



Investigation of shock boundary layer interactions to unravel the physics of unstart in axisymmetric inlets

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Investigation of shock boundary layer interactions to unravel the physics of unstart in axisymmetric inlets

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April 4, 2020

Abstract

The vision of the project is to bring about an unprecedented understanding of the mechanisms that result in the inflated propensity of the axisymmetric inlets to unstart. Given the preponderance of the shock boundary layer interactions (SBLI) in inlets and their critical role towards unstart in planar geometries, the objective of the present effort is to unravel how the shock interactions with axisymmetric inlet/isolator boundary layers can inflate their unstart propensity. The objective was approached from three specific angles:

- Unravel the response of the started inlet with unseparated boundary layers to back pressure fluctuations and the length scales over which a given shock leg would respond to a downstream pressure fluctuation within the isolator.
- Make detailed investigations of the unique phenomena and mechanisms that occur with shock interactions with axisymmetric inlet/isolator geometries, which causes the inflated propensity to unstart.
- Investigate the changes in the SBLI within the inlet/isolator duct caused by circumferential variations in the incoming boundary layer, which both provide insights into situations that occur in angle of attack flight as well as opportunity to delay the onset of inlet unstart.

A primarily experimental approach with complementary computational simulations that augment the experimental findings was undertaken. The experiments were performed on two test configurations, a 2D axisymmetric inlet/isolator to study the shock train response to back pressure fluctuations, and a half isolator with an axisymmetric compression ramp fitted within the model to make detailed studies on SBLI in axisymmetric configurations. A suite of surface and off-surface flow quantification methods were implemented to gain a detailed understanding of the underlying flow dynamics. The following enumerates some of the accomplishments made through this project.

- We delineated the shock oscillation spectrum in started inlets containing unseparated boundary layer and developed a predictive physical model that can capture these shock oscillations in both internal and external SBLI settings. We also experimentally determined the length scales over which back pressure fluctuations can propagate upstream in started isolators, which provided critical information on the prospects of developing precursors that sense unstart as well as the time window that the precursor would allow for countering the unstart propagation.
- For the first time, we demonstrated an early onset of separation and over 50% inflation in the separation size generated in the axisymmetric SBLI configuration compared to planar SBLI under matching inviscid shock strength. This was shown to be cause of increased propensity of the axisymmetric inlets to unstart. Using multivariate measurements, we also elucidated the mechanisms that result in the inflated separation size in axisymmetric SBLI, which engendered several possible approaches to mitigate the separation size.
- We further elucidated the effect of having circumferentially varying incoming boundary layer on the mean and unsteady characteristics of the shock-induced separated flow. We found that circumferential variations of the incoming boundary layer engendered a corresponding variation in the mean separation size and the shock oscillation spectrum.

1 Statement of Objectives and Summary of Accomplishments

The vision of the proposed work is to enable scramjet technology that is capable of operating at lower Mach numbers. The overall goal is to contribute towards the current understanding of inlet unstart, which is one of the important bottlenecks in lowering the starting Mach number. The objective of the proposed work is to unravel the mechanisms that drive the shock interactions with inlet/isolator boundary layers in axisymmetric configurations, which substantially contribute to inlet unstart. The said shock boundary layer interaction (SBLI) unit has several complexities that arise from its inherent 3D nature, isolator boundary layer distortions at non-zero incidence angles, cross-sectional mass flux variations, and temporally varying back pressure during unstart. We build our understanding of this very complicated flow unit by addressing the following topics:

1. **Mechanisms causing higher propensity for axisymmetric inlets to unstart.** We reported for the first time a significantly inflated separation size in our model axisymmetric SBLI configuration compared to a planar SBLI. The earlier onset of separation and substantial inflation of the separation size causes the axisymmetric inlets be more prone to unstart. We also made a deep dive into the mechanisms that cause the inflated separation in axisymmetric SBLI, which are summarized in [1, 2] and elaborated in Section 6.2.
2. **Ability of conventional approaches to extend unstart margins.** The traditional approach to extending the unstart margin has been using vortex generators to energize the boundary layer to delay the separation onset and separation size. We implemented the vortex generators in axisymmetric SBLI and quite surprisingly observed that the impact of using single vortex generators were negligible while an array made noticeable separated flow mitigation; however, the extent of mitigation is substantially smaller than in a planar SBLI. These findings and the mechanisms are detailed in [3], and the highlights are summarized in Section 6.3.
3. **Shock oscillations in a 2D axisymmetric model inlet and communication pathways prior to unstart.** Prior to unstart, and especially in hypersonic inlets, boundary layer separation is less likely to occur due to the high inflow momentum and relatively benign back pressure ratio that the flow needs to handle. The foundational question is how the duct will know about a back pressure fluctuation or an incoming unstart shock, and what is the length scale over which this communication occurs. These findings are detailed in our recently submitted article [4], and the major findings are summarized in Section 6.1.
4. **Development of ideal tools to probe the complex flow interactions to make novel flow quantification.** The highly complicated and unsteady nature of the flowfield necessitates multivariate measurements, at least a few in a time-resolved manner. We developed and validated the use of high-bandwidth pressure sensitive paints to study the SBLI and inlet unstart related problems, wherein we came up with a systematic approach to characterize the data quality and the associated highest frequencies that could be measured [5]. Similarly, we implemented a novel laser-based imaging technique to obtain the gas density fields in inlet/isolators [6, 1]. This technique is particularly versatile and useful in flows that have highly three-dimensional and curved shock structures and provides a very accurate quantification at a micron-level spatial resolution. In keeping with the overall scope of this project and brevity of this report, the reader is referred to the aforementioned article for the details.

