conducted at the freestream conditions without shock impingement are evaluated to validate the flutter analysis and characterize the panel response without the influence of STBLI-induced loading.

4.1 Facility Characterization

As discussed in Section 2.1.1, experiments for this work are primarily conducted at one set of flow conditions corresponding to a 250 kPa reservoir pressure. All freestream characterization in this section are performed at this flow condition, and nominal freestream values are listed in Table 2.1.

Freestream pressure measurements are recorded by the flush-mounted Kulite XCS-062 pressure transducer denoted ‘K6’ in Fig. 3.1. For these measurements, the shock generator is removed from the experimental model and the rigid panel insert is mounted. Pressure measurements inside the Ludwig Tube Mach 4 nozzle are recorded from a pressure tap located 76.2 mm downstream of the throat of the nozzle and 1156.2 mm upstream of the nozzle exit. FLDI freestream measurements are recorded with the beam pair oriented parallel to the flow direction.

A laser displacement sensor (LDS) and two accelerometers are used to measure facility structural vibrations during test time. The LDS measures the streamwise movement of the test section relative to the optics table. Single axis PCB 352A21 accelerometers are mounted on the test section and optics table to record streamwise direction acceleration measurements. The optics table is comprised of two broad-

boards that are connected to each other by steel framing that spans underneath the test section and nozzle. The optical breadboards are passively dampened by air dampers (ThorLabs PFP90120-8), and neither the table nor steel frame are connected to the test section, which effectively isolates them from facility vibrations.

A comparison of freestream flow field measurements as well as facility vibration measurements are presented in Fig. 4.1. The gray shaded region corresponds to measurements prior to facility start up. As soon as the piston valve begins to retract to initialize the flow, the nozzle pressure begins to rise. Facility start up is shaded red in the figure and lasts for nearly 40 ms until the flow reaches steady Mach 4 conditions. Test time is indicated by the blue shaded region in the figure and is determined manually from the nozzle pressure transducer trace. Specifically, the start and end of test time are defined as the points when the moving-average filtered trace first exhibits and subsequently departs from having a slope of zero. The post-test time region after the flow is no longer constant is shaded green in the figure.

Note that all measurements in Fig. 4.1 are simultaneously recorded from the same shot, and these measurements are repeatable and representative of the typical facility response. Time zero, $t = 0$ ms, corresponds to the start of data acquisition and is defined by the time at which the nozzle pressure transducer rises above an output voltage of 50 mV, or approximately 9.5 kPa.

Test section motion relative to the optics table, denoted as Δx_{tube} , is shown in the LDS trace in Fig. 4.1. During test time, the test section shifts by up to about 0.2 mm from its flow off position. The LDS signal primarily captures low-frequency motion of the test section, with a dominant oscillation period longer than the length of test time and orders of magnitude larger than the flow field time scales ($f_{\text{tube}} = 1/T_{\text{tube}} < 16$ Hz).

The accelerometer mounted on the test section registers substantially higher vibration amplitudes than the accelerometer on the optics tables, which remains nearly stationary throughout flow start up and test time. The vibration amplitude of the optics table is approximately two orders of magnitude smaller, indicating effective facility vibration isolation.

Around $t \approx 30$ ms, the test section accelerometer detects a spike in acceleration followed by low-frequency oscillations. Simultaneously, the FLDI trace also measures a sudden phase shift followed by a similar low-frequency oscillatory trend.

Given the lack of a similar response by the pressure traces, this behavior is likely not due to disturbances in the freestream, but rather to facility vibrations. Although the FLDI optical components are isolated from mechanical vibrations on the passively damped optics table, the test section is subject to structural motion. As the entire test section vibrates, the relative position of the FLDI beam pair within the flow field shifts. Consequently, the observed phase jump and oscillations in the FLDI signal reflect structural vibrations altering the location of the probe within the flow rather than true freestream flow disturbances. The timing of this sudden facility vibration is repeatable between experiments and likely originates from the piston valve abruptly stopping as it reaches its fully retracted state.

The time history and power spectral density of freestream static pressure fluctuations are shown in Fig. 4.2. Pressure fluctuations measured prior to flow start up, denoted as flow off, are plotted in dark gray while pressure fluctuations during test time are shown in blue. The freestream pressure fluctuates up to a maximum amplitude of approximately 6% of p_∞ .

A box plot of the percent error between the time-averaged measured freestream pressure, $\bar{p}_{K6}$, and theoretical freestream pressure, p_∞ , is presented in Fig. 4.3 to evaluate shot-to-shot consistency across nine repeated shots. In this plot, the red line indicates the median percent error, the box edges denote the 25th and 75th percentiles, and the whiskers indicate the full range from minimum to maximum. The results show excellent agreement, with the largest observed deviation being only about 1.5% from the theoretical value.

Similarly, the root mean square (RMS) of the measured pressure fluctuations are computed for each of the nine shots and summarized in a separate box plot in Fig. 4.3. The RMS values demonstrate strong consistency across runs, with a maximum value of approximately 2.33% of p_∞ .

As shown in the PSD plot in Fig. 4.2, the freestream spectral energy is roughly three orders of magnitude greater than the noise floor of the pressure transducers. The spectrum is relatively flat over the 1–40 kHz range, with a slight increase in broadband energy observed in the 150–400 Hz band.

The time history and power spectra of the freestream FLDI response are presented in Fig. 4.4. As discussed earlier, facility structural vibrations not associated with freestream disturbances can be observed in the FLDI response for $t > 30$ ms. This behavior appears in the power spectrum as a broadband peak below 2 kHz. At fre-

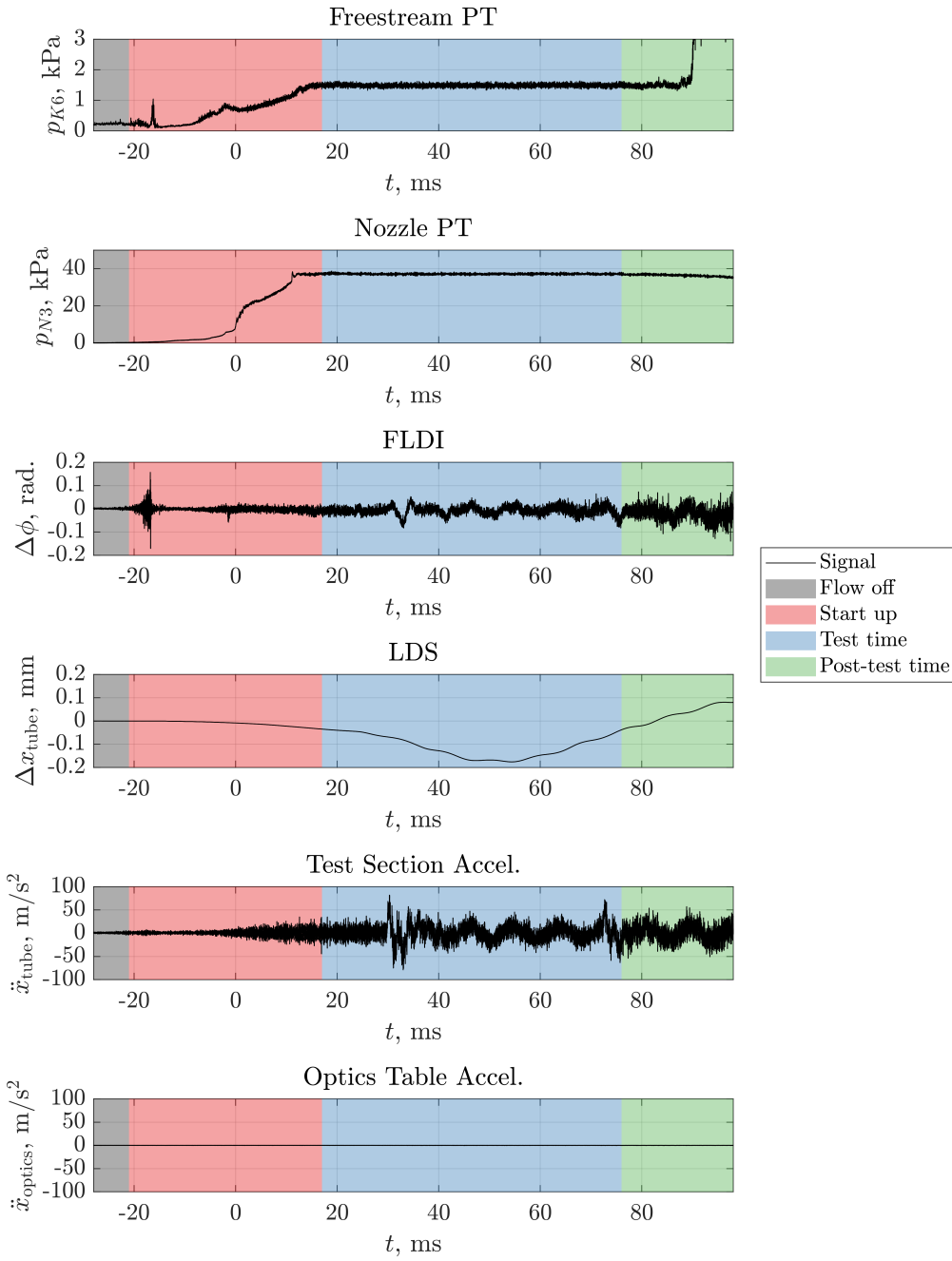


Figure 4.1: Comparison of freestream.

quencies above the structural vibration range up to 100 kHz, the freestream spectral energy exceeds the measurement noise floor by one to two orders of magnitude. The spectral energy approaches the noise floor near 300 kHz. A distinct peak at 247

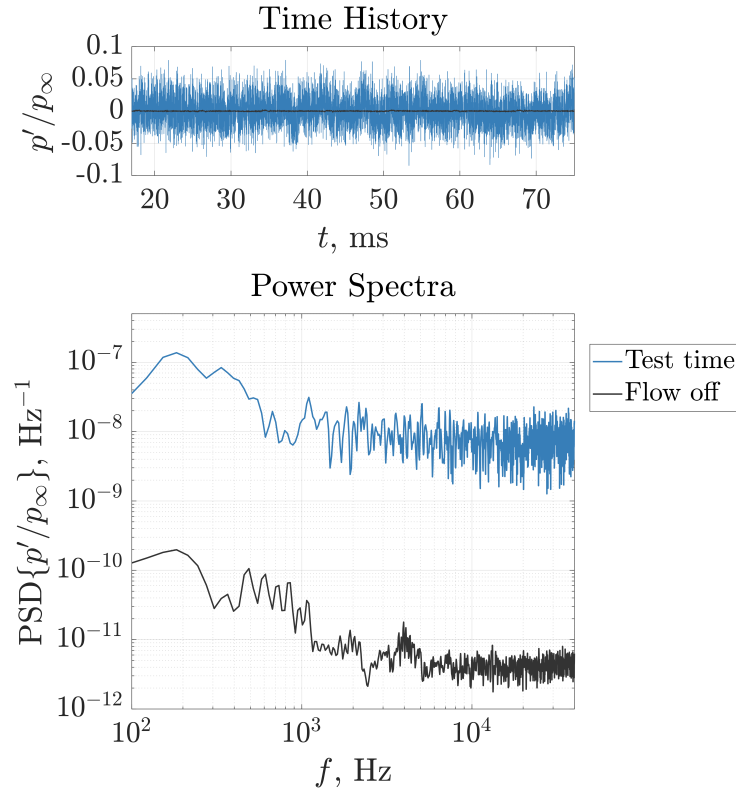


Figure 4.2: Freestream static pressure.

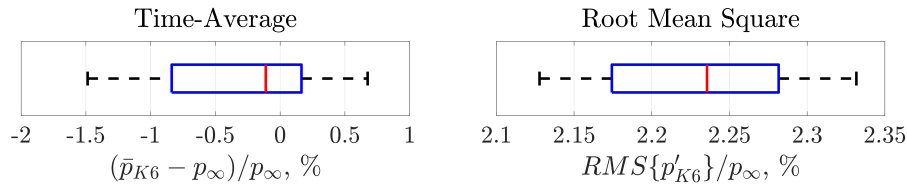


Figure 4.3: Box plots of measured freestream pressure.

kHz is detected by the FLDI measurement. This peak has been observed in other independent measurements in neighboring laboratories and likely corresponds to the pick up of a transmitted radio frequency. Additional peaks in the flow off power spectrum are attributed to laser noise.

Power spectra of the test section and optics table vibrations are shown in Fig. 4.5. The test section vibrations exhibit spectral energy over five orders of magnitude greater than that of the optics table vibrations. The optics table spectrum closely aligns with the flow off power spectrum, indicating that its vibration levels are on the same order as the measurement noise floor. The test section vibrations have the largest spectral energy at frequencies below 150 Hz, with an additional peak near

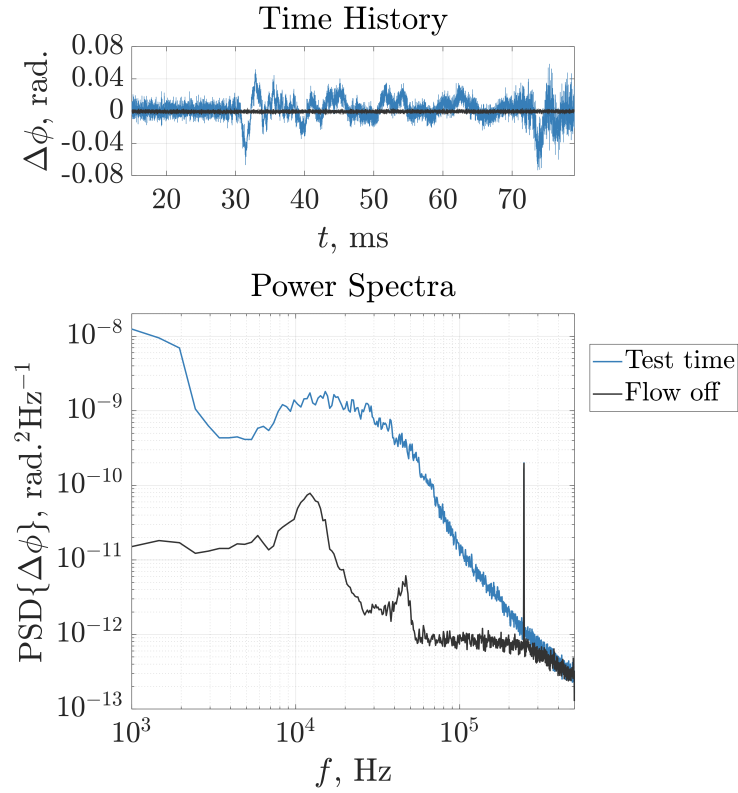


Figure 4.4: Freestream FLDI measurement.

500 Hz and several minor peaks distributed between 1 to 10 kHz.

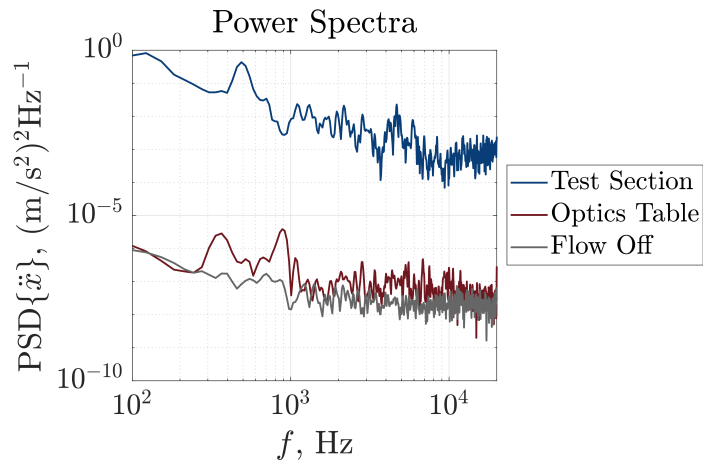


Figure 4.5: Power spectra of facility vibrations.

4.2 Boundary Layer Characterization

The following section characterizes the incoming turbulent boundary layer through quantitative schlieren measurements in Section 4.2.1 and pressure transducer mea-

surements in Section 4.2.2.

4.2.1 Boundary Layer Thickness

The incoming turbulent boundary layer profile over the flat plate without shock impingement is investigated using schlieren imaging. Schlieren images of the flow field are acquired with the knife edge oriented horizontally to visualize vertical density gradients. Prior to the experiment, a calibration image (Fig. 4.6) is obtained using a $F = 10$ m focal length lens to related the magnitude of the pixel intensities to the light deflection angle induced the lens, denoted as ϵ_z . The lens is plano-convex and has a radius of $R_{\text{lens}} = 12.7$ mm.

Parallel light rays passing through the lens are refracted depending on the radial distance from the lens center, and the vertical distance between the incident light ray location and lens center is defined as r_{lens} . Rays passing through the center experience zero deflection, whereas those near the outer radius undergo maximum deflection. The relationship between the deflection angle, radial position, and focal length is given in Eq. 4.1, which simplifies under the weak lens approximation ($R_{\text{lens}} \ll F$).

$$\frac{r_{\text{lens}}}{F} = \tan \epsilon_z \approx \epsilon_z \quad (4.1)$$

A flow off image captured without the lens is subtracted from the calibration image to reduce background image artifacts. Vertical slices within the lens in the calibration image provide the variation of pixel intensity with deflection angle. Pixel intensities between the leftmost and rightmost vertical slices, shown in Fig. 4.6, are extracted and plotted as a function of r_{lens} in Fig. 4.7a, and a horizontally-average pixel intensity profile is obtained to mitigate noise.

Following a similar procedure to that of Kennedy et al. (2017), the averaged pixel intensity profile is fitted to a sixth-order polynomial to establish a calibration curve. The radial coordinate is then converted to deflection angle using Eq. 4.1, and the sign of the deflection angle is inverted to align with the direction of the flow-induced density gradients. Due to non-ideal light transmission through the lens, the pixel intensity at the lens center is slightly lower than the background level. To correct for this bias, the vertical intercept of the fitted polynomial is adjusted such that the intensity is zero at $\epsilon_z = 0$ rad.

For a light ray passing through a spatially varying refractive index field, the light

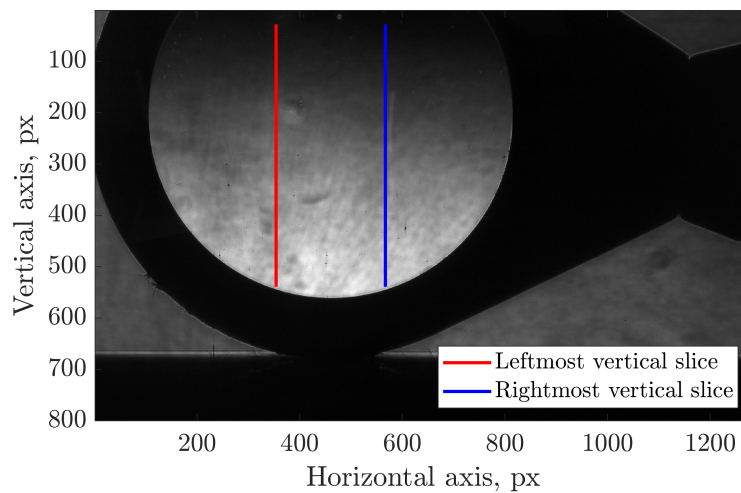
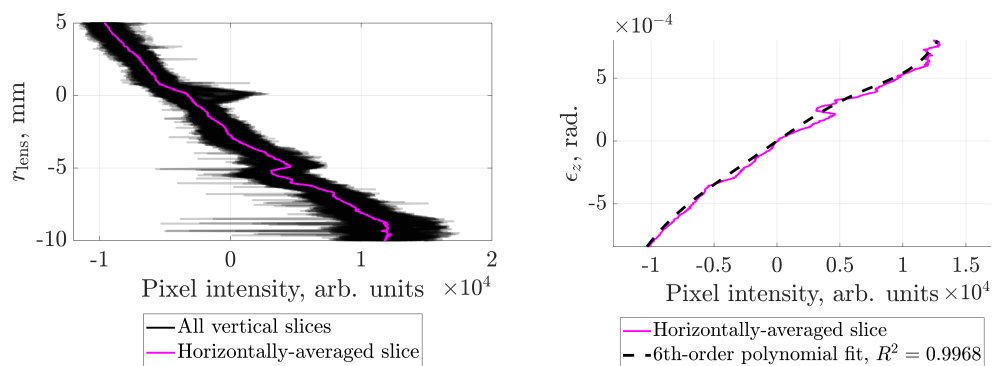


Figure 4.6: Schlieren calibration image of $F = 10$ m lens.



(a) Radial distance versus pixel intensity. (b) Deflection angle versus pixel intensity.

Figure 4.7: Schlieren pixel intensity calibration.

deflection angle is proportional to the first spatial derivative of the refractive index, as expressed in Eq. 4.2, where n is the refractive index, y is the direction along the optical axis, and z is the direction perpendicular to the knife edge orientation. The refractive index derivative is integrated along the optical path, and, assuming that $\frac{\partial n}{\partial z}$ is uniform in the spanwise direction, the integral simplifies and can be evaluated directly. The effective path length through the refractive index field is denoted by L_{path} , and n_{∞} represents the freestream refractive index.

$$\epsilon_z = \frac{1}{n} \int \frac{\partial n}{\partial z} dy = \frac{L_{\text{path}}}{n_{\infty}} \frac{\partial n}{\partial z} \quad (4.2)$$

Substituting the Gladstone-Dale relation (Eq. 3.5) into Eq. 4.2 shows that ϵ_z is proportional to the first spatial derivative of density, $\frac{\partial \rho}{\partial z}$, as given in Eq. 4.3.

$$\epsilon_z = \frac{\kappa L_{\text{path}}}{n_{\infty}} \frac{\partial \rho}{\partial z} \quad (4.3)$$

A time-averaged schlieren image of the turbulent boundary layer over the rigid flat plate is shown in Fig. 4.8a, where the vertical density gradients are visualized. The image is background subtracted from a flow off reference image, and the pixel intensities are converted to deflection angle using the calibration curve obtained from Fig. 4.7, yielding the calibrated $\bar{\epsilon}_z$ field shown in Fig. 4.8b. Due to light diffraction near the wall, the magnitude of $\bar{\epsilon}_z$ is unreliable close to the surface. Therefore, data for $z < 0.5$ mm are excluded. Additionally, to mitigate freestream noise, negative deflection angles are set to zero.

By integrating Eq. 4.3 vertically along the z -direction from a known reference location, such as the freestream, the density field can be reconstructed from the deflection angle field. At each streamwise coordinate, $\bar{\epsilon}_z$ is integrated from the freestream to a specified vertical height following Eq. 4.4, and the resulting density field is shown in Fig. 4.8c.

$$\rho(z) - \rho_{\infty} = \frac{n_{\infty}}{\kappa L_{\text{path}}} \int_{\infty}^z \epsilon_z dz \quad (4.4)$$

Assuming constant static pressure across the boundary layer, the temperature profile can be inferred from the density field using the ideal gas law shown in Eq. 4.5, where R_g refers to the specific gas constant of air. The resulting temperature field is shown in Fig. 4.8d.

$$T = \frac{p_\infty}{\rho R_g} \quad (4.5)$$

The mean density and temperature profiles at the panel leading edge ($x/a = -0.5$) are extracted and plotted in Fig. 4.9. Measurement uncertainty is estimated as the root mean square of the residuals from the calibration curve fit, and is propagated through the density and temperature calculations to obtain corresponding uncertainty bounds. Because density is obtained via integration from the freestream, the uncertainty increases with proximity to the wall.

By applying the turbulent Crocco-Busemann relation (Eq. 4.6), the mean temperature profile within the boundary layer can be expressed as a function of the mean velocity profile (White, 1991). In this equation, T_w is the wall temperature, T_{aw} is the adiabatic wall temperature defined in Eq. 4.7, and r_{rec} is the turbulent recovery factor defined in Eq. 4.8. The Prandtl number, Pr , is defined as $\frac{c_p \mu_\infty}{k_\infty}$, where c_p is the specific heat capacity at constant pressure, μ_∞ is the freestream dynamic viscosity, and k_∞ is the freestream thermal conductivity.

With the temperature profile known, the velocity profile is obtained by solving the quadratic form of Eq. 4.6 for $\bar{u}$. The resulting velocity profile at $x/a = -0.5$ is shown in Fig. 4.10.

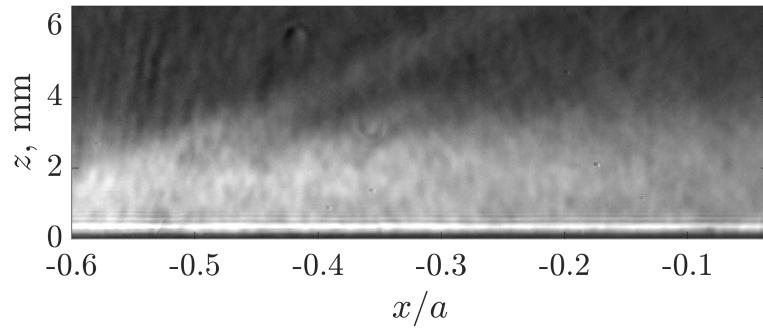
$$\bar{T} \approx T_w + (T_{aw} - T_w) \frac{\bar{u}}{u_\infty} - \frac{r_{rec} \bar{u}^2}{2c_p} \quad (4.6)$$

$$T_{aw} = T_\infty + \frac{r_{rec} u_\infty^2}{2c_p} \quad (4.7)$$

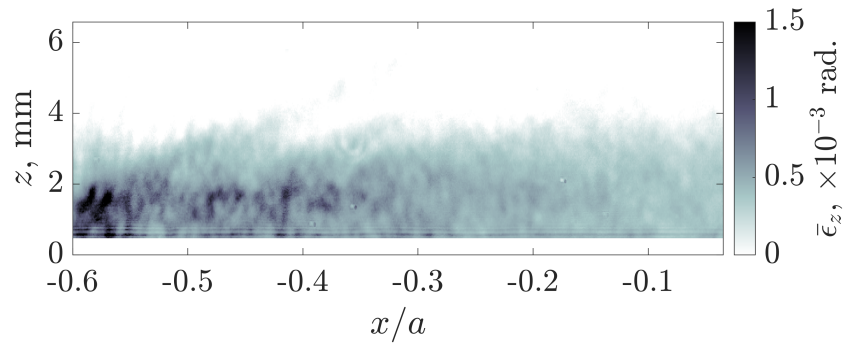
$$r_{rec} \approx Pr^{1/3} = \left(\frac{c_p \mu_\infty}{k_\infty} \right)^{1/3} \quad (4.8)$$

To extend the boundary layer profiles to the wall, compressible turbulent boundary layer theory is used to fit the near-wall region with analytic velocity profiles. The velocity and wall-normal distance are non-dimensionalized using the wall density and viscosity, as shown in Eqs. 4.9 & 4.10, where ν^* is defined in Eq. 4.11. Here, τ_w is the wall shear stress, ρ_w is the wall density, and ν_w is the wall kinematic viscosity.

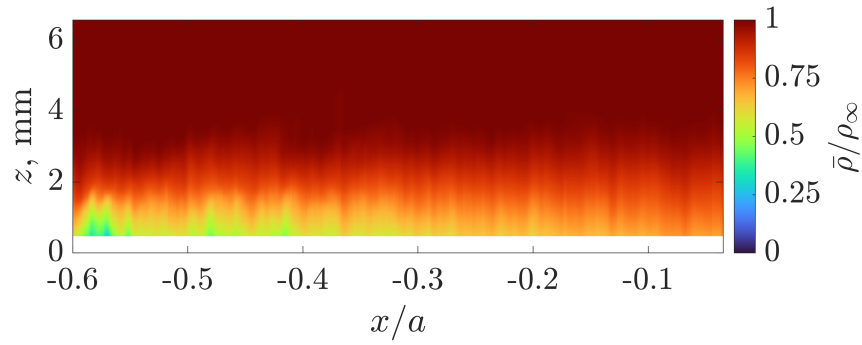
$$u^+ = \frac{\bar{u}}{\nu^*} \quad (4.9)$$



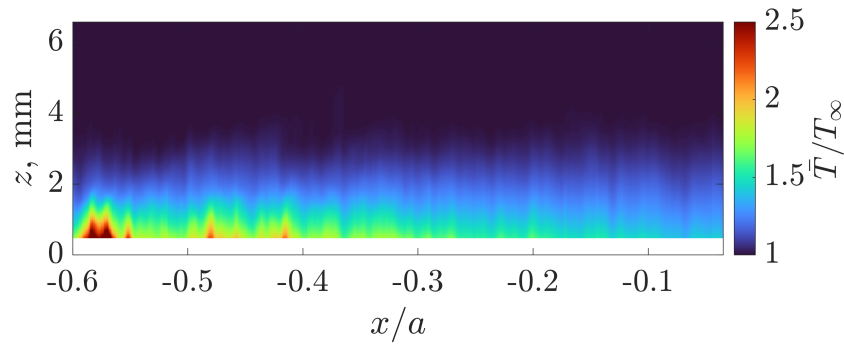
(a) Time-averaged schlieren image.



(b) Deflection angle.



(c) Density.



(d) Temperature.

Figure 4.8: Conversion to quantitative schlieren.

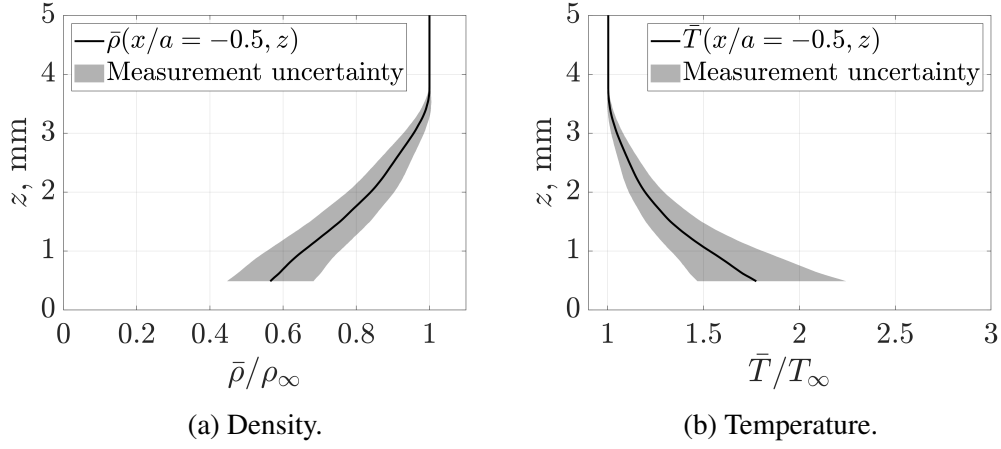


Figure 4.9: Density and temperature boundary layer profile at panel leading edge ($x/a = -0.5$).

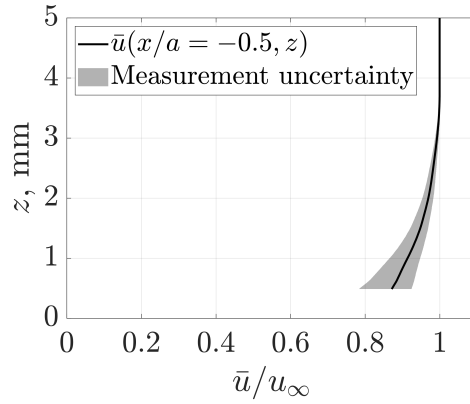


Figure 4.10: Velocity boundary layer profile at panel leading edge ($x/a = -0.5$).

$$z^+ = \frac{z\nu^*}{\nu_w} \quad (4.10)$$

$$\nu^* = \left(\frac{\tau_w}{\rho_w} \right)^{1/2} \quad (4.11)$$

The wall shear stress, τ_w , is not directly measured and is, therefore, estimated using the reference temperature method (Eckert, 1956; Meador and Smart, 2005). In this approach, a reference temperature is defined to determine representative gas properties for the entire boundary layer. These properties are then used to evaluate skin friction via a compressible turbulent flat plate correlation, which is a compressibility-corrected form of the incompressible relation.

For this analysis, the reference temperature, T_{ref} , is computed using the formulation of Meador and Smart (2005), as given in Eq. 4.12. The corresponding reference density and dynamic viscosity, ρ_{ref} and μ_{ref} , are evaluated at T_{ref} . The Reynolds number, Re_x , defined in Eq. 4.14, is based on the characteristic length scale $L_{\text{BLT-LE}} = 138.43$ mm, corresponding to the distance between the leading edges of the boundary layer trip and panel.

Applying the compressible turbulent flat plate skin friction correlation of Meador and Smart (2005), as given in Eq. 4.13, the skin friction is obtained as $C_f = 0.0019$. Finally, using the definition of skin friction (Eq. 4.15), the wall shear stress, τ_w , is computed.

$$\frac{T_{\text{ref}}}{T_{\infty}} = 0.5 \left(1 + \frac{T_w}{T_{\infty}} \right) + 0.16 r_{\text{rec}} \left(\frac{\gamma - 1}{2} \right) M_{\infty}^2 \quad (4.12)$$

$$C_f = \frac{0.02296}{\text{Re}_x^{0.139}} \left(\frac{\rho_{\text{ref}}}{\rho_{\infty}} \right)^{0.861} \left(\frac{\mu_{\text{ref}}}{\mu_{\infty}} \right)^{0.139} \quad (4.13)$$

$$\text{Re}_x = \frac{\rho_{\text{ref}} u_{\infty} L_{\text{BLT-LE}}}{\mu_{\text{ref}}} \quad (4.14)$$

$$C_f = \frac{\tau_w}{\frac{1}{2} \rho_{\infty} u_{\infty}^2} \quad (4.15)$$

Thus, using Eqs. 4.9-4.11, the measured velocity profile is transformed into u^+ and z^+ coordinates and is plotted in Fig. 4.11. According to the law of the wall, the velocity profile in a boundary over a flat plate follows a logarithmic form given in Eq. 4.16 when outside of the viscous sub-layer (White, 1991). For supersonic and hypersonic flows, van Driest showed to first order that $C_1 \approx 0.41$ and $C_2 \approx 5.0$ (White, 1991). In the near-wall viscous sub-layer, the velocity profile follows a linear relationship, as expressed in Eq. 4.17.

In Fig. 4.11, both the log-law and the viscous sub-layer relations are plotted. The log-law relation with the van Driest constants are plotted as the blue dotted curve, while a nonlinear least squares fit to the experimental data yields optimized values of $C_1 = 0.90$ and $C_2 = 10.3$, shown as the dotted orange line. The discrepancy between the fitted parameters and the van Driest constants is likely attributed to measurement uncertainty.

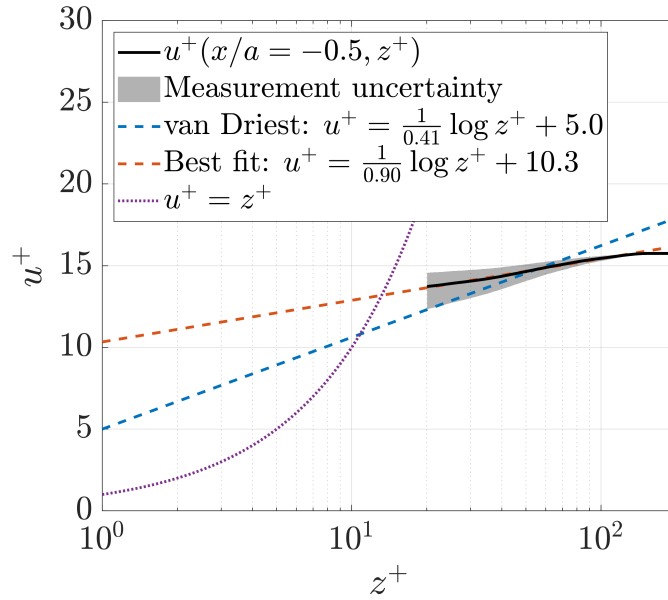


Figure 4.11: Velocity boundary layer profile in z^+ coordinates at panel leading edge ($x/a = -0.5$) fitted to boundary layer functions.

For $z^+ < 10$, the profile follows the viscous sub-layer behavior, while around $z^+ \approx 13$, the linear sub-layer and log-law curves intersect when using the fitted parameters. In this region, these two boundary layer functions are typically blended to provide a smooth transition between regimes.

$$u^+ = \frac{1}{C_1} \ln z^+ + C_2 \quad (4.16)$$

$$u^+ = z^+ \quad (4.17)$$

Converting back to the dimensional coordinate system, the full velocity profile reconstructed using the fitted boundary layer functions is presented in Fig. 4.12. The near-wall density and temperature profiles are extrapolated by reapplying Eqs. 4.6 & 4.5, and the resulting extended profiles are shown in Figs. 4.13 & 4.14.

The compressible displacement thickness is defined in Eq. 4.18, and the compressible momentum thickness is defined in Eq. 4.19. The 99% boundary layer thickness, δ , represents the wall-normal location at which the mean velocity reaches 99% of the freestream velocity, such that $\bar{u}(\delta) = 0.99u_\infty$. The values for all three boundary layer metrics at the panel leading edge location are reported in Table 4.1.

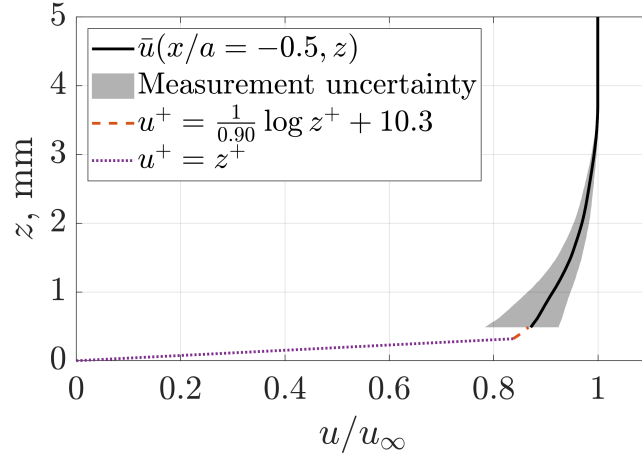


Figure 4.12: Velocity boundary layer profile at panel leading edge ($x/a = -0.5$) extended to wall using boundary layer functions.

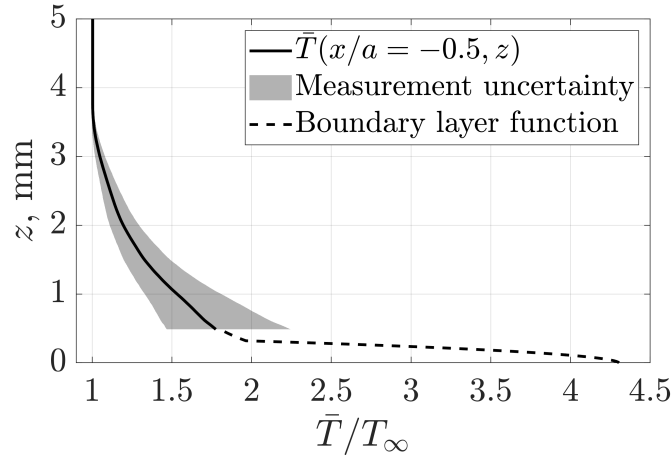


Figure 4.13: Temperature boundary layer profile at panel leading edge ($x/a = -0.5$) extended to wall using boundary layer functions.

$$\delta^* = \int_0^\infty \left(1 - \frac{\rho}{\rho_\infty} \frac{u}{u_\infty} \right) dz \quad (4.18)$$

$$\theta_{BL} = \int_0^\infty \frac{\rho}{\rho_\infty} \frac{u}{u_\infty} \left(1 - \frac{u}{u_\infty} \right) dz \quad (4.19)$$

4.2.2 Zero Pressure Gradient

The static surface pressure over the rigid flat plate is measured at multiple stream-wise locations, and the resulting distribution is shown in Fig. 4.15. The pressure transducer measurements remain approximately constant and are consistent with the

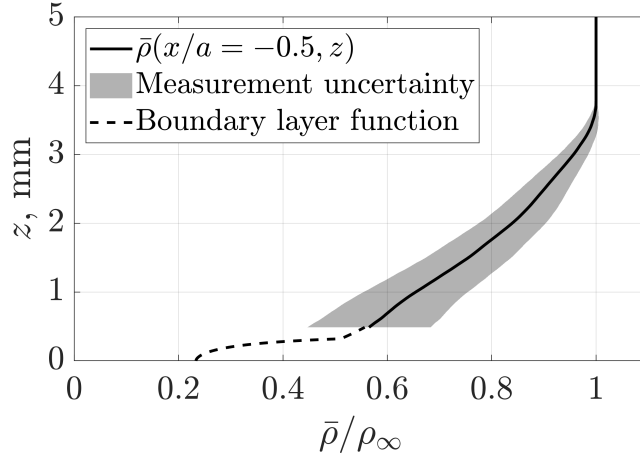


Figure 4.14: Density boundary layer profile at panel leading edge ($x/a = -0.5$) extended to wall using boundary layer functions.

Table 4.1: Boundary layer parameters at panel leading edge, $x/a = -0.5$ (units in mm).

Displacement thickness, δ^*	1.02
Momentum thickness, θ_{BL}	0.12
Boundary layer thickness, δ	2.87

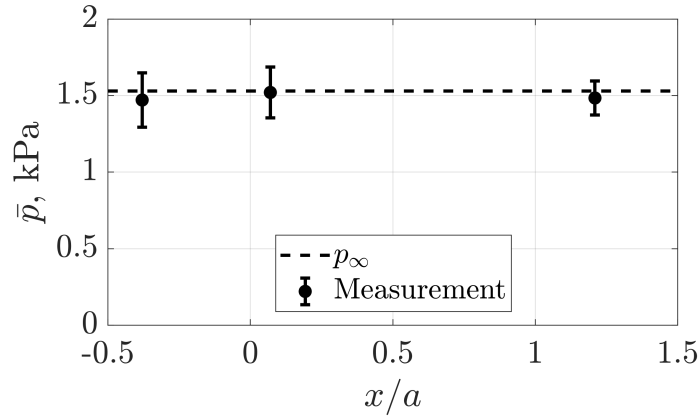


Figure 4.15: Static pressure over flat plate under no shock impingement.

nominal freestream static pressure, p_{∞} , within the measurement uncertainty. This uniformity confirms that the incoming boundary layer develops under effectively zero pressure gradient in the absence of shock impingement.

4.3 Compliant Panel Characterization

The following section details numerical and experimental characterizations of the compliant panel structural response. Section 4.3.1 investigates the panel deflection

when loaded with a static pressure difference, Section 4.3.2 characterizes the free vibrational response via a roving hammer test, and Section 4.3.3 characterizes the panel cavity acoustical resonances. A numerical, linear flutter analysis is performed in Section 4.3.4 to predict the flutter boundaries of the panel without STBLI loading, and experiments conducted to verify the behavior are discussed in Section 4.3.5. Non-dimensionalized parameters to characterize the aeroelastic properties of the interaction are defined in Section 4.3.6.