2 Overall Background and Motivation

Shock boundary layer interactions (SBLI) are a common occurrence on the bodies of supersonic and hypersonic vehicles. If sufficiently strong, they can result in large scale flow separation[7]. These separations are accompanied by increased skin friction drag due to the enhanced production of turbulence and large transient mechanical/thermal loads[8]. In internal flows, shock-induced separations elevate distortion levels and reduce total pressure recovery. Further, separated regions near the wall impose an area blockage on the inviscid core flow and cause a subsequent deceleration [9]. In extreme situations (like when separation is prompted by the pressure rise accompanying combustion-induced heat release), the core flow may choke prior to the combustor entrance. This results in a process known as unstart, wherein a shock system propagates upstream through the isolator, disgorging the shock train and causing a severe reduction in the engine mass flow rate [10, 11, 12, 13].

Whereas a considerable literature is available in unstart of rectangular (planar) inlets, comparatively, very few works have focused on unstart of axisymmetric inlets, where aerodynamic causes like strong cross-sectional variations in incoming boundary layer properties play a vital role. Most early works were made towards measuring the mean pressure fields during unstart perpetuation [14, 15, 16]. In particular, Choby [14] noted that the inlet unstart occurred at a mere 3° angle of attack, which is evidently much smaller than the angles of attack for most tactical maneuvers. Zha et al. [17] simulated the unstart process in NASA Variable Diameter Inlet geometry caused by non-zero incidence flight [15]. They found that unstart initiation is preceded by boundary layer separation at the most vulnerable location within the isolator and circumferential spreading of the separated flow that resulted in local and global choking. Contrasting rectangular inlets, Zha et al. observed that the upstream shock motion at different azimuthal locations began and perpetuated with different time histories. Further, very similar to rectangular inlets, Zha et al. showed that the continual spreading of choked flow in the inlet perpetuates the unstart process. Thus, there appear many interesting similarities and differences in the flow events associated with unstart between rectangular and axisymmetric inlets.

3 Impact to DoD

Developing innovative high-speed propulsion technologies that are applicable to both military and commercial application is of particular significance to US Air Force. In this regard, developing autonomous hypersonic platforms can make game-changing impact on missile propulsion (global strike and reconnaissance), hypersonic cruises, and commercial space access propulsion system. Of particular interest in future scramjet technology is decreasing the lower operating Mach number limit to $M=3.5$, which closes the gap between low speed propulsion systems (e.g., turbojets) and scramjet engines. This has direct advantages by eliminating a number of components required for ramjet operation, which can result in overall weight reduction, more available payload weight, and fewer components that must reliably function (thereby improving overall safety). However, an important bottleneck with decreasing the starting Mach number is the significantly higher propensity for the inlet to unstart during off-design and off-cruise (e.g., pitch up, climbing maneuvers) conditions. Inlet unstart denotes the disgorging of inlet/isolator shock train to result in fully subsonic flow inside scramjet engine, which results in catastrophic failure of the engine. To delay/offset the inlet unstart at low Mach numbers, an excessively long isolator section is required, which offsets the gains obtained from lowering the starting Mach number. Hence, there is a critical need to obtain basic understanding of unstart physics as well as develop tools to predict and control unstart, which forms the overall vision of the proposed effort. Specific areas where the proposed research will complement Air Force's vision and research agenda include:

- **Insights into axisymmetric inlet configurations to address practical engines:** While most experimental scramjet engines have rectangular inlets (e.g., X-43, X-51, etc.), larger scale versions for practical use will have axisymmetric or shape changing configurations (e.g., buseman inlets) because of their short length, light weight, and high total pressure recovery. These axisymmetric inlets have inherently distorted boundary layer even at zero incidence, in stark contrast to rectangular inlets where boundary layer distortions are not very strong. So far, very few works have examined the shock interactions with non-canonical boundary layers, which is responsible for inlet unstart in axisymmetric inlets. The proposed work will result in unprecedented insights into shock interactions with realistic boundary layers that occur in axisymmetric inlets.
- **Transfer of knowledge from rectangular inlet works to practical inlets:** Significant efforts are made in WPAFB to sense and control unstart in rectangular inlets (Dr. Donbar's group) and to integrate rectangular inlets to main airframe without performance loss (Dr. Hagenmaier's group). There is a huge interest in knowing how much of the knowledgebase gained from these effort in rectangular inlets can be transferred to axisymmetric inlets. The proposed work will address this issue by exploring the similarities and differences in the mean features and driving mechanisms of shock/boundary layer interactions in rectangular and axisymmetric isolators.

4 Experimental Setup

4.1 Freestream Test Conditions

All experiments were performed in the North Carolina State University supersonic wind tunnel, which is a blowdown type, variable throat area facility capable of producing test section Mach numbers from 1.5 to 4. The test section measures 150×150 mm in cross section, and is 650 mm long. The test section Mach number was fixed at $M_\infty = 2.5$ for all tests, yielding the the freestream test conditions compiled in Table 1. A PID hydraulic valve control was implemented and used to maintain the settling chamber pressure, ensuring minimal variation (less than 3%) in freestream conditions over the course of a run.

Table 1: Freestream test conditions.

Parameter	Value
M_∞	2.5
u_∞	583 m/s
T_∞	140 K
Re/m	5.3×10^7 m ⁻¹
p_∞	32.5 kPa

4.2 Experimental Model

Two separate experimental campaigns using two different test articles were made. The first article is a half-isolator model, which was used to probe the details of the shock boundary layer interactions that occur in axisymmetric configuration that is characterized by negative curvature surfaces (surfaces with area constrictions) that has a common occurrence in axisymmetric inlet/isolator. The second article is an axisymmetric inlet isolator, which was employed to probe the shock oscillations at started conditions as well as establish the communication pathways within the isolator.

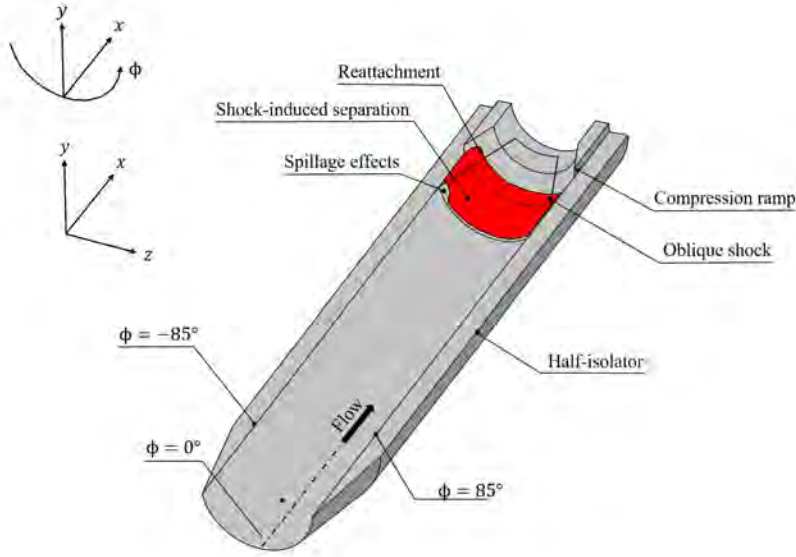


Figure 1: Isometric view of half-isolator model and example flow features of interest, separated flow regions in red. Cylindrical and cartesian coordinate systems also pictured.

4.3 Half-Isolator Model

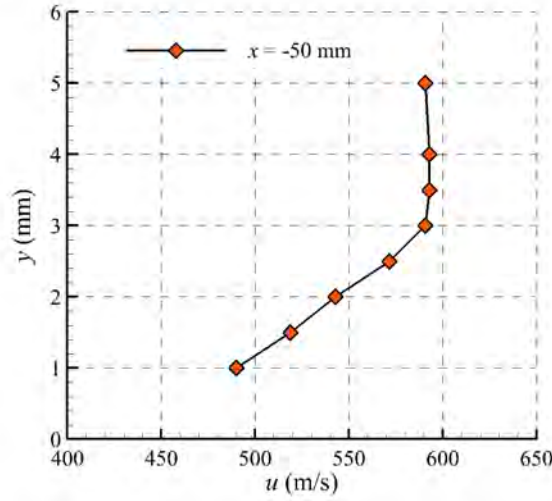
The half-isolator (H-I) model in Fig. 1 was first developed to characterize the effect of negative curvature on the mean structure of the SBLI, and was introduced by Funderburk & Narayanaswamy[2]. The main body has an outer diameter of 100 mm, which tapers inwards to a sharp leading edge from the outside, forming an interior measurement domain with a diameter of 75 mm ($R = 37.5$ mm). The model was mounted on the test section sidewall using a 20 mm tall strut (not depicted) which also lifted the model clear of the tunnel boundary layer and into the freestream. Optical access to the flowfield was provided by a high clarity quartz window opposite the model. A boundary layer forms along the inner surface of the model and undergoes a natural transition to turbulence. The boundary layer interacts with an axisymmetric oblique shock wave generated by a compression ramp located 380 mm downstream of the leading edge; the ramp configuration chosen for testing is discussed in Section 4.4.3.1. The cylindrical and cartesian coordinate systems utilized are also displayed in the figure, the origin lies on the floor at the center of the compression ramp leading edge. While the open top of the half-isolator geometry will naturally allow some spillage near the edges of the model ($\phi = \pm 85^\circ$), it will be shown subsequently that axisymmetry is maintained for the bulk of the flow in the circumferential direction.

4.3.1 Interaction Strengths

Compression ramp half-angles of $\theta = 16^\circ$, 20° , and 24° were tested for both the H-I and 2D SBLI; the dimensions of the ramps are presented in Table 2. The majority of the results presented in this study focus on the 20° H-I interaction; any deviations from this baseline case are mentioned explicitly. From oblique shock theory and the Korkegi incipient separation limit[18], planar weak-solution waves at 20° and 24° should be strong enough to produce mean flow separation at the test Mach number, whereas the flow across a 16° deflection should remain attached. The effect of ramp height was also examined for the 20° H-I interaction. For a given value of θ , the compression ramp face of the 2D model is consistently longer than that of the H-I interaction when normalized by the corresponding incoming boundary layer thickness. Based on the criterion

Table 2: H-I compression ramp configurations tested.

<i>Model</i>	θ (degrees)	h (mm)	h/R	h/δ_0
H-I	16°	7.28	0.194	2.42
	20°	7.28	0.194	2.42
	20°	9.24	0.246	3.08
	24°	11.43	0.305	3.81
2D	16°	12.7	-	4.23
	20°	19.05	-	6.35
	24°	19.05	-	6.35

**Figure 2:** Incoming boundary layer profile for the half-isolator model.

of Hunter & Reeves[19] for 2D compression ramp interactions, the 2D ramps are all of sufficient length for the SBLI to produce infinite ramp solutions. More specifically, the separation length scales should not be affected by ramp height or the centered expansion at the ramp shoulder.

4.3.2 Inflow Characterization

Before experiments were performed, a boundary layer profile was obtained for the half-isolator model via wall-normal pitot sweeps with the compression ramp removed. A custom pitot probe with a tip diameter of 0.25 mm was used for the scans. The measurement location was 50 mm upstream of the nominal compression ramp leading edge ($x = -50\text{ mm}$) at $\phi = 0^\circ$; sweeps began with the pitot orifice centered at $y = 10.5\text{ mm}$, moving inwards toward the model wall in 1 mm increments. The step-size was reduced to 0.5 mm once stagnation pressure losses were detected, continuing downwards to the closest achievable distance from the wall where reliable velocities could be obtained ($y = 1.0\text{ mm}$). The resulting boundary layer velocity profile is shown in Fig. 2, computed parameters of interest are presented in Table 3. The 99% boundary layer velocity thickness is $\delta_0 = 3\text{ mm}$. The boundary layer has a Reynolds number based on compressible momentum thickness of $Re_\theta = 2250$.

4.3.3 VG Geometry and Placement

The VG designed for the study is the axisymmetric equivalent of the rectangular geometry first proposed by Anderson *et al.*[20], and is pictured in Fig. 3. A height of $h = 3\text{ mm}$ was chosen,

Table 3: Incoming boundary layer characteristics for the half-isolator model.