4.3.1 Static Deflection

The static deflection of the panel under uniform pressure loading is examined to characterize the panel structural response and initial shape prior to flow start-up. Photogrammetry is utilized to measure the surface deflection as the panel cavity is gradually pressurized from the evacuated test section pressure, $p_c(t_i) = p_{\text{vac}} = 300 \text{ Pa}$, to the pre-shot cavity fill pressure, $p_c(t_f) = 6.5 \text{ kPa}$ or 9.0 kPa . The pressurization process is performed slowly, with a total fill time of approximately 160 seconds.

4.3.1.1 Classical Plate Theory

Classical plate theory is a simplified 2D model that can be used to describe the bending of thin, flat plates under small deflections. For a rectangular panel with fixed or simply supported boundary conditions along all edges, Young and Budynas (2002) provides a formula based on classical plate theory to calculate the center point deflection when uniformly loaded (Eq. 4.20). The panel has a thickness h , length a , and width b , where $a < b$. The modulus of elasticity is denoted as E , and the parameter α depends on the aspect ratio, b/a , and boundary conditions. For the experimental model in this study, $b/a = 1.4765$, and interpolation of the tabulated values in Young and Budynas (2002) yields $\alpha \approx 0.0236$ for fixed boundary conditions and $\alpha \approx 0.0822$ for simply supported boundary conditions. The applied pressure differential across the panel is defined as $\Delta p = p_c - p_{\text{vac}}$. This model assumes the panel is a homogeneous, isotropic material with uniform thickness ($h < a/4$) and that the panel undergoes only small deflections ($z_{\text{center}} < h/2$).

$$z_{\text{center}} = \frac{\alpha \Delta p a^4}{E h^3} \quad (4.20)$$

4.3.1.2 Finite Element Analysis

An FEA static simulation is also performed to validate the plate theory predictions and investigate the nonlinear panel response under large deflections. The simulations are carried out using ANSYS. Both fixed and simply supported edge boundary conditions are applied, and a uniform pressure loading is applied over the panel. The large deflection setting is enabled to account for structural nonlinearity.

4.3.1.3 Comparison with Static Deflection Predictions

A comparison of center point deflection as a function of pressure loading from plate theory, nonlinear FEA, and experimental measurements is displayed in Fig. 4.16. For small deflections under $z_{\text{center}} < h/2$, the fixed boundary condition plate theory and FEA solutions closely match the experimental measurements. This close agreement lasts until about $\Delta p = 1.3p_{\infty}$. As the pressure loading increases further, both fixed boundary condition predictions begin to diverge from the experimental results. At the pre-shot cavity pressure condition of $\Delta p = 6.2 \text{ kPa} = 4.05p_{\infty}$, the panel is measured to be $z_{\text{center}} = 1.33h$, which is under-predicted by the fixed FEA simulations by about 28%. At the pre-shot pressure load of $\Delta p = 8.7 \text{ Pa} = 5.69/p_{\infty}$, the panel is measured to be at a center point deflection of $z_{\text{center}} = 1.64h$, which is under-predicted by the fixed FEA simulations by about 29%.

In contrast, the simply supported plate theory and FEA predictions greatly over-predict the center point deflection by up to 108%. Thus, the experimental boundary conditions are closer to being fixed than simply supported. However, these results reveal that the boundary conditions are not perfectly fixed. Discrepancies between the fixed FEA simulation and experimental results may arise from incorrect boundary condition assumptions or variations in structural properties. While the fixed boundary condition simulation assumes the panel edges are perfectly clamped to the panel base via a rigid epoxy bond, the epoxy layer may exhibit some degree of elasticity. This would allow limited edge compliance, relaxing the idealized zero-displacement and zero-slope boundary conditions. Additionally, the paint layer applied to the panel surface for photogrammetry tracking may introduce minor changes to the panel thickness. Although more detailed modeling of boundary conditions and material properties are required for higher-fidelity predictions, these results imply that the panel is closer to being fixed than simply supported, and for small deflections the results agree well with theory.

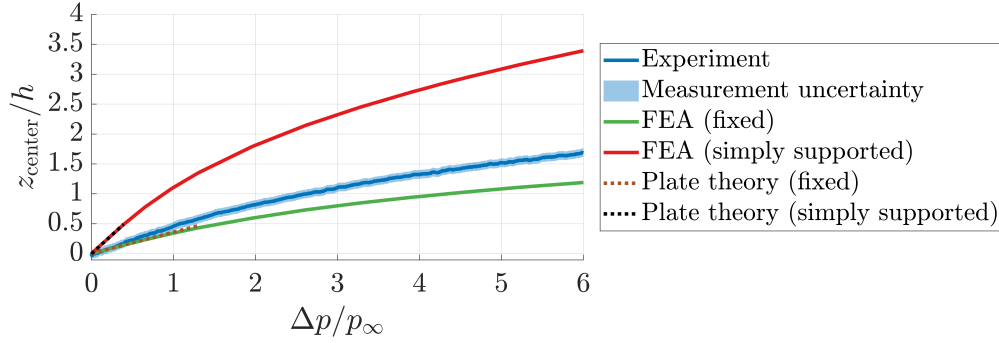


Figure 4.16: Center point deflection of panel versus pressure difference.

4.3.1.4 Yielding Condition

A widely used criterion for assessing yielding in ductile materials is the von Mises failure criterion, which states that yielding occurs when the von Mises stress, σ_{vm} , exceeds the yield strength, σ_y , of the material. Yielding predictions for the panel are predicted using nonlinear FEA simulations assuming fixed boundary conditions, which will result in more conservative estimates than the simply supported boundary condition case.

According to the 316 stainless steel shim stock manufacturer's certificate of compliance, the yield strength of the compliant panel is $\sigma_y = 275$ MPa. Before flow start up, the panel experiences a constant, uniform pressure loading of $\Delta p = 6.2$ or 8.7 kPa, depending on the cavity pressure condition. Nonlinear FEA predicts the maximum von Mises stress at this loading is approximately 86 MPa and 112 MPa, respectively, which corresponds to factors of safety of 3 and 2.5. This indicates that the panel remains within the elastic regime prior to flow startup.

Furthermore, during test time, the time-averaged STBLI-induced loading over the upper surface of the panel decreases the total pressure difference between the cavity pressure. Though, local pressure peaks and unsteady fluctuations still induce pressure peaks and surface deformations on the order of about $100\text{--}200\ \mu\text{m}$. However, these pressure peaks remain well below the yielding threshold, and the panel is not expected to plastically deform during test time. Similarly, during flow startup, the panel surface pressure does not exceed the test time surface pressure values, which implies yielding is also not expected during this phase.

After test time, the facility eventually reaches a quiescent state in which the pressure is constant throughout the entire tube. Boyle's law can be applied to estimate the post-shot quiescent state pressure, p_f (Eq. 4.21). The initial driver tube pressure

and volume are p_{res} and V_{res} , respectively, and the total volume of the Ludwig tube, including the driver tube, Mach 4 nozzle, test section, and dump tank, is V_f . For a $p_{\text{res}} = 250$ kPa condition shot, this results in a final post-shot pressure of $p_f \approx 41$ kPa, or a panel pressure differential of $|\Delta p| = |p_c - p_f| = 34.5$ or 32 kPa, depending on the cavity pressure condition. The predicted yielding pressure from nonlinear fixed boundary condition FEA simulations is $\Delta p_{\text{yield}} = 31.5$ kPa. Therefore, since the post-shot pressure loading at both cavity pressure conditions exceeds the yielding threshold ($|\Delta p| > \Delta p_{\text{yield}}$), the panel is expected to plastically deform after the shot.

$$p_{\text{res}} V_{\text{res}} = p_f V_f \quad (4.21)$$

This analysis indicates that each compliant panel can withstand only a single test before yielding. Although varying panel properties or dimensions could stiffen the panel to withstand the post-shot static pressure loading, those modifications may attenuate the desired panel vibration amplitudes during test time. Reduced amplitudes would not only decrease the level of fluid-structure coupling in the interaction but also decrease the signal to noise ratio of the panel deflection measurement. To ensure consistent experiment repeatability, the panels are manufactured following a consistent procedure. Additionally, panel to panel variation is characterized in the following sections of this chapter.

4.3.2 Modal Analysis

When a thin structure is subjected to external loading, the low flexural rigidity per unit mass of the structure makes it highly sensitive to vibrations. The subsequent vibrational response may include vibration at specific characteristic frequencies, known as natural frequencies. The mode shapes associated with these frequencies describe the displacement profiles of the structure during vibration. If the external force excites the structure at a frequency that matches one of the natural frequencies, resonance may be induced, resulting in significant amplification of the vibration amplitude. Given the unsteady, low-frequency behavior of STBLIs, characterizing the modal properties of the panel prior to wind tunnel testing is essential for understanding how unsteady aerodynamic loading and fluid-structure coupling influence the vibrational response.

A roving hammer test is conducted to experimentally determine the natural frequencies and mode shapes of the panel. Testing is performed after the panel is bonded

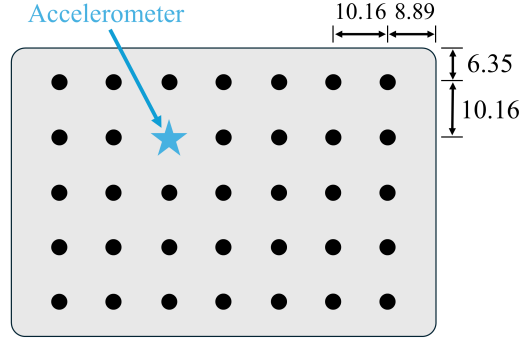


Figure 4.17: 5-by-7 impact point grid and accelerometer placement for roving hammer test (dimensions in mm).

to the panel base using epoxy but prior to installation in the test section. During the hammer test, the panel base is clamped to a metal breadboard on a benchtop with foam tape placed between the bottom of the panel insert and the breadboard to suppress any vibrations from the breadboard. The panel cavity is unsealed to ensure no pressure differential across the panel surface during testing. The panel surface is discretized into a 5-by-7 grid of impact points, as shown in Fig. 4.17. A single-axis PCB 352A21 accelerometer is mounted beneath the panel at grid point (2,3) to measure out-of-plane acceleration. This location is selected because it is responsive to all of the predicted lowest-order vibration modes since it does not lie along any nodal lines. Each impact point is gently tapped with a PCB 086E80 impulse hammer to impulsively excite the panel, and the corresponding hammer force and panel acceleration are recorded. Care is taken to align the hammer tip perpendicular to the panel surface prior to each strike. Any tap that results in multiple unintended contacts is discarded and repeated. To increase the accuracy of the statistical estimate, each impact point is tested ten times. The recorded data are processed using the MATLAB Signal Processing Toolbox functions, `modalord()` and `modalfit()`, to compute the frequency response function and perform modal parameter estimation.

Six repeated hammer tests are conducted on the same panel to assess the measurement uncertainty in estimating the natural frequencies of the panel. For each of the six lowest-order vibrational modes, the standard deviation of the measured natural frequency, σ_{f_k} , is computed. The average percent uncertainty, $\varepsilon_f^{\text{exp}}$, is then calculated by averaging the ratio of standard deviation to trial-averaged natural frequency, $\langle f_k \rangle$, as shown in Eq. 4.22. This results in an estimated measurement uncertainty of $\varepsilon_f^{\text{exp}} \approx 5.2\%$.

$$\varepsilon_f^{\text{exp}} = \frac{1}{n_{\text{modes}}} \sum_{k=1}^{n_{\text{modes}}} \frac{\sigma_{f_k}}{\langle f_k \rangle} \quad (4.22)$$

Theoretical natural frequency and mode shape calculations for both fixed and simply supported boundary conditions are detailed in Appendix B. A comparison between the predicted and measured natural frequencies for three unique panels is shown in Fig. 4.18, and the predicted lowest-order mode shapes for both fixed and simply supported boundary conditions are illustrated in Figs. 4.19 & 4.20. The predicted and trial-averaged experimental frequencies are also tabulated in Table 4.2, where $f_{\text{theory}}^{\text{fixed}}$ are the predicted fixed boundary condition frequencies, $f_{\text{theory}}^{\text{ss}}$ are the predicted simply supported boundary condition frequencies, and $\langle f_{\text{exp}} \rangle$ are the trial-averaged experimental frequencies.

The lowest-order mode, (1,1), consists of nodal lines along the edges of the panel, where the center point of the panel is an anti-node that exhibits maximum deflection. High-order modes consist of additional nodal lines and anti-nodes. As the number of nodal lines in the mode shape increases, the natural frequency tends to increase.

Overall, the measured natural frequencies between all three panels tested all lie within the measurement uncertainty for each mode. This signifies that there is generally no strong, measurable difference between the natural frequencies of each panel. This indicates that the panel-to-panel variability is within the estimated measurement uncertainty of 5.2% of the natural frequency.

In general, the fixed boundary condition natural frequency estimates tend to better predict the measured hammer test natural frequencies compared to that of the simply support boundary conditions. For mode (1,1), the measured frequencies deviate about 13% from the fixed boundary condition estimate. For all modes except mode (2,1), the measured frequencies deviate less than 15% compared to the fixed boundary condition estimates. Though, for mode (2,1), the percent difference is 21%, and the measured frequencies tend to be closer to the simply supported frequencies, with a percent difference is 19%.

The discrepancy between theory and experiment are consistent with that in Section 4.3.1. The experimental results lie between the fixed and simply supported estimates, with a bias towards the fixed boundary conditions. This discrepancy may additionally be attributed to elasticity of the adhesive used to clamp the panels, modified structural properties due to the painted panel surface, and the intrusive nature of the hammer test due to the added accelerometer mass.

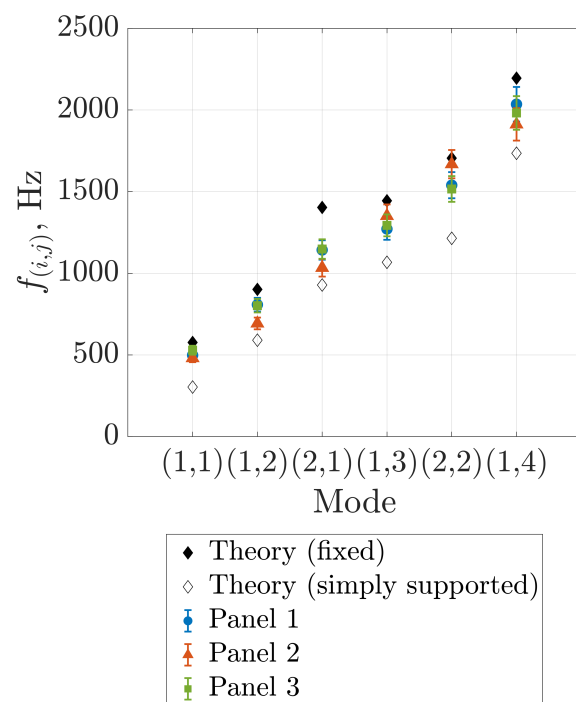


Figure 4.18: Measured natural frequency of each panel mode.

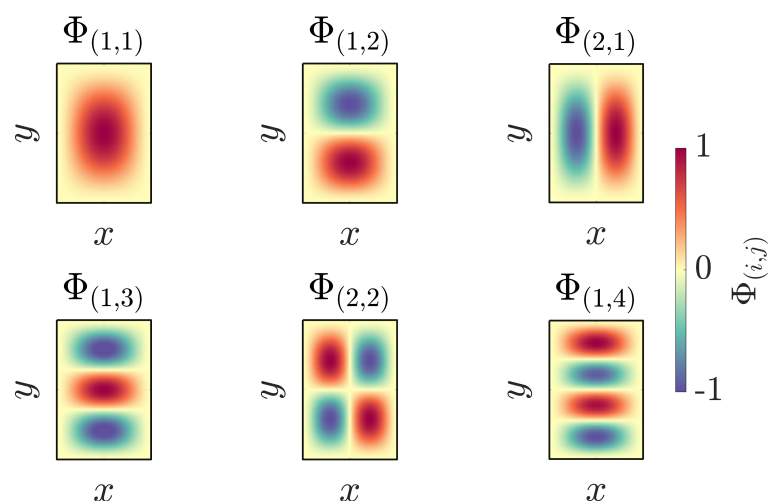


Figure 4.19: Lowest-order theoretical panel vibration modes for fixed boundary conditions.

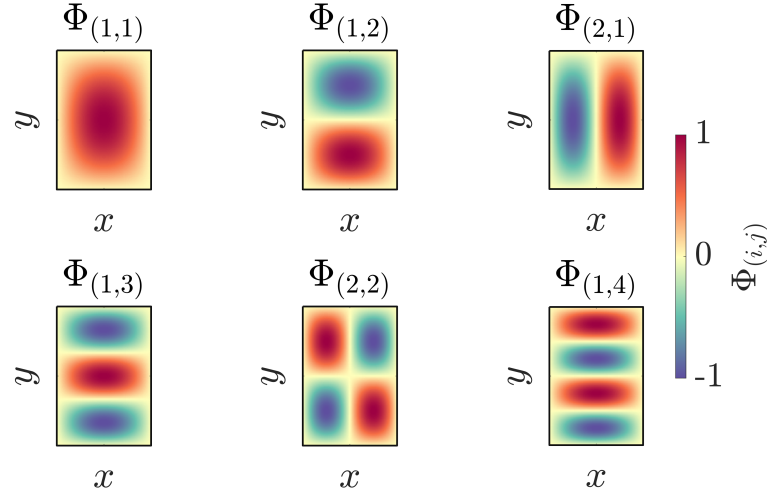


Figure 4.20: Lowest-order theoretical panel vibration modes for simply supported boundary conditions.

Mode	$f_{\text{theory}}^{\text{fixed}}$, Hz	$f_{\text{theory}}^{\text{ss}}$, Hz	$\langle f_{\text{exp}} \rangle$, Hz
(1,1)	576	303	502
(1,2)	901	590	767
(2,1)	1403	928	1108
(1,3)	1444	1067	1305
(2,2)	1705	1214	1574
(1,4)	2195	1735	1976

Table 4.2: Theoretical and trial-averaged panel natural frequencies.

The effect of paint on the natural frequency of the panel is investigated via roving hammer tests conducted on the same panel before and after it is painted. The percent difference between the painted and unpainted panel natural frequencies, $f_{(i,j)}^{\text{painted}}$ and $f_{(i,j)}^{\text{unpainted}}$, are shown in Fig. 4.21a. Although the natural frequencies of the painted panel tend to be slightly lower than those of the unpainted panel, the associated measurement uncertainties suggest that the addition of paint has no measurable effect on the dynamics response of the panel.

Uniform pressure loading over the panel can modify the in-plane stress state by inducing out-of-plane deformation. This increase in panel deflection from an unpressurized, zero deflected state increases the effective bending stiffness of the panel and, consequently, raises the natural frequencies. An additional series of roving hammer tests is conducted with the panel mounted in the flat plate model. One test is performed unpressurized, $\Delta p = 0$ kPa, and the following test is performed

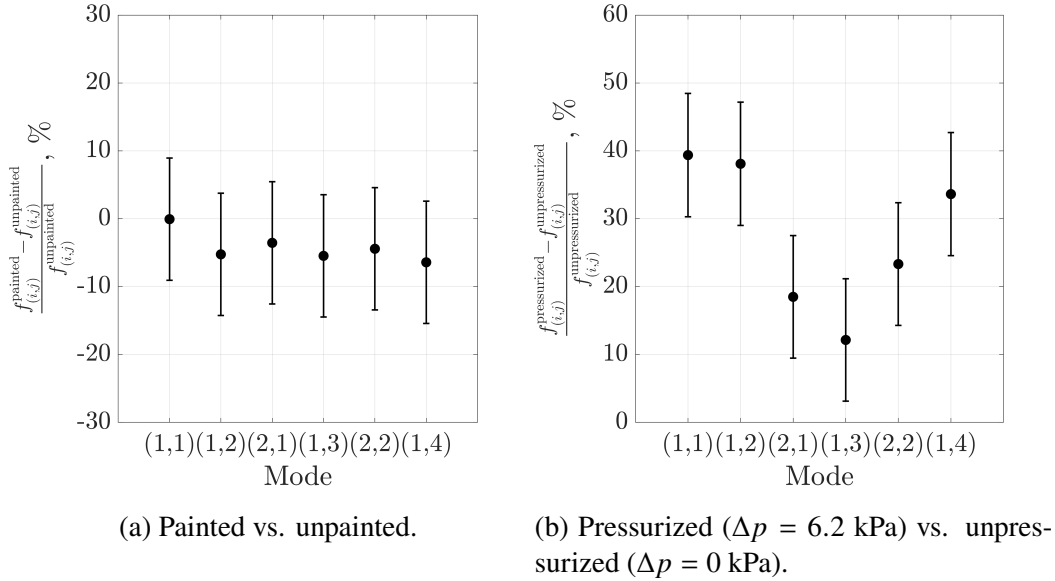


Figure 4.21: Comparison of natural frequency shifts.

with the same panel with a pressure difference of $\Delta p = 6.2$ kPa, which is representative of the pre-shot pressure loading for Cases A and B. The percent differences of the resulting hammer tests are shown in Fig. 4.21b. For mode (1,1), the natural frequency increases by roughly 39% when the panel is pressurized. The subsequent higher-order modes also exhibit measurable rises, with mode (1,3) exhibiting the lowest rise at only an increase of 12%. This result confirms that the in-plane stress state is modified when a static pressure difference is applied over the panel surfaces. The presence of non-zero pressure loading and out-of-plane deformation during test time under STBLI loading may also modify the natural frequencies relative to the unpressurized values.

4.3.3 Cavity Resonance

The underside of the panel forms an enclosed cavity where acoustic waves can reflect from the boundaries, leading to acoustical resonances. If pressure fluctuations align with these resonant frequencies, the pressure field within the cavity can become significantly amplified. Although the panel cavity is pressurized to a constant value prior to an experimental run, panel oscillations may induce low-frequency pressure fluctuations in the cavity during test time. When cavity pressure fluctuations coincide with the acoustic resonant frequencies, intensified pressure fluctuations may significantly increase the panel vibrations and modify the fluid-structure coupling. To isolate the effect of the low-frequency STBLI unsteadiness over the panel, it

is critical to avoid overlap between the cavity acoustic resonances and the natural frequencies of the panel. As discussed below, the theoretical cavity resonant frequencies are predicted and the cavity pressure is dynamically monitored during the experiments to examine the influence of cavity resonance on the fluid-structure interaction.

4.3.3.1 Predicted Cavity Modes

The predicted lowest-order cavity resonance mode shapes are shown in Fig. 4.22, and their corresponding predicted frequencies are listed in Table 4.3. Details of the theoretical cavity mode shape and frequency calculations are provided in Appendix C. The first three modes correspond to the lowest-order acoustic resonances resulting from pressure waves reflecting between the cavity walls along each principal axis. The subsequent three modes represent higher-order acoustic resonances involving more complex wave patterns and additional nodal surfaces. The lowest-order acoustic mode occurs at a resonant frequency of $f_{(0,1,0)} = 2180$ Hz, and the sixth mode reaches a frequency of $f_{(0,2,0)} = 4360$ Hz. The lowest predicted resonant frequency is greater than the natural frequencies of the five lowest-order vibration modes. As a result, the cavity resonances are expected to have minimal influence on the panel response at the lowest-order frequencies since there is no overlap between the lowest-order resonant and vibrational modes.

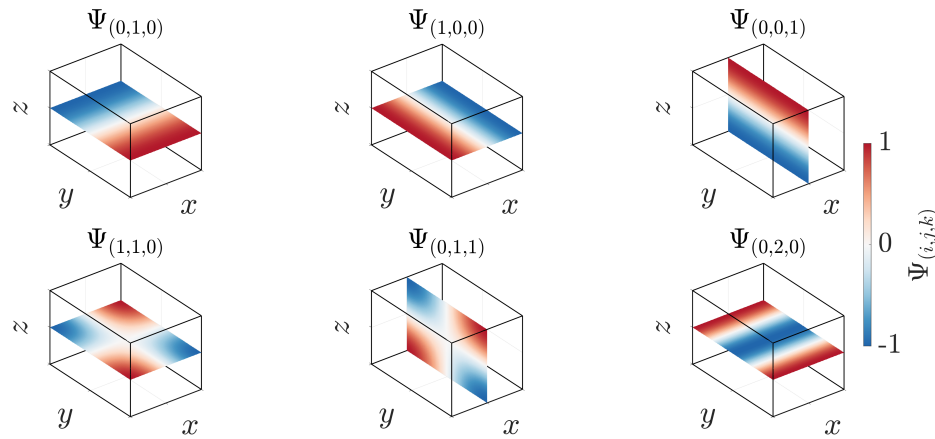


Figure 4.22: Lowest-order cavity resonance mode shapes.

4.3.3.2 Measured Cavity Response

The time histories of the cavity pressure, p_{KC} , measured inside the cavity during test time for the four compliant cases are presented in Fig. 4.23. In all four pressure

Mode	$f_{(i,j,k)}$, Hz
(0,1,0)	2180
(1,0,0)	3219
(0,0,1)	3391
(1,1,0)	3888
(0,1,1)	4031
(0,2,0)	4360

Table 4.3: Predicted cavity resonant frequencies.

traces, the cavity pressure remains at a relatively constant level and exhibits only minor fluctuations typically less than $0.1p_\infty$. A plot of the percent difference between the time-averaged measured cavity pressure, $\bar{p}_{KC}$, and the nominal cavity fill pressure, p_c , is shown in Fig. 4.24. Each data point corresponds to the trial-averaged cavity pressure percent difference, and each horizontal error bar indicates one standard deviation of variability between repeated experiments. Overall, the measured cavity pressures in all four cases closely match their nominal values, where the percent differences all lie below 1.2% for the No-STBLI case and 0.5% for the other three compliant STBLI cases. Similarly, the trial-averaged RMS of cavity pressure fluctuations is also plotted in Fig. 4.24, where the error bars similarly represent one standard deviation. The RMS values are relatively small and range up to 3.5% of the freestream pressure, which is approximately the same order of magnitude as the freestream pressure RMS level. Interestingly, Case B exhibits RMS values slightly larger than that of Cases A and C. This may be attributed to differences in its mean panel deformation shape, and further discussion on the panel response is provided in Section 5.2.1.

Power spectra of cavity pressure fluctuations for each case are shown in Fig. 4.25. In all four cases, the highest energy spectral content tends to lie within the 2 to 10 kHz range, which corresponds to the range of predicted cavity resonance frequencies and their harmonics. Smaller peaks below 2 kHz exhibit less spectral energy and are likely attributed to the panel vibration modes that induced pressure fluctuations within the cavity. Notably, the strongest resonance peaks occur between 5 and 8 kHz, suggesting that the most energetic cavity resonant frequencies correspond to higher-order cavity modes.

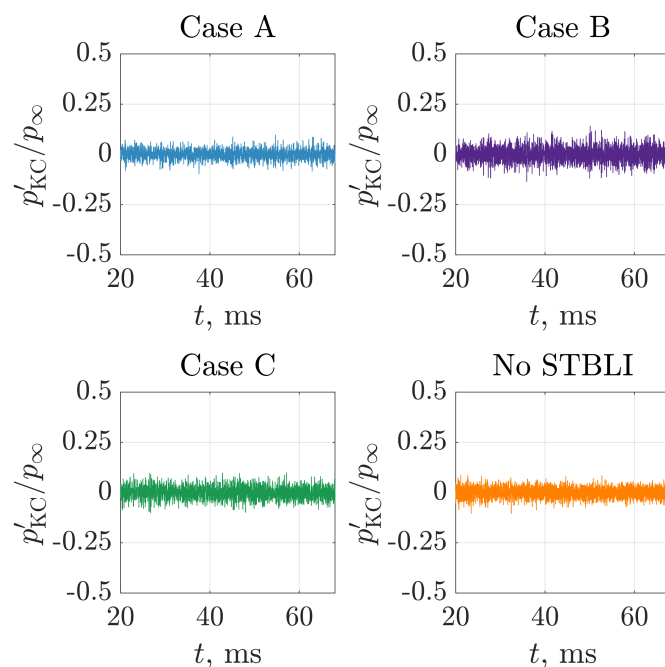


Figure 4.23: Cavity pressure during test time.

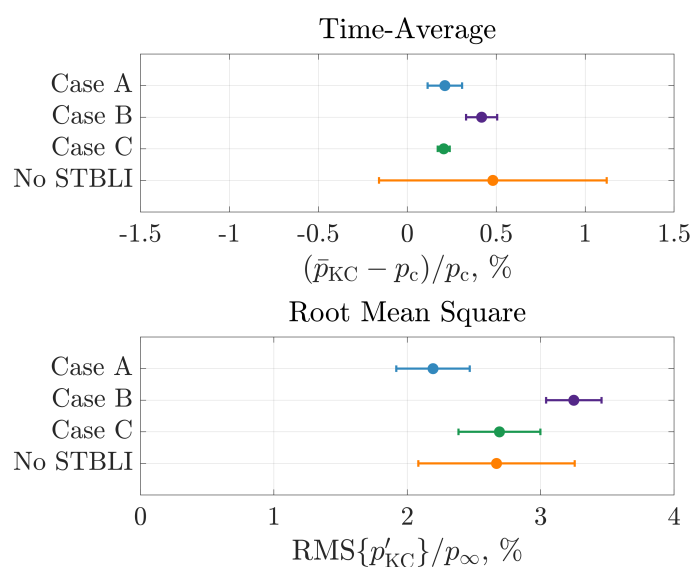


Figure 4.24: Time-average and root mean square of measured cavity pressure.

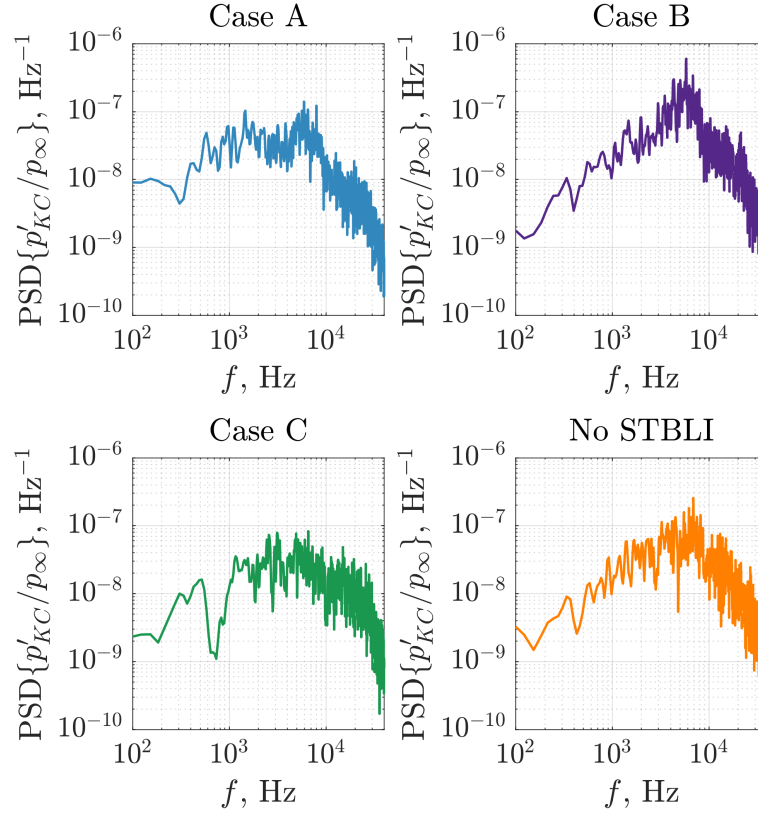


Figure 4.25: Cavity pressure power spectra during test time.

4.3.4 Panel Flutter

Although an in-depth analysis of panel flutter is beyond the scope of the present work, it is nevertheless important to understand how the freestream conditions and panel structural properties relate to the theoretical flutter boundary. Characterizing flutter onset and structural behavior in the absence of shock-induced disturbances provides context for how STBLIs may modify the amplitude and spectral content of the response.

4.3.4.1 Linear Flutter Analysis

An eigenvalue analysis is employed to determine flutter onset by linearizing Eq. 1.1. Following a similar methodology to Freydin and Dowell (2021), the linearization is performed by neglecting nonlinear structural stiffness, assuming zero static pressure differential and thermal stresses, and simplifying the cavity model to a steady-state stiffness contribution. These simplifications yield the reduced form given in Eq. 4.23, for which the solution is taken to be harmonic and expressed in the form shown in Eq. 4.24.

$$M_{nk}\ddot{w}_k + C_{PT_{nk}}\dot{w}_k + K_{nk}w_k + K_{PT_{nk}}w_k + K_{c_{nk}}w_k = 0 \quad (4.23)$$

$$\mathbf{w} = \hat{\Phi} e^{i\omega t} \quad (4.24)$$

Substituting Eq. 4.24 into Eq. 4.23 results in Eq. 4.25, which is a quadratic eigenvalue problem that produces n_{modes} unique pairs of complex eigenvalues, ω_n , and mode shapes, $\hat{\Phi}_n$, where n_{modes} is the number of modes used to represent the panel out-of-plane motion.

$$\left(-\omega^2 \mathbf{M} + i\omega \mathbf{C}_{PT} + (\mathbf{K} + \mathbf{K}_{PT} + \mathbf{K}_c) \right) \hat{\Phi} e^{i\omega t} = 0 \quad (4.25)$$

The mass, damping, and stiffness matrices ($\mathbf{M}$, $\mathbf{C}_{PT}$, $\mathbf{K}$, $\mathbf{K}_{PT}$, and $\mathbf{K}_c$) are assembled according to their definitions provided in Freydin and Dowell (2021), and the complex eigenvalues, ω_n , are obtained using the MATLAB function, `polyeig()`. Structural and flow parameters are set to their nominal values given in Tables 2.1 & 2.2 with the exception of panel thickness, h , and freestream density, ρ_∞ . The panel thickness is varied from 0.025 mm to 1 mm, and for each thickness, the freestream density is incrementally increased until flutter onset is detected. At each combination of h and ρ_∞ , the system is classified as stable if $\text{Im}(\omega_n) > 0$ for all n or unstable if $\min [\text{Im}(\omega_n)] < 0$. Flutter onset corresponds to the transition between these two states when $\min [\text{Im}(\omega_n)] = 0$ (Freydin et al., 2020).

From first-order piston theory (Eq. 1.8), the unsteady aerodynamic pressure induced by surface motion depends on both the time rate of change of the panel displacement and its spatial gradient in the flow direction. In the linearized fluid-structure interaction model (Eq. 4.23), these contributions appear as aerodynamic damping and stiffness terms, $C_{PT_{nk}}$ and $K_{PT_{nk}}$, which are scaled by $\frac{\rho_\infty u_\infty}{M_\infty}$ and $\frac{\rho_\infty u_\infty^2}{M_\infty}$, respectively. The structural stiffness matrix, K_{nk} , in Eq. 4.23 is a function of the flexural rigidity of the panel, $D = \frac{Eh^3}{12(1-\nu^2)}$. In linear theory, the flutter boundary typically depends on both the aerodynamic and structural stiffness scaling terms, and these factors are often combined and nondimensionalized into the flutter parameter, λ (Eq. 4.26). This parameter represents the ratio of aerodynamic to structural stiffness, and is widely used in classical panel flutter studies (Dowell, 1970).

$$\lambda = \frac{\rho_\infty u_\infty^2 a^3}{M_\infty D} \quad (4.26)$$

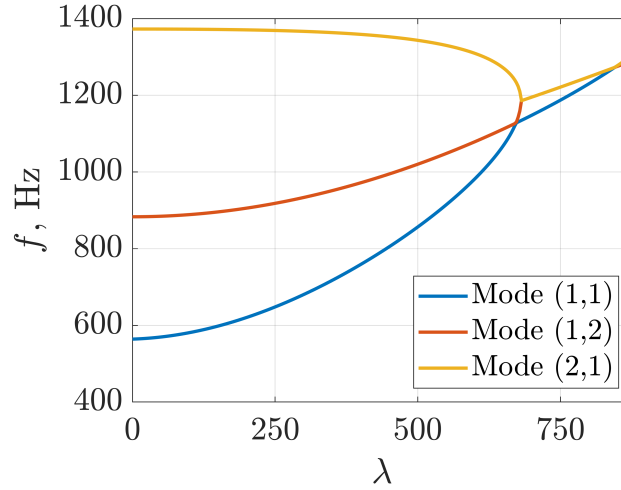


Figure 4.26: Influence of aerodynamic stiffness on natural frequencies.

When λ is small, the panel is relatively stiff compared to the aerodynamic loading, and its response is dominated by low-amplitude vibrations at its natural frequencies. In this regime, fluid-structure coupling is weak, and aerodynamic stiffness has minimal influence on the vibration characteristics. As λ increases, the contribution of aerodynamic stiffness on the structural response becomes more significant. For high supersonic Mach numbers and moderately small a/b ratios, the natural frequencies of the lowest-order modes shift closer together as λ grows. At some critical value of λ , these modes coalesce at a single frequency, signaling the onset of coupled mode flutter (Dowell, 1970; Eitner et al., 2023).

The influence of aerodynamic stiffness on the natural frequencies of the three lowest-order panel modes for the panel examined in this study is shown in Fig. 4.26. As λ increases from zero, the natural frequencies of modes (1,1) and (1,2) increase while that of mode (2,1) decreases. At a sufficiently large value of λ , modes (1,2) and (1,1) coalesce into a single natural frequency. As λ increases beyond this point, mode (1,2) shifts to match the frequency of mode (2,1), and with further increase, all three modes ultimately converge to the same frequency. The coalesced frequency is approximately twice that of mode (1,1) and about 90% that of mode (2,1).

The critical value of λ corresponding to flutter onset, denoted λ_{flutter} , is defined as the point where the minimum imaginary component of the eigenvalues becomes zero. As shown in Fig. 4.27, this is predicted to occur at $\lambda_{\text{flutter}} = 700$. By comparison, the experimental freestream conditions yield a flutter parameter of $\lambda_{\text{exp}} = 4.5$. Thus, the predicted critical flutter parameter is approximately two orders of magnitude larger

than that of the experimental conditions. Therefore, the panel is expected to remain stable under the freestream conditions tested.

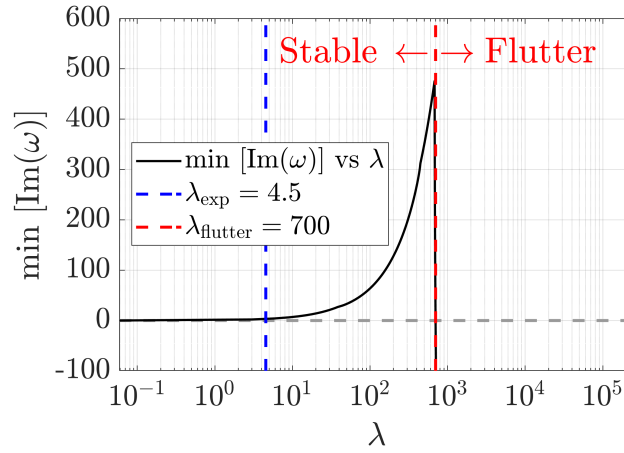


Figure 4.27: Eigenvalue stability for flutter onset.

Furthermore, the effect of panel thickness is also examined, and the resulting flutter boundary in the $h - \rho_\infty$ space is presented in Fig. 4.28. The vertical line at $h^{\text{exp}} = 0.254$ mm represents the thickness of the panel tested in this study. For a given thickness, freestream density values below the critical value, $\rho_\infty^{\text{flutter}}$, corresponds to a stable system, whereas values above $\rho_\infty^{\text{flutter}}$ indicate flutter. The nominal experimental freestream density, $\rho_\infty^{\text{exp}} = 0.0770$ kg/m³, is plotted as a horizontal dashed line, and it intersects the flutter boundary at approximately $h = 0.0254$ mm. This implies that, under the experimental freestream conditions, the panel needs to be about an order of magnitude thinner to reach flutter onset.

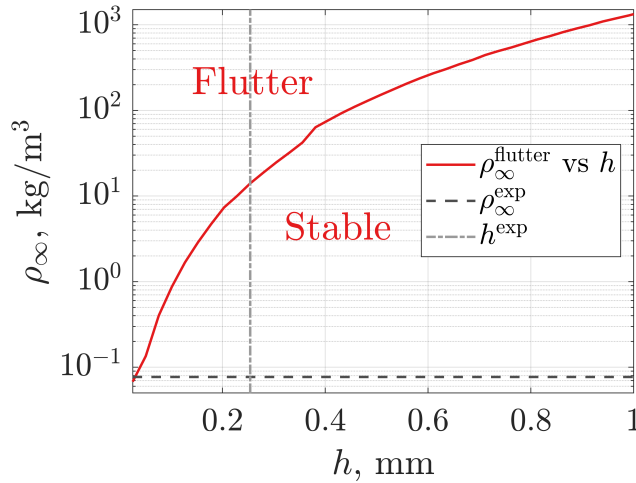


Figure 4.28: Flutter boundary in $h - \rho_\infty$ space.

The assumptions of the linear fluid-structure model do not include the complex dynamics of panel flutter, particularly when nonlinear structural stiffness and unsteady aerodynamic effects become significant. The present theoretical flutter analysis serves as a useful first-order investigation into the stability characteristics of the panel. However, accurately defining the flutter boundary ultimately requires experimental measurements or high-fidelity numerical simulations that can capture these additional effects. Despite the inherent limitations of linear theory, the predicted flutter boundary lies at least an order of magnitude away from the experimental parameters in the $h - \rho_\infty$ space. This large margin suggests that the panel is unlikely to experience flutter in the absence of STBLI loading.

4.3.5 No-STBLI Experiments

Experiments are additionally conducted on the flat plate model assembly without the shock generator to further characterize the panel response without STBLI loading. The cavity pressure is set equal to the nominal freestream pressure so that the static pressure difference over the panel surface is approximately zero.

4.3.5.1 Experimental Results

The panel center point deflection and static pressure response for a representative No-STBLI compliant case are shown in Fig. 4.29. Prior to flow start up, the panel center point is positively deflected by about $85 \mu\text{m}$ due to the pressure difference between the ambient test section pressure and cavity pressure. Soon after the piston valve begins to retract to initialize flow start up around $t = -22 \text{ ms}$, the pressure spikes at $t = -15 \text{ ms}$. This impulsive load excites the panel, which begins to abruptly oscillate at the fundamental vibration mode. As the freestream static pressure gradually increases between $t = -15$ to 15 ms before reaching steady test time conditions, the mean center point deflection decreases until it approaches about zero mean panel deflection. During test time, the panel continues to primarily oscillate at the first vibration mode with oscillation amplitudes ranging approximately from 30 to $60 \mu\text{m}$, or about 0.1 to 0.2 panel thicknesses.