Parameter	Value
δ_0	3 mm
δ^*	0.044 mm
θ	0.042 mm
H	1.05
Re_θ	2250

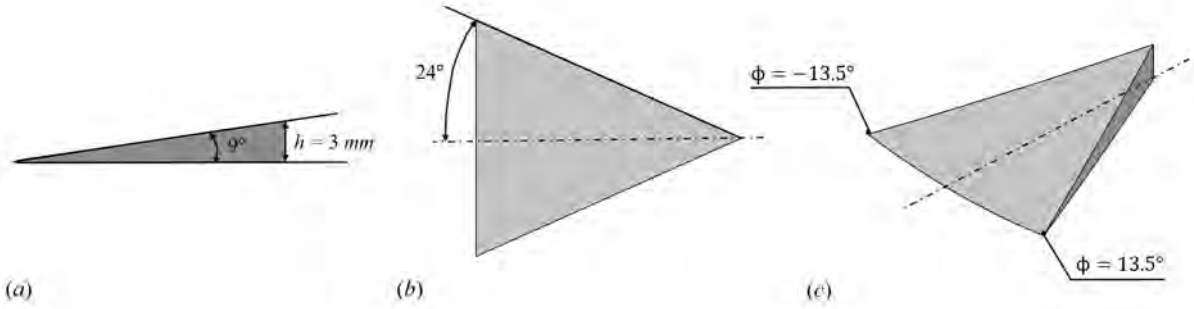


Figure 3: Axisymmetric VG geometry (a) device cross-section (b) top-view (c) isometric view.

which is the same thickness as the incoming boundary layer ($h = \delta_0$). While VGs with heights less than δ_0 will incur lesser drag penalties, the focus of the present work is on the physical changes induced by the VGs themselves, rather than optimization. As shown in Fig. 3(a), the microramp half angle is approximately 9° at any given circumferential location. This ramp cross-section is swept in a $\pm 24^\circ$ arc about the device centerline (with a radius equal to that of the half-isolator surface), per Fig. 3(b). This yields a width in the cylindrical model coordinate system of $\phi = 27^\circ$. Hence, a microramp placed coincident with the model centerline would have spanwise limits of $\phi = \pm 13.5^\circ$.

The effects of both individual devices and arrays on the SBLI were examined. Single VGs were placed coincident with the model centerline at $\phi = 0^\circ$. Babinsky *et al.*[21] showed that the modifications to the separated flow caused by the VGs did not vary considerably with streamwise placement in a range between 19 and 56 device heights upstream of the inviscid reflection point. Therefore, only two streamwise placement locations were tested for the single microramps. The first and closest position to the interaction resulted in a distance of 50 mm between the microramp trailing edge and compression corner (i.e. $x_{TE} = -50 \text{ mm}$). In nondimensional terms, this is roughly 17 device heights, or less than the minimum spacing of Babinsky *et al.*[21]. A single VG and array of three devices were tested at twice this streamwise distance (33 device heights) from the compression ramp leading edge, at $x_{TE} = -100 \text{ mm}$. In the array configuration, two microramps were placed symmetrically about a central device with centerlines at $\phi = \pm 45^\circ$.

4.4 Axisymmetric Inlet Isolator Model

A fully axisymmetric inlet/isolator with a constant area circular cross-section isolator (i.d. = 38 mm) was used. The overall schematic of the test article can be seen in figure 4. The test article consists of a steel inlet section, a transparent isolator section made of acrylic, and a steel end section for the isolator. The steel inlet can be swapped out for different compression angles; in this paper we will predominantly look at the results obtained with a 10° compression angle. The contraction ratio, defined as the ratio between the inlet capture area to the isolator cross-

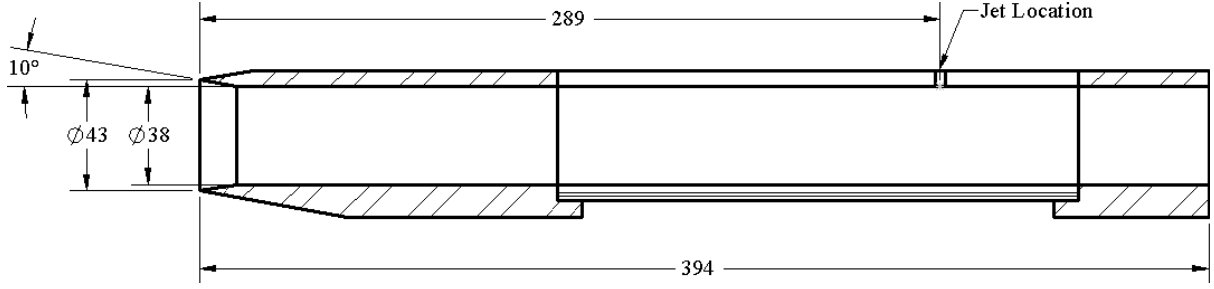


Figure 4: Axisymmetric inlet/isolator configuration with flow from left to right (dimensions in mm)

sectional area, is 1.284. In all discussions, the x location is defined as the streamwise distance downstream of the inlet plane (defined as $x = 0$) and horizontal mid-plane of the isolator connects the azimuths $\phi = 0^\circ$ and 180° .