A spectrogram of the center point deflection, presented in Fig. 4.30, indicates that the panel response is dominated by the fundamental vibration mode both immediately after the initial impulsive excitation due to the piston valve opening and during test time. A slight downward shift in the dominant frequency is observed between flow start up and steady test conditions. This reduction is attributed to the changes in

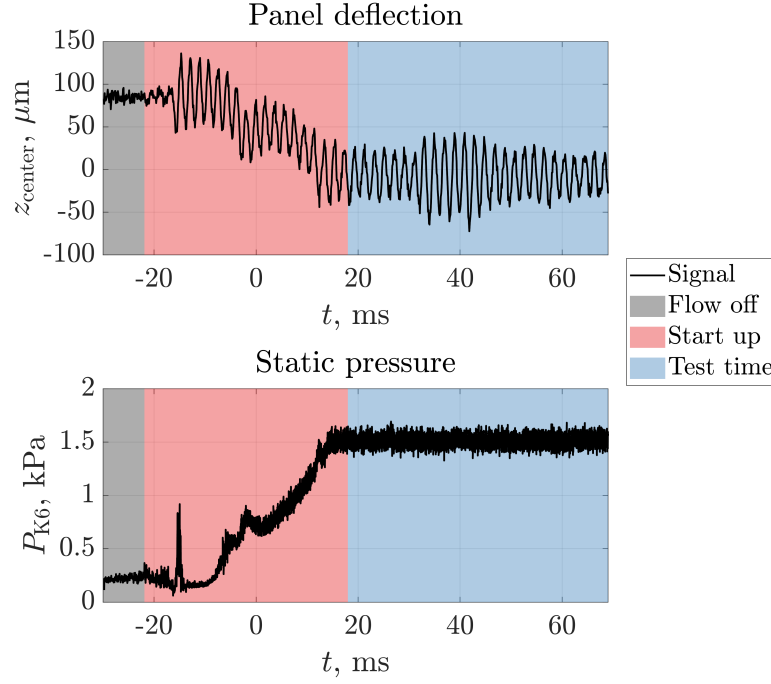


Figure 4.29: Panel center point deflection and static pressure downstream of panel.

the static pressure loading across the panel. As the pressure difference decreases, the associated in-plane stresses are relieved, leading to a corresponding decrease in structural stiffness, and, consequently, a lower vibration frequency.

Within the quasi-steady interval around $t = -16$ to -10 ms after piston valve retraction, the vibration frequency is representative of the natural frequency under a static pressure difference of $\Delta p = 1.23$ kPa. The peak vibration frequencies for this quasi-steady period and test time are extracted using peak detection in the power spectral density of the panel fluctuations. The precision of the peak frequency estimate is improved by using a three point Gaussian fit. The percent difference in vibration frequency between test time and the quasi-steady period after piston valve retraction is shown in Fig. 4.31 for four repeated experiments. On average, the fundamental frequency decreases by approximately 7% from the quasi-steady pre-test time to test time conditions, with all four experiments exhibiting close agreement within the bounds of measurement uncertainty. The panel response is, therefore, consistent between experiments and is characterized by impulsive excitation due to the piston valve retracting to initialize flow start up.

Moreover, the PSD of the out-of-plane panel fluctuations at a nodeless point excited by all of the lowest order vibrational modes, $z_{\text{nodeless}}(x/a = -0.19, y/b = -0.11, t)$,

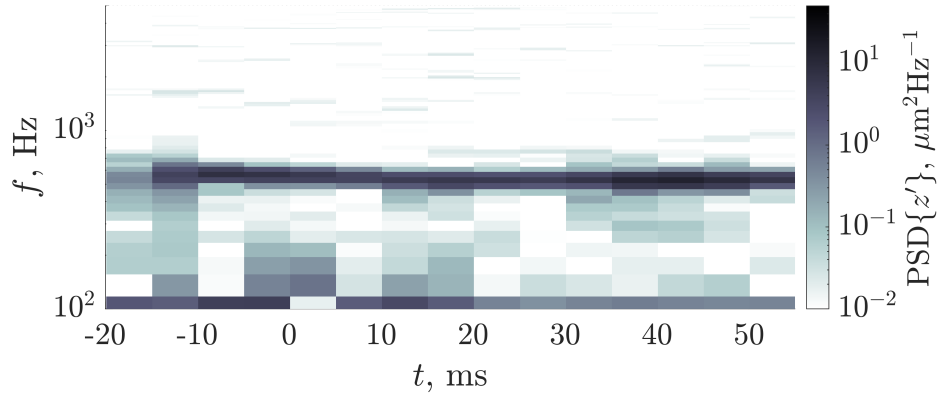


Figure 4.30: Spectrogram of panel center point deflection.

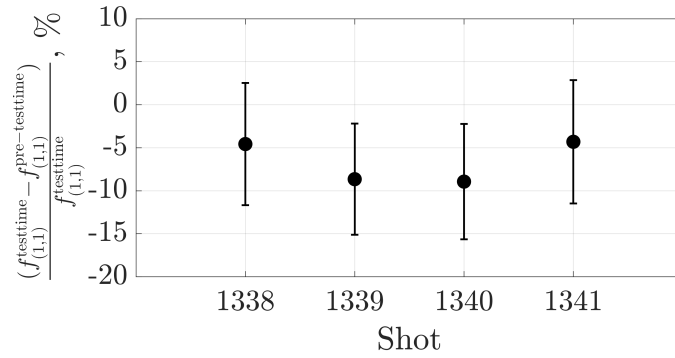


Figure 4.31: Percent difference of fundamental vibration frequency before and during test time.

is shown in Fig. 4.32 to identify whether higher order modes are present in the panel response during test time. Though, similar to that of the spectrogram, the PSD reveals that the panel response is dominated by the fundamental vibration mode, which is centered around 500 Hz. The spectral energy of the fundamental vibration mode is over two orders of magnitude greater than that of the next leading vibration mode.

The time-averaged panel deflection shape, presented in Fig. 4.33, shows the panel exhibits close to zero mean deflection, which is consistent with the zero average static pressure difference set by the cavity pressure. The oscillation of the panel during a full cycle of the fundamental vibration mode is illustrated in four representative phase snapshots in Fig. 4.34. At phase $\phi_{(1,1)} = 0$ rad., the panel is maximally deflected upward into the flow by nearly 0.2 panel thicknesses. A quarter oscillation cycle later, the panel crosses the zero deflection axis, and at $\phi_{(1,1)} = \pi$ rad., the panel is negatively deflected out of the flow by about 0.3 panel thicknesses.

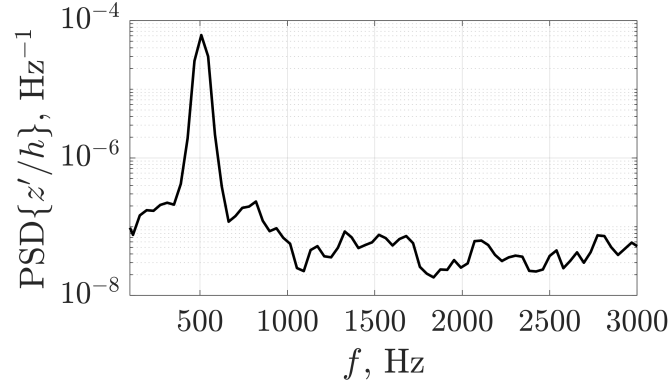


Figure 4.32: Power spectral density of panel oscillations.

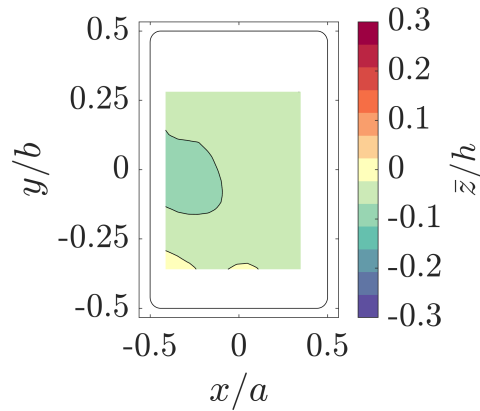


Figure 4.33: Time-averaged panel deflection under turbulent boundary layer with no STBLI loading.

4.3.5.2 Panel Excitation Sources and Fluid-Structure Coupling Mechanisms

As shown in Fig. 4.29, the panel abruptly oscillates at the fundamental vibration mode following the impulsive loading of flow arrival. Similar behavior was reported by Vasconcelos et al. (2023) in their Ludwig Tube FSI experiments, where a rapid transfer of energy from the unsteady, impulsive loading of flow arrival excited the compliant panel. In both cases, this impulsive excitation drives a strong, periodic structural response at the fundamental vibration mode that extends into test time. The nature of the panel response and comparisons with the structural dynamics further into test time led them to conclude this early time behavior is not indicative of two-way fluid-structure coupling. Rather, it reflected a coupling independent, one-way response, in which the panel oscillates under imposed static aerodynamic loading without appreciable dynamic feedback from the flow. They further observed that the initial excitation of the panel can eventually decay, such that local pressure fluctuations induced by the structural deflections are sufficiently strong to influence

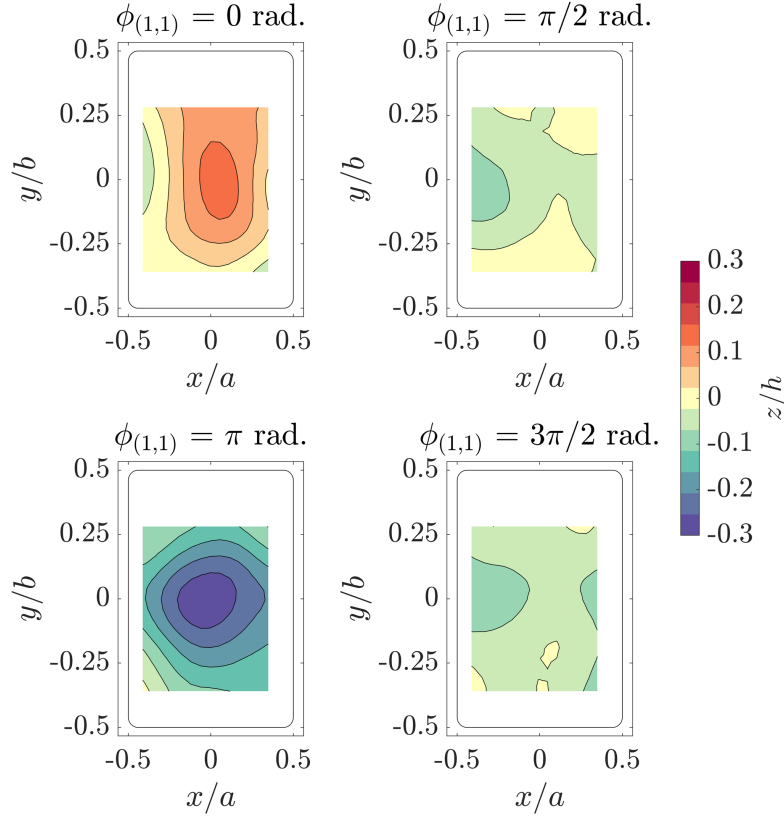


Figure 4.34: Panel deflection for a full oscillation cycle at vibration mode (1,1).

the panel dynamics, marking the onset of two-way fluid-structure coupling.

Thus, the observed panel behavior in these no shock impingement experiments is consistent with the early test time behavior of the thick and thin compliant panels tested in the experiments conducted by Vasconcelos et al. (2023). However, in contrast to the behavior of their thicker compliant panels, the panel response in these No-STBLI experiments does not transition to a two-way coupling dependent regime. Instead, the length of test time is not sufficient for the initial one-way coupling independent response caused by the impulsive excitation of flow arrival to dissipate under the freestream conditions. Furthermore, a secondary excitation event occurs at approximately $t = 30$ ms, resulting in the oscillation amplitude to increase by roughly $0.1h$ before decaying back to the prior level. This moment of time coincides with facility-induced vibrations associated with the abrupt stop of the piston valve retraction. The resulting structural disturbance acts as a secondary impulsive excitation that further enhances the panel oscillation at the fundamental vibration mode. The subsequent decay in amplitude following the abrupt rise

suggests that the excitation is relatively weak and does not fundamentally alter the system dynamics.

These observations indicate that the panel response in the No-STBLI case experiments is primarily dominated by external, facility-driven excitations and remain largely uncoupled from the flow field. The response is characterized by low-amplitude, first mode oscillations with no evidence that flutter onset is reached.

4.3.6 Aeroelastic Interaction Parameters

Non-dimensional aeroelastic parameters are important quantities that govern the interaction dynamics. These parameters not only help facilitate comparisons across different configurations but also clarify the relative importance of the underlying fluid and structure properties in the aeroelastic system.

The non-dimensional mass ratio, m_r , characterizes the relative inertia of the fluid and structure and is defined in Eq. 4.27, where ρ_s is the material density. The quantity provides a measure of whether the dynamics are dominated by the flow or structural response. A low mass ratio indicates structural inertia dominates, implying that the flow exerts a comparatively weak influence on the structural response. Conversely, a higher mass ratio corresponds to the fluid inertia playing a more significant role in driving the fluid-structure coupling dynamics.

$$m_r = \frac{\rho_\infty a}{\rho_s h} \quad (4.27)$$

The reduced frequency, defined in Eq. 4.28, represents the ratio of characteristic flow and structural time scales. Specifically, the flow time scale is taken as the time required for a disturbance convecting at the freestream velocity to traverse the streamwise panel length while the structural time scale is given by the period of the fundamental vibration mode. This parameter, therefore, quantifies how rapidly the structure oscillates relative to the time scale of the flow. Low reduced frequency corresponds to quasi-static behavior, signifying that the fluid can adjust nearly instantaneously to the structural motion since the flow effectively sees the structure as nearly stationary. In contrast, higher reduced frequency indicates an increasing degree of system unsteadiness.

$$f_r = \frac{a f_{(1,1)}}{u_\infty} \quad (4.28)$$

As introduced in Section 4.3.4.1, the flutter parameter, λ , provides a measure of the ratio of aerodynamic-to-structural stiffness. In classical panel flutter interactions, small values of λ correspond to structurally stiff systems in which aerodynamic loading induces only minor structural motion. As λ increases, the influence of aerodynamic forces becomes more pronounced, enhancing the coupling between the flow and structural response. Beyond a critical value of λ , this coupling leads to the dynamic instability known as flutter.

Building on this, Vasconcelos et al. (2023) extended the classic flutter parameter to impinging STBLIs by replacing the Mach number in Eq. 4.26 with the hypersonic similarity parameter, $M_\infty \theta_f$, where θ_f is the flow deflection angle induced by the incident oblique shock. The resulting aerodynamic-to-structural compliance ratio for STBLIs, denoted Λ and given in Eq. 4.29, retains an analogous physical interpretation to λ .

$$\Lambda = \frac{\rho_\infty u_\infty^2 a^3}{DM_\infty \theta_f} \quad (4.29)$$

The nondimensional parameters specific to the experimental model and flow conditions for this work are reported in Table 4.4. In this interaction, $\lambda \approx O(1)$, signifying the aerodynamic and structural stiffnesses are approximately equal. Both linear flutter analysis and experimental no shock impingement experiments in Section 1.2 showed that the panel does not experience flutter, signifying the freestream aerodynamic conditions are not large enough to make the panel experience the onset of flutter.

Table 4.4: Aeroelastic interaction parameters.

m_r	f_r	λ	Λ
0.0020	0.040	4.5	15

RESULTS AND DISCUSSION

This chapter details the results obtained from simultaneous photogrammetry, schlieren, FLDI, and pressure transducer measurements for both the rigid and compliant panel STBLI experiments. The chapter begins with an overview of the key flow features and surface pressure loading profile observed in the rigid panel case. The behavior of the compliant panel cases is then examined in both time and frequency domains and compared against the rigid panel baseline case. Finally, the synchronized measurements are analyzed using cross-correlation and phase analysis techniques to evaluate the degree and extent of fluid-structure coupling.

5.1 Overview of Rigid Panel STBLI Flow Field

An annotated schlieren image of the flow field studied is shown in Fig. 5.1. Vertical density gradients are visualized due to the horizontal knife-edge cut off in the schlieren set up. An attached oblique shock forms at the leading-edge of the shock generator in the freestream Mach 4 flow. This originally planar shock slightly curves due to interaction with the expansion fan generated by the trailing edge of the shock generator before becoming nominally planar again prior to boundary layer impingement. The turbulent boundary layer grows in thickness over the length of the flat plate and then encounters an adverse pressure gradient due to shock impingement, causing the flow to separate. Compression and expansion waves form due to the deflection of the boundary layer, and the compression waves coalesce to form the separation and reattachment shocks. Weak shocks visible in the bottom left region of the image originate from the flat plate model leading edge and boundary layer trip, and they eventually intersect with the impinging shock farther downstream.

Time-resolved schlieren snapshots recorded at a 100 kHz sampling rate with a 30 ns, or $0.007\delta/u_\infty$, exposure time are shown in intervals of 50 μs , or $11.6\delta/u_\infty$, in Fig. 5.2. Note that the field of view is smaller than that in Fig. 5.1 to achieve finer spatial resolution of the near-wall structures within the STBLI, and the exposure time is decreased such that the distance traveled by a disturbance at the nominal freestream velocity during a single exposure is less than one pixel width. The co-linear optics (Section 3.4) were designed such that the FLDI beam locations are also tracked by the schlieren camera, appearing as two white dots. For this

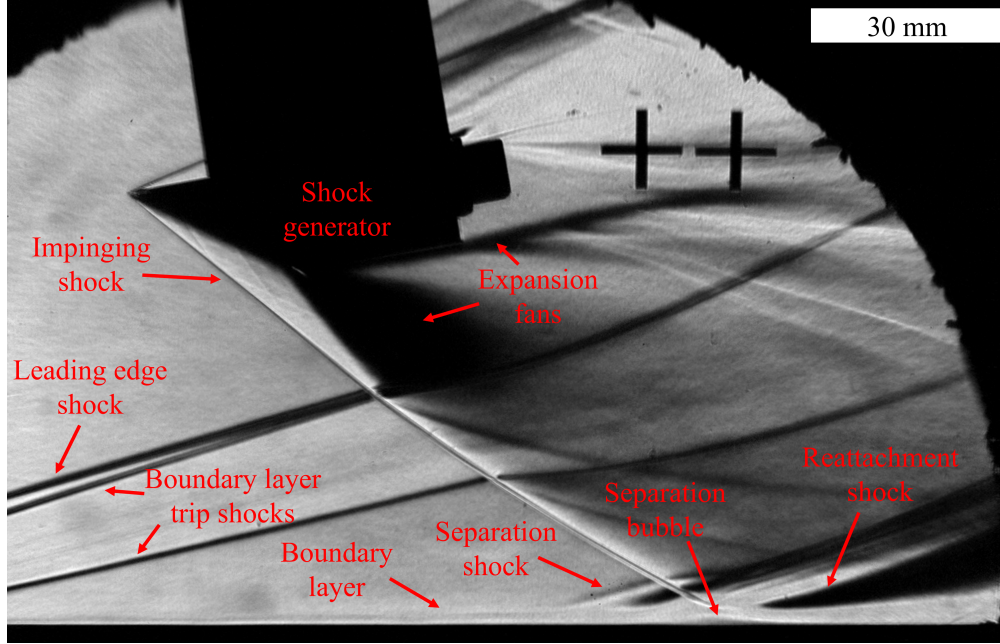


Figure 5.1: Schlieren image of full field of view SBLI over rigid wall ($55.17 \mu\text{s}$, or $12.8\delta/u_\infty$, exposure time used to obtain average flow features).

particular FLDI alignment location, the beam pair is inside the separation bubble around $(x - x_{\text{imp}})/a = -0.08$.

The dark region of pixel intensities above the separation point and boundary layer represents the separation compression waves that form the separation shock, which will be analyzed further in Section 5.2.2. The sequence of four schlieren snapshots reveals that the instantaneous separation shock front is not consistently planar. Instead, it exhibits localized bulges corresponding to spanwise and streamwise curvature of the shock and global bursts that shift the entire shock front upstream relative to the mean shock position. In the frames at $t_1 + 11.6\delta/u_\infty$ and $t_1 + 23.2\delta/u_\infty$, the separation shock front briefly protrudes upstream before receding again by $t_1 + 34.8\delta/u_\infty$. Similar three-dimensional effects in the separation shock were observed by Whalen et al. (2019) in a compression ramp STBLI. Their conditional statistical analysis indicated that the occurrence of bulges correlated with the instantaneous separation length. They hypothesized that these features arise from the ejection of low-momentum fluid from the separation bubble into the separation shock layer. The observed bulges, therefore, reflect the breathing motion of the separation bubble associated with intermittent mass ejection. Further analysis of the shock dynamics, including shock tracking, is discussed in Section 5.2.2.

The regions downstream of the separation shock correspond to the expansion waves

and reattachment shock. Although features in these regions vary over the four frames in the snapshot sequence, the observed unsteadiness is less pronounced than that associated with the separation shock.

5.1.1 Surface Pressure Measurements

Time-averaged surface pressure measurements obtained at several streamwise locations along the rigid panel are plotted in Fig. 5.3. As a result of viscous effects that lead to boundary layer separation, the pressure rises more gradually over a finite streamwise distance rather than exhibiting the abrupt pressure jump predicted by the inviscid flow solution (Sabnis and Babinsky, 2023). Consequently, the pressure rise begins upstream of the nominal inviscid shock impingement location, x_{imp} , and it reaches a maximum slightly downstream of x_{imp} around $(x - x_{\text{imp}})/a = 0.16$ with a magnitude of $\bar{p}/p_{\infty} = 7.32$. In separated interactions, boundary layer separation typically occurs shortly after the onset of the pressure rise (Pasquariello et al., 2017; Shinde et al., 2022). Downstream of the peak, the pressure decreases due to the influence of expansion waves generated by the shock generator wedge geometry.

5.2 Compliant Panel STBLI Response

The addition of surface compliance to the interaction modifies the flow field and introduces additional dynamics through fluid-structure coupling. Three compliant panel STBLI configurations are tested, and the interaction parameters for each case are detailed in Table 2.3. Representative snapshots of the synchronized schlieren images and surface deformation fields over a full panel oscillation cycle at the first vibration mode for these compliant cases are presented in Figs. 5.4-5.6. The four snapshots in each figure correspond to different phases of the vibration mode (1,1) oscillation cycle in intervals of $\pi/2$ radians. The phase, $\phi_{(1,1)} = 0$ rad., corresponds to the instant at which the panel is deflected in its most upward state, whereas the phase, $\phi_{(1,1)} = \pi$ rad., corresponds to the most downward deflection state of the panel. In each snapshot, the black rectangle edges in the x - y plane outline the panel boundary, the red-blue colormap represents the measured out-of-plane surface deformation, and the grayscale image shows the schlieren visualization of vertical density gradients in the flow field.

At $\phi_{(1,1)} = 0$ rad., the panel is partially deflected into the flow for Cases A and C and completely deflected out of the flow for Case B. In Case A, the upstream half of the panel deflects slightly into the flow while the downstream half deflects out of the flow, resembling vibrational mode (2,1). The maximum deflection in Case C occurs

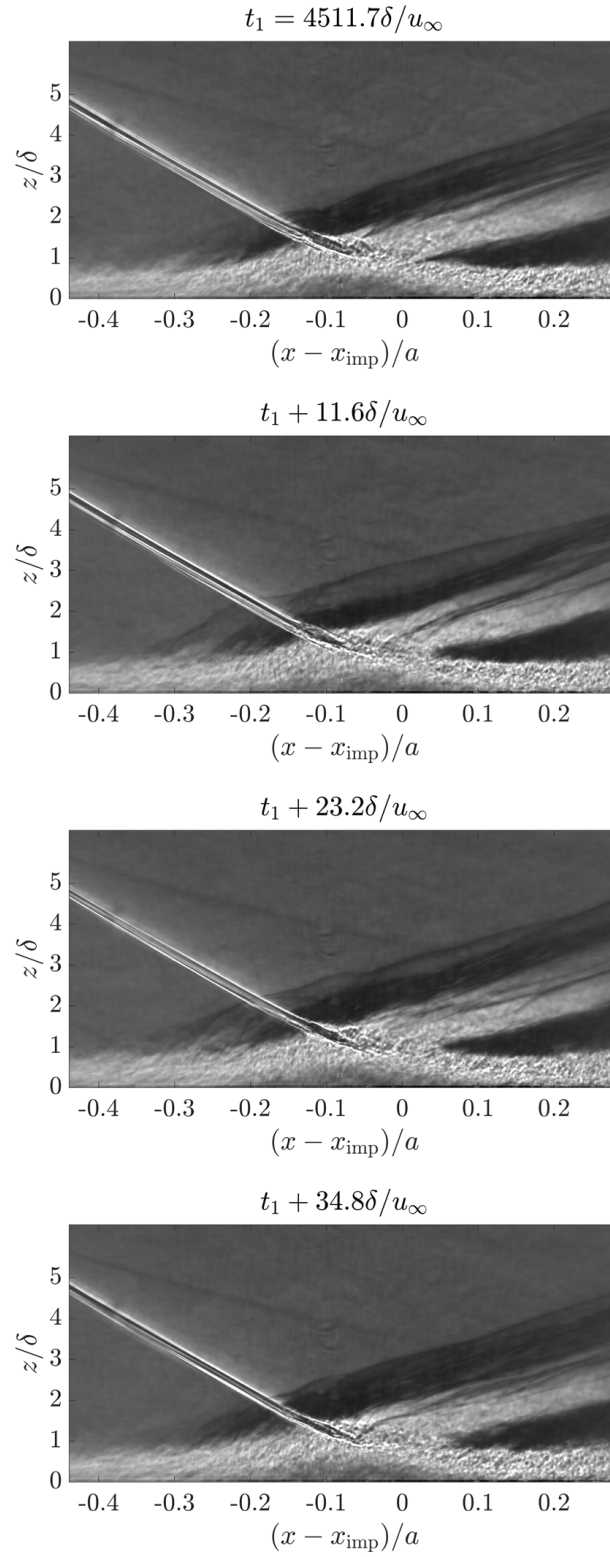


Figure 5.2: Schlieren snapshots separated by $11.6\delta/u_\infty$ at $0.007\delta/u_\infty$ exposure time.

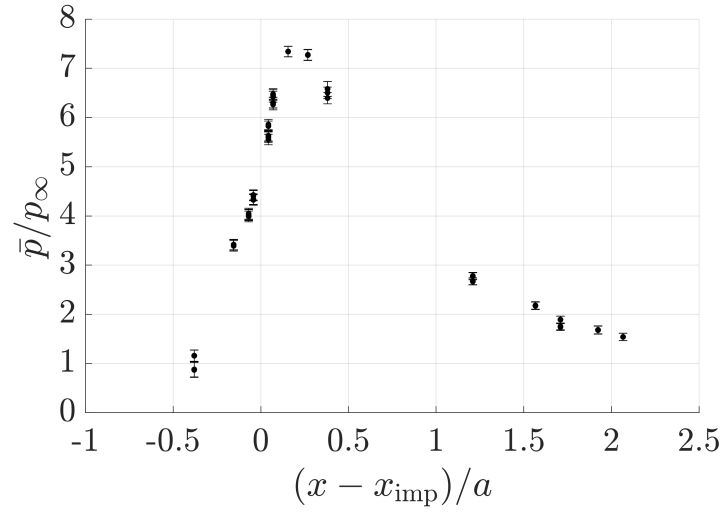


Figure 5.3: Time-averaged rigid wall pressure.

at the panel center, and the deflection shape more closely resembles mode (1,1). In contrast, Case B exhibits a pronounced deflection out of the flow that resembles a negatively deflected mode (1,1). At the most downward state at $\phi_{(1,1)} = \pi$ rad., the panel is deflected out of the flow in all three cases, with Case B exhibiting the largest negative deflection. This behavior illustrates that the panel deflects both into and out of the flow in Cases A and C while the panel solely deflects out of the flow in Case B.

Due to the change in the shock impingement location between cases, the separation bubble position differs in Cases B and C compared to that in Case A. In Case A, the separation and reattachment points both reside on the compliant panel surface with the separation bubble roughly centered over the center point of the panel. However, in Cases B and C, while the reattachment point resides on the compliant panel, the separation point is located on the rigid wall upstream of the panel.

Time-resolved schlieren snapshots for the same four phases of the panel oscillation cycle at mode (1,1) for each case are displayed in Figs. 5.7-5.9. The overall structure of the flow field between all three compliant cases closely resembles that of the rigid wall interaction depicted in Fig. 5.2, indicating that the primary features of the STBLI are preserved despite the addition of surface compliance. Similar to that in the rigid case, the separation shock fronts in the compliant cases also exhibit localized curvature and global displacements inherent to the unsteady STBLI.

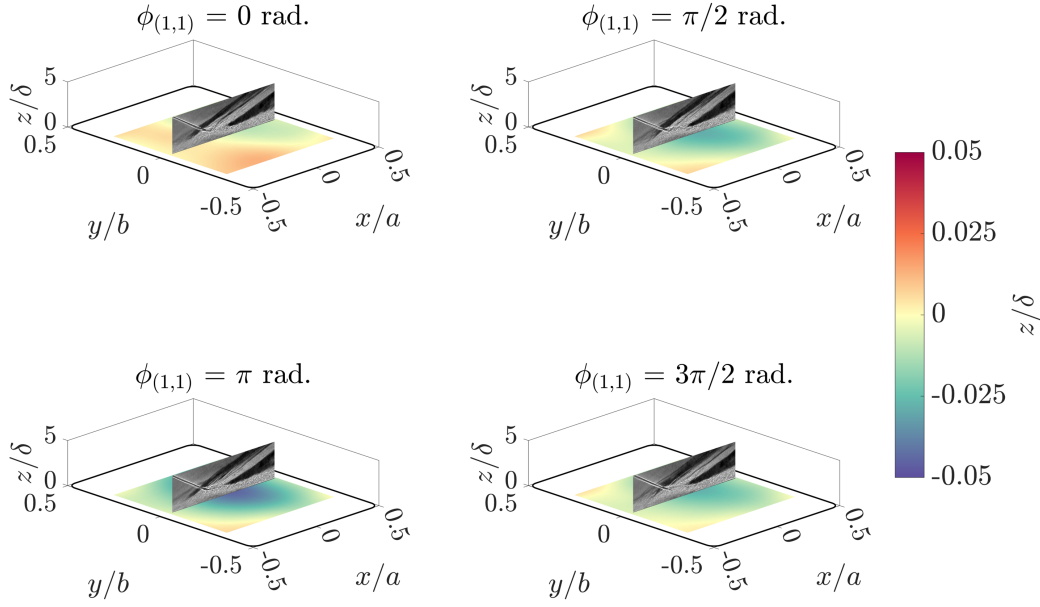


Figure 5.4: Simultaneous schlieren and photogrammetry snapshots for a representative full panel oscillation cycle at vibration mode (1,1) for Case A.

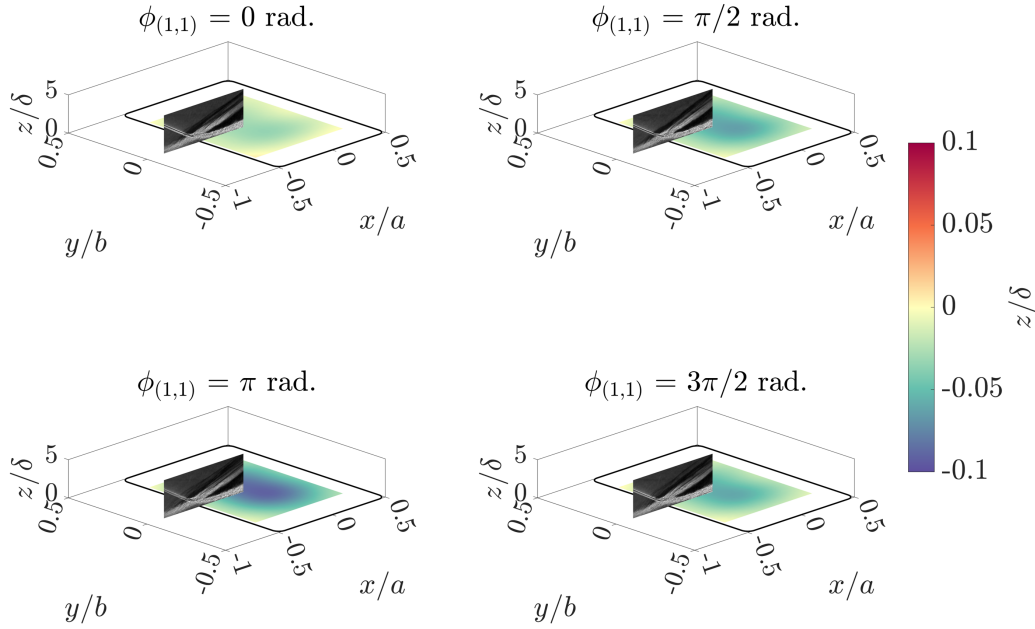


Figure 5.5: Simultaneous schlieren and photogrammetry snapshots for a representative full panel oscillation cycle at vibration mode (1,1) for Case B.

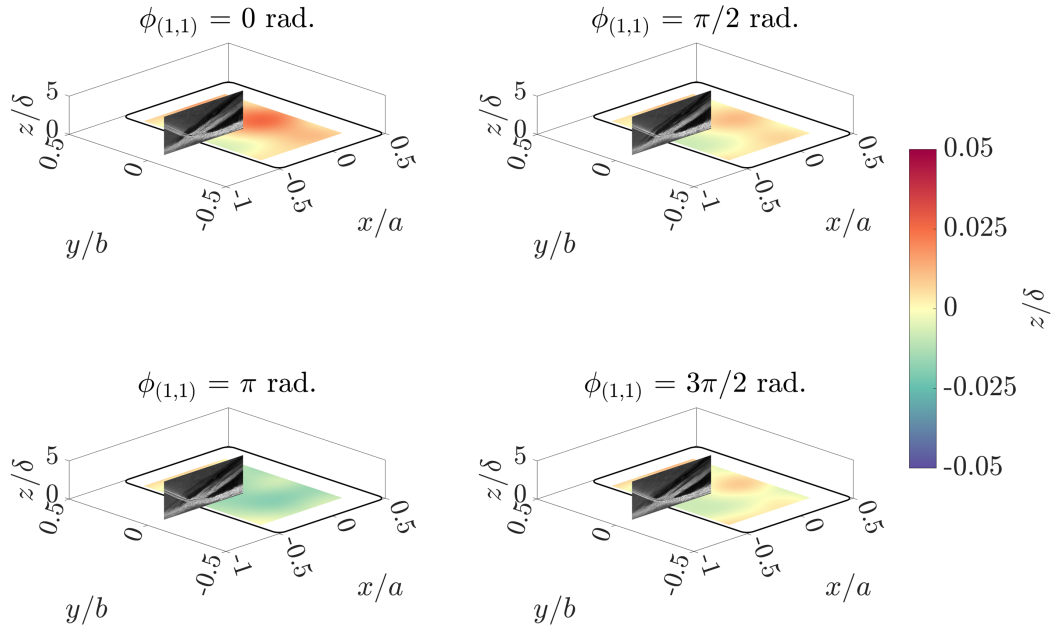


Figure 5.6: Simultaneous schlieren and photogrammetry snapshots for a representative full panel oscillation cycle at vibration mode (1,1) for Case C.

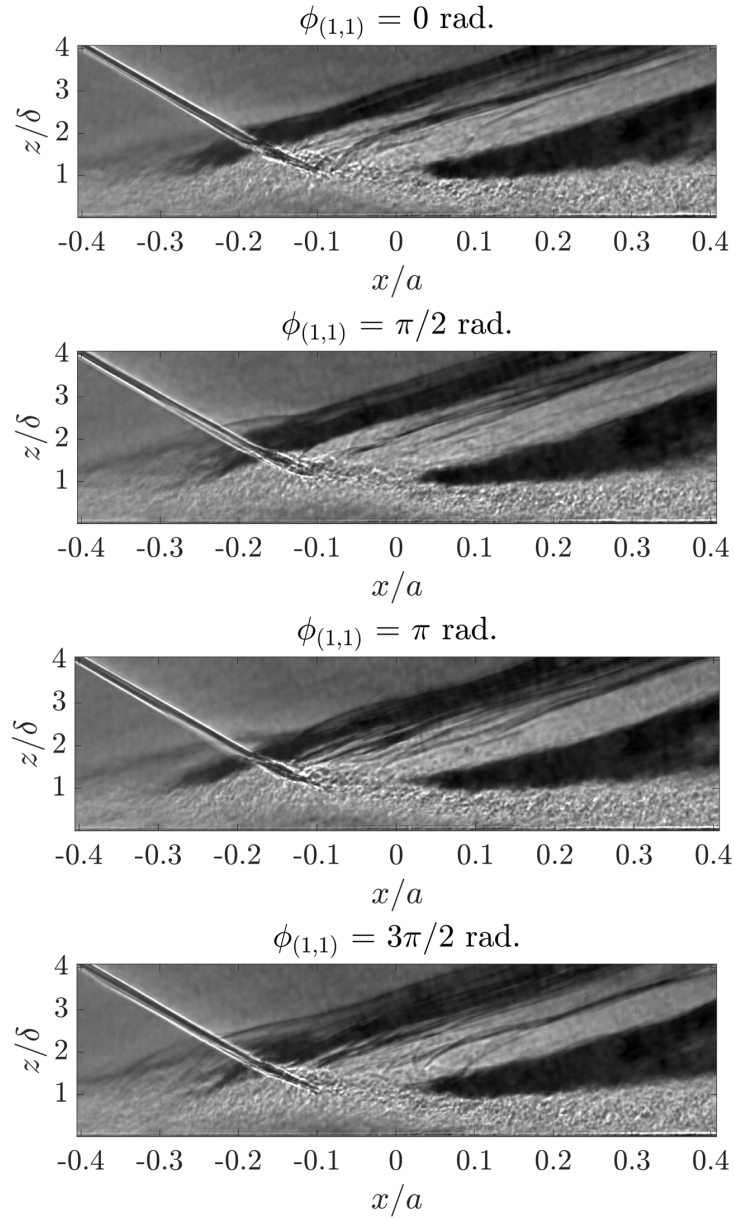


Figure 5.7: Schlieren snapshots for a representative full panel oscillation cycle at vibration mode (1,1) for Case A.

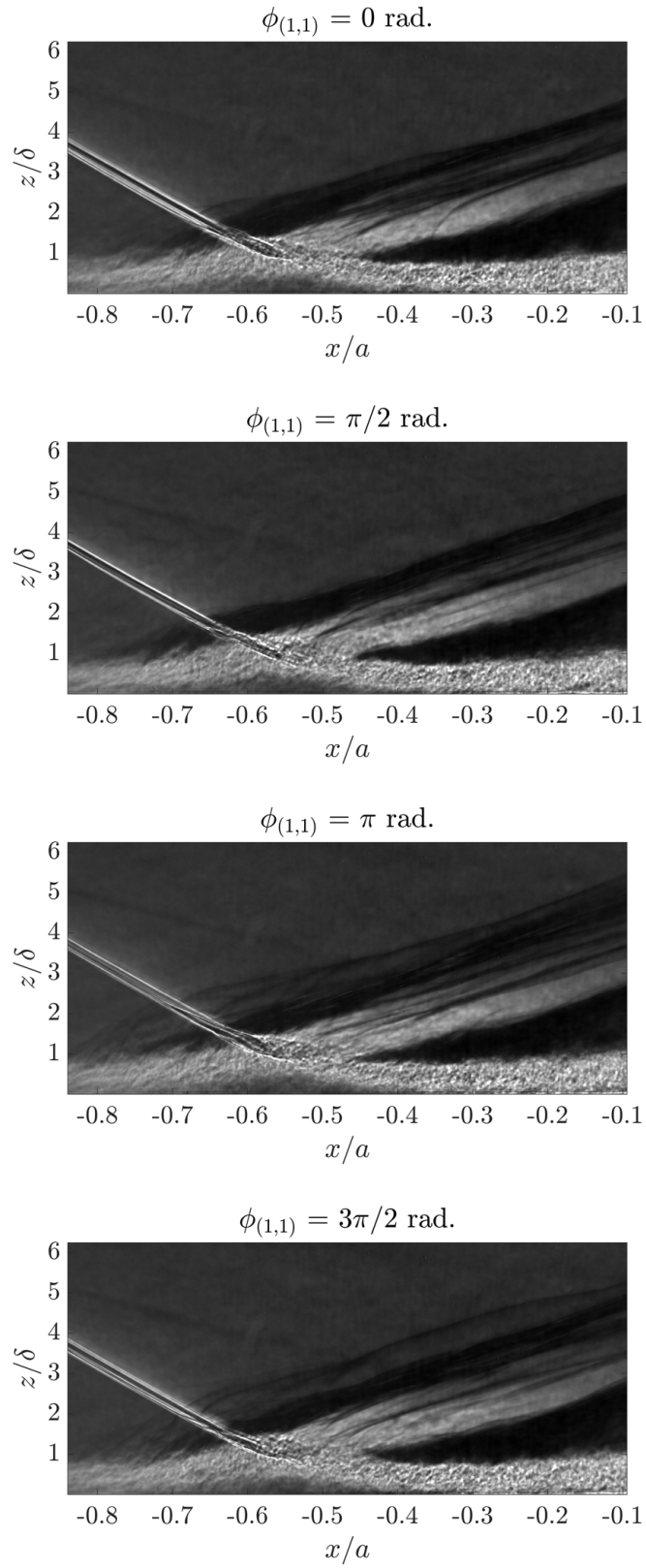


Figure 5.8: Schlieren snapshots for a representative full panel oscillation cycle at vibration mode (1,1) for Case B.

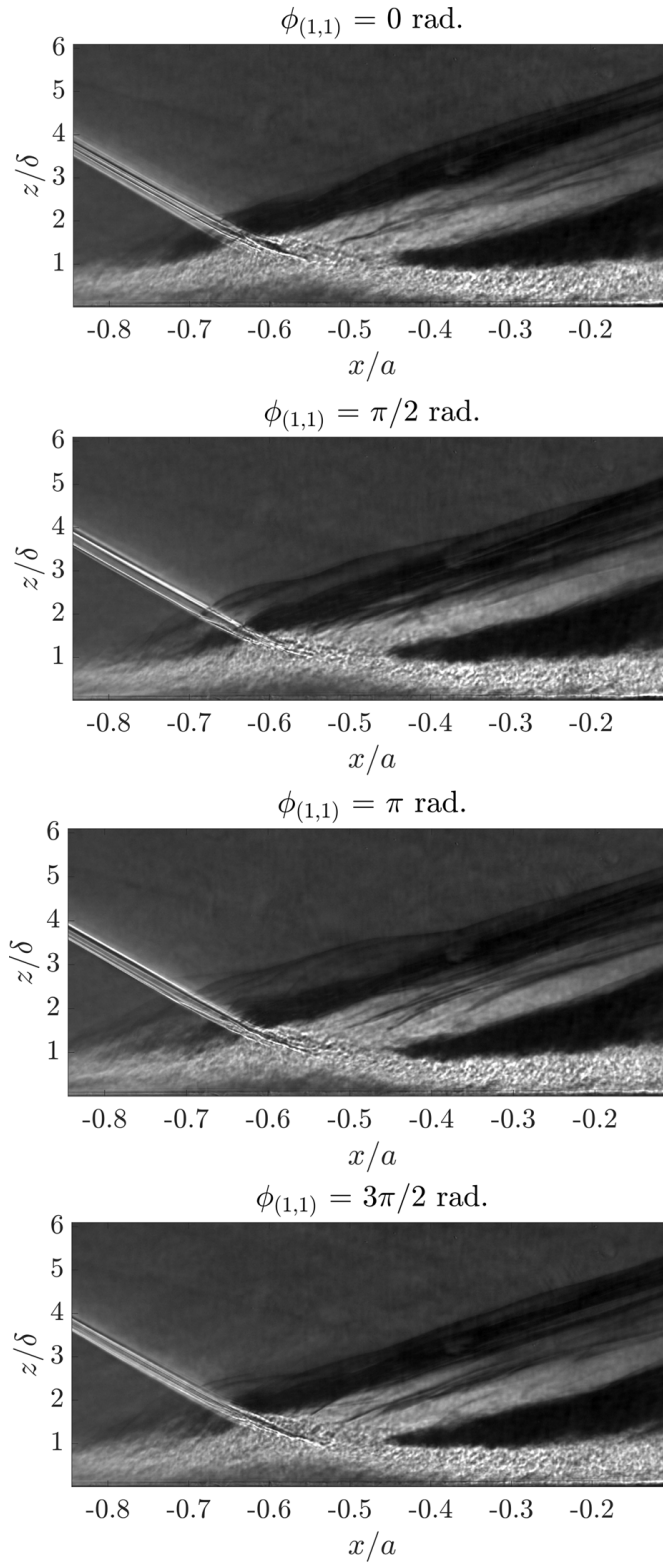


Figure 5.9: Schlieren snapshots for a representative full panel oscillation cycle at vibration mode (1,1) for Case C.

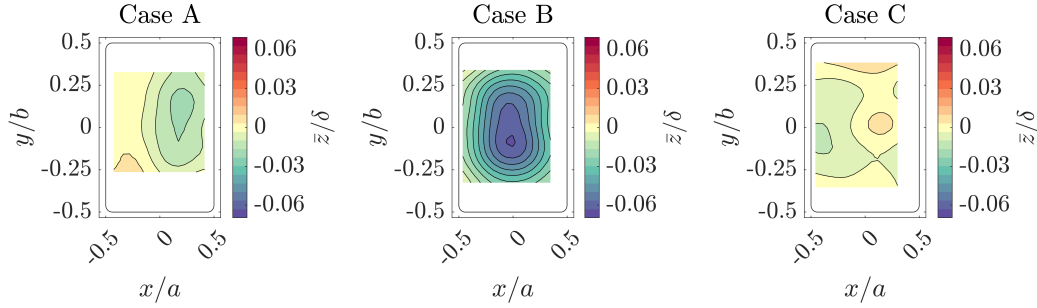


Figure 5.10: Time-averaged panel deflection.

5.2.1 Photogrammetry Results

In this section, the time history and spectral content of the panel responses for each case are analyzed both at discrete points as well as in the full-field dataset measured by the photogrammetry system.

Contour plots of the time-averaged panel deformation for each case are presented in Fig. 5.10. For Cases A and C, the panel experiences approximately zero mean displacement within $\bar{z}/\delta \approx \pm 0.015$. In Case A, the downstream half of the panel is biased slightly downward, whereas in Case C, the upstream half is biased downward. This behavior is consistent with the location of the strongest time-averaged surface pressure load, which occurs slightly downstream of the shock impingement location. The greater average STBLI-induced loading over the panel relative to the cavity pressure for Case B results in a significant downward mean deformation.

The time histories of the panel center point deflection scaled by the incoming boundary layer thickness, z_{center}/δ , during test time are shown in Fig. 5.11 for each case. In Cases A and C, the center point oscillates both into and out of the flow and remains centered near zero displacement. By comparison, the center point in the non-cavity pressure matched Case B remains displaced out of the flow throughout the entire test duration, with a mean deflection of approximately $\bar{z}_{\text{center}}/\delta \approx -0.06$.

5.2.1.1 Time Dependency of Panel Response

Similar to the panel response under no shock impingement, the panel responses in all STBLI experiments also exhibit an abrupt onset of oscillation at the fundamental vibration mode following the impulsive loading of flow arrival. The periodic oscillations at the fundamental vibration mode indicate that the panel response is dominated by a one-way uncoupled fluid-structure mechanism, where feedback from the flow to the panel has minimal impact on the structural dynamics.

All six repeated experiments for mid-chord and pressure-balanced shock impingement (Case A) exhibit the same characteristics, where the panel primarily oscillates for roughly 30 periods at the fundamental vibration mode during test time. Just after $t = 30$ ms, a small rise in the oscillation amplitude occurs in response to facility-induced vibrations caused by the abrupt stop of the piston valve retraction. This increase in amplitude at $t = 30$ ms is also observed for the No-STBLI case. The consistent behavior across all repeated experiments reveals that the panel response in Case A is dominated by a one-way uncoupled fluid-structure response that does not transition to two-way coupling behavior.

In contrast, some experiments for Cases B and C experience a transition in the panel behavior approximately halfway through test time. The panel response for shots 1327 and 1334 in Fig. 5.11 show that around $t \approx 45$ ms, the periodic mode (1,1) oscillations transition to lower amplitude oscillations composed of multiple higher-order vibration modes. Although the amplitudes approach close to the limit of what can be resolved within the photogrammetry measurement uncertainty, as will be discussed in Sections 5.2.2 & 5.2.3, changes to the flow behavior are also observed following this transition. Consequently, this transition indicates that the initial excitation of the panel dissipates sufficiently for two-way coupled feedback from the flow to dominate the structural dynamics. The red shaded regions in Fig. 5.11 represent the one-way uncoupled response, the gray regions correspond to the transition from the one-way to two-way coupling mechanism, and the blue regions correspond to the two-way coupled response.

However, not all repeated experiments for Cases B and C exhibit a transition from one-way to two-way coupling within test time. As shown in Fig. 5.11, shot 1329 does not exhibit any transition to two-way coupling, and the response is similar to that observed in Case A and No-STBLI experiments. Shot 1333 primarily exhibits one-way uncoupled behavior for the majority of test time until around $t = 55$ ms, where the response begins but does not fully transition to two-way coupled behavior before the end of test time.

A summary of the time taken for each experiment to either transition or not transition to two-way coupled behavior is listed in Table 5.1. The transition time, denoted τ_{1-2} , is the time the panel begins to transition subtracted by the time of flow start up, which is approximately $t = -15$ ms. The time is normalized by the period of the fundamental vibration mode, $T_{(1,1)} = 1/f_{(1,1)}$. For Case B, three out of the four repeated experiments experience a transition in coupling mechanism, whereas all

four Case C experiments exhibit a transition.

As characterized in Section 4.1, the freestream conditions and facility-induced vibrations are highly repeatable between shots. On the other hand, the structural parameters may exhibit a small degree of variability because each repeated experiment uses a different compliant panel. Minor structural imperfections, initial panel curvatures, variations in paint thickness, and epoxy adhesive material property differences could potentially affect the panel damping ratios, which influence the time for the initial mode (1,1) oscillations to dissipate.

To account for the different dominant coupling mechanisms present during test time in Cases B and C, the later sections in this work conditionally analyze the flow and structural response based on whether the panel is exhibiting one-way uncoupled or two-way coupled dominated behavior.

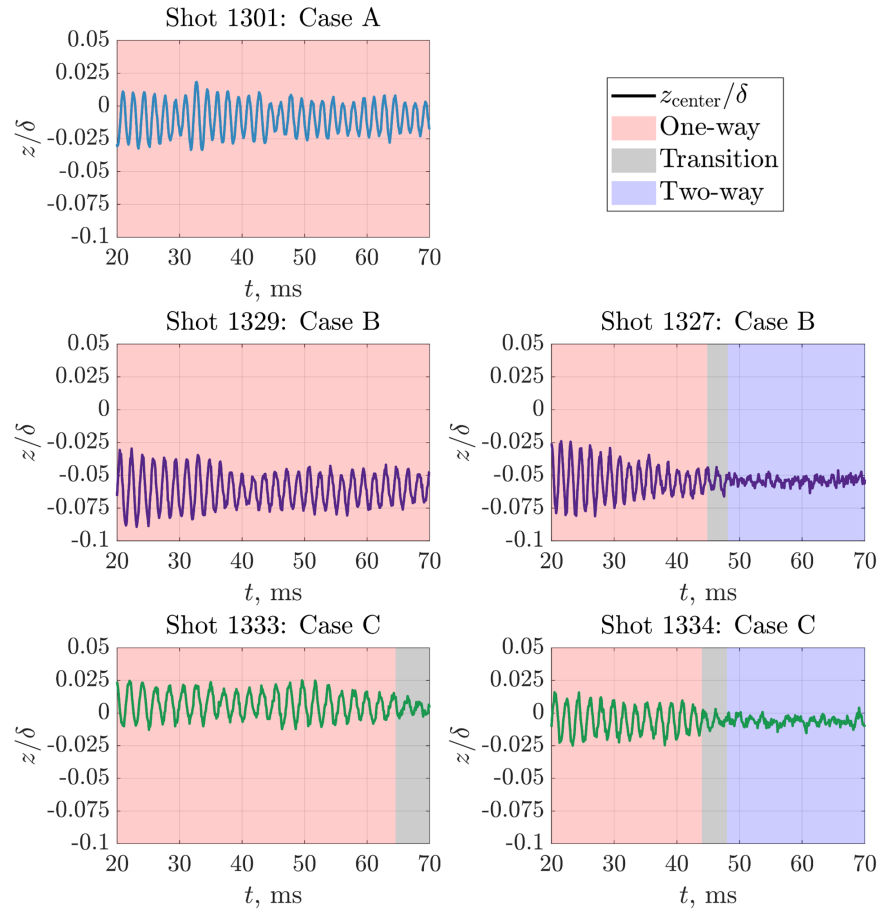


Figure 5.11: Center point deflection of panel for each complaint case.

Shot	Case	$T_{(1,1)}$, ms	$\tau_{1-2}/T_{(1,1)}$
1301	A	1.67	-
1302	A	1.83	-
1303	A	1.64	-
1304	A	1.76	-
1305	A	1.71	-
1306	A	1.74	-
1327	B	1.55	38.5
1328	B	1.68	50.2
1329	B	1.77	-
1330	B	1.68	28.5
1333	C	2.12	37.6
1334	C	1.85	31.9
1335	C	2.00	32.4
1336	C	1.98	31.3

Table 5.1: Time for panel response to transition from one-way uncoupled to two-way coupled behavior.

5.2.1.2 Root Mean Square of Center Point Deflection

The trial-averaged root mean square (RMS) values of the panel center point deflections are reported in Fig. 5.12. Horizontal error bars indicate one standard deviation of variability across repeated experiments. Closed circular markers correspond to responses dominated by one-way uncoupled fluid-structure mechanisms while open diamond markers indicate two-way coupled dominated behavior.

The most significant change in RMS is due to the transition from one-way to two-way dominated coupling behavior. For Cases B and C, the value of RMS decreases by over 70% when the panel switches from a one-way uncoupled interaction to a two-way coupled interaction. This attenuation occurs because the structurally dominated oscillations driven by the impulsive loading from flow arrival eventually dampen out. As this occurs, the relative strength of pressure fluctuations driven by the STBLI-induced unsteadiness and panel motion and curvature amplifies. The low amplitudes of oscillation signify that despite the interaction transitioning to a two-way coupling dominated mechanism, the STBLI-induced loading does not promote the onset of flutter under these conditions.

Although the trial-averaged RMS value for the one-way uncoupled Case B is about

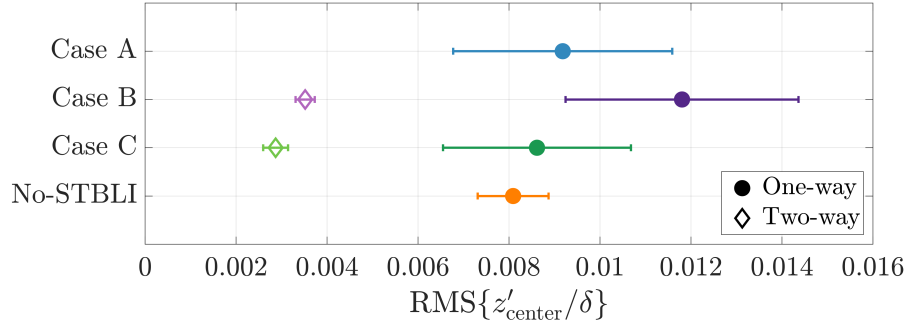


Figure 5.12: Root mean square of center point panel deflection.

35% larger than that of one-way coupled Cases A and C, the shot-to-shot variability suggests that this difference is not statistically significant. In addition, the RMS for the No-STBLI case is within the range of that of the one-way uncoupled Cases A and C. Therefore, these results confirm that the addition of STBLI dynamics has a limited effect on modifying the amplitude of panel oscillations when the interaction exhibits one-way dominated coupling behavior.

5.2.1.3 Spectral Analysis of Panel Motion

The frequency content of the out-of-plane oscillations at a nodeless point excited by all of the lowest order vibrational modes, $z_{\text{nodeless}}(x/a = -0.19, y/b = -0.11, t)$, is shown in the power spectral density plots in Fig. 5.13. In all three one-way uncoupled cases, mode (1,1) governs the panel dynamics regardless of shock impingement location or cavity pressure. The first vibrational mode produces a dominant peak in the power spectra and carries nearly two orders of magnitude greater spectral energy than the next highest mode, which is consistent with that of the one-way uncoupled No-STBLI case.

A distinct, broad secondary peak near 400 Hz, denoted f_{STBLI} , appears in the power spectrum for Case A. When non-dimensionalized by the freestream velocity and interaction length scale defined in Section 5.2.2, L_{shock} , the associated Strouhal number is $\text{St}_{L_{\text{shock}}} = f_{\text{STBLI}} L_{\text{shock}} / u_{\infty} \approx 0.01$. This frequency range corresponds to the low-frequency STBLI unsteadiness, and its presence suggests that energy is transferred from the unsteady flow field to the panel motion. Despite the dominance of a one-way uncoupled fluid-structure mechanism for this case, the spectral energy at f_{STBLI} is over an order of magnitude greater than the noise floor of the measurement and implies that the structural dynamics of the panel are slightly modified by the flow field. The lack of a peak at this frequency for the one-way uncoupled Cases B and C

suggest that the transfer of energy from the unsteady separation shock foot motion to the panel motion is strongest under mid-chord shock impingement compared to that of leading-edge impingement. This is likely due to the difference in the separation point location, which resides directly on the panel for the mid-chord impingement case.

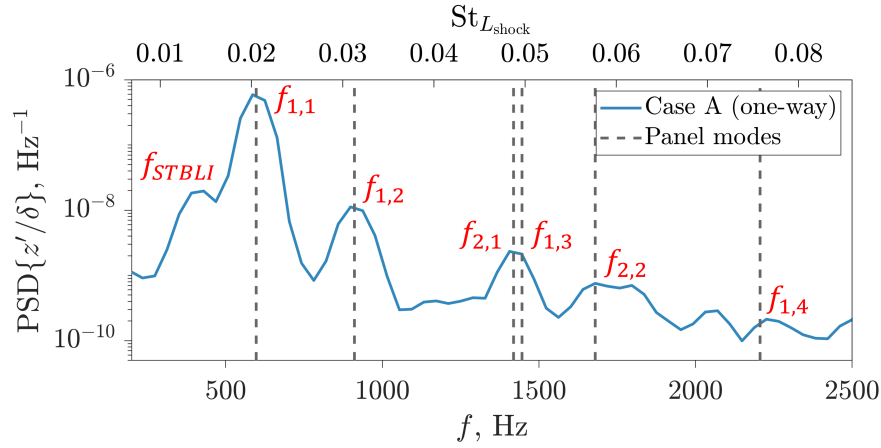
When the interaction transitions to two-way coupled behavior for Cases B and C, the spectral energy at the fundamental vibration mode decreases drastically by over one order of magnitude. Even though the spectral energy at the higher order modes are only modified to a minor degree, the relative strength of the higher order modes compared to that of the first mode increases substantially. Consequently, the two-way coupled behavior of the panel in Cases B and C is characterized by fluctuations at multiple vibrational modes. A factor of two increase in the spectral energy at f_{STBLI} is also observed for the two-way coupled Case B compared to that of the one-way uncoupled Case B.

5.2.1.4 Modal Decomposition of Panel Motion

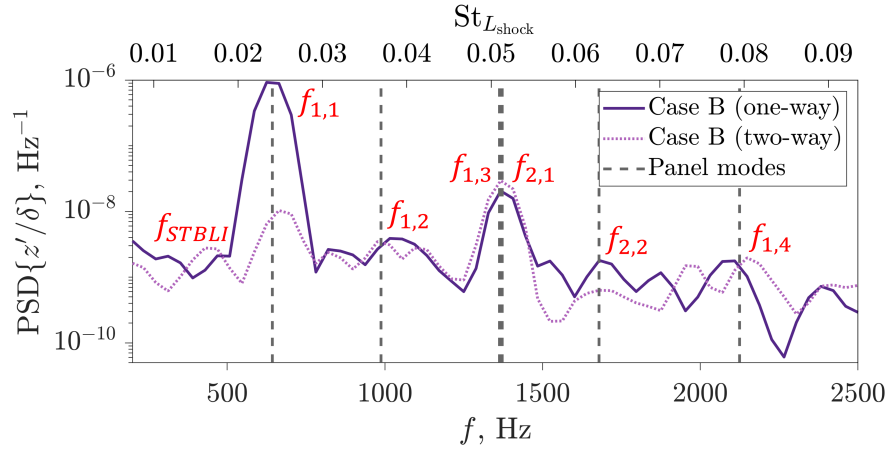
POD is applied to the mean-subtracted panel deformation field, $z'(x, y, t)$, to decompose the structural response into a linear combination of spatial modes, $\Phi_i^z(x, y)$, and temporal coefficients, $\psi_i^z(t)$. In this modal decomposition, the spatial modes represent the dominant vibration mode shapes, and the temporal coefficients describe the time-dependent amplitude and phase evolution of each mode. Representative POD spatial mode shapes of the panel response are presented in Fig. 5.14 for the nine lowest-order modes. Contour values near zero correspond to nodal lines, and positive and negative contour levels indicate deflection in opposite out-of-plane directions.

Across all compliant cases, the extracted POD mode shapes are qualitatively similar and exhibit minimal variation. There are no distinguishable differences in mode shape between the one-way uncoupled and two-way coupled panel response in Cases B and C. The modes closely resemble the theoretical vibrational mode shapes, indicating that the mode shapes are not significantly modified by the unsteady STBLI loading.

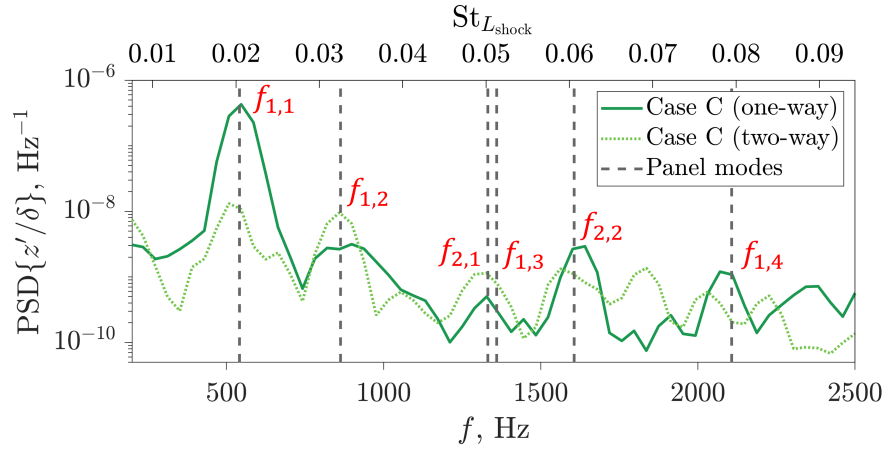
Trial-averaged frequencies of the six lowest-order POD modes are presented in Fig. 5.15, with vertical error bars representing one standard deviation of variability. The POD mode frequencies are identified by calculating the power spectral density for each temporal coefficient, detecting the maximum peak in the spectrum, and



(a) Case A.



(b) Case B.



(c) Case C.

Figure 5.13: Power spectral density of panel oscillations. Gray dashed lines indicate the experimental panel vibrational modes as detected by the power spectra of the POD temporal coefficients in Section 5.2.1.4. Theoretical and experimental free vibration natural frequencies are reported in Table 4.2.

performing a three point Gaussian fit about the peak and two neighboring points. As a baseline, the natural frequencies measured from roving hammer tests under unpressurized conditions are included, and the error bars denote the measurement uncertainty quantified in Section 4.3.2.

Notably, differences in the mode frequencies between the one-way and two-way coupled responses in Cases B and C are minimal, suggesting that the transition from one-way to two-way coupling does not significantly affect the vibrational frequency. This result supports the notion that the mean aerodynamic loading over the panel is consistent between the two types of coupled responses.

The vibrational frequencies of Cases A and B closely align with one another and are consistently elevated relative to the hammer test values by about 13% on average. In contrast, Case C generally exhibits frequencies lower than those of Cases A and B and more closely aligns with the hammer test for all modes except (2,1). This trend suggests that, for the STBLI cases, cavity pressure plays a more significant role in modifying the panel modal frequencies than the shock impingement location. However, despite the differences in cavity pressure and flow field environment, leading-edge impingement (Case C) and the No-STBLI case share nearly identical frequencies for a majority of the modes. This similarity may be a reflection of the comparable mean panel deflection shape and aerodynamic loading over the panel.

The trial-averaged POD modal energies for the four leading modes are plotted in Fig. 5.16 and listed in Table 5.2, with error bars and uncertainties indicating one standard deviation across repeated experiments. All one-way uncoupled cases display a similar energy distribution, where mode (1,1) dominates the interaction by capturing approximately 90% of the total POD energy while higher-order modes contribute progressively less energy content. The agreement reinforces the conclusion that the panel response is largely uncoupled from feedback from the flow, as neither shock impingement location nor cavity pressure substantially alter the dominant mode or overall modal energy distribution.

However, some minor deviations in the modal energy order do exist for the one-way uncoupled cases. For instance, in the one-way uncoupled leading-edge cases, some experiments swap the modal order of modes (1,2) and (1,3). Under no shock impingement, the panel exhibits greater modal energy at (1,3) than that of (1,2) for all experiments. Furthermore, the No-STBLI case exhibits measurably less shot-to-shot variation in modal energy compared to that of the STBLI cases, which suggests that the modal order of the structural response is more consistent in the absence

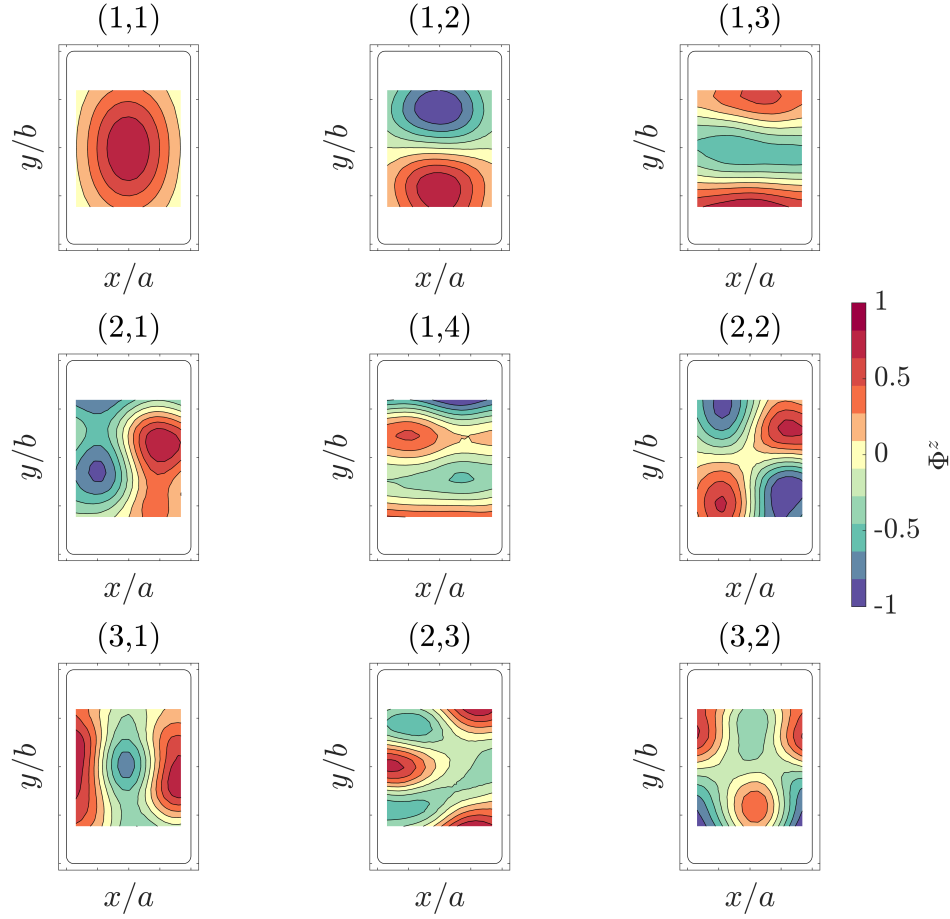


Figure 5.14: Representative POD mode shapes of panel deflection.

forcing due to shock impingement.

For the two-way coupled cases, there is a reduction of POD mode energy at the first mode by about 20% of the total POD mode energy compared to that of the one-way uncoupled cases. The POD energy is more distributed over the higher-order modes, and the modal order is swapped for several of the higher-order modes. These differences indicate that the higher-order modes play a more significant role in the interaction dynamics under two-way coupling.

5.2.2 Schlieren Results

Time-averaged schlieren images for each rigid and compliant STBLI case are presented in Fig. 5.17a. The overall mean flow structure is qualitatively similar across all four cases. In each case, a mean separated region forms and extends at least $L_{\text{sep}} > 0.25a$ in length at the wall. The regions of the separation and reattachment shocks appear slightly blurred rather than sharply defined, indicating the unsteady-

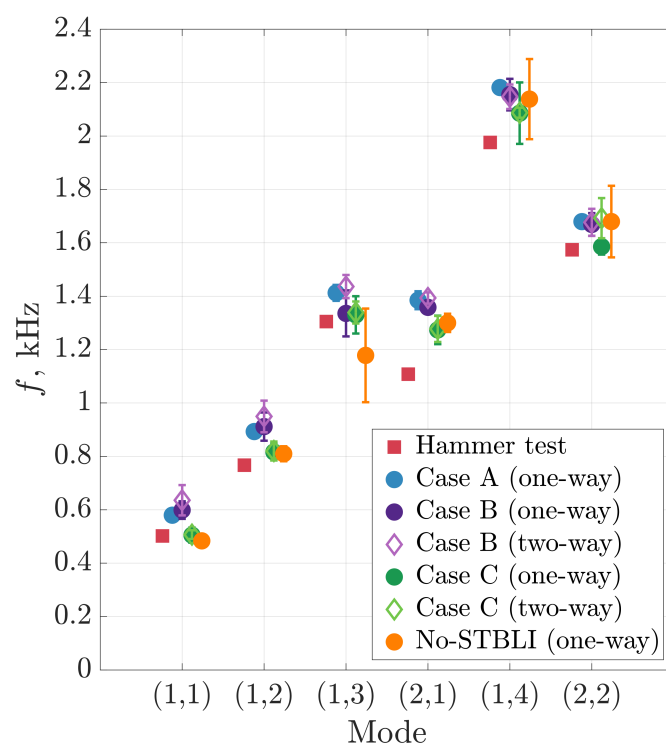


Figure 5.15: Vibrational mode frequencies.

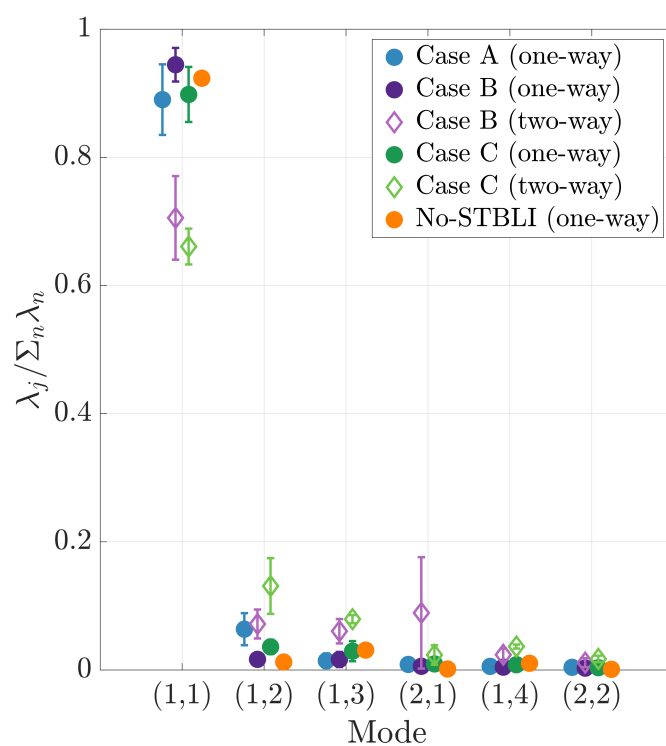


Figure 5.16: Vibrational POD mode energy distribution.

Table 5.2: Percent POD mode energy of out-of-plane panel motion

Mode	(1,1)	(1,2)	(1,3)	(2,1)	(1,4)	(2,2)
Case A (one-way)	89.0 $\pm$ 5.5	6.4 $\pm$ 2.5	1.4 $\pm$ 1.1	0.9 $\pm$.6	0.6 $\pm$ 0.4	0.4 $\pm$ 0.3
Case B (one-way)	94.5 $\pm$ 2.6	1.7 $\pm$ 0.8	1.6 $\pm$ 1.2	0.6 $\pm$ 0.5	0.4 $\pm$ 0.4	0.3 $\pm$ 0.1
Case B (two-way)	70.6 $\pm$ 6.5	7.2 $\pm$ 2.3	6.0 $\pm$ 1.9	8.9 $\pm$ 8.7	2.4 $\pm$ 0.9	1.1 $\pm$ 0.7
Case C (one-way)	89.8 $\pm$ 4.3	3.6 $\pm$ 1.1	2.9 $\pm$ 1.6	0.9 $\pm$ 0.8	0.8 $\pm$ 0.4	0.4 $\pm$ 0.1
Case C (two-way)	66.1 $\pm$ 2.8	13.1 $\pm$ 4.4	7.9 $\pm$ 0.6	2.3 $\pm$ 1.5	3.7 $\pm$ 0.3	1.7 $\pm$ 0.5
No-STBLI (one-way)	92.4 $\pm$ 1.0	1.2 $\pm$ 0.3	3.1 $\pm$ 0.4	0.1 $\pm$ 0.1	1.0 $\pm$ 0.2	0.1 $\pm$ 0.1

ness of the shock motion that is spatially smeared over its range of motion in the time-averaged image.

The standard deviation of each pixel intensity normalized by the spatially-averaged pixel intensity is shown in Fig. 5.17b. In the incoming boundary layer, weak to moderate standard deviation levels are observed, corresponding to turbulent structures convecting downstream. Near shock impingement and continuing downstream, the standard deviation levels in the boundary layer increase substantially. This amplification suggests that the interaction intensifies the density gradient fluctuations associated with turbulent structures. Elevated standard deviation levels present in the shock and expansion wave regions also highlight the unsteady motion of these flow features. It is worth noting that the incident oblique shock exhibits comparatively high standard deviation levels, which are largely attributable to slight spanwise variations in the incident shock front arising from three-dimensional flow effects and the interaction between the incident shock and the slightly spanwise non-uniform weak shock generated by the boundary layer trip leading-edge.

5.2.2.1 Schlieren Feature Tracking

As discussed in Section 3.3.3, a threshold edge detection method is applied to the schlieren footage to track the separation and reattachment shock fronts. The fluctuation of streamwise shock position over time for each case are plotted in Fig. 5.18 for the separation shock at $z/\delta = 3.05$ and in Fig. 5.19 for the reattachment shock at $z/\delta = 1.90$. Both the separation and reattachment shock positions tend to be consistently centered about a mean position. However, the separation shock experiences a greater range of motion compared to that of the reattachment shock. In Cases B and C, there are no observable differences in the shock motion between the one-way and two-way coupled responses.

Trial-averaged RMS values of the separation and reattachment shock motion are compared in Fig. 5.20, with horizontal error bars representing one standard deviation across repeated experiments. Overall, the separation shock oscillates with an RMS three to four times greater than that of the reattachment shock. The reattachment shock RMS values show no significant difference between the rigid and compliant cases, indicating that surface compliance does not significantly influence the extent of the reattachment shock motion. Furthermore, there are no measurable differences in the RMS values of both the separation and reattachment shock motion between the one-way and two-way coupled cases, suggesting that the type of coupling mechanism

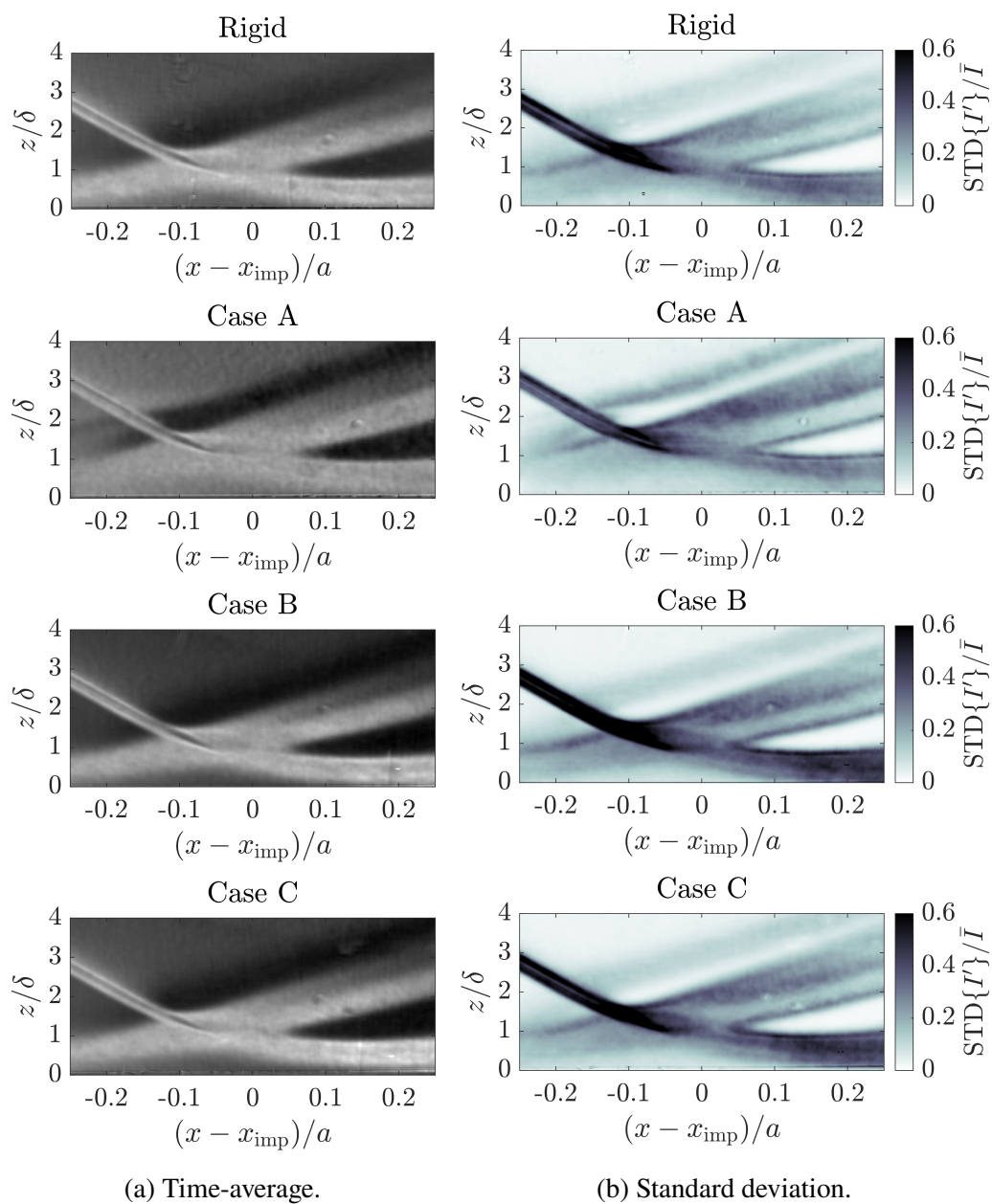


Figure 5.17: Comparison of time-average and standard deviation of schlieren pixel intensities.

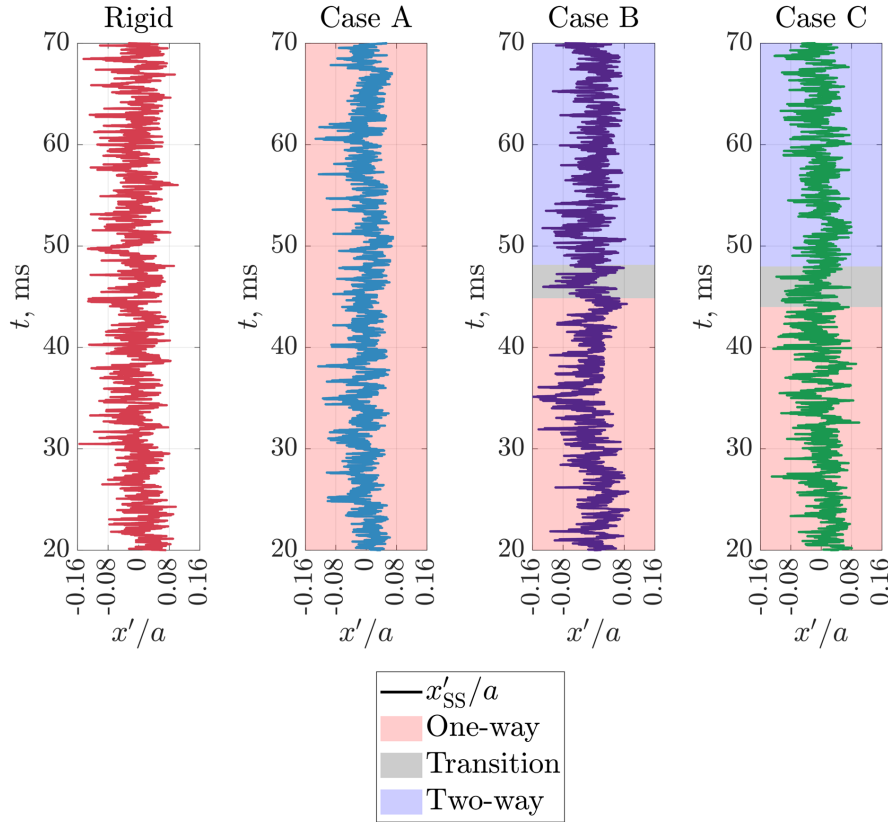


Figure 5.18: Comparison of time history of separation shock streamwise fluctuations at $z/\delta = 3.05$.

has minimal influence on the extent of the shock motion.

For the separation shock, Case A exhibits a slightly lower RMS than the other compliant cases, indicating that mid-chord shock impingement slightly reduces the extent of the separation shock motion relative to that of leading-edge impingement. The rigid case exhibits the greatest variability in RMS that exceeds the full range of RMS values observed in Cases A through C. This implies that surface compliance reduces the shot-to-shot variability in the separation shock oscillation amplitude compared to that of the rigid case.

5.2.2.2 Interaction Length Scale

Although the separation bubble structure is distinguishable in the schlieren images, directly measuring the separation bubble length, L_{sep} , from the footage may result in a biased, inconsistent measurement because the exact separation and reattachment points are not clearly defined in the visualization of density gradients. Therefore, the streamwise distance between the separation and reattachment shocks, L_{shock} , is

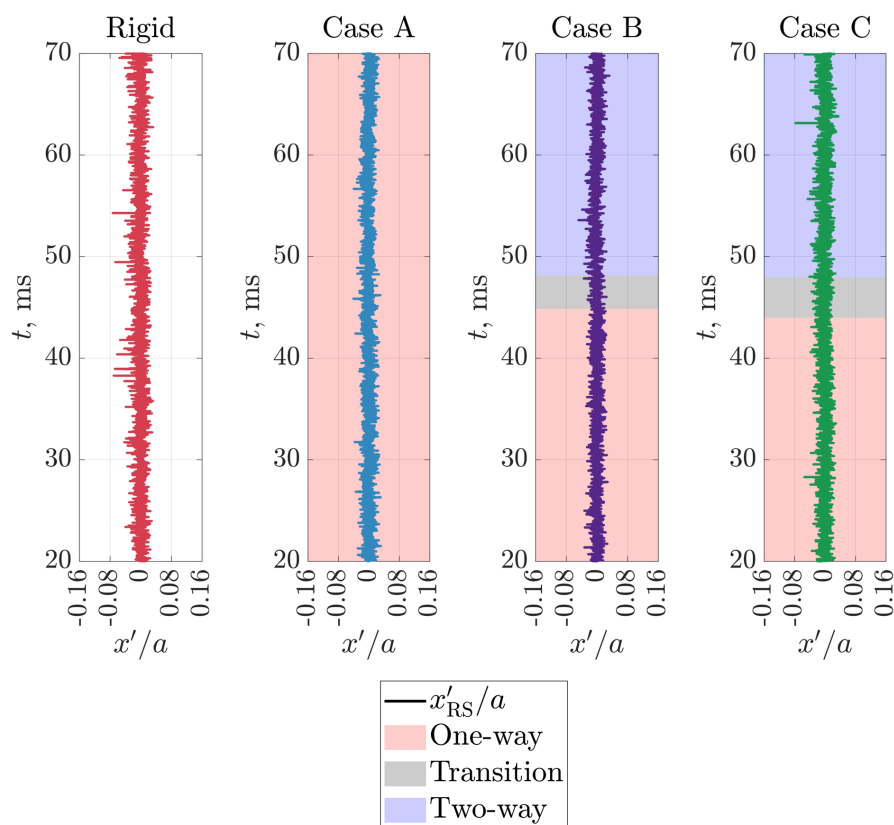


Figure 5.19: Comparison of time history of reattachment shock streamwise fluctuations at $z/\delta = 1.90$.