For a limited set of experiments to delineate the upstream propagation length scale of acoustic information, an external shock wave was generated and located at a predetermined location by injecting a steady sonic jet. A circular jet injection port of diameter 4.8 mm was located at $x = 289 \text{ mm}$ and the sonic jet was injected normal to the streamwise direction. Compressed nitrogen was used for high-momentum sonic jet injection and jet to isolator mass flow ratio was varied between 1.3% and 6.6%. A Peter Paul solenoid valve with a 2.4 mm orifice was used to precisely control the starting time of the jet and allows for a rapid rise in jet pressure. For a typical test run, the jet was injected for 5 seconds of steady mass discharge.

5 Experimental Methods

The experimental methods utilized in this study are highly similar to the ones used in previous examinations of the H-I SBLI by the present authors[2, 1]. A key difference is the usage of a higher sensitivity camera, which results in improved signal-to-noise ratios.

5.1 Surface Streakline Visualizations

Surface streakline visualizations were performed for the baseline interaction and all VG-controlled configurations in order to gain a qualitative understanding of the flowfields. The visualization medium consisted of a mixture of mineral oil and DayGlo Rocket Red pigment, which fluoresces red when exposed to UV light, providing excellent contrast against the flat black model surfaces. The mixture was painted onto the model prior to each run and remained wet until well after tunnel shutdown. Top-down videos of the flowfield were recorded at 60 Hz using a Nikon D5200 DSLR camera, with illumination provided by an LED UV flashlamp. In this way, regions of low shear stress (near the separation line, for instance) could be identified through pigment accumulation. Moreover, as the pigment followed the flow over the entire duration of each test, flow directions could be deduced in some areas, although only in a mean sense as the streakline motions were effectively low-pass filtered through the inertia of the visualization mixture. The video field-of-view was sufficient to achieve simultaneous imaging of the entire interaction, including reattachment. All streakline visualizations depicted in the present work are averages of 200 instantaneous frames.

5.2 Pressure Sensitive Paint Measurements

Pressure sensitive paint (PSP) was used to provide additional insights into the near-wall pressure fields of the VG and interaction flowfields. The PSP system utilized is commercially available from Innovative Scientific Solutions Inc. (ISSI). Regions of interest (interaction regions/VGs) were first coated with a basecoat of polymer binder to generate a uniformly reflective surface

and minimize illumination errors. Next, several coats of single-luminophore UniCoat PSP were applied. UniCoat has an effective pressure range of $1 - 200 \text{ kPa}$ and a response time of 750 ms , limiting its application to mean measurements only. When subjected to 400 nm excitation (LM2X-400 LED module), the PSP fluoresces red with an intensity that is inversely proportional to the partial pressure of oxygen at the surface. Imaging was performed at 10 Hz using a 14-bit pco.2000 camera fitted with a Nikon 50 mm telephoto lense and long pass filter. The camera field of view was similar to that of the surface streakline visualizations.

A calibration between PSP intensity and wall pressure was initially performed by mounting the painted model in the tunnel, sealing the test section using a custom chamber, and evacuating it with a vacuum pump. Air at the same nominal humidity as the air in the tunnel storage tanks (dew point of 222 K) was then gradually introduced into the vacuum chamber to produce a calibration curve. An in-house MATLAB code was developed for the generation of the subsequent pressure fields from the raw data (calibration applied at the pixel level), which are averaged over 250 wind-on frames for each configuration (5 separate runs each). All pressure fields displayed in the present work are unbinned (spatial resolution of 16 pixels/mm), and all pressure profiles were produced by binning the pressure fields at 25×25 pixels, resulting in a spatial resolution of around 0.7 pixels/mm . Errors in the PSP values are expected to be 10% or less. Wherever possible, the PSP is directly compared to transducer data.

5.3 Planar Laser Scattering

Planar laser scattering (PLS) was conducted to obtain details about the streamwise development of off-body structures. Water vapor was introduced into the wind tunnel storage tanks by increasing the humidity of the air delivered by the compressor unit to a dew point of $\approx 273 \text{ K}$. After expanding through the wind tunnel nozzle, the vapor condensed into uniformly distributed ice particulates that scattered the light provided by a dual-cavity Continuum Surelite PIV laser. The intensity of the scattering is directly proportional to the density of ice particulates and increases in regions of high fluid density, which allows shock waves and other flow constructs to be identified[22]. The laser pulse repetition rate was 10 Hz , at an energy of approximately 80 mJ/pulse (wavelength of 532 nm). The laser beam was converted into a transverse/vertical sheet with a width of approximately 80 mm and thickness of around 1 mm . Measurements were made at multiple streamwise stations behind the VGs and throughout the interactions. Images were captured using an interline CCD camera (pco.2000) fitted with a Nikon 105 mm telephoto lens and a laser line filter. The camera field of view measured approximately $105 \text{ mm} \times 105 \text{ mm}$ in the transverse/vertical direction, yielding a spatial resolution of around 15 pixels/mm . The imaging plane was offset from the laser sheet by less than 5° to facilitate viewing through the tunnel window, the resulting perspective distortion was accounted for and corrected. Spatial errors in the presented intensity fields are estimated to be less than 5%. The PLS images were processed using an in-house MATLAB code by first correcting for the background obtained from an average of 20 wind-off images prior to tunnel start. Following this operation, row-by-row intensity normalization by the freestream PLS signal was performed for 100 wind-on images to correct for transverse laser sheet inhomogeneity.

5.4 High-Frequency Wall Static Pressure Measurements

In order to quantify the mean and unsteady characteristics of each SBLI, wall static pressure measurements were performed. The sensor utilized was the Kulite Semiconductor Products, Inc. model XCQ-062-15A ultraminiature pressure transducer. The Kulite was selected for its compactness (and subsequently good spatial resolution), having a nominal head diameter of 1.7 mm . The transducer also has an effective frequency response of 50 kHz , which is adequate to capture the full range of unsteady motions characteristic of the interactions. The transducer signals were first amplified using a Vishay model 2350 signal conditioning unit before being

low-pass filtered at a cutoff frequency of 50 kHz with a DL Instruments dual filter; the signals were finally acquired at a rate of 400 kHz using a 16-bit National Instruments USB-6366 analog-to-digital converter. Three independent sets of pressure measurements were acquired for each configuration, mean wall pressures were computed from a 2 s snippet (8×10^5 samples) of each test.

The transducer heads were mounted flush with the interior surface of the half-isolator model to allow for the best possible sensitivity. One streamwise row of transducers was located directly behind the VGs at $\phi = 0^\circ$, with the most downstream transducer edge coincident with the leading edge of the compression ramp as depicted in Fig. 5. Upstream transducers were spaced every 4.5 mm , measured center to center. It should be mentioned that the compression ramps could be moved upstream or downstream by around 2 mm , this artificially doubled the achievable spatial resolution. Measurements were also performed outside of the VG wake at $\phi = 30^\circ$ (or in the case of the arrays, between VGs at $\phi = 22.5^\circ$) to gage the effect of the devices on the non-energized flow regions. Because the transducers could not be moved to a different circumferential location for these tests, the devices themselves were physically shifted along the circumference instead. The transducer leads ran out of the back of the model through a shielding strut before exiting the test section. For all tests, one or more transducers were located beneath the incoming boundary layer, separation shock foot, and downstream separated flow regions.

All pressure signals were recorded over a 5 s period, excluding tunnel startup time. Three independent sets of pressure measurements were acquired for each test case and circumferential location. An in-house MATLAB code was developed for data processing, the methodology of which was as follows; First, a 2 s period of data (8×10^5 samples) was selected for each test. A 25 kHz software low-pass filter was then applied to this period, before mean/fluctuating wall pressures were computed at each station. Next, power spectral densities were calculated for the same 2 s interval using Welch's method of windowed Fourier transforms[23] with a window size of 5000 samples and 50% overlap between windows, resulting in a frequency resolution of $\Delta f \approx 50\text{ Hz}$. A two-point cross-coherence analysis was also performed for each test with the same parameters, excluding the software low-pass filter. Uncertainty in the transducer pressure values has been estimated at 3%.