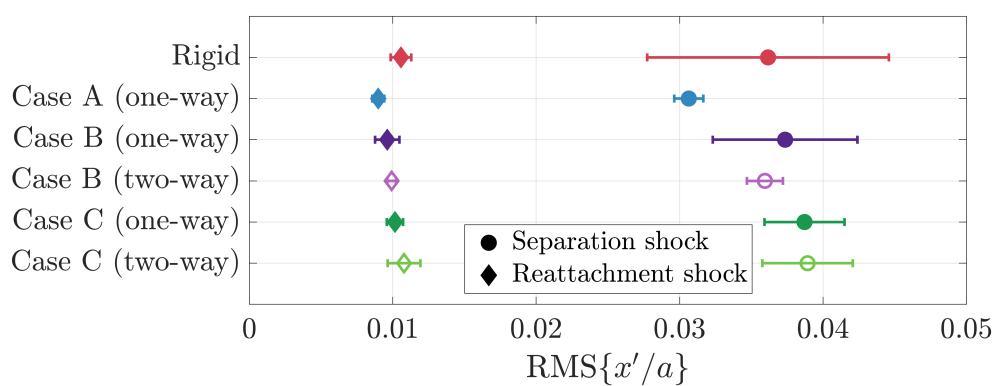


Figure 5.20: Trial-averaged root mean square of shock motion for x'_{SS} ($z/\delta = 3.05$) and x'_{RS} ($z/\delta = 1.90$).

defined and used as a comparable interaction length scale. For this shock distance measurement analysis, the time-averaged shock and boundary layer positions are fit with polynomials that best represent their shape, as illustrated in Fig. 5.21. The incident and separation shocks are fit with a linear function, the reattachment shock is fit with a quadratic curve, and the boundary layer downstream of reattachment is fit with a fourth-order polynomial. The intersection of the fitted incident shock and the wall denotes the inviscid shock impingement location, x_{imp} . The fitted reattachment shock and boundary layer functions are extrapolated upstream until they intersect, defined as point p_{RS} . The fitted separation shock line is also extrapolated upstream toward the wall, and the point, p_{SS} , denotes the location along this line that lies at the same vertical height as p_{RS} . The shock-based interaction length, L_{shock} , is then defined as the streamwise distance between points p_{SS} and p_{RS} . The length between the extrapolated separation shock impingement location on the wall, x_{SS_0} , and inviscid incident shock impingement location, x_{imp} , is denoted $L_{\text{SS-IS}}$.

Trial-averaged values of L_{shock} and $L_{\text{SS-IS}}$ are presented in Fig. 5.22, where the horizontal error bars represent one standard deviation of variability between repeated experiments. Overall, all cases closely agree in magnitude of L_{shock} , with the average shock distance being approximately $0.37a$, or 6.9δ , in magnitude. However, Case A exhibits a slightly smaller value than the rigid and other compliant cases by approximately 5%, or $0.02a$, indicating that the mean separation bubble length is slightly reduced under mid-chord shock impingement. A similar trend was reported by Shinde et al. (2022), who observed a slight reduction in the separation bubble length for the compliant case compared to the rigid case under mid-chord shock impingement. Interestingly, there are no measurable differences in shock distance between the one-way and two-way coupled responses in Cases B and C. The distance between the separation and incident shock impingement locations also does not significantly vary between cases, where the average distance is approximately $L_{\text{SS-IS}} \approx 0.52a$.

5.2.2.3 Shock Angles

The time-averaged incident and separation shock angles, β_{IS} and β_{SS} , are determined from the linear fits to the time-averaged shock positions. The resulting shock angles, along with horizontal error bars representing one standard deviation of shot-to-shot variability, are shown in Fig. 5.23. Across all cases, the incident shock angle is highly consistent with an average shock angle of $\beta_{\text{IS}} = 29.5^\circ$, such that the initial

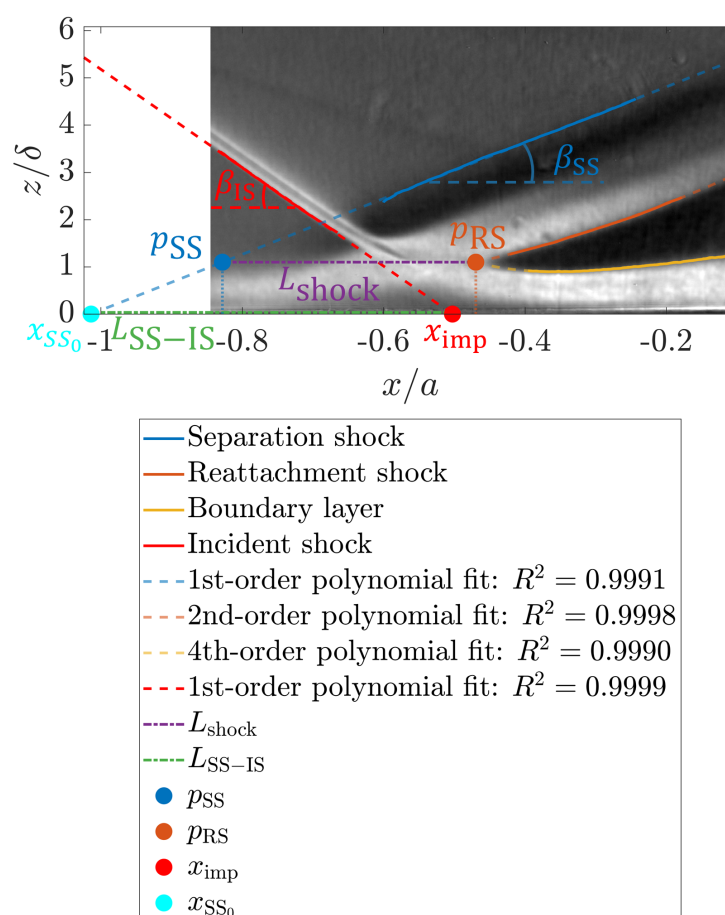


Figure 5.21: Time-averaged shock and boundary layer positions fitted to polynomial functions and overlaid on mean schlieren image.

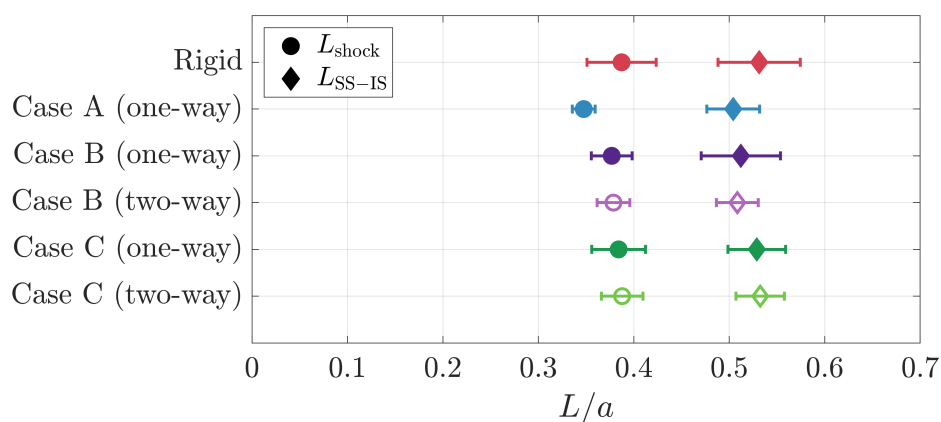


Figure 5.22: Comparison of time-averaged streamwise length between separation and reattachment shocks, L_{shock} .

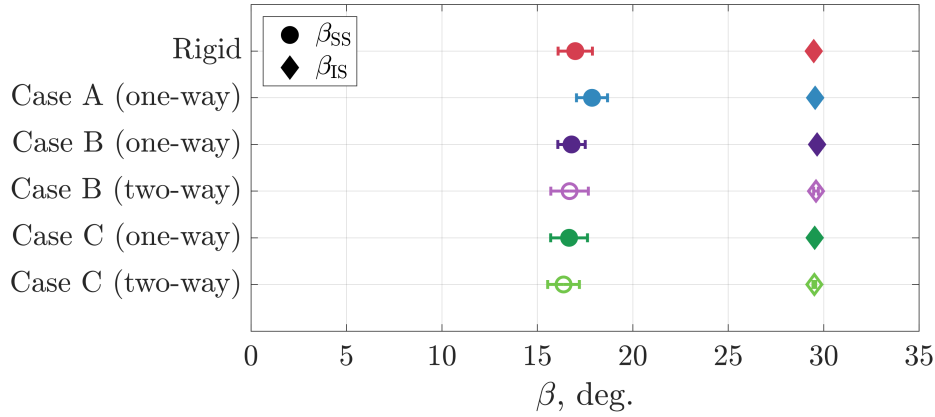


Figure 5.23: Separation shock angle.

condition of the incident oblique shock is essentially identical in all cases.

The separation shock angle relative to the horizontal wall, β_{SS} , also exhibits relatively consistent behavior across all cases, with a mean value of $\beta_{SS} \approx 17^\circ \pm 1^\circ$. Although some variability may be attributed to differences in the schlieren knife edge sensitivity and pixel intensity threshold for shock tracking, the biggest contributing factor is likely due to variation in the separation bubble size. Slight differences in the mean panel deformation shape may influence the mean separation bubble shape, resulting in the separation shock to exhibit minor variations in shock angle.

Applying oblique shock theory for an inviscid Mach 4 flow with an incident shock angle of $\beta_{IS} = 29.5^\circ$, the resulting reflected shock angle relative to the wall is predicted to be $\beta_{RS} = 19.4^\circ$. The static pressure behind the reflected shock is expected to increase to $13.1p_\infty$. This value is about 80% higher than the experimentally measured peak surface pressure of $\bar{p}/p_\infty \approx 7.3$ reported in Fig. 5.3. The discrepancy between the inviscid prediction and the measured response highlights the influence of viscous effects, where the associated shock-induced boundary layer separation modifies the shock structure, leading to a substantially lower surface pressure rise.

5.2.2.4 Modal Decomposition of Shock Motion

POD is applied to the unsteady separation and reattachment shock motion to decompose the fluctuations into the highest energy spatial mode shapes, $\Phi_i(z)$, and corresponding temporal coefficients, $\psi_i(t)$. Representative mode shapes and time-averaged shock positions are shown in Fig. 5.24, and the associated POD modal energies for all cases are presented in Fig. 5.25 and tabulated in Tables 5.3 & 5.4,

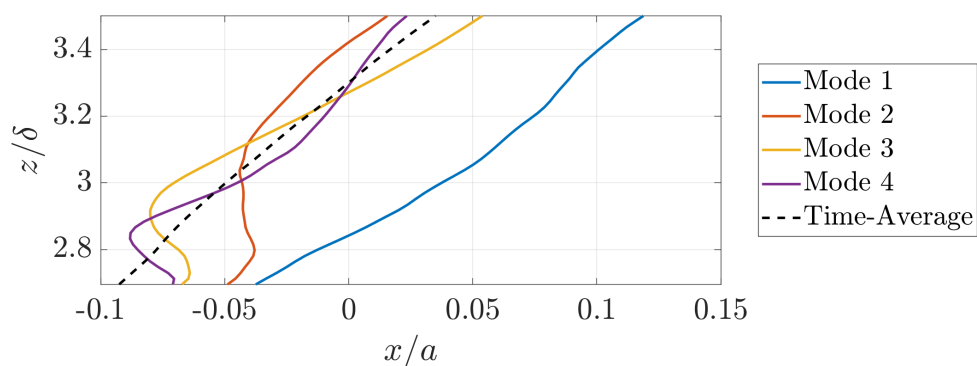
where the error bars and tabulated uncertainty denote one standard deviation across repeated experiments. Note that the same vertical range of tracked shock positions is used for each case. Both the separation and reattachment shocks share similar POD mode shapes that do not significantly vary between the rigid and compliant cases. Mode 1 is characterized by a nearly uniform shock displacement parallel to the mean shock position, and each higher order mode introduces additional crossings of the mean shock position.

Consistent with the similarity of the spatial modes, the POD modal energies of the separation shock motion also show minimal variation across cases and dominant coupling mechanism. Mode 1 captures about 85% of the total POD mode energy, indicating that the majority of the shock motion is dominated by a single displacement mode. This implies that within the measured vertical range of $z/\delta = 2.69$ to 3.51, surface compliance does not significantly alter the global structure of the separation shock motion, and the shock tends to shift upstream and downstream as a coherent structure with a uniform phase of motion.

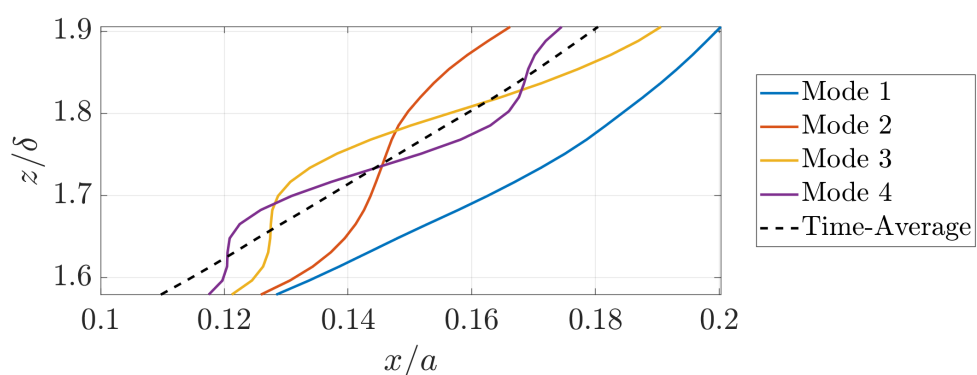
The reattachment shock motion is also dominated by a single, coherent displacement mode, with the first mode energy ranging between 60% to 80% of the total POD mode energy. The mode 1 energies of the two-way coupled cases are slightly lower than that of the one-way uncoupled cases, revealing that the structure of the reattachment shock motion is modified under two-way coupling. In particular, Case B exhibits the greatest change in the reattachment shock POD energies, with a decrease in mode 1 energy by about 14% of the total POD mode energy. Higher order modes contribute more significant rotational and bending motion in the reattachment shock motion compared to that in the separation shock in all cases. This is likely attributed to the more complex, spanwise non-uniform reattachment point dynamics. As observed by Pasquariello et al. (2017), pairs of counter-rotating streamwise vortices induce spanwise variations in the reattachment region. These Görtler-like vortices may influence the reattachment shock dynamics, and the resulting spanwise variation could be partially obscured in the tracked shock position due to the spanwise integrated nature of the schlieren visualization.

5.2.2.5 Spectral Analysis of Separation Shock Motion

The spectral content of the separation shock streamwise fluctuations at $z/\delta = 3.05$ is shown in Fig. 5.26 for each case. The dominant spectral energy of the separation shock motion in the rigid case is concentrated in the low-frequency range

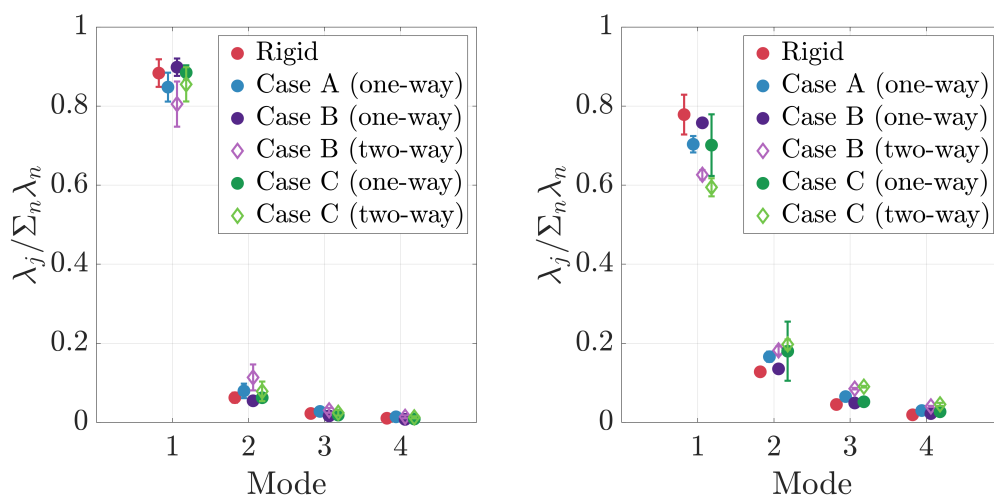


(a) Separation shock.



(b) Reattachment shock.

Figure 5.24: Representative separation and reattachment shock POD mode shapes.



(a) Separation shock.

(b) Reattachment shock.

Figure 5.25: POD modal energy of tracked shock motion.

Table 5.3: Percent POD mode energy of separation shock streamwise motion.

	Mode 1	Mode 2	Mode 3	Mode 4
Rigid	88.4 ± 3.5	6.2 ± 1.3	2.2 ± 0.9	1.0 ± 0.7
Case A (one-way)	84.8 ± 3.7	8.0 ± 1.8	2.8 ± 0.4	1.4 ± 0.6
Case B (one-way)	89.8 ± 2.2	5.5 ± 1.0	1.62 ± 0.2	0.7 ± 0.2
Case B (two-way)	80.5 ± 5.7	11.4 ± 3.3	3.0 ± 0.6	0.14 ± 0.4
Case C (one-way)	88.5 ± 3.5	6.2 ± 1.1	1.8 ± 0.3	0.9 ± 0.1
Case C (two-way)	85.5 ± 4.3	7.9 ± 2.5	2.4 ± 0.7	1.3 ± 0.3

Table 5.4: Percent POD mode energy of reattachment shock streamwise motion.

	Mode 1	Mode 2	Mode 3	Mode 4
Rigid	77.9 ± 5.0	12.8 ± 0.7	4.5 ± 1.0	1.9 ± 1.0
Case A (one-way)	70.4 ± 2.1	16.6 ± 1.3	6.5 ± 0.6	3.0 ± 0.2
Case B (one-way)	75.8 ± 0.8	13.5 ± 0.9	4.9 ± 0.1	2.2 ± 0.1
Case B (two-way)	62.6 ± 1.2	18.2 ± 1.1	8.5 ± 0.2	4.0 ± 0.1
Case C (one-way)	70.1 ± 7.8	18.0 ± 7.5	5.2 ± 0.6	2.7 ± 0.6
Case C (two-way)	59.5 ± 2.3	19.7 ± 1.3	9.0 ± 0.2	4.5 ± 0.4

between 300 to 600 Hz. When scaled by the interaction length scale and incoming boundary layer thickness, this corresponds to Strouhal numbers in the range of $St_{L_{\text{shock}}} = f L_{\text{shock}} / u_{\infty} \approx 0.01 - 0.02$ and $St_{\delta} = f \delta / u_{\infty} = 0.001 - 0.003$. The resulting order of magnitudes of these Strouhal numbers are consistent with values reported in previous studies and confirm that the dominant peak in the separation shock PSD represents the characteristic low-frequency STBLI unsteadiness (Clemens and Narayanaswamy, 2014; Dussauge et al., 2006; Pasquariello et al., 2017). Consistent with typical separated STBLI unsteady behavior, the dominant shock motion frequencies are three orders of magnitude lower than the characteristic boundary layer frequencies, which are about $St_{\delta} \approx O(1)$. Subsequent higher frequency peaks at progressively lower energy levels in the rigid case PSD spectrum represent harmonics of this fundamental interaction unsteadiness frequency.

In Case A, the addition of surface compliance attenuates the low-frequency STBLI unsteadiness and transfers energy at the fundamental vibration mode frequency, $f_{(1,1)}$, to the separation shock. Similarly, both the one-way and two-way coupled responses in Case C also exhibit dominant peaks at $f_{(1,1)}$ and lower energies within the low-frequency STBLI unsteadiness range. This behavior indicates that the structural oscillations not only transfer energy at the vibration mode frequencies

but also modify the strength of the low-frequency STBLI unsteadiness under both mid-chord and leading-edge shock impingement.

In the two-way coupled response in Case C, the energy at $f_{(1,1)}$ is attenuated by about 30% compared to that of the one-way uncoupled response. This reduction in spectral content aligns with the substantial decrease in relative strength of mode (1,1) in the panel response. The modification to the separation shock motion is also complemented by a 50% increase in energy within the STBLI low-frequency unsteadiness range.

In contrast, both the one-way and two-way coupled responses in Case B do not exhibit dominant energy at $f_{(1,1)}$. Instead, the one-way uncoupled response retains a similar magnitude of spectral energy at the low-frequency STBLI compared to that of the rigid case. Panel vibration modes play a less significant role in modifying the one-way uncoupled separation shock response, which is likely a result of the larger negative mean panel deflection compared to that of the cavity pressure matched cases. The low-frequency STBLI peak is reduced in the two-way coupled response, signifying that the vibration modes exhibit greater relative strength compared to that of the low-frequency STBLI unsteadiness.

These observations indicate that surface compliance influences the separation shock spectral content even when the shock foot resides upstream of the compliant panel. The cavity pressure, and consequently, mean panel deformation shape, contributes substantially to the extent that energy is transferred from the panel oscillations to the shock motion. The type of fluid-structure coupling mechanism also has an impact on attenuating or amplifying the spectral energy at the vibration modes and low-frequency STBLI unsteadiness.

5.2.2.6 Spectral Analysis of Reattachment Shock Motion

The power spectra of reattachment shock streamwise fluctuations at $z/\delta = 1.90$ for each case are presented in Fig. 5.27. Overall, the spectral energies for the reattachment shock motion are about two orders of magnitude lower than that of the separation shock. Additionally, the low-frequency STBLI unsteadiness range is less dominant compared to that in the separation shock power spectrum for the rigid case.

When surface compliance is introduced, the power spectra reveal that structural oscillations also transfer energy at the vibration mode frequencies to the reattachment

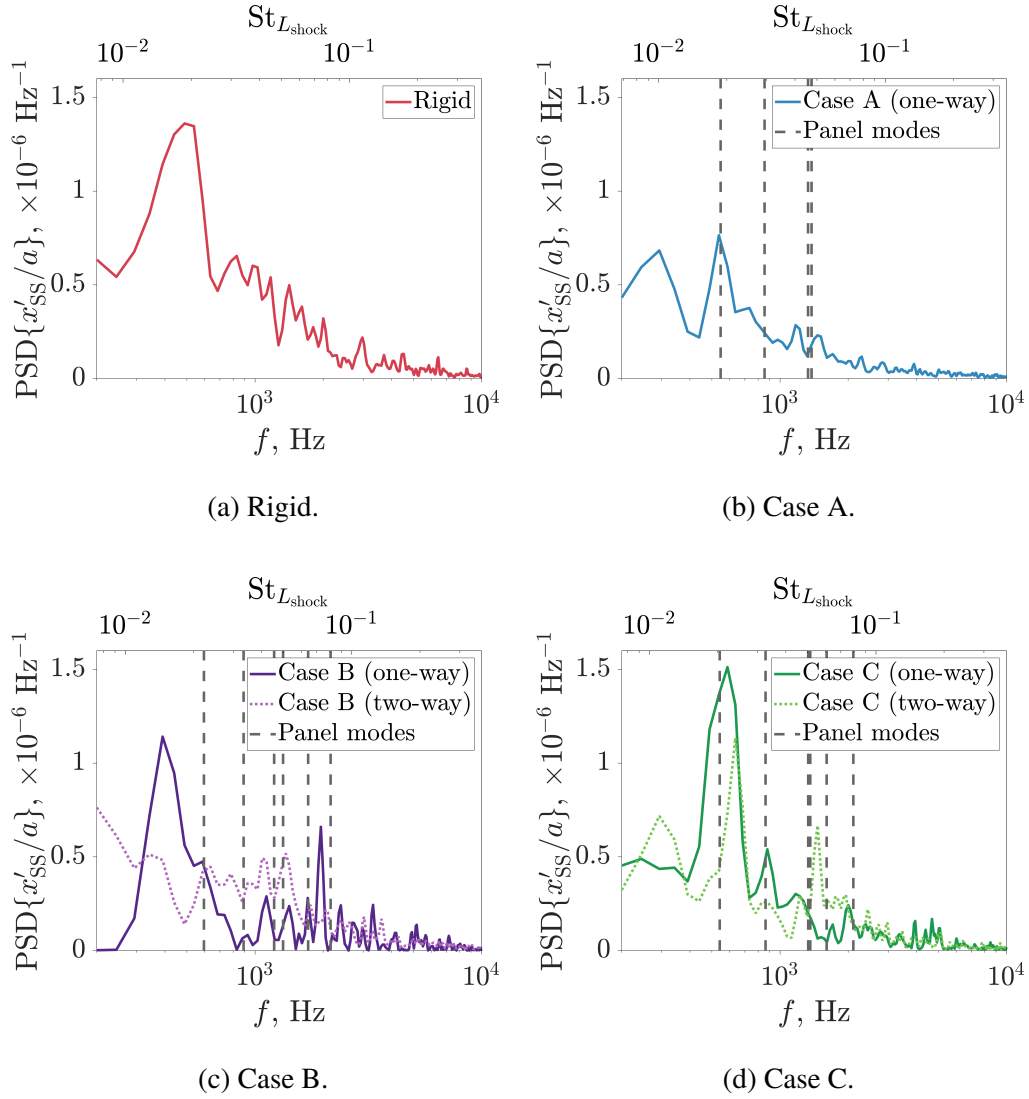


Figure 5.26: Power spectra of separation shock streamwise fluctuations at $z/\delta = 3.05$.

shock motion. For the one-way uncoupled mid-chord impingement case (Case A), the peak at $f_{(1,1)}$ is more than two times greater energy than that of the low-frequency STBLI unsteadiness. Under leading-edge impingement, the energy at $f_{(1,1)}$ is insignificant for the one-way uncoupled response but amplified by a factor of ten in the two-way coupled response for Case C. The increase in energy transfer from the vibration mode to the reattachment shock motion is coupled with a decrease in the low-frequency STBLI unsteadiness by a factor of three. Notably, this result contrasts with the trend observed in the separation shock spectra, where the energy at the first vibration mode and low-frequency STBLI unsteadiness underwent opposite changes after the transition to two-way coupling.

The spectrum for the one-way coupled response in Case B is primarily characterized by similar behavior to that of the rigid case, with the addition of a small degree of energy at the vibration modes. However, the spectrum does not change substantially when the interaction transitions to a two-way coupled response.

5.2.2.7 Spectral Analysis of Boundary Layer Thickness Unsteadiness

The power spectra of the visual boundary layer thickness fluctuations at $(x - x_{\text{imp}})/a = 0.25$ are displayed in Fig. 5.28. Similar to that of the separation and reattachment shock motion, boundary layer thickness unsteadiness in the rigid case consists of energy at the low-frequency STBLI unsteadiness range and subsequent harmonics. The relative strength of the low-frequency STBLI unsteadiness peak is not as significant as that in the separation shock spectrum.

In the one-way uncoupled compliant cases, the boundary layer thickness fluctuations exhibit measurable peaks at the fundamental vibration mode, and peaks at higher-order vibration modes and harmonics are present and stronger than at $f_{(1,1)}$ for Cases B and C. When Cases B and C transition to a two-way coupled interaction, the energy at the dominant vibration mode decreases.

5.2.3 Unsteady Pressure Fluctuations Downstream of Interaction

Power spectral density plots of the unsteady surface pressure fluctuations measured 13.2δ downstream of the panel trailing edge are presented in Figs. 5.29 & 5.30. The rigid and compliant cases shown in Fig. 5.29 correspond to flow conditions without shock impingement, whereas those in Fig. 5.30 represent STBLI conditions with shock impingement. The rigid No-STBLI case exhibits low-frequency energy content inherent to the freestream noise. However, when surface compliance is

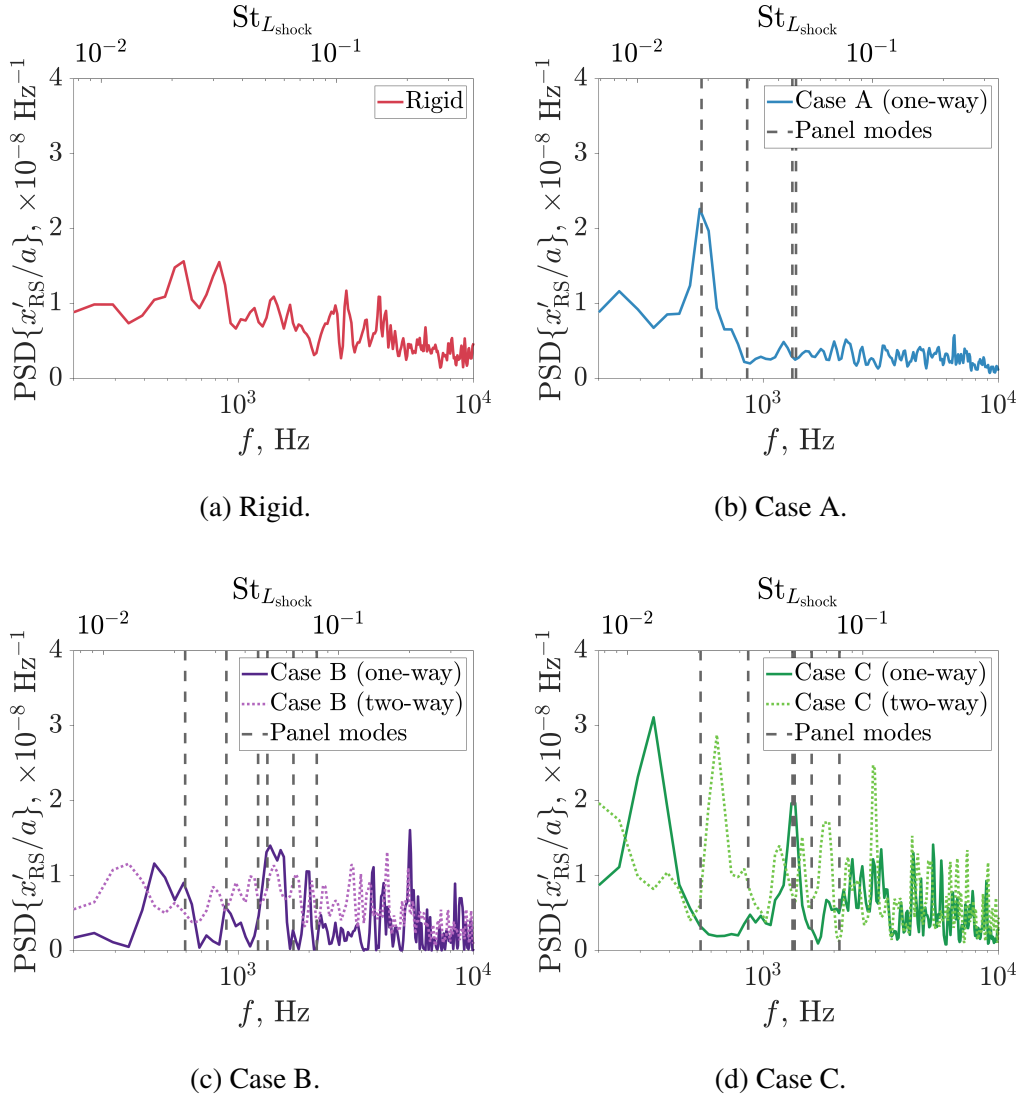


Figure 5.27: Power spectra of reattachment shock streamwise fluctuations at $z/\delta = 1.90$.

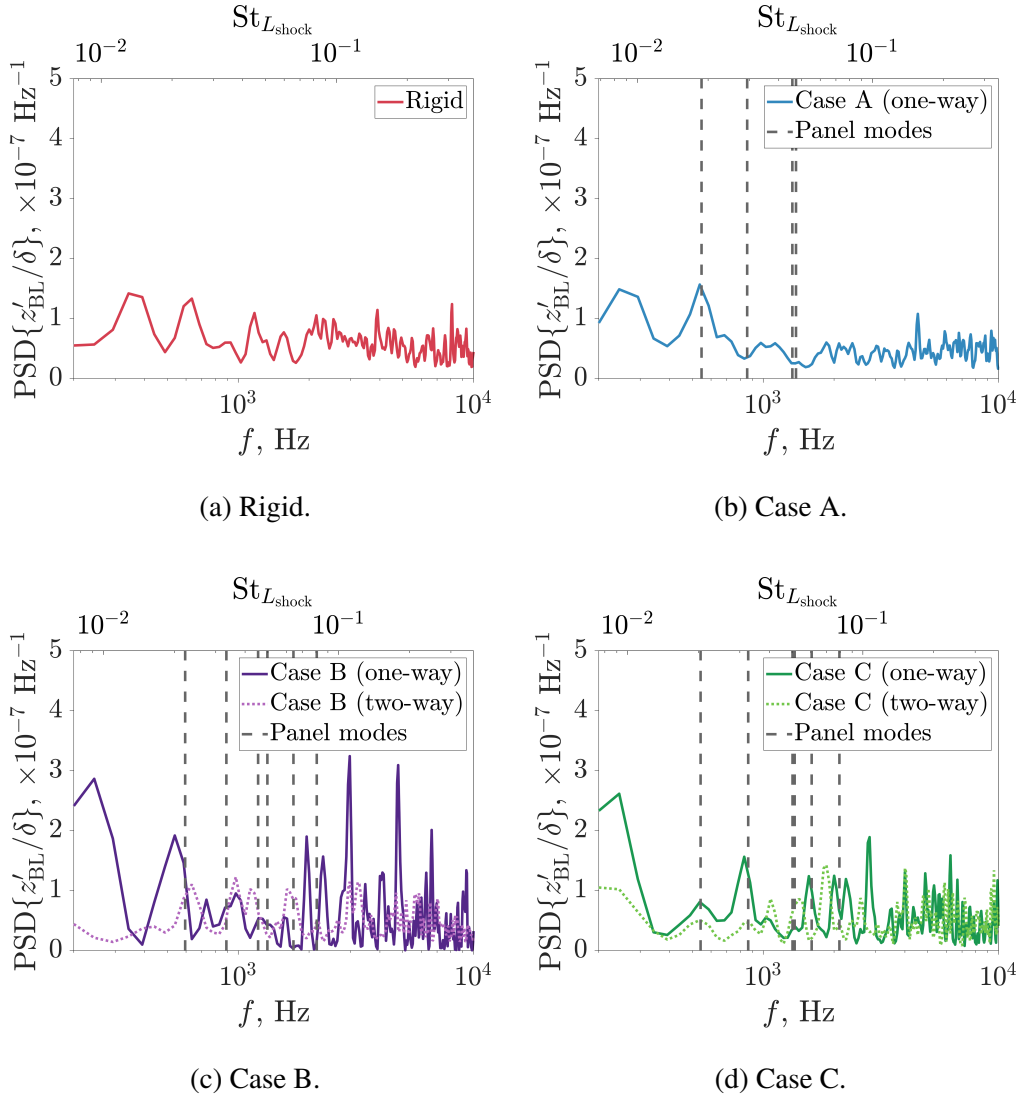


Figure 5.28: Power spectra of boundary layer thickness fluctuations at $(x - x_{\text{imp}})/a = 0.25$.

introduced, the spectral content of the pressure fluctuations becomes characterized by clear peaks at the vibration modes, with the most dominant peak at $f_{(1,1)}$. This signifies that pressure fluctuations induced by the surface motion and convected downstream are still observable 13.2δ away from the panel.

The power spectra of the STBLI case pressure fluctuations shown in Fig. 5.30 exhibit over an order of magnitude greater spectral energy than that of the No-STBLI cases. The rigid spectrum consists of low-frequency energy content and subsequent harmonics likely associated with pressure fluctuations induced by the low-frequency unsteadiness of the separation shock. Similar to that of the No-STBLI compliant case, all one-way uncoupled STBLI compliant cases have dominant peaks at the fundamental vibration mode, confirming that the periodic structural oscillations under STBLI loading also induce pressure fluctuations that convect downstream.

For the leading-edge impingement cases (Case B and C), when the interaction transitions to a two-way coupled response, there is a decrease in the strength of the pressure fluctuations at the vibration mode frequencies. This is consistent with the attenuation of the oscillation amplitudes and energy at mode (1,1) in the panel response under the two-way dominated coupling behavior. The transition also leads to an increase in spectral energy within the low-frequency STBLI unsteadiness range for Case C. Though, for Case B, the energy at the low-frequency STBLI unsteadiness range exhibits similar magnitudes for both one-way and two-way coupling, indicating that the coupling mechanism does not affect the strength of the convected low-frequency fluctuations induced by the separation shock in Case B.

Trial-averaged RMS values of the pressure fluctuations are shown in Fig. 5.31. The pressure fluctuations are normalized by the corresponding time-averaged pressure, and the horizontal error bars denote one standard deviation across repeated experiments. Despite the modifications of the spectral content due to the influence of surface compliance, all rigid and compliant STBLI cases exhibit negligible differences in the normalized RMS levels. The STBLI RMS values are centered around 2.1% of the time-averaged pressure magnitude, and the consistency between cases demonstrates that surface compliance has minimal impact on the overall amplitude of surface pressure fluctuations downstream of the panel.

In contrast, for the No-STBLI cases, the introduction of surface compliance leads to a measurable increase in fluctuation amplitude, with the normalized RMS rising by approximately 0.5% relative to the rigid case. Although limited in magnitude, this increase suggests that, in the absence of shock impingement, one-way uncoupled

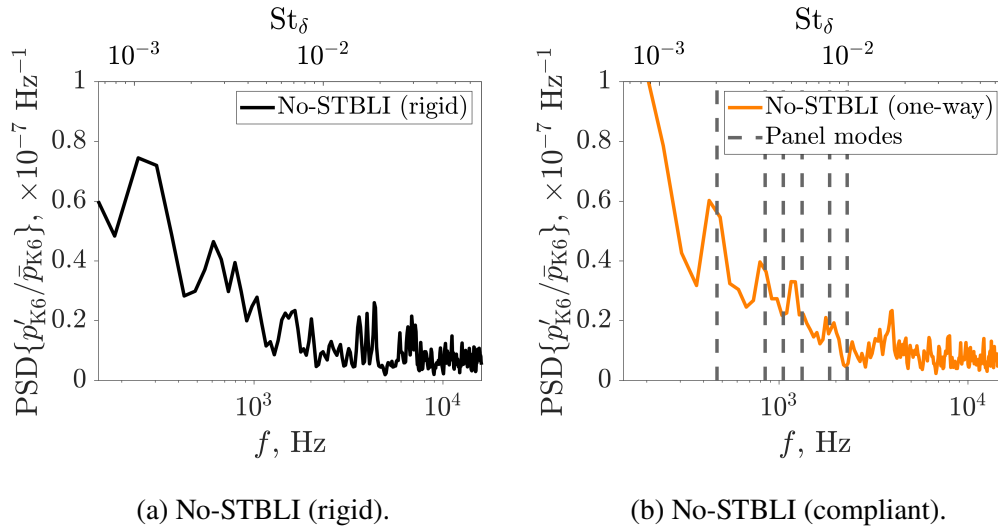


Figure 5.29: PSD of pressure fluctuations downstream of panel without shock impingement.

surface oscillations can influence the wall pressure RMS 13.2δ downstream of the panel.

5.2.4 FLDI Results

The spectral content of density fluctuations within the boundary layer and separation bubble is investigated through near-wall FLDI measurements. The FLDI beam pairs are oriented parallel to the wall, and the approximate measurement locations within the flow field are overlaid on a representative schlieren image in Fig. 5.32. Incoming boundary layer measurements are obtained upstream of the panel at approximately $(x - x_{\text{imp}})/a = -0.69$. Measurements inside the separation bubble are acquired near $(x - x_{\text{imp}})/a = -0.08$, and those in the boundary layer downstream of flow reattachment are collected at approximately $(x - x_{\text{imp}})/a = 0.16$.

The time histories of the phase differences measured in the rigid case experiments are shown in Fig. 5.33. Note that the signals in Fig. 5.33 are high-pass filtered at 1 kHz to reduce the spectral content related to the external, facility-induced low-frequency vibrations discussed in Section 4.1. Thus, the measurements are representative of the mid- and high-frequency spectral content of the density fluctuations. Notably, the fluctuation amplitude in the boundary layer after flow reattachment is greater than that of the incoming boundary layer, signifying that the interaction enhances the amplitude of the density fluctuations.

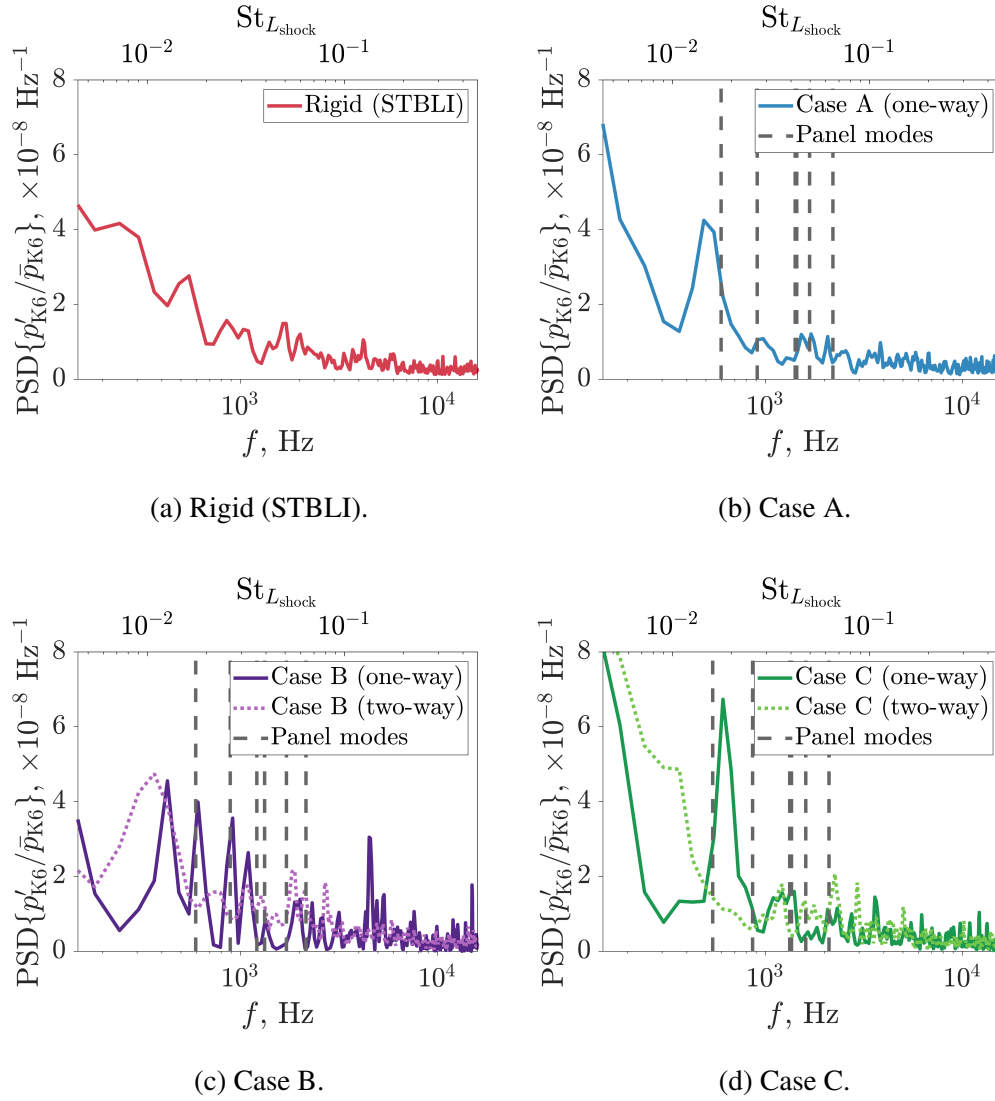


Figure 5.30: PSD of pressure fluctuations downstream of panel.

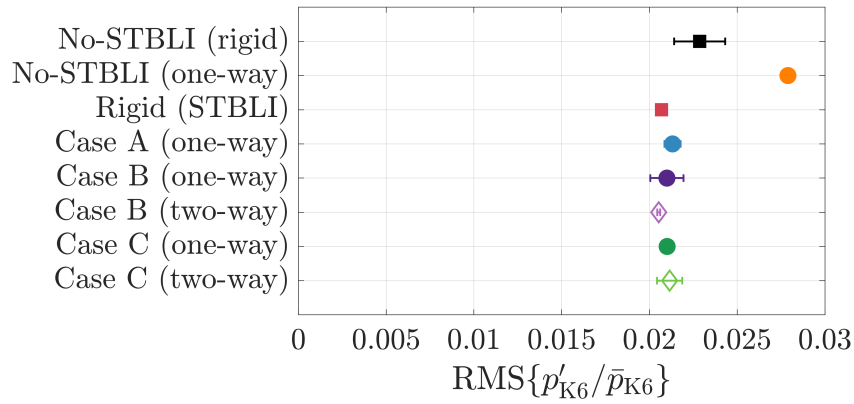


Figure 5.31: RMS of pressure fluctuations downstream of panel.

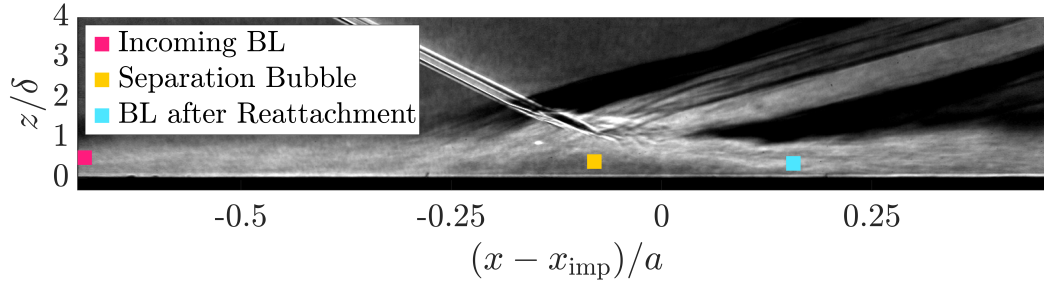


Figure 5.32: Representative FLDI beam pair locations overlaid on schlieren image.

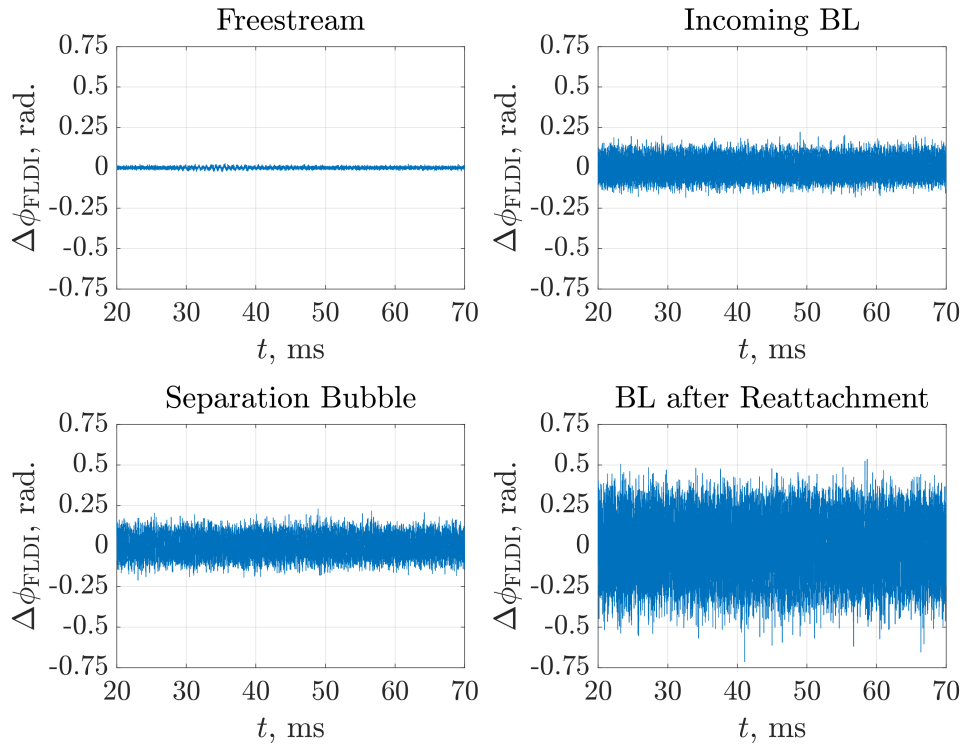


Figure 5.33: Time histories of FLDI response over rigid panel STBLI.

Power spectral density plots for rigid panel STBLI measurements are presented in Fig. 5.34. The incoming boundary layer exhibits a broadband peak centered near 120 kHz, which is equivalent to $St_{L_{\text{shock}}} = 3.6$ and $St_{\delta} = 0.52$ when non-dimensionalized by the interaction length scale and boundary layer thickness, respectively. The peak exceeds the spectral energy of the freestream at the same frequency range by over three orders of magnitude. Downstream of flow reattachment, the boundary layer spectral energy across all frequencies is about an order of magnitude greater compared to that of the incoming boundary layer. In addition, the dominant peak becomes narrower and shifts to a lower frequency of roughly 70 kHz, or $St_{L_{\text{shock}}} = 2.1$. These modifications to the spectral content indicate that the STBLI

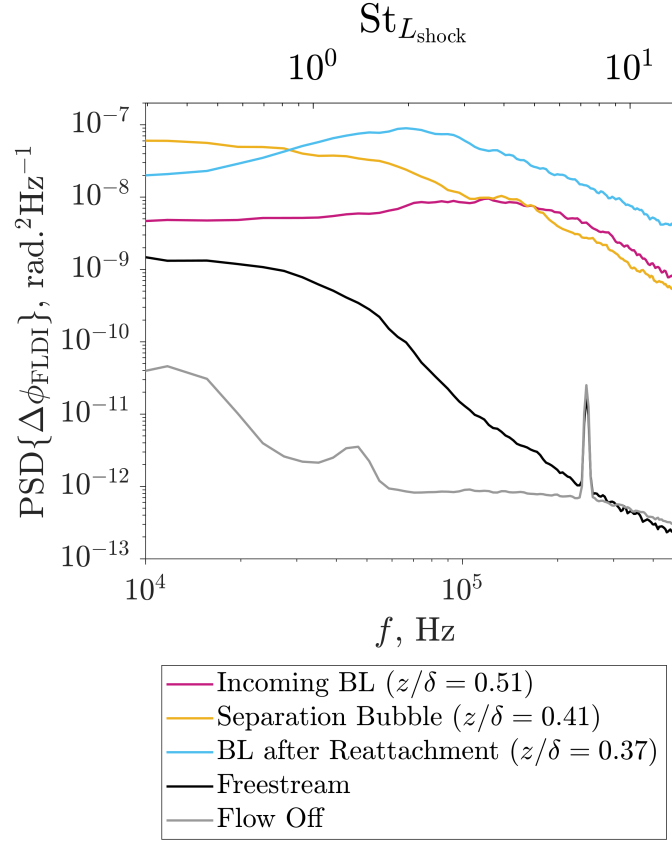


Figure 5.34: Power spectra of FLDI response over rigid panel STBLI.

substantially amplifies and redistributes the boundary layer energy relative to the upstream condition. The decrease in the boundary layer peak frequency downstream of the interaction is consistent with previously observed trends (Helm et al., 2021; Pasquariello et al., 2017; Priebe and Martín, 2012). Within the separation bubble, elevated spectral energy is observed in the mid-frequency band between 10 and 20 kHz, or $\text{St}_{L_{\text{shock}}} = 0.30 - 0.59$, which is likely associated with shear layer vortices convecting over the separation bubble (Dupont et al., 2006; Pasquariello et al., 2017).

A comparison of the rigid and compliant case spectral content in the boundary layer and separation bubble is shown in Figs. 5.35-5.37. In both cases, the overall spectral energy generally increases with FLDI measurement height above the wall. While this trend may, in part, reflect variations in the intensity of density fluctuations with wall height, it is also strongly influenced by the degree of FLDI beam clipping due to the model geometry. Because the experimental model consists of a wide flat plate, the converging and diverging FLDI beams become partially obstructed as the focal point moves closer to the wall. Lawson (2021) demonstrated that increased beam clipping

reduces the measured signal magnitude without altering the underlying frequency or phase content. Therefore, the distribution of spectral energy is unaffected by the degree of beam clipping, but comparisons of PSD magnitude across different wall heights should be interpreted with caution, as variations in spectral energy magnitude may primarily reflect changes in beam clipping rather than in flow physics. Although this signifies that maintaining a consistent measurement wall height across experiments is preferable for comparisons, the passively damped optics tables that supported the FLDI optical setup in these experiments make precise alignment of the FLDI beam pair at the same wall height challenging.

Positive panel deformations can also attenuate the FLDI signal by introducing an additional source of beam clipping. However, the vibration frequencies of the panel oscillations are at least an order of magnitude lower than the frequency range plotted in Figs. 5.35-5.37. As a result, any beam clipping effects induced by panel motion are expected to be confined primarily to the vibration mode frequencies and subsequent harmonics, with negligible influence on measurements of the mid- and high-frequency STBLI content. Moreover, the amplitudes of panel displacement are relatively small compared to the measurement wall heights.

In the separation bubble PSD in Fig. 5.36a, the compliant cases exhibit elevated mid-frequency content between 10 to 20 kHz similar to that of the rigid case. There are no measurable differences in the mid- and high-frequency spectral content for Cases B and C when the interaction transitions from a one-way uncoupled to two-way coupled fluid-structure mechanism. The peak magnitudes in Case B are comparable to that of the rigid case near the same wall height. Although the measurements for Case C are obtained at a relatively low wall height around $z/\delta = 0.17 - 0.18$, the magnitude of the spectral energy is comparable to that of Case B at a wall height of $z/\delta = 0.51$. On the other hand, the peak magnitude for Case A at a wall height of $z/\delta = 0.32$ is a factor of two lower than that of Case C. Because this measurement is taken higher above the wall than in Case C, this signifies the reduction in magnitude is a physical phenomenon associated with differences in impingement location and surface compliance effects rather than due to beam clipping. In the mid-chord impingement case, the separation bubble resides over the panel center and, therefore, is subjected to larger amplitude surface fluctuations compared to that in the rigid and leading-edge impingement cases. These surface oscillations may modify both the mean and instantaneous separation bubble volume, which would affect the development of shear layer vortices and the strength of the

recirculated flow. Therefore, density fluctuations originating from the convected shear layer vortices could be attenuated due to this change.

When the power spectra are pre-multiplied and normalized, as shown in Fig. 5.36b, the spectra generally collapse to the same distribution profile. Even though the spectra for Case C collapse within the mid-frequency range, a narrow peak at $St_{L_{\text{shock}}} \approx 2$ is more dominant than in the other cases. This frequency aligns with the peak frequency in the boundary layer downstream of flow reattachment and could reflect a greater influence of turbulent structures convecting over the separation bubble in Case C. It should be noted that the closeness of these measurements to the wall compared to that of the other separation bubble measurements may have contributed bias in the spectra due to beam clipping (Lawson, 2021).

Downstream of flow reattachment, surface compliance does not significantly modify the overall spectral energy distribution of the density fluctuations in the boundary layer compared to in the rigid case. In Fig. 5.37b, the spectra for each case collapse and follow a similar trend to that of the rigid case, where the peak boundary layer frequency is decreased relative to the incoming boundary layer spectrum. However, the magnitude of the spectral content is enhanced relative to that of the rigid case. Surface compliance leads to an amplification of the spectral energy by at least a factor of two or more relative to that of the rigid case near the same wall height. These results indicate that surface compliance primarily affects fluctuation intensity rather than the underlying spectral distribution of density fluctuations in the boundary layer after flow reattachment.

5.2.5 Cross-Correlation Analysis

Cross-correlation analysis is conducted to quantify both the degree of linear similarity and time delay between the unsteady flow dynamics and structural response. The value of the correlation coefficients, $\hat{R}_{s_1, s_2}(t_m)$, is calculated by taking the inner product of two signals, s_1 and s_2 , as a function of the time lag between them. For this analysis, the raw correlation coefficients are normalized by the square root of the product of the zero-lag autocorrelations, $\hat{R}_{s_1, s_1}(0)$ and $\hat{R}_{s_2, s_2}(0)$ (Eq. 5.1). This normalization bounds the correlation coefficient values between -1 and 1 , where values with magnitude approaching 1 indicate strong linear similarity between the two signals at the specified time lag. Typically, correlation coefficient magnitudes around 0.5 correspond to a moderate degree of similarity, magnitudes around 0.3 correspond to weak similarity, and magnitudes less than 0.1 indicate largely in-

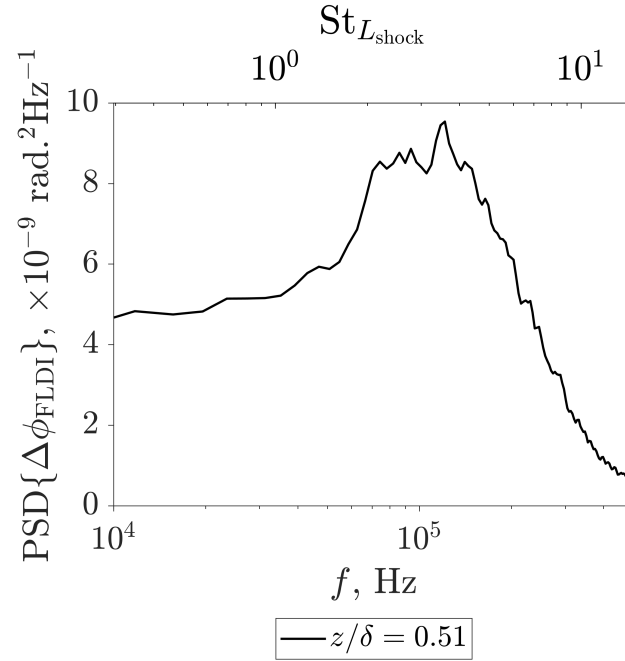


Figure 5.35: Power spectrum of FLDI response in incoming boundary layer.

significant similarity. The sign of the coefficient reflects whether the signals are positively or negatively correlated.

$$R_{s_1, s_2}(t_m) = \frac{\hat{R}_{s_1, s_2}(t_m)}{\sqrt{\hat{R}_{s_1, s_1}(0)\hat{R}_{s_2, s_2}(0)}} \quad (5.1)$$

5.2.5.1 Coupling between Panel and Shock Motion

The cross-correlation between the out-of-plane panel oscillations at a point excited by all of the lowest-order panel modes, $z_{\text{nodeless}}(t)$, and the tracked separation and reattachment shock motion at a representative wall-normal height, $x_{\text{SS}}(z/\delta = 3.05, t)$ and $x_{\text{RS}}(z/\delta = 1.90, t)$, are compared for each compliant case in Fig. 5.38. Note that the synchronized shock signals are low-pass filtered at $f_s^{\text{photogrammetry}} = 10$ kHz and downsampled by $f_s^{\text{schlieren}} / f_s^{\text{photogrammetry}} = 100 \text{ kHz} / 10 \text{ kHz} = 10$ to match the sampling rate of the photogrammetry measurements.

For Case A, both the separation and reattachment shocks exhibit a nearly sinusoidal cross-correlation with the panel displacement, where the period of correlation coefficient oscillation is equal to that of the vibration mode (1,1), $T_{(1,1)} = 1/f_{(1,1)}$. The amplitude of the cross-correlation varies between 0.1 and 0.2, which indicates a

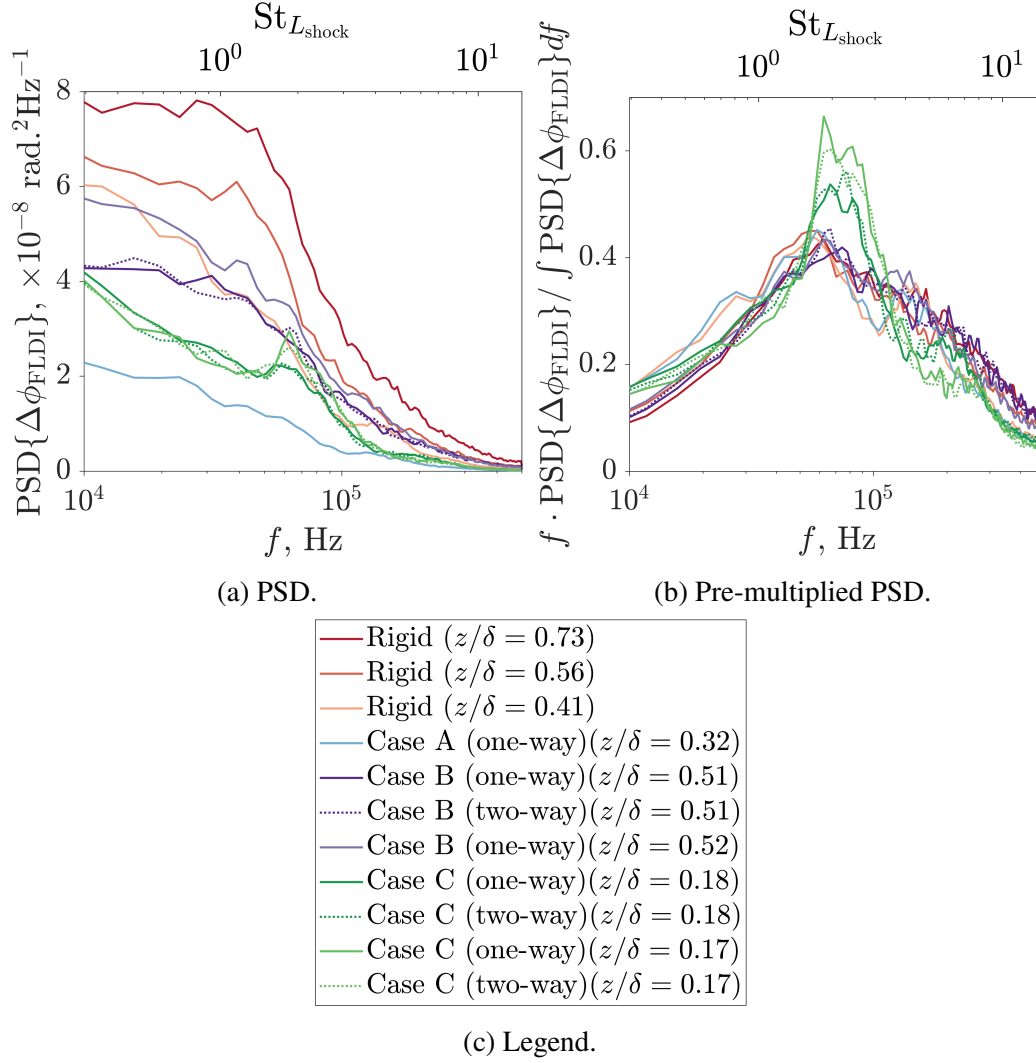


Figure 5.36: Power spectra of FLDI response in separation bubble.

consistently weak level of linear coupling between the shock dynamics and structural response.

Because the cross-correlation is strongly periodic, no single dominant global maximum or minimum peak clearly identifies a unique lead-lag relationship. Instead, peaks within half a period of the mode (1,1) oscillation, $-T_{(1,1)}/2 \leq t_{\text{lag}} \leq T_{(1,1)}/2$, can inform the phase relationship between the shock and panel motion. For the separation shock, the closest negative peak to zero lag occurs at a positive time lag, which signifies that, at $f_{(1,1)}$, the separation shock reaches its most upstream position before the panel attains its maximum positive displacement. Although this does not imply causality, it demonstrates that the phase of the separation shock motion leads that of the panel oscillations at $f_{(1,1)}$. In contrast, the reattachment shock exhibits its

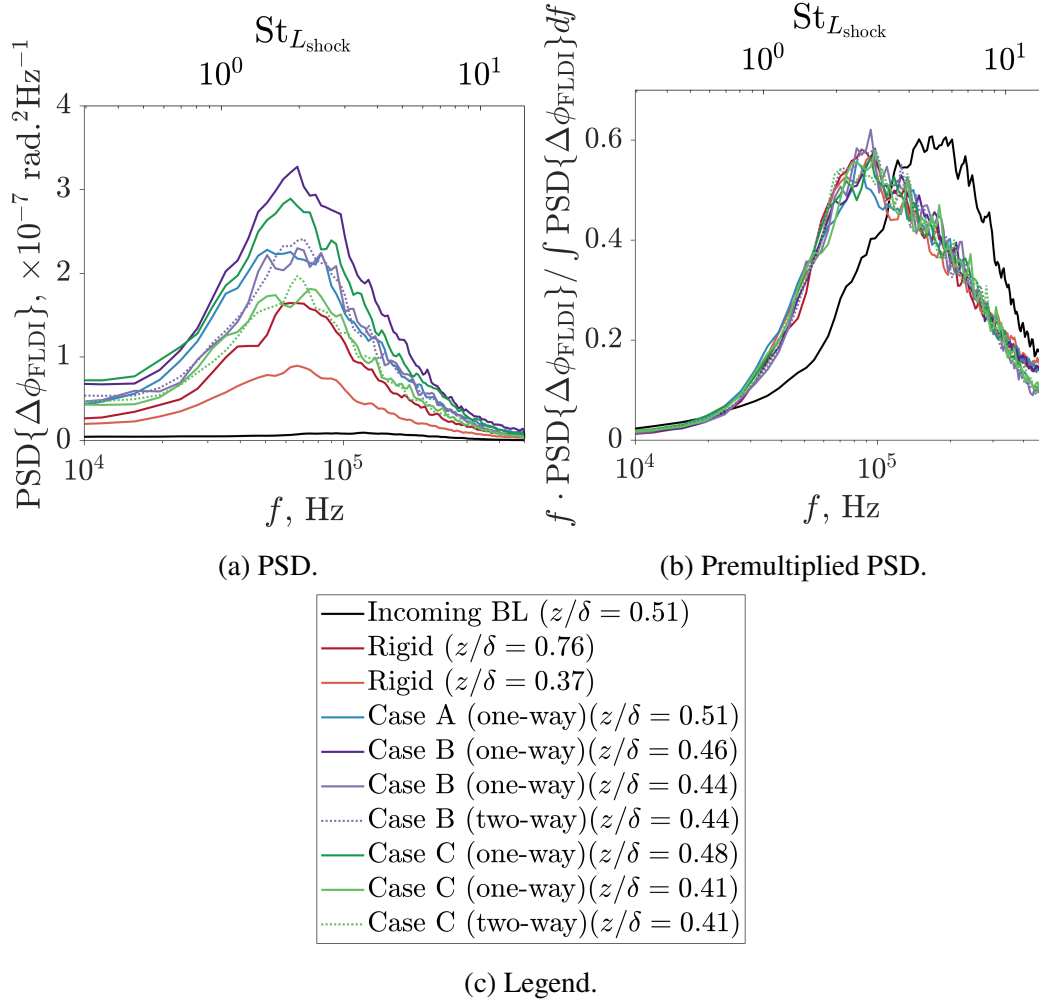


Figure 5.37: Power spectra of FLDI response in boundary layer after flow reattachment.

nearest negative peak at a negative time lag, which means that the shock reaches its most upstream position after the panel reaches its maximum positive displacement. Collectively, these results reveal a contrast in the phase behavior between the two shocks. Within a single panel oscillation cycle, the separation and reattachment shocks are not synchronized to the same phase of the panel motion but rather exhibit a nearly equal and opposite phase delay relative to the panel motion. The consistency of the phase relationship supports the conclusion that the dominant mode (1,1) panel oscillations in this one-way uncoupled interaction force the shocks to respond at the same frequency due to the imposed structural dynamics.

The one-way uncoupled responses in Cases B and C exhibit a similar periodic structure of the cross-correlation between the panel and shock motion at the fundamental vibration mode. However, the magnitudes of the peak correlation coefficient are

noticeably lower at approximately 0.15 or less. This indicates that the coupling between the panel and shock motion is weaker under leading-edge impingement compared to that of mid-chord impingement. Moreover, the reattachment shock motion generally exhibits slightly greater peak correlation coefficient magnitudes compared to that of the separation shock, signifying that the reattachment shock is more strongly coupled to the panel motion than the separation shock is. These observations are consistent with the flow structure under leading-edge impingement, where the separation point resides off the compliant panel surface and receives less direct influence from the structural dynamics. Instead, the state of the panel is communicated to the separation point through separation bubble motion and modification of the separated shear layer structure.

When Cases B and C transition to a two-way coupled interaction, the cross-correlations between the panel and shock motion change considerably. Rather than a periodic structure at the fundamental vibration mode, the separation shock exhibits fluctuations in correlation at multiple higher-order vibration mode frequencies. Near zero time lag, the separation shock exhibits a substantially larger correlation to the panel motion, with a correlation coefficient magnitude between 0.2 to 0.3. The sign of this peak is negative, signifying that phase of the separation shock motion is synchronized to that of the panel motion, such that when the panel is maximally deflected, the separation shock similarly reaches a maximum upstream extent. Despite the significant attenuation of the panel oscillation amplitude in the two-way coupled response compared to that of the one-way uncoupled response, the two-way feedback mechanism strengthens the correlation between the panel and separation shock motion.

In contrast, the reattachment shock is largely uncorrelated with the panel motion under two-way coupling. Pressure fluctuations associated with the separation shock foot likely exert a greater force on the panel than those from the reattachment shock, leading to strengthened coupling between the separation shock and panel motion.

5.2.5.2 Cross-Correlation between Panel Motion and Schlieren Footage

As a complement to the cross-correlation analysis between the panel and tracked shock motion, the cross-correlation between the panel motion, $z_{\text{nodeless}}(t)$, and schlieren pixel intensities, $I(x, z, t)$, is examined to visualize the regions in the flow field that are most correlated to the structural oscillations. Colormaps of the maximum negative peak correlation coefficient, $R_{z_{\text{nodeless}}, I}$, within $-T_{(1,1)}/2 \leq t_{\text{lag}} \leq$

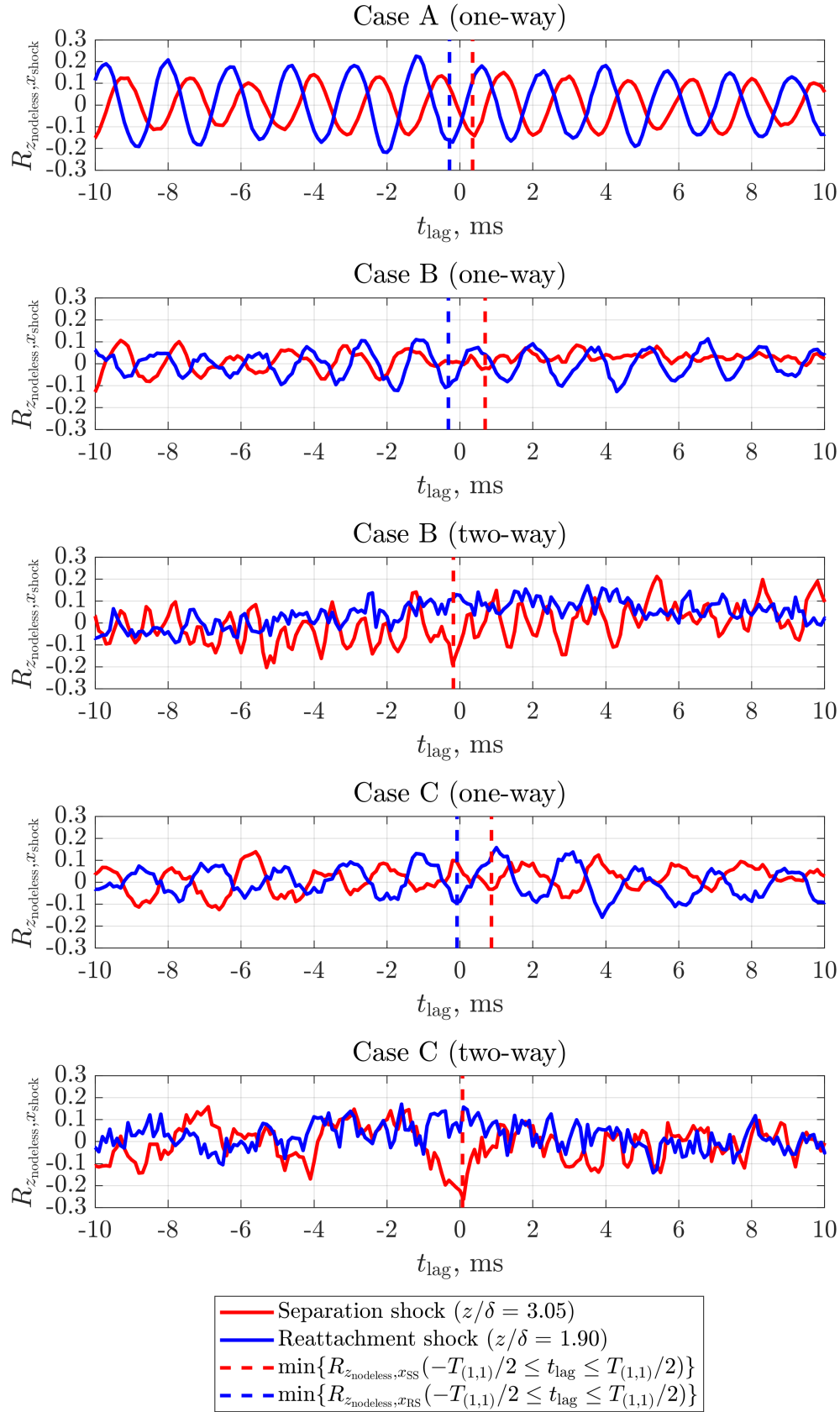


Figure 5.38: Cross correlation between panel oscillations and shock motion.

$T_{(1,1)}/2$ are overlaid on schlieren snapshots for $R_{z_{\text{nodeless}},I} < -0.15$ for each compliant case in Fig. 5.39.

The magnitudes and regions of peak correlation between the panel motion and schlieren pixel intensities are largely consistent with that of the tracked shock motion cross-correlations in Fig. 5.38. For the one-way uncoupled interaction of Case A, the separation and reattachment shock regions exhibit nearly sinusoidal behavior at the fundamental vibration mode frequency. The magnitudes of the peak correlation coefficients reach up to 0.2, confirming the weak degree of coupling between the panel and shock motion.

Unlike that of Case A, the one-way uncoupled interactions under leading-edge impingement do not exhibit any significant correlation with the panel motion at the separation and reattachment shock fronts. A region downstream of the separation shock front in Case B has a weak degree of correlation with the panel. When the interactions transition to a two-way coupled response, the coupling between the panel and flow is strengthened in the separation shock region, which has correlation coefficient magnitudes up to about 0.25.

The time lags, t_{lag} , of the maximum negative correlation coefficient magnitudes within half a period of $T_{(1,1)}$ are overlaid on a schlieren snapshot in Fig. 5.40 for each compliant case. Only the time lags for $R_{z_{\text{nodeless}},I} < -0.15$ are visualized. Time lags represented by a red color correspond to a positive delay, signifying that the phase of the fluctuations in the flow leads that of the panel motion. Blue represents negative time lags, and yellow corresponds to zero time lag.

The opposite signs of peak time lag in the separation and reattachment region for Case A in Fig. 5.40a align with the previous shock tracking cross-correlation results. Furthermore, the colormap reveals that the phase of the separation and reattachment shocks does not significantly vary along the shock. The uniform correlation and phase signify that the shocks are displaced as a coherent structure as the panel oscillates. The spread of time lags in the separation shock region for the two-way coupled Cases B and C also display a similar trend, and the close to zero magnitudes indicate that the separation shock is synchronized to the phase of the panel motion.

5.2.5.3 Correlation between Separation and Reattachment Shock Motion

The cross-correlations between the streamwise fluctuations of the separation shock, $x_{\text{SS}}(z/\delta = 3.05, t)$, and reattachment shock, $x_{\text{RS}}(z/\delta = 1.90, t)$ for each case are

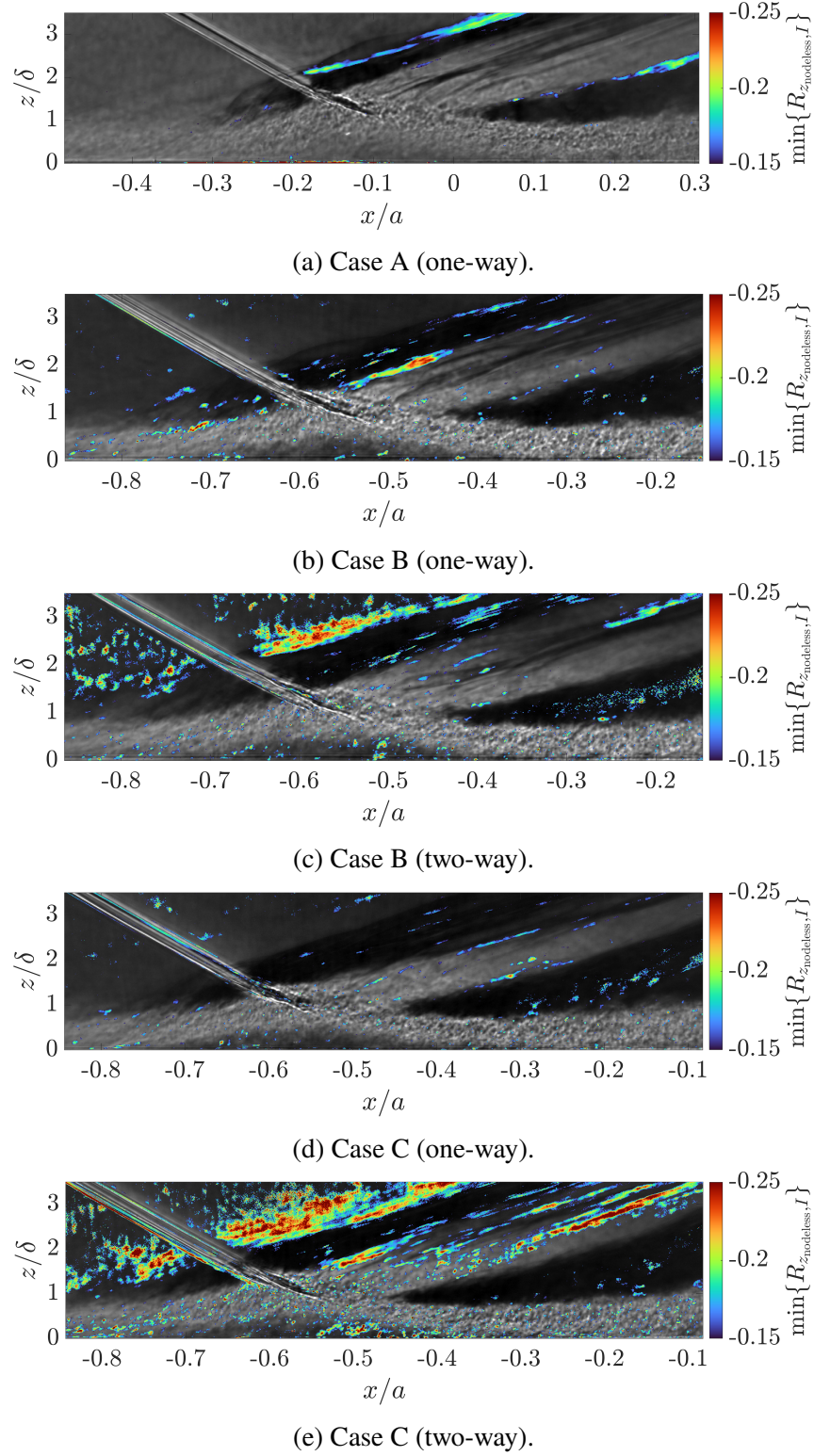
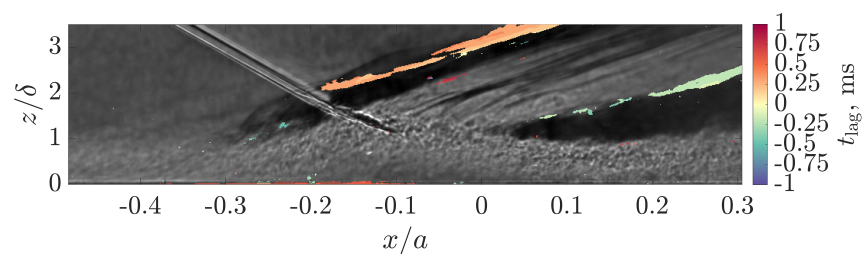
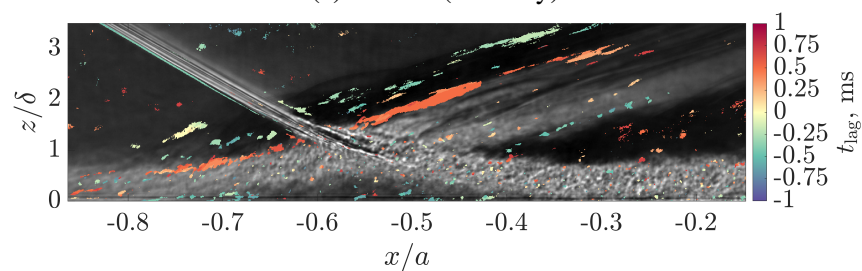


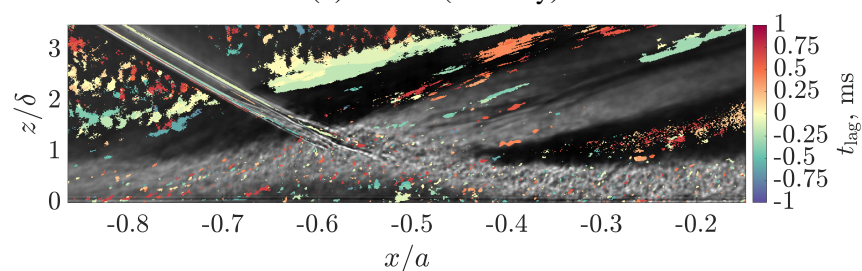
Figure 5.39: Peak negative correlation coefficient within half a period of $f_{(1,1)}$ from the cross-correlation between the panel motion, z_{nodeless} , and schlieren pixel intensity, $I(x, z)$. The correlation coefficient color-map is visualized for $R_{z_{\text{nodeless}}, I} < -0.15$ and overlaid on a schlieren snapshot.



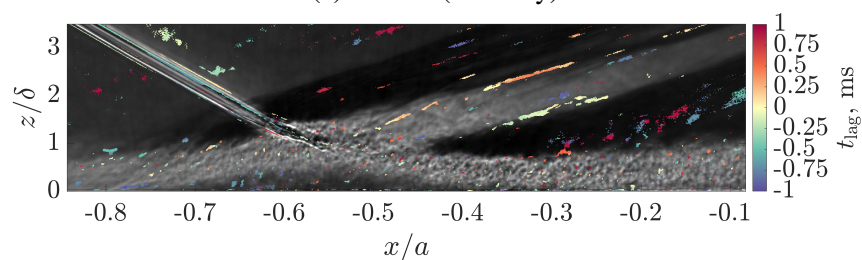
(a) Case A (one-way).



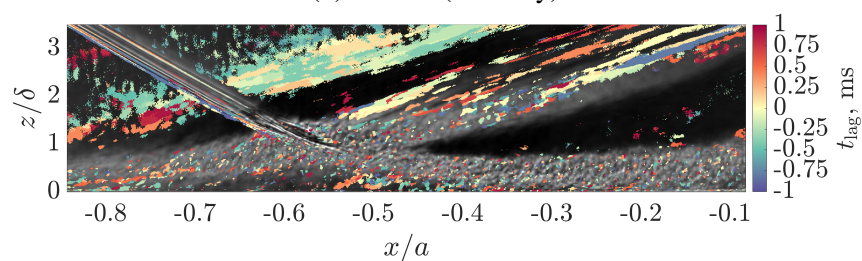
(b) Case B (one-way).



(c) Case B (two-way).



(d) Case C (one-way).



(e) Case C (two-way).

Figure 5.40: Time lag of peak negative correlation coefficient within half a period of $f_{(1,1)}$ from the cross-correlation between the panel motion, z_{nodeless} , and schlieren pixel intensity, $I(x, z)$. The time lag color-map is visualized for $R_{z_{\text{nodeless}}, I} < -0.15$ and overlaid on a schlieren snapshot.

shown in Fig. 5.41. The original, non-downsampled shock position signals are used to preserve the full temporal resolution of the shock dynamics. Across all cases, the cross-correlation exhibits the largest peak magnitude near zero time-lag, occurring at a small positive delay. The peak correlation coefficients are negative, signifying that the shock motions are anti-correlated. That is, when one shock shifts upstream, the other tends to shift downstream. For the rigid case, this behavior is consistent with previous literature and is driven by the breathing motion of the separation bubble inherent to the STBLI unsteadiness (Wu and Martín, 2008). As the separation bubble grows and shrinks, the separation and reattachment points translate in opposite streamwise directions, which is subsequently transferred to opposite motions in the separation and reattachment shocks. The cross-correlations of the compliant cases confirm that this fundamental anti-correlated behavior persists even when the interaction is one-way or two-way coupled.

Moreover, the positive time lag indicates that the reattachment shock motion precedes that of the separation shock. This observation is similarly reported by Wu and Martín (2008) and Varigonda and Narayanaswamy (2023) in rigid and compliant STBLI studies. The time lag corresponding to the largest negative peak in each case is determined using a peak-detection algorithm applied to the cross-correlation signal. A seven-point Gaussian function is fit to the negative peak and six neighboring points to improve the precision of time lag estimate beyond the sampling resolution. The resulting time lags are non-dimensionalized using the freestream velocity, u_∞ , and representative interaction length scale, L_{shock} , for each case. The trial-averaged time lags are presented in Fig. 5.42, where the horizontal error bars denote one standard deviation across repeated experiments. The non-dimensionalized time lags exhibit close agreement across all cases, collapsing to approximately $t_{\text{lag}}u_\infty/L_{\text{shock}} \approx 1$. This scaling indicates that the characteristic delay between the separation and reattachment shock motion is on the order of the time required for a disturbance convecting at the freestream velocity to traverse the distance between the separation and reattachment shocks. The consistency in time lag between cases indicates that the fluid-structure coupling mechanism does not fundamentally affect the communication between the shocks.

5.2.6 Phase Analysis of Shock Motion

Cross-correlation analysis in Section 5.2.5 revealed that the separation and reattachment shocks tend to fluctuate with periodic behavior relative to the panel motion in the one-way uncoupled cases. To further characterize the phase relationship

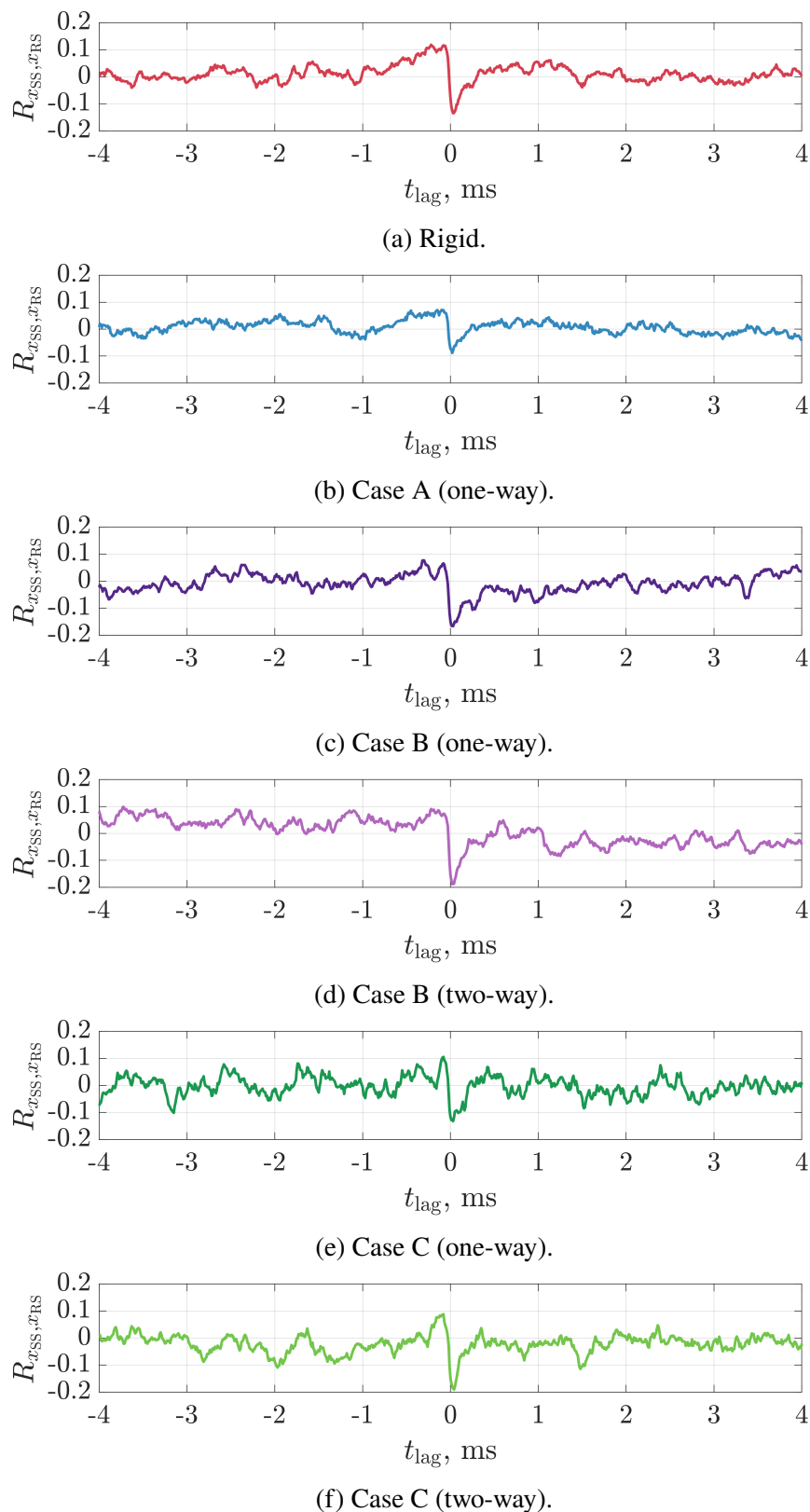


Figure 5.41: Cross-correlation between separation and reattachment shock stream-wise fluctuations, $x'_{SS}(z/\delta = 3.05)$ and $x'_{RS}(z/\delta = 1.90)$.

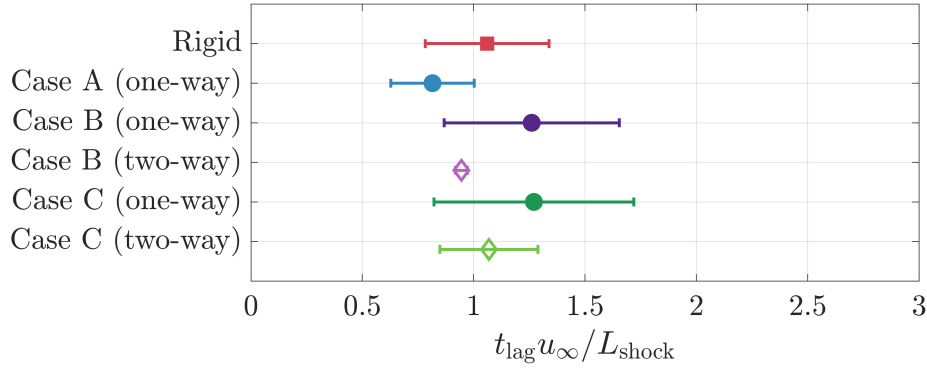


Figure 5.42: Normalized time lag of negative peak from cross-correlation between separation and reattachment shock motion.

between the shocks at the fundamental vibration frequency, phase analysis is performed. Specifically, the time-averaged phase difference between the separation and reattachment shock motion at $f_{(1,1)}$, denoted $\overline{\Delta\phi_{\text{SS,RS}}}$, is estimated using the cross-power spectral density.

POD analysis discussed in Section 5.2.2 showed that the first POD mode dominates the separation shock dynamics, such that the shock translates upstream and downstream as a coherent structure with a uniform phase of motion. The leading POD mode temporal coefficient, $\psi_{1\text{ss}}(t)$, is subsequently used to represent the separation shock motion in the cross-power spectral density estimate. The reattachment shock motion is represented by the streamwise position of the shock at a representative vertical height, $x_{\text{RS}}(z/\delta = 1.90, t)$.

The time-averaged phase difference between the two signals is obtained by computing the principal argument of the complex-valued cross-spectral density between $\psi_{1\text{ss}}(t)$ and $x_{\text{RS}}(z/\delta = 1.90, t)$ at the frequency $f_{1,1}$. The resulting phase difference is wrapped within the interval $[-\pi, \pi]$ radians. A phase difference of $\Delta\phi_{\text{SS,RS}} = 0$ rad. indicates the shock motions are synchronized, whereas a phase difference of $\Delta\phi_{\text{SS,RS}} = \pm\pi$ rad. corresponds to perfectly out of phase shock motion. Positive phase differences represent that the phase of the separation shock leads that of the reattachment shock, while negative phase differences indicate that it lags. Because the phase difference is wrapped within the range of 2π rad., the sign of the phase difference does not imply that one signal leads or lags the other.

The time-averaged phase differences obtained from repeated experiments for Cases A are presented in Figs. 5.43. On average, the phase of the separation shock leads that of the reattachment shock by approximately one quarter of a panel oscillation

cycle. This means that under mid-chord impingement, the one-way uncoupled panel oscillations at $f_{(1,1)}$ typically shift the separation shock upstream $\pi/2$ rad. before the reattachment shock.

Two complementary metrics are defined to quantify and compare the degree of phase alignment between the separation and reattachment shock motion. The first metric, $\sigma_{\overline{\Delta\phi}}$, represents the standard deviation of the time-averaged phase differences across repeated experiments and is defined in Eq. 5.2. This metric quantifies the spread in the estimated time-averaged phase difference between independent, repeated experiments. The operator, $\text{Arg}()$, in Eq. 5.2 denotes the principal argument, computed using the MATLAB function, `angle()`. Additionally, the operator, $\langle \cdot \rangle_{\text{circ}}$, represents a trial-averaged quantity obtained using the circular mean (Eq. 5.3). A low value of $\sigma_{\overline{\Delta\phi}}$ indicates that the time-averaged phase difference is consistent across repeated experiments, whereas larger values signify significant shot-to-shot variability in the preferred phase relationship.

$$\sigma_{\overline{\Delta\phi}} = \sqrt{\frac{\sum_{j=1}^{N_{\text{trials}}} \text{Arg}\left(\exp\left(i(\overline{\Delta\phi}_j - \langle \overline{\Delta\phi} \rangle_{\text{circ}})\right)\right)^2}{N_{\text{trials}} - 1}} \quad (5.2)$$

$$\langle \overline{\Delta\phi} \rangle_{\text{circ}} = \text{Arg}\left(\sum_{j=1}^{N_{\text{trials}}} \exp(i \cdot \overline{\Delta\phi}_j)\right) \quad (5.3)$$

The second metric, $\sigma_{\Delta\phi(t)}$, characterizes the temporal standard deviation of the instantaneous phase difference within a single experiment and is defined in Eq. 5.4. This quantity measures the degree to which the two signals remain phase-locked over the duration of test time. The instantaneous phase difference between the separation and reattachment shock motion is defined as $\Delta\phi_{\text{SS,RS}}(t) = \phi_{\text{SS}}(t) - \phi_{\text{RS}}(t)$, where the phases are obtained from the analytic signals of the shock position time series. The signals are first bandpass filtered within ± 50 Hz of $f_{(1,1)}$ to isolate the phase associated with the dominant vibration mode frequency. The analytic signal for each shock position time series is obtained using the discrete Hilbert transform via the MATLAB function, `hilbert()`, which yields a complex signal containing the instantaneous amplitude and phase. The instantaneous phase is then computed by taking the principal argument of the analytic signal. A low value of $\sigma_{\Delta\phi(t)}$ indicates that the phase difference remains relatively constant over time, reflecting strong phase locking between the two signals, whereas larger values signify weaker phase

locking. The quantity $\langle \sigma_{\Delta\phi(t)} \rangle$ denotes the trial-averaged value of this metric across repeated experiments.

$$\sigma_{\Delta\phi(t)} = \sqrt{\frac{\sum_{j=1}^{N_{\text{samples}}} \text{Arg} \left(\exp \left(i(\Delta\phi(t_j) - \overline{\Delta\phi}) \right) \right)^2}{N_{\text{samples}} - 1}} \quad (5.4)$$

The values of both standard deviation metrics are summarized in Fig. 5.44. Across the repeated experiments for Case A, the time-averaged phase differences exhibit a narrow spread, with $\sigma_{\overline{\Delta\phi}} < \pi/8$ rad. This low level of variability highlights a well-defined trend in preferred average phase alignment of the shock motion between repeated experiments. Though, the instantaneous phase difference exhibits greater variability, with $\sigma_{\Delta\phi(t)} \approx 3\pi/8$ rad. This indicates that the shocks are not perfectly phase synchronized over time, such that the shock motions only exhibit a weak degree of phase locking.

By comparison, the one-way uncoupled interactions under leading-edge impingement exhibit stronger phase locking behavior, with $\sigma_{\Delta\phi(t)} \approx 5\pi/16$ rad. for Case B and $\sigma_{\Delta\phi(t)} \approx \pi/4$ rad. for Case C. However, the time-averaged phase differences vary significantly between repeated experiments, where $\sigma_{\overline{\Delta\phi}} > \pi/2$ rad. for both cases. Although the one-way uncoupled panel response tends to better phase lock the separation and reattachment motion at the fundamental vibration mode under leading-edge impingement compared to under mid-chord impingement, the phase alignment is not repeatable between experiments.

In addition, the phase differences for the two-way coupled interactions in Cases B and C vary significantly both over time for a single experiment as well as between repeated experiments. This is consistent with the reduction in spectral content at the fundamental vibration mode in the panel response, which decreases the strength of the mode (1,1) oscillations and subsequently lowers the degree of phase lock between the separation and reattachment shock motion.

The comparatively more consistent preferred phase alignment observed for Case A is likely attributed to the mid-chord shock impingement location, where the compliant panel directly influences both the separation and reattachment points. In Cases B and C, the separation point lies upstream of the compliant surface, which reduces the direct influence of panel deformation on the separation shock.

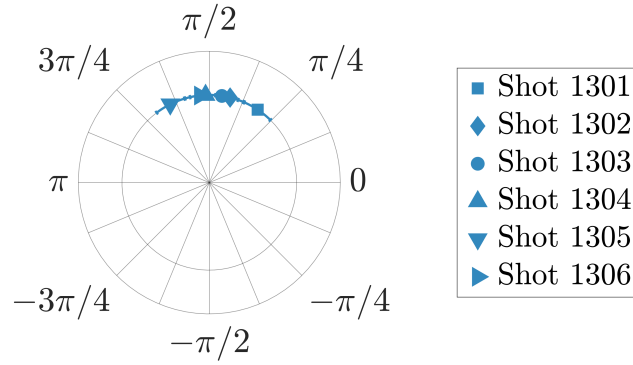


Figure 5.43: Phase difference between separation and reattachment shock motion at $f_{1,1}$ for Case A. The leading POD temporal coefficient, $\psi_{1ss}(t)$, represents the separation shock motion, and the streamwise shock position at $z/\delta = 1.90$ represents the reattachment shock motion in this cross-spectral phase analysis.

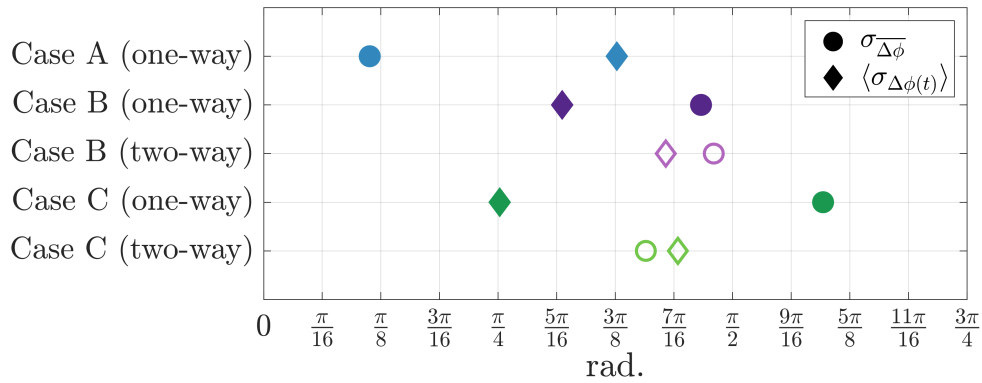


Figure 5.44: Temporal and shot-to-shot standard deviation of phase difference between separation and reattachment shock motion at $f_{(1,1)}$.

CONCLUSIONS AND FUTURE WORK

This chapter summarizes the major achievements of this work, which consist of advancements to the measurements obtained in compliant STBLIs and contributions to the understanding of how surface compliance alters STBLI dynamics and the extent that impingement location, cavity pressure, and coupling mechanism influence the behavior of the flow and structural responses. Recommendations for further research to complement this study are proposed.

6.1 Conclusions

In this work, multiple configurations of an impinging STBLI over both rigid and compliant surfaces were experimentally investigated in the Caltech Ludwig Tube. These experiments address key knowledge gaps in the current understanding of how interaction parameters and coupling mechanisms modify the transfer of energy between the flow and structure.

6.1.1 Contributions to Simultaneous Flow and Structure Measurements

To the best of the author's knowledge, this is the first application of simultaneous photogrammetry, colinear FLDI, and high-speed schlieren measurements to characterize the structural and flow responses in compliant surface STBLIs. Due to the complexity of fluid-structure coupling mechanisms, synchronized measurements are necessary to fully capture the unsteady feedback mechanisms between the panel and flow field. The simultaneous optical measurements implemented in these experiments proved successful in characterizing the structural dynamics, tracking unsteady flow features, and correlating panel oscillations with responses from the flow.

6.1.2 Contributions to Understanding Effects of Surface Compliance on STBLI Dynamics

Time-resolved FLDI, pressure transducer, and schlieren feature tracking measurements were used to investigate the influence of surface compliance on STBLI dynamics. RMS analysis of the shock motion and pressure fluctuations revealed that the introduction of surface compliance did not significantly modify the streamwise

extent of the separation and reattachment shock motion or the amplitudes of pressure fluctuations 13.2δ downstream of the panel compared to that of the rigid case. Modal decompositions of the separation shock motion demonstrated that surface compliance had minimal effect on the global structure of the shock motion. In all cases, the separation shock tended to shift upstream and downstream as a coherent structure with a uniform phase of motion.

The separation bubble over a compliant panel exhibited a breathing motion similar to that observed in the rigid case. Cross-correlation analysis between the separation and reattachment shock motions in all compliant cases showed that the shocks tended to move in opposing directions led by the reattachment shock. The time delay between the reattachment and separation shock motion was found to be approximately one when scaled by the freestream velocity and shock distance. This behavior is consistent with that of the rigid case and highlights the marginal influence surface compliance had on the global structure of the flow field and STBLI unsteadiness mechanism.

Despite those similarities, surface compliance played a major role in modifying the spectral content and transfer of energy within the flow field. Structural oscillations induced pressure fluctuations and promoted shock unsteadiness at the vibration mode frequencies. The characteristic low-frequency STBLI unsteadiness was attenuated to varying degrees depending on the interaction parameters. Surface compliance also further enhanced the overall magnitude of the high-frequency density fluctuations in the boundary layer downstream of flow reattachment compared to that in the rigid case. Yet, in both the separation bubble and boundary layer, surface compliance did not significantly alter the distribution of the mid- and high-frequency spectral energy.

While some fluctuation amplitudes did not exhibit strong differences when surface compliance was introduced, such as the shock and downstream pressure fluctuations, other regions of the flow did exhibit modifications to the flow field fluctuations, specifically in the turbulent boundary layer and separation bubble. Surface compliance also modified the strength of the low-frequency STBLI unsteadiness and transferred energy at the vibration mode frequencies to the flow field.

6.1.3 Contributions to Understanding Effects of Shock Impingement Location and Cavity Pressure

The effect of shock impingement location on the flow and structural responses was investigated at two impingement locations: mid-chord and leading-edge. For the leading-edge case, experiments were additionally conducted under two distinct cavity pressure conditions to assess the influence of mean panel deformation on the fluid-structure interaction behavior. For the panel, changing the cavity pressure was found to have a more pronounced impact on modifying the vibration mode frequencies than changes in impingement location. However, neither parameter produced observable differences in the vibration mode shapes compared to the free vibration mode shapes under no aerodynamic loading.

An interaction length scale was defined as the distance between the separation and reattachment shocks and was measured using the schlieren footage. The time-averaged shock distances were fairly consistent between all compliant cases apart from the mid-chord impingement case exhibiting a slight reduction in length by about 5% compared to the other rigid and compliant cases.

Spectral analysis of the separation and reattachment shock motions showed that structural oscillations transferred energy at the dominant vibration mode frequencies to the shock dynamics under both impingement locations. Notably, surface compliance altered the separation shock spectral content even when the shock foot resided upstream of the panel in the leading-edge impingement cases. Cavity pressure affected the strength of this energy transfer, with the negative mean panel deformation case having exhibited reduced contributions from the structural oscillations in both separation and reattachment shock spectra. The mid-frequency content of density fluctuations inside the separation bubble was weakened compared to that of the leading-edge impingement case, signifying that stronger structural forcing under the separation bubble and the resulting changes in volume attenuated the density fluctuations associated with the separated shear layer vortices convecting over the separation bubble.

6.1.4 Contributions to Understanding One-Way and Two-Way Fluid-Structure Coupling Mechanisms

Experiments conducted without shock impingement revealed that impulsive start of the Ludwig tube flow excited the compliant panel vibration and led to structural oscillations dominated by a one-way uncoupled fluid-structure interaction.

With shock impingement, the initial response of the panel matched that of the No-STBLI experiments, as defined by periodic oscillations at the fundamental vibration mode with approximately 0.1 to 0.2 panel thicknesses in oscillation amplitude. The subsequent panel responses in both leading-edge impingement cases then exhibited sufficient damping of the fundamental vibration mode to transition the interaction from one-way uncoupled to two-way coupled fluid-structure behavior. This shift was characterized by reduced oscillation amplitudes, increased relative contribution of high-order vibration modes, and a fundamental change in the spectral content and synchronization of the shock dynamics and downstream pressure fluctuations.

The mid-chord impingement cases were dominated by one-way uncoupled structural oscillations; however, a secondary peak in the panel response spectra indicated that the separation shock foot transferred energy to the panel at the frequency corresponding to STBLI unsteadiness, with spectral energy exceeding the measurement noise floor by more than an order of magnitude.

Both the type of dominant fluid-structure coupling mechanism and the mean panel deflection (due to the selected cavity pressure) influenced the spectral energy of the separation shock motion at the low-frequency STBLI unsteadiness. Under leading-edge impingement, when the mean panel deformation shape was close to zero, the two-way coupled interaction exhibited greater energy in the separation shock motion at the low-frequency STBLI unsteadiness compared to that of the one-way uncoupled interaction. In contrast, with negative mean panel deformation, the one-way uncoupled interaction exhibited strengthened energy at the low-frequency STBLI unsteadiness compared to that of the two-way coupled interaction.

Pressure fluctuations 13.2δ downstream of the trailing edge of the panel revealed that the spectral content at the vibration modes were attenuated when the fluid-structure response transitioned from one-way uncoupled to two-way coupled behavior. This is consistent with the reduction in strength at the fundamental vibration mode in both the structural oscillations and separation shock motion under two-way coupling. In addition, for the zero mean panel deformation leading-edge impingement case, this is also combined with a significant increase in the low-frequency STBLI unsteadiness content in the pressure fluctuations.

Cross-correlation analysis between the panel and shock motion showed that the one-way uncoupled mid-chord impingement case exhibited a consistently weak level of coupling between the shock and panel, which was stronger than that of the one-way uncoupled leading-edge impingement cases. All one-way uncoupled cases

exhibited consistent signs of the phase differences between the shock and panel motion, where the separation and reattachment shocks had opposite phase delays relative to the panel motion. Furthermore, the reattachment shock motion was found to exhibit slightly stronger coupling to the panel motion than the separation shock when the structural oscillations were dominated by a one-way uncoupled response.

When the leading-edge impingement cases transitioned to a two-way coupled response, the separation shock became more strongly synchronized to the panel motion, with a peak correlation coefficient magnitude between 0.2 to 0.3 at close to zero time lag. This indicated that the panel and separation shock fluctuated at a similar phase of motion despite the attenuation of the structural oscillation amplitudes under two-way coupling. On the other hand, the coupling between the panel and reattachment shock motion was not significantly strengthened.

Phase analysis at the fundamental vibration mode revealed that the phase difference between the separation and reattachment shock exhibited a preferred phase alignment for the one-way uncoupled mid-chord impingement case. Across repeated experiments, the phase of the separation shock tended to lead that of the reattachment shock by approximately one quarter of a panel oscillation cycle. Although the other cases showed greater variation in preferred phase alignment between repeated experiments, the one-way uncoupled leading-edge impingement cases demonstrated stronger phase locking behavior than that of the mid-chord impingement case.

6.2 Recommendations for Future Work

6.2.1 STBLI Measurement Additions

The FLDI measurements in this work revealed that low-frequency structural oscillations could modify the strength of the mid-frequency spectral content in the separation bubble. The magnitude of density fluctuations in the boundary layer after flow reattachment was also observed to be increased due to surface compliance. However, one limitation of these measurements was the variability in the amount of beam clipping near the wall, which can hinder direct comparisons of the magnitude of FLDI phase difference fluctuations. Therefore, a promising area of further experimental measurements would be to implement a colinear cylindrical lens FLDI and high-speed schlieren system to minimize FLDI beam clipping in the near wall measurements. In addition, obtaining simultaneous double or multi-point FLDI measurements would be beneficial for measuring convective velocities and performing cross-correlations within different regions of the flow to better examine

how fluid-structure coupling mechanisms modify the STBLI dynamics.

Another limitation of the measurements obtained in this work was due to the spanwise integrated nature of the schlieren visualization. Although the flow field is nominally two-dimensional, the interaction exhibits three-dimensionality in the vibration modes and spanwise non-uniform flow features. Thus, the tracked shock motion from the schlieren footage may obscure trends in the spectral content and fluctuation amplitudes due to spanwise variation. To isolate the flow structures at a single spanwise coordinate, such as the model centerline plane, Molecular Tagging Velocimetry (MTV) measurements would provide a valuable addition for characterizing the flow behavior. Previous MTV experiments of rigid wall SBLIs, such as Krypton Tagging Velocimetry (KTV) measurements performed by Parziale et al. (2018) and Mustafa and Parziale (2019), have proved successful in capturing turbulence amplification, shear layer development, and changes to the flow structures in the measured velocity profiles. If applied to compliant surface STBLIs, these techniques could further elucidate the changes in the flow field due to fluid-structure coupling.

6.2.2 Evaluating the Influence of Surface Curvature

The types of structural behavior investigated in this work was restricted to one-way uncoupled and two-way coupled panel responses, which provided valuable insights into how feedback between the flow and structure influence the overall interaction. However, these experiments exhibited the combined effects of panel surface curvature and velocity. To distinguish the effects of surface curvature in the interaction, statically deformed panel geometries could be investigated. Measuring the flow response under the time-averaged panel deformation shape or static vibration mode shapes can isolate the effect that changes to the mean flow field structure has on the interaction. These experiments could then inform the extent that the panel deformation shape influences the fully coupled fluid-structure interaction response.

6.2.3 Sensitivity to Initial Conditions

A natural extension of this work is to investigate how the coupled behavior is affected by changes to the initial conditions of the interaction. In particular, buckled and thermally loaded panels are likely to exhibit changes in the coupled fluid-structure behavior. Kirkpatrick et al. (2026) recently observed a softening effect on the panel dynamic response when the panel was mechanically or thermally buckled prior to the experiment. The degree and extent of how these changes to the initial structure

condition affect the coupling mechanisms between the flow and structure is largely unexplored.

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*Appendix A***COLINEAR FLDI AND HIGH-SPEED SCHLIEREN OPTICAL
SETUP**

Fig. [A.1](#) provides a detailed top-down diagram of the colinear FLDI and high-speed schlieren system used in this work. Dimensions of the optical component locations are provided and further description of the setup is discussed in Sections [3.3.1](#) & [3.4](#).

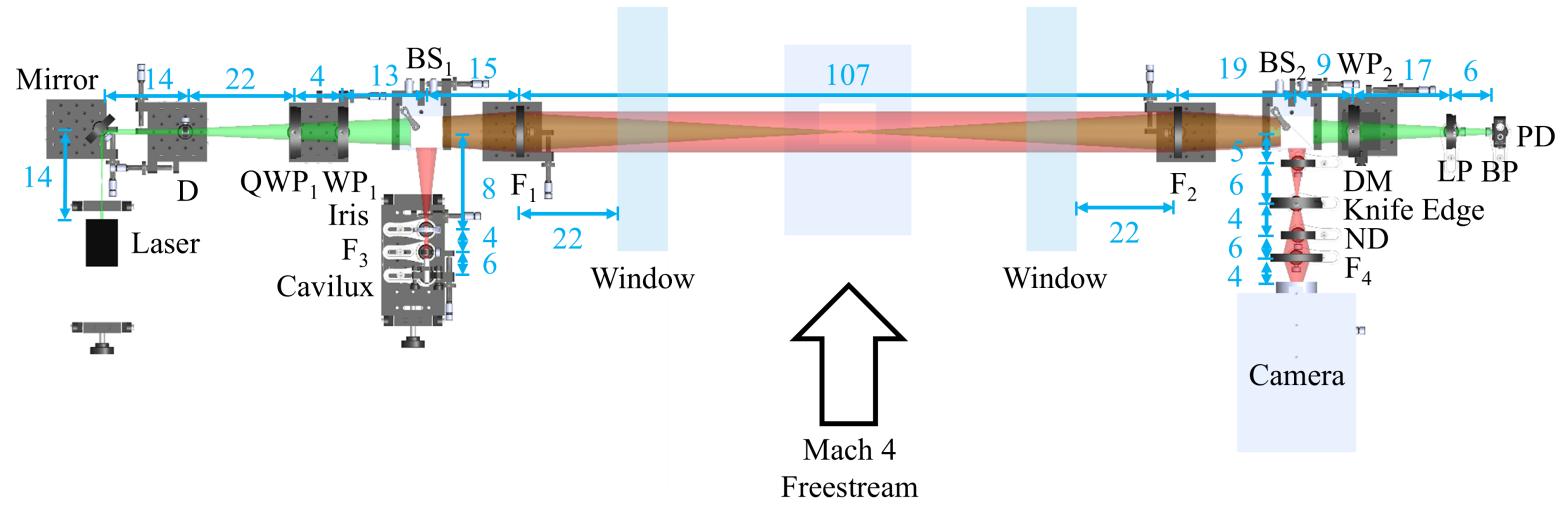


Figure A.1: Top-down diagram of colinear FLDI and high-speed schlieren system. Dimensions are in centimeters and descriptions of the optical components are provided in Table 3.3.

Appendix B

THEORETICAL PLATE NATURAL FREQUENCY AND MODE SHAPE CALCULATIONS

The following natural frequency and mode shape analysis is derived by Warburton (1954).

A rectangular plate with length a and width b is shown in Fig. B.1. The vibration of this plate must satisfy both the boundary conditions at the edges and the plate equation, Eq. B.1.

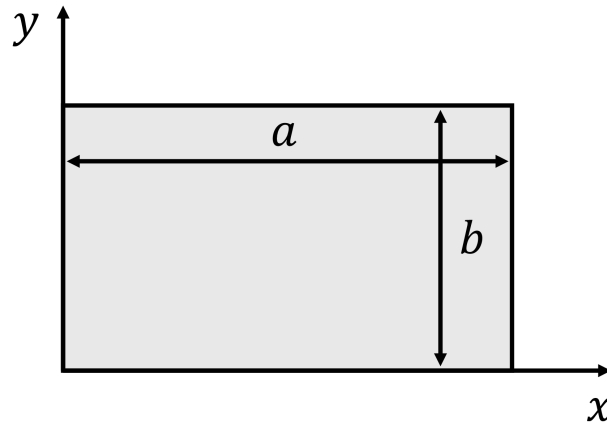


Figure B.1: Diagram of rectangular plate.

$$\frac{\partial^4 w}{\partial x^4} + 2\frac{\partial^4 w}{\partial x^2 \partial y^2} + \frac{\partial^4 w}{\partial y^4} + \frac{12\rho_s(1-\nu^2)}{Eh^2} \frac{\partial^2 w}{\partial t^2} = 0 \quad (\text{B.1})$$

$w(x, y, t)$ is the displacement of a point on the plate at a given time, given by Eq. B.2.

$$w(x, y, t) = \sum_{m=2}^{\infty} \sum_{n=2}^{\infty} A_{mn} \Theta_i(x) \Omega_j(y) \sin 2\pi f t \quad (\text{B.2})$$

B.1 Mode Shape

B.1.1 Fixed Boundary Conditions

If the rectangular plate is fixed along $x = 0$ and $x = a$, $\Theta_m(x)$ for $m = 2, 4, 6, \dots$ is calculated by Eq. B.3, where $\kappa = \frac{\sin \frac{1}{2}\eta}{\sinh \frac{1}{2}\eta}$ and $\tan \frac{1}{2}\eta + \tanh \frac{1}{2}\eta = 0$.

$$\Theta_m(x) = \cos \eta \left(\frac{x}{a} - \frac{1}{2} \right) + \kappa \cosh \eta \left(\frac{x}{a} - \frac{1}{2} \right) \quad (\text{B.3})$$

For $m = 3, 5, 7, \dots$, $\Theta_m(x)$ is instead calculated by Eq. B.4, where $\kappa' = -\frac{\sin \frac{1}{2}\eta'}{\sinh \frac{1}{2}\eta'}$ and $\tan \frac{1}{2}\eta' + \tanh \frac{1}{2}\eta' = 0$.

$$\Theta_m(x) = \sin \eta' \left(\frac{x}{a} - \frac{1}{2} \right) + \kappa' \sinh \eta' \left(\frac{x}{a} - \frac{1}{2} \right) \quad (\text{B.4})$$

Similarly, $\Omega_n(y)$ is calculated using the same expressions, Eqs. B.3 & B.4, for $\Theta_m(x)$ by substituting n , y , and b for m , x , and a . The mode shapes, $\Phi_{(i,j)}(x, y)$, are, therefore, calculated by $\Phi_{(i,j)}(x, y) = \Theta_{i+1}(x)\Omega_{j+1}(y)$.

B.1.2 Simply Supported Boundary Conditions

If the rectangular plate is simply supported along $x = 0$ and $x = a$, for $m = 2, 3, 4, \dots$ is calculated by Eq. B.5.

$$\Theta_m(x) = \sin \frac{(m-1)\pi x}{a} \quad (\text{B.5})$$

$\Omega_n(y)$ is calculated using the same expression, Eq. B.5, for $\Theta_m(x)$ by substituting n , y , and b for m , x , and a . The mode shapes, $\Phi_{(i,j)}(x, y)$, are calculated by $\Phi_{(i,j)}(x, y) = \Theta_{i+1}(x)\Omega_{j+1}(y)$.

B.2 Natural Frequency

The natural frequency of any given mode, (m, n) , for a rectangular plate with any possible combination of boundary conditions is given by Eqs. B.6 & B.7, where h is plate thickness, E is modulus of elasticity, ν is Poisson's ratio, and ρ_s is material density.

$$f_{m,n} = \frac{\xi_{m,n} h \pi}{a^2} \left(\frac{E}{48 \rho_s (1 - \nu^2)} \right)^{\frac{1}{2}} \quad (\text{B.6})$$

$$\xi_{m,n}^2 = G_m^4 + G_n^4 \left(\frac{a}{b} \right)^4 + 2 \left(\frac{a}{b} \right)^2 (\nu H_m H_n + (1 - \nu) J_m J_n) \quad (\text{B.7})$$

B.2.1 Fixed Boundary Conditions

For fixed boundary conditions, if $m = 2$, then $G_m = 1.506$ and $H_m = J_m = 1.248$. Otherwise, if $m = 3, 4, 5, \dots$, then they are defined by Eqs. B.8 & B.9.

$$G_m = m - \frac{1}{2} \quad (\text{B.8})$$

$$H_m = J_m = \left(m - \frac{1}{2}\right)^2 \left(1 - \frac{2}{(m - \frac{1}{2})\pi}\right) \quad (\text{B.9})$$

G_n , H_n , and J_n are defined similarly with n substituted for m .

B.2.2 Simply Supported Boundary Conditions

For simply supported boundary conditions for $m = 2, 3, 4, \dots$, $G_m = m - 1$ and $H_m = J_m = (m - 1)^2$. G_n , H_n , and J_n are defined similarly with n substituted for m .

Appendix C

THEORETICAL CAVITY ACOUSTIC MODE CALCULATIONS

The following cavity acoustic mode shape and frequency analysis is derived by Vér and Beranek (2005).

The acoustical resonances in an enclosed cavity are determined by solving the wave equation (Eq. C.1) with free-vibration boundary conditions for the walls. The nondimensional pressure distribution, $\Psi_n(x, y, z)$, corresponds to the acoustic mode shapes for $n = 0, 1, 2, \dots$, and $f_n = \omega_n/2\pi$ corresponds to the acoustic natural frequencies.

$$\nabla^2 \Psi_n + \left(\frac{\omega_n}{a_c}\right)^2 \Psi_n = 0 \quad (\text{C.1})$$

For a closed rectangular volume of length a , width b , and depth d (Fig. C.1), the natural frequencies in units of Hertz are given by Eq. C.2, where $i, j, k = 0, 1, 2, \dots$ and a_c is the speed of sound.

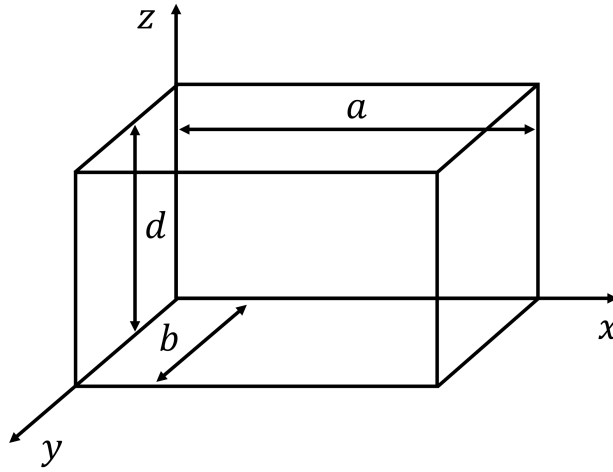


Figure C.1: Diagram of enclosed rectangular cavity.

$$f_{ijk} = \frac{a_c}{2} \left(\left(\frac{i}{a}\right)^2 + \left(\frac{j}{b}\right)^2 + \left(\frac{k}{d}\right)^2 \right)^{1/2} \quad (\text{C.2})$$

Additionally, the mode shapes are given by Eq. C.3.

$$\Psi_{ijk} = \cos \frac{i\pi x}{a} \cos \frac{j\pi y}{b} \cos \frac{k\pi z}{d} \quad (\text{C.3})$$

Appendix D

SHOT LIST

A complete list of Ludwig Tube shots conducted for this work is provided in Table [D.1](#). The sampling rate, f_s^{DAQ} , refers to the sampling rate for the FLDI, Kulite, and NovaSensor measurements. $f_s^{\text{photogrammetry}}$ is the sampling rate of the photogrammetry high-speed camera, and $f_s^{\text{schlieren}}$ is the sampling rate of the schlieren high-speed camera.

Table D.1: Shot List.

Shot	Case Type	Diagnostics	f_s^{DAQ} [kHz]	$f_s^{\text{photogrammetry}}$ [kHz]	$f_s^{\text{schlieren}}$ [kHz]
1105	No-STBLI (Rigid)	Kulite	500		
1106	No-STBLI (Rigid)	Kulite	500		
1118	No-STBLI (Rigid)	Kulite, NovaSensor	500		
1119	No-STBLI (Rigid)	Kulite, NovaSensor	500		
1120	No-STBLI (Rigid)	Kulite, NovaSensor	500		
1121	No-STBLI (Rigid)	Kulite, NovaSensor	500		
1122	No-STBLI (Rigid)	Kulite, NovaSensor	500		
1123	No-STBLI (Rigid)	Kulite, NovaSensor	500		
1124	No-STBLI (Rigid)	Kulite, NovaSensor	500		
1125	No-STBLI (Rigid)	Kulite, NovaSensor	5000		
1126	No-STBLI (Rigid)	Kulite, Photogrammetry	500	8	
1127	No-STBLI (Rigid)	Kulite, Photogrammetry	500	8	
1156	Rigid	Kulite, Photogrammetry, Schlieren	500	8	2.5
1182	Rigid	Kulite, Schlieren	500		16
1202	Rigid	Kulite, Photogrammetry, Schlieren	500	8	32
1203	Rigid	Kulite, Photogrammetry, Schlieren	500	8	32

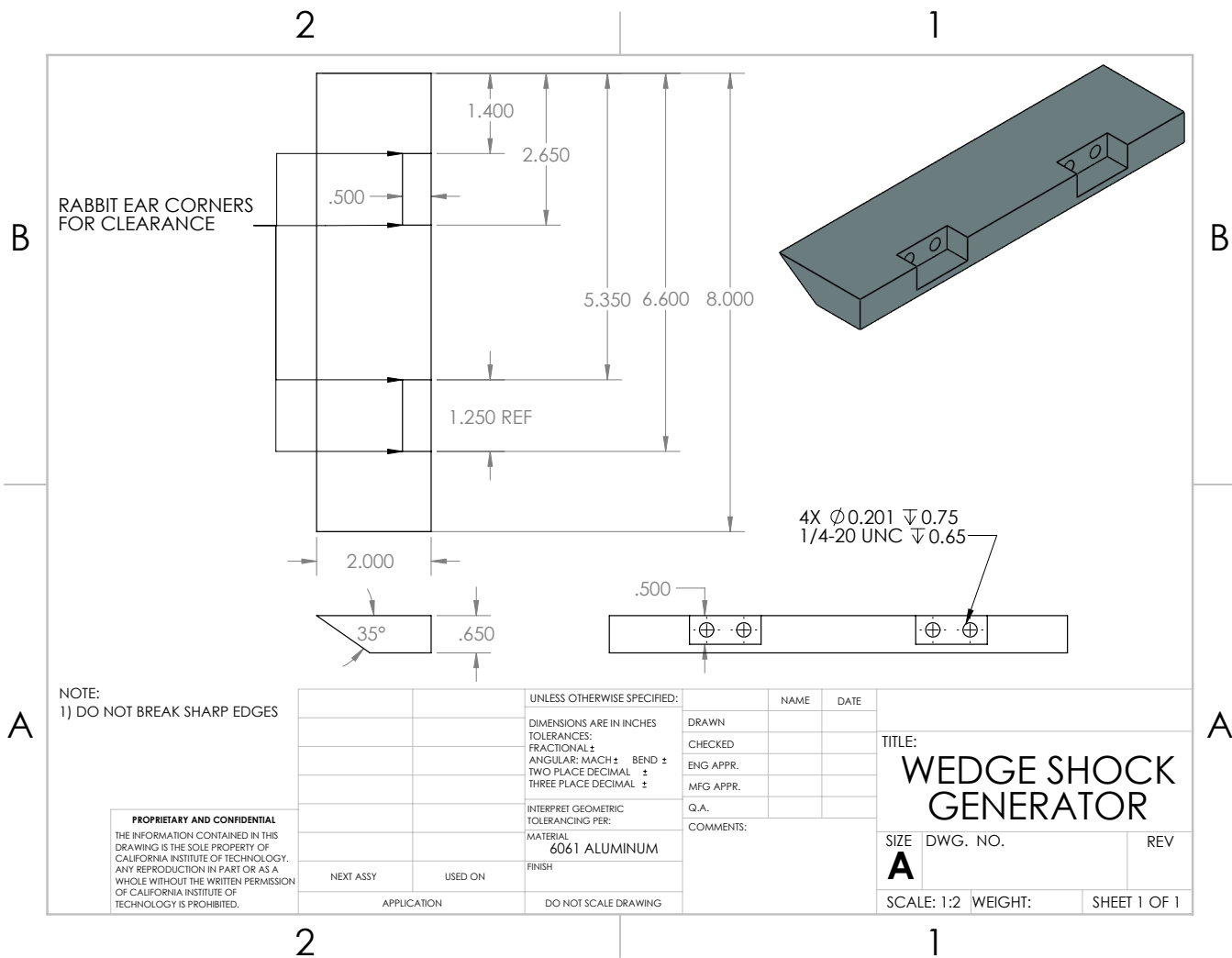
1204	Rigid	Kulite, Photogrammetry, Schlieren	500	8	32
1211	A	Kulite, Photogrammetry, Schlieren	500	8	32
1212	A	Kulite, Photogrammetry, Schlieren	500	8	32
1213	A	Kulite, Photogrammetry, Schlieren	500	8	32
1241	Rigid	Schlieren	500		100
1242	A	Kulite, Schlieren	500		100
1271	None (Freestream)	FLDI	1000		
1272	None (Freestream)	FLDI	1000		
1275	Rigid	FLDI, Schlieren	1000		5
1276	Rigid	FLDI, Schlieren	1000		5
1277	Rigid	FLDI, Schlieren	1000		5
1278	Rigid	FLDI, Schlieren	1000		5
1279	Rigid	FLDI, Schlieren	1000		5
1280	Rigid	FLDI, Schlieren	1000		5
1281	Rigid	FLDI, Schlieren	1000		5
1282	Rigid	FLDI, Schlieren	1000		5
1283	Rigid	FLDI, Schlieren	1000		5
1284	Rigid	FLDI, Schlieren	1000		5

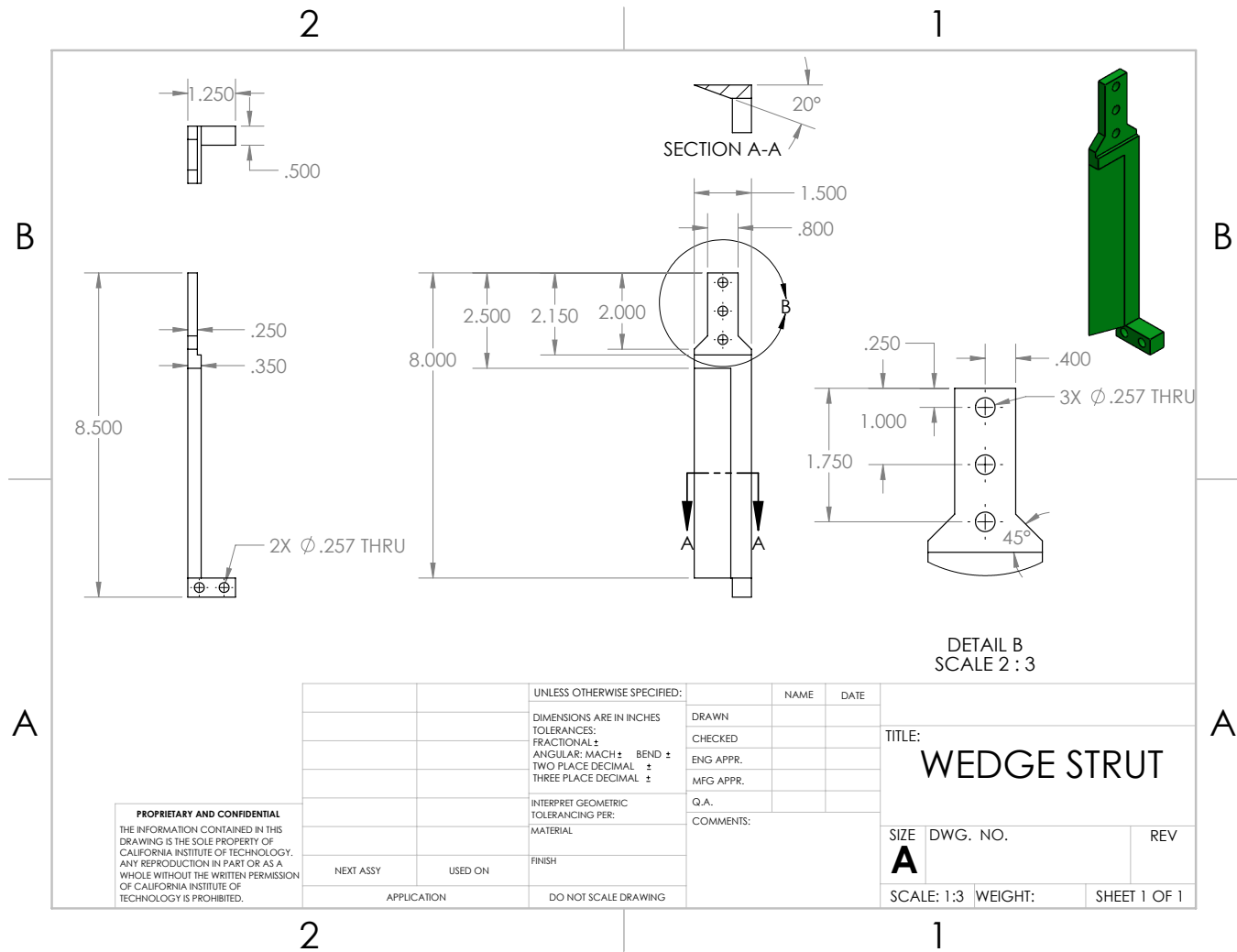
1285	Rigid	FLDI, Schlieren	1000		5
1286	Rigid	FLDI, Schlieren	1000		5
1287	Rigid	FLDI, Schlieren	1000		5
1288	Rigid	FLDI, Schlieren	1000		5
1289	Rigid	FLDI, Schlieren	1000		5
1290	Rigid	FLDI, Schlieren	1000		5
1291	Rigid	FLDI, Schlieren	1000		5
1292	A	Kulite, FLDI, Schlieren	1000		100
1293	A	Kulite, FLDI, Schlieren	1000		250
1294	A	Kulite, Schlieren	1000		100
1296	A	Kulite, FLDI, Schlieren	1000		250
1297	A	Kulite, FLDI, Schlieren	1000		5
1301	A	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1302	A	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1303	A	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1304	A	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
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1306	A	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100

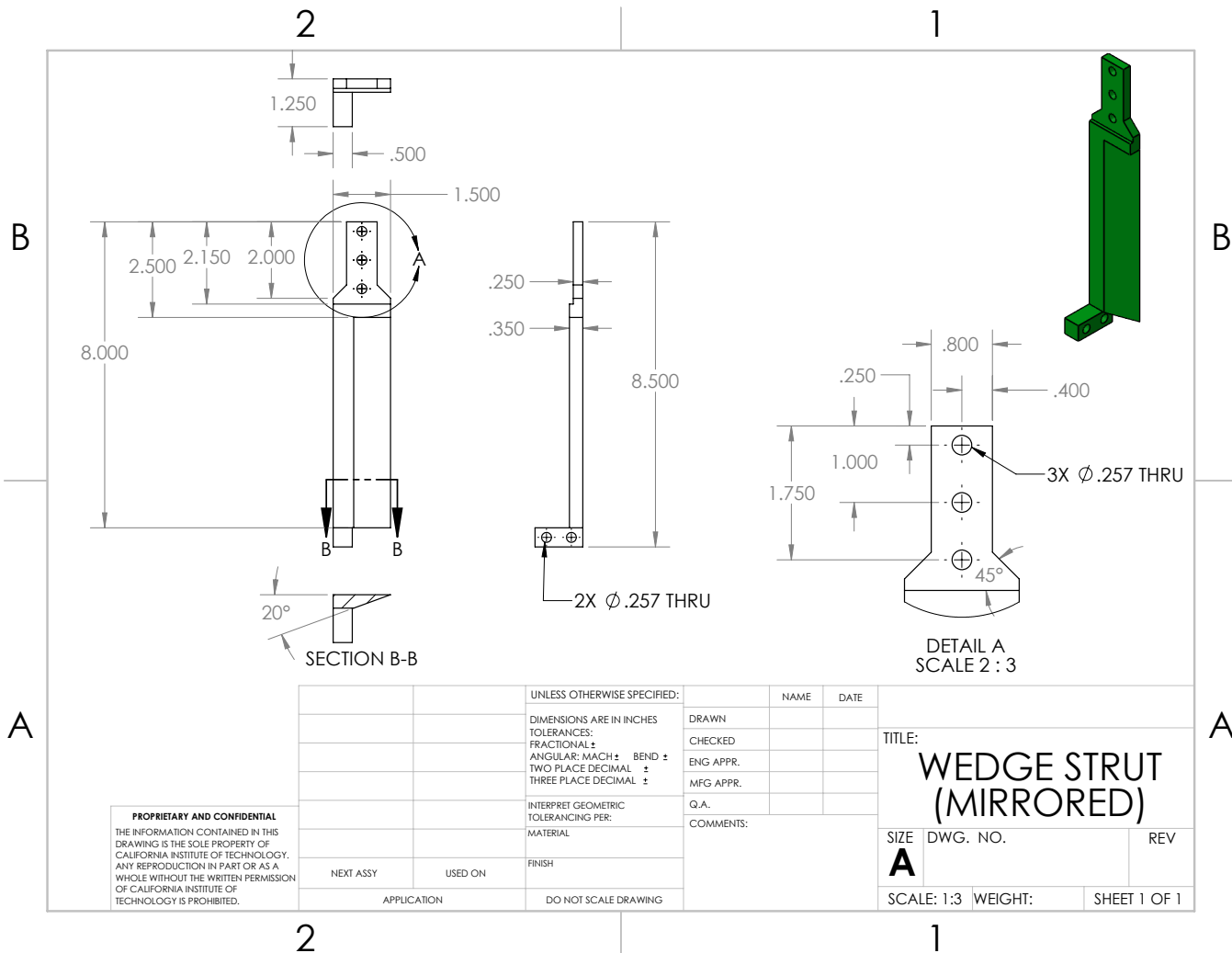
1324	Rigid	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1325	Rigid	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1327	B	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1328	B	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1329	B	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1330	B	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1333	C	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1334	C	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1335	C	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1336	C	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1337	No-STBLI (Rigid)	FLDI, Kulite, Photogrammetry, Schlieren	1000	10	100
1338	No-STBLI	Kulite, Photogrammetry	1000	10	
1339	No-STBLI	Kulite, Photogrammetry	1000	10	
1340	No-STBLI	Kulite, Photogrammetry	1000	10	
1341	No-STBLI	Kulite, Photogrammetry	1000	10	

*Appendix E***ENGINEERING DRAWINGS**

Detailed engineering drawings for the flat plate, shock generator, and compliant and rigid panel insert models are provided in this Appendix. Note that all dimensions are in inches, unless otherwise specified.



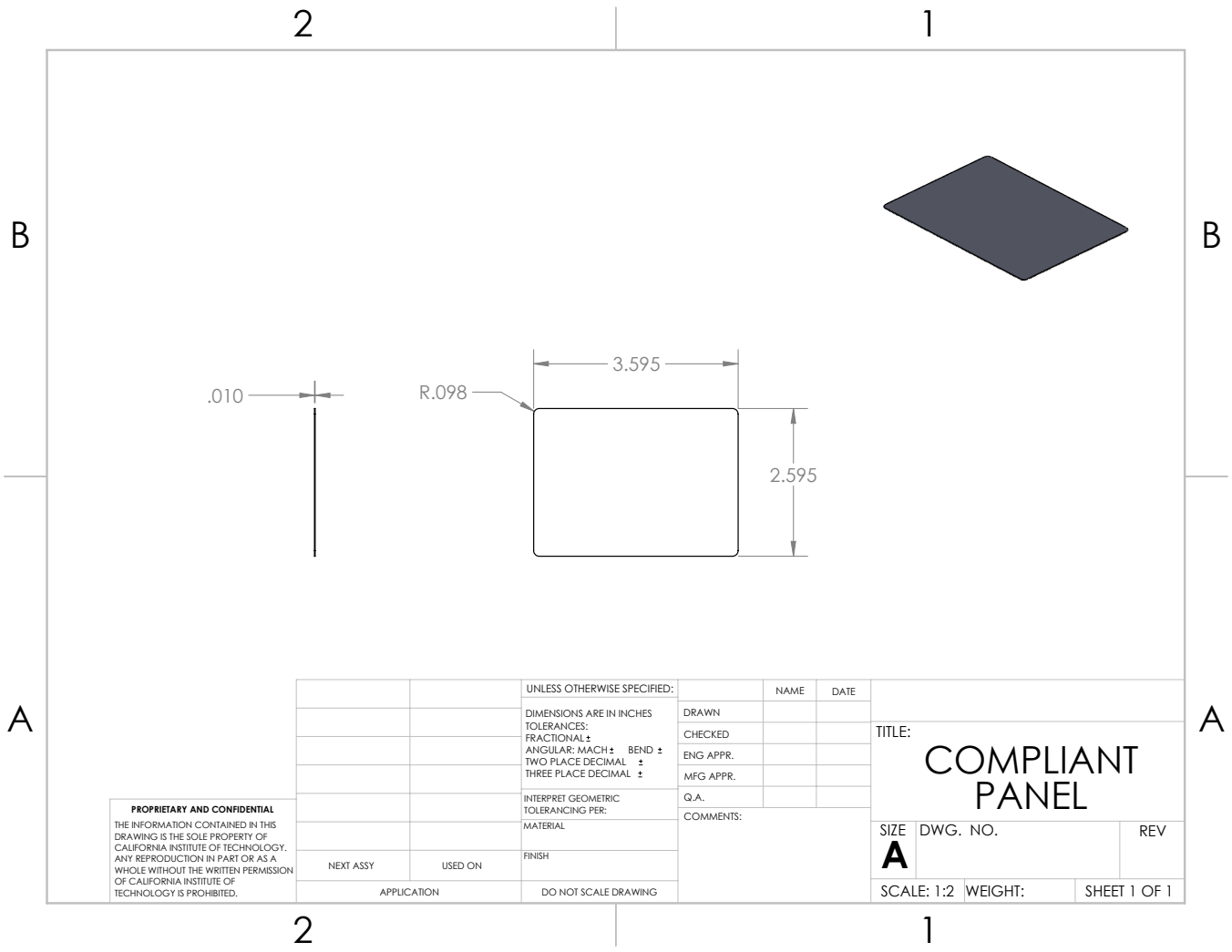




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		ANGULAR: MACH ± BEND ±	MFG APPR.	
		TWO PLACE DECIMAL ±	Q.A.	
		THREE PLACE DECIMAL ±	COMMENTS:	
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		FINISH		
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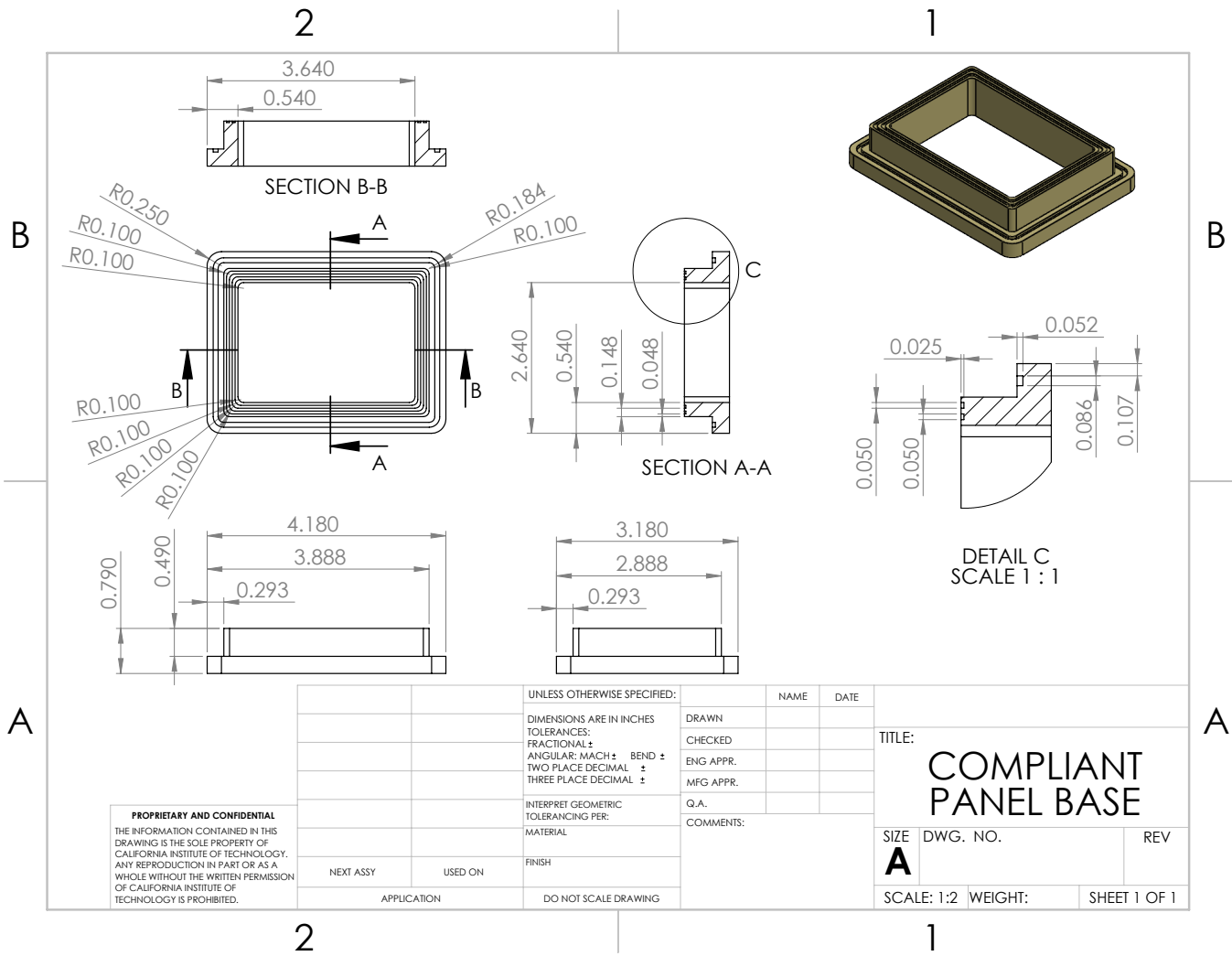
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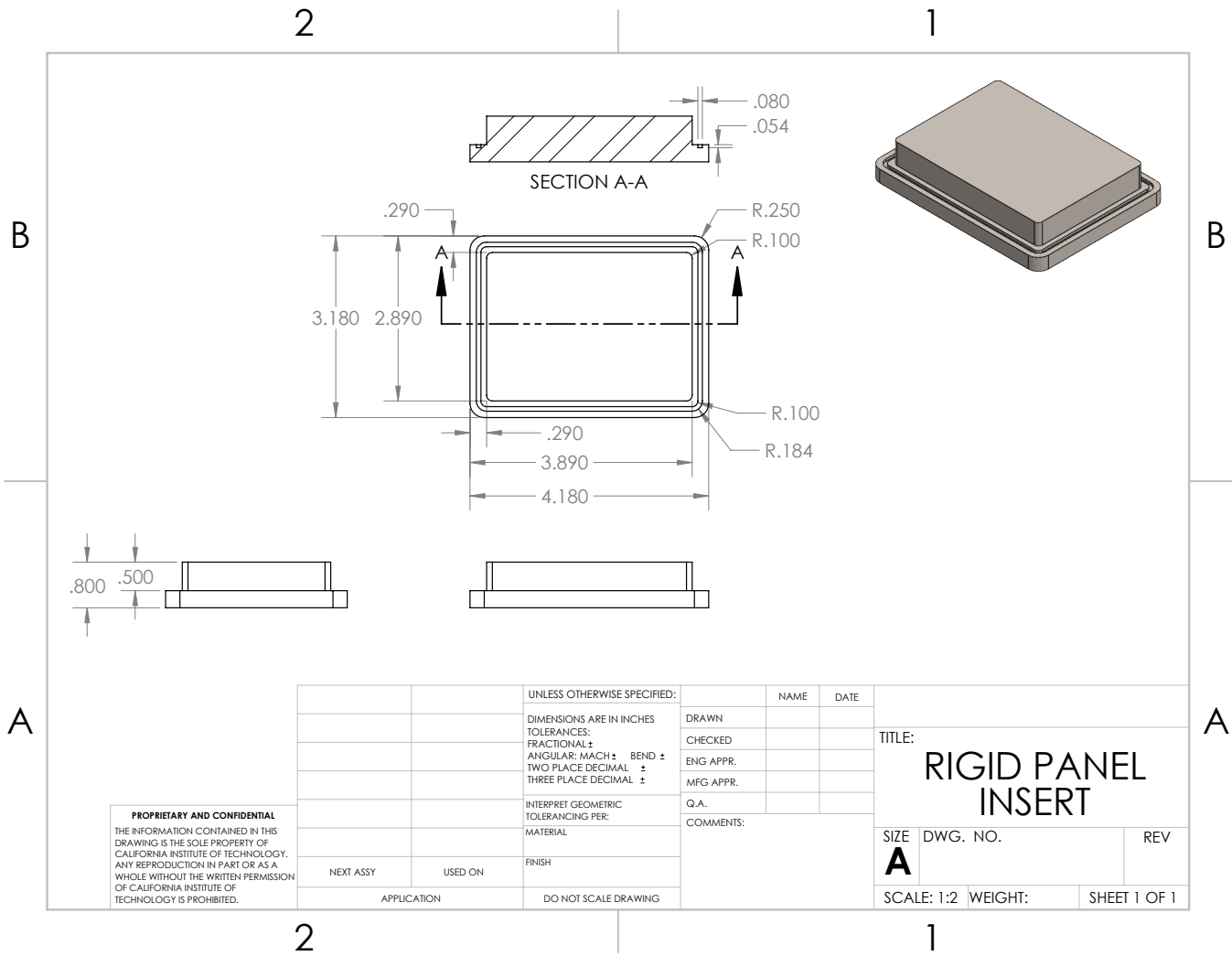


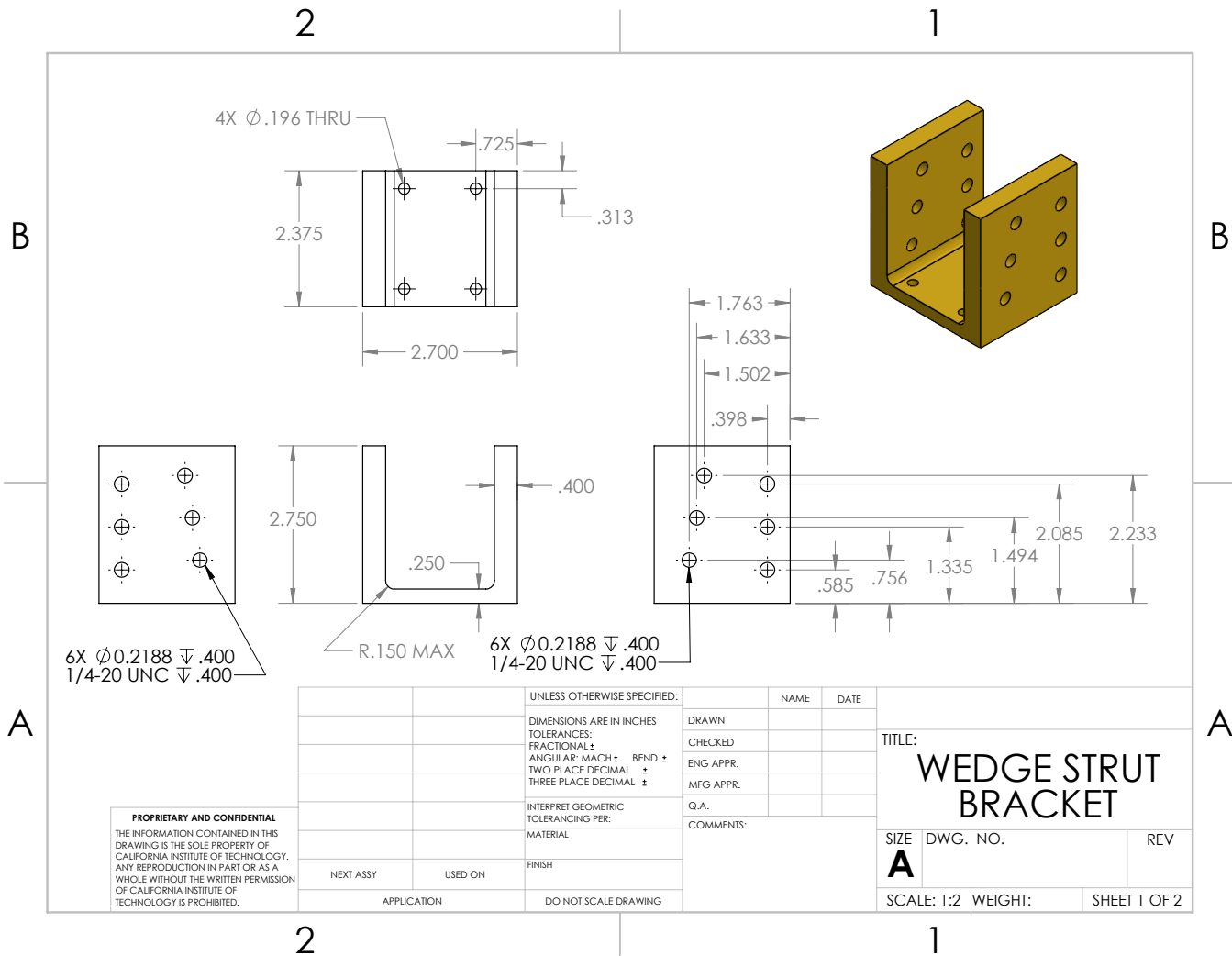
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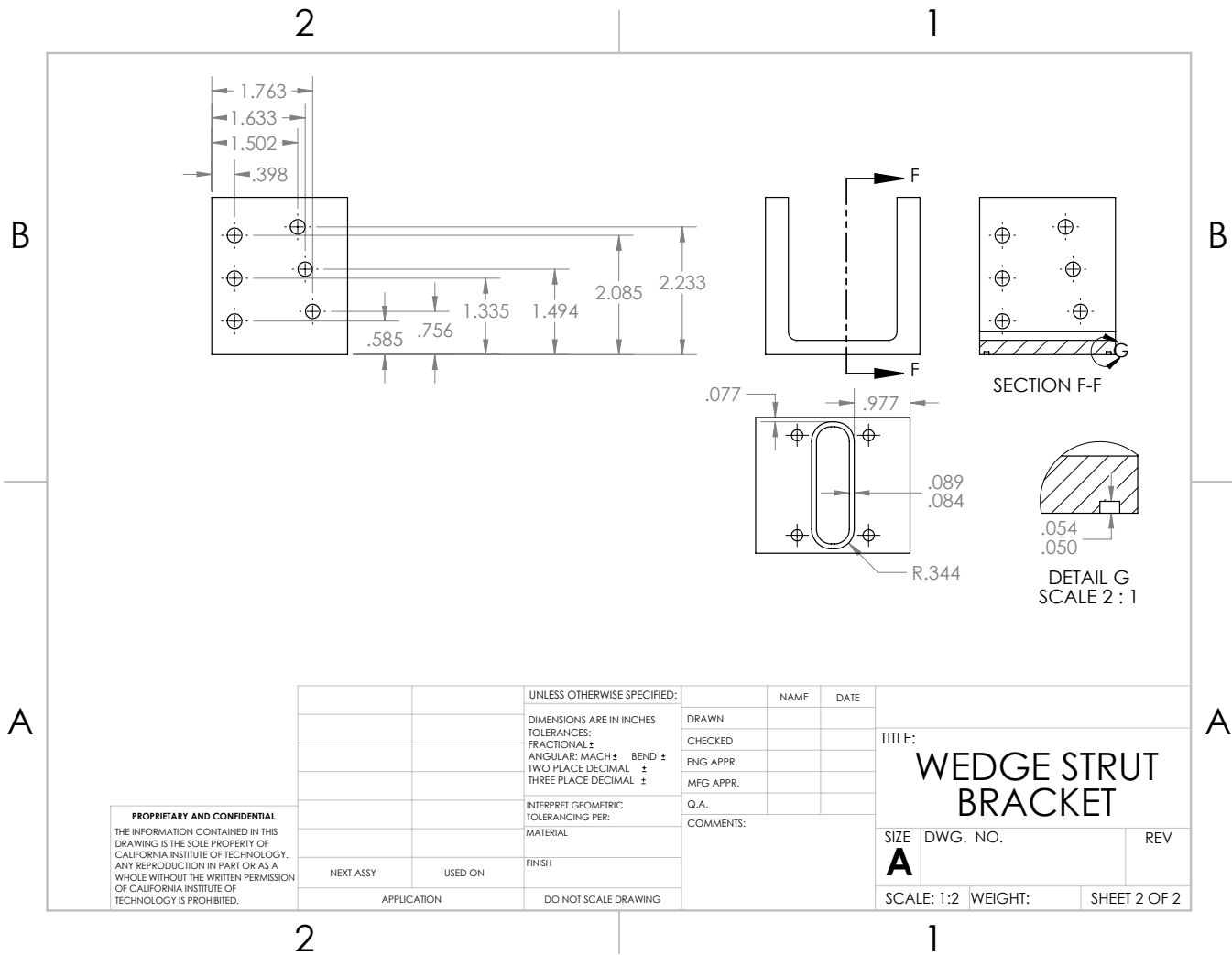
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		ANGULAR: MACH ± BEND ±	MFG APPR.		
		TWO PLACE DECIMAL ±	Q.A.		
		THREE PLACE DECIMAL ±	COMMENTS:		
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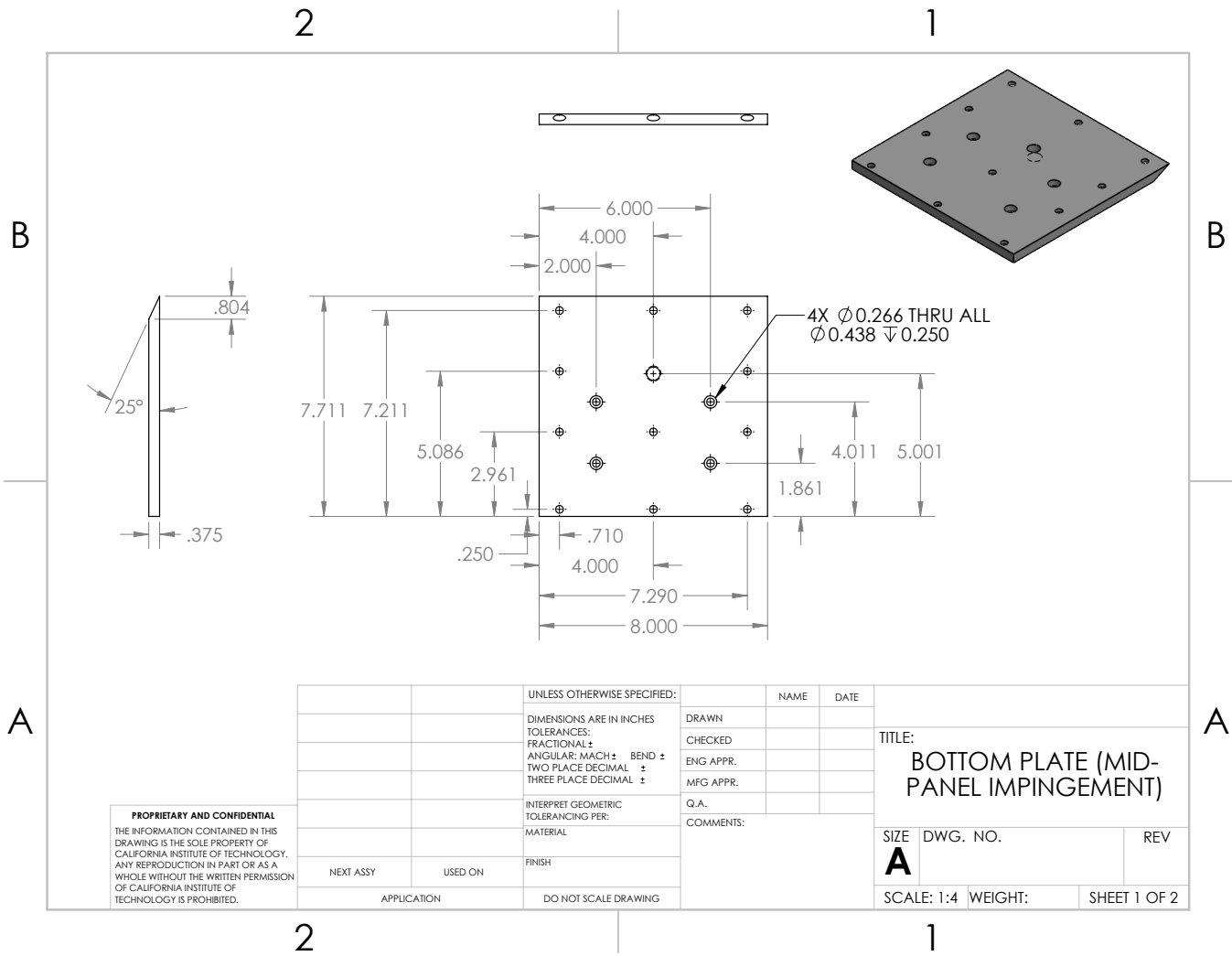
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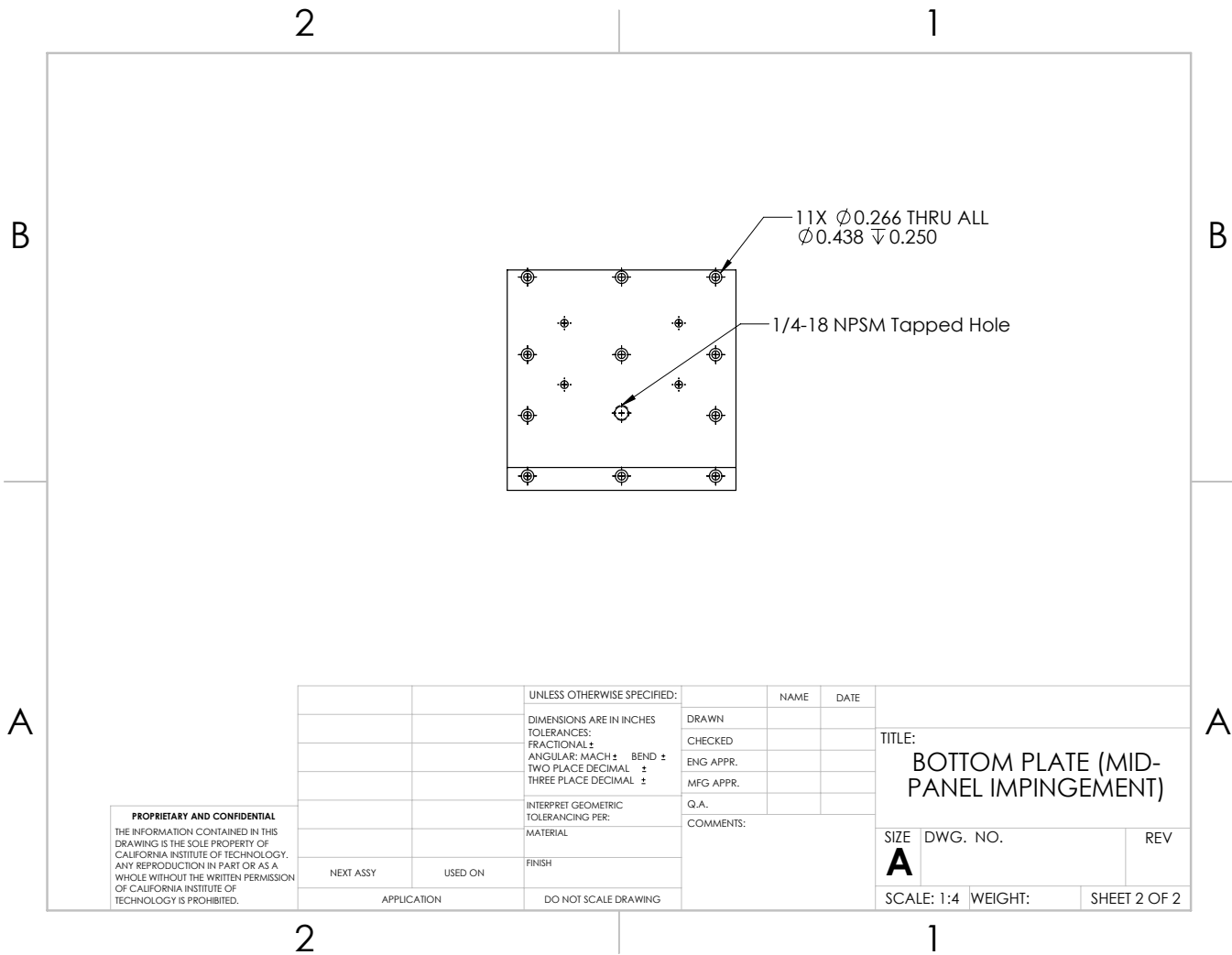


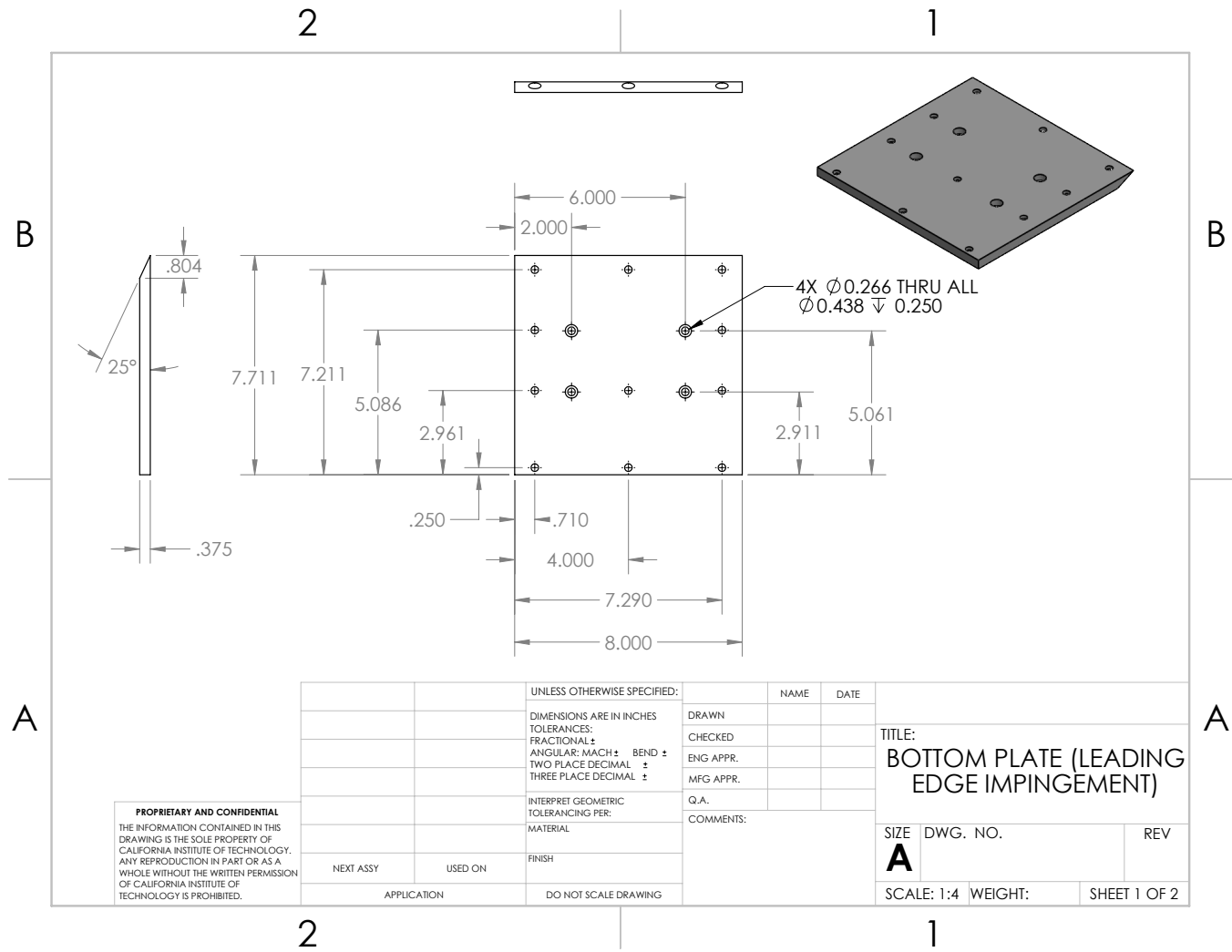












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		UNLESS OTHERWISE SPECIFIED:		NAME	DATE	TITLE: BOTTOM PLATE (LEADING EDGE IMPINGEMENT)
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		ANGULAR: MACH ± BEND ±	MFG APPR.			
		TWO PLACE DECIMAL ±				
		THREE PLACE DECIMAL ±				
		INTERPRET GEOMETRIC TOLERANCING PER:	Q.A.			
		MATERIAL	COMMENTS:			SIZE DWG. NO. REV
		FINISH				A
NEXT ASSY	USED ON					
APPLICATION		DO NOT SCALE DRAWING			SCALE: 1:4 WEIGHT: SHEET 1 OF 2	