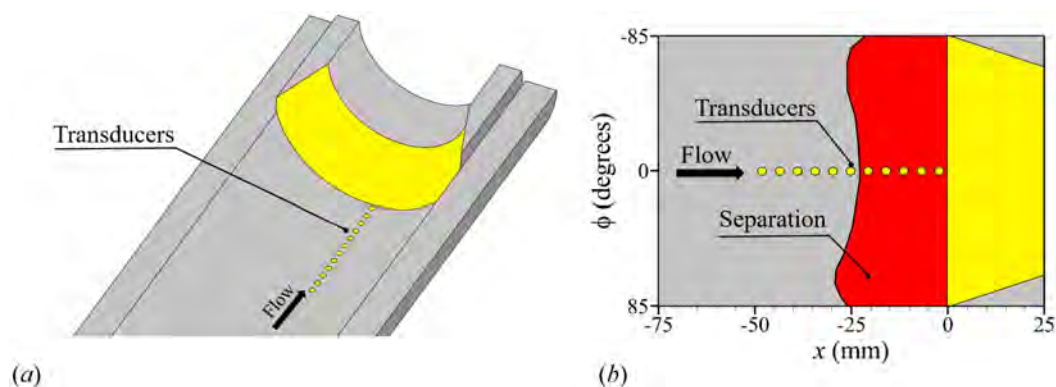


Figure 5: (a) Isometric view of half-isolator instrumentation, ramp face and pressure transducers in yellow (b) top-view of half-isolator instrumentation relative to example flow features of interest, coordinate system also pictured.

6 Results and Discussions

6.1 Investigations of shock oscillations and communication pathways in an Axisymmetric Inlet Isolator

6.1.1 Executive Summary

An axisymmetric 2D inlet/isolator was investigated to elucidate the shock foot dynamics and boundary layer fluctuation modifications that occur within the duct. The chosen inlet angles and application of no back pressure caused the boundary layer to remain attached across each shock impingement location along the entire duct length. The absence of local mean and instantaneous flow separation was also confirmed by multiple redundant measurements. Wall pressure fluctuations were measured across two SBLI units that occurred within the inlet/isolator to shed light on the dynamics of pressure fluctuations and shock foot oscillation spectrum. The shock foot pressure fluctuations on the other hand exhibit the high frequency content that arise from the passage of the boundary layer structures as well as a characteristic broadband hump at a very low frequency on the order of 50 Hz. This hump was not observed in either of the incoming or relaxing boundary layer. Further investigations revealed that these shock oscillations are caused by near-wall interactions that occurred within the local influence zone. A physical model is proposed that postulated the low frequencies result from the damping nature of the skin friction to the shock motions as the shock foot responds to the sudden excursions in the incoming boundary layer momentum. The PSD obtained from this model compared very well with both internal and external flow SBLI that maintained unseparated boundary layers. The details of this study are elaborated below.

6.1.2 Inlet Flowfield Characterization

A wall static pressure profile sweep along the entire isolator section was performed to quantify the flow development within the isolator section. Figure 6(a) shows the mean and r.m.s. pressure profiles measured along the isolator at $\phi = 0^\circ$ by the transducer, and figure 6(b,c) shows the corresponding 2D measurements using the PCPSP. No back pressure was introduced while making these measurements. For the point measurements, region near shock impingement locations have a finer transducer spacing for more in-depth quantifications. The mean wall static pressure shown in figure 6 reveals four sharp increases (red regions in figure 6(b)) in the mean pressure field measured by the PCPSP of which two are captured by the pressure transducers figure 6(a)). The pressure peaks correspond to where the inlet/isolator shock train impinge the isolator surface. The pressure peaks are followed by a gradual decrease due to the expansion fan that originate from the inlet shoulder. The azimuthal variation of pressure field for at a given streamwise location exhibits a slightly higher value at the middle of frame ($\phi = 0^\circ$) compared to other locations. A part of this inflated pressure could be due to slight defocusing of the PCPSP signal in the region away from $\phi = 0^\circ$ and the extent of defocusing increases with increasing azimuthal offset from $\phi = 0^\circ$.

The corresponding unsteady pressure loading are shown as the wall-pressure normalized $p_{r.m.s.}$ profiles in figure 6. Interestingly, both pressure transducer and PCPSP reveal only two peaks in the $p_{r.m.s.}$ which contrasts the four peaks observed in the mean pressure field of PCPSP measurements figure 6(b)). The absence of the other two peaks (at $x = 150$ mm and $x = 335$ mm) from the PCPSP is because the PCPSP measurement domain just excludes the peak shock loading locations in the most upstream and most downstream shock legs. As one traverses across any individual shock foot, the normalized $p_{r.m.s.}$ increased from about 0.01 in the boundary layer to 0.03 beneath the shock foot. The normalized observed from the PCPSP measurements is slightly depreciated because the maximum frequency that is resolved is 4 kHz, compared to 25 kHz of kulite pressure transducers. The bands of elevated normalized $p_{r.m.s.}$ that indicates the shock foot maintains a consistent value across the azimuthal direction and show a maximum

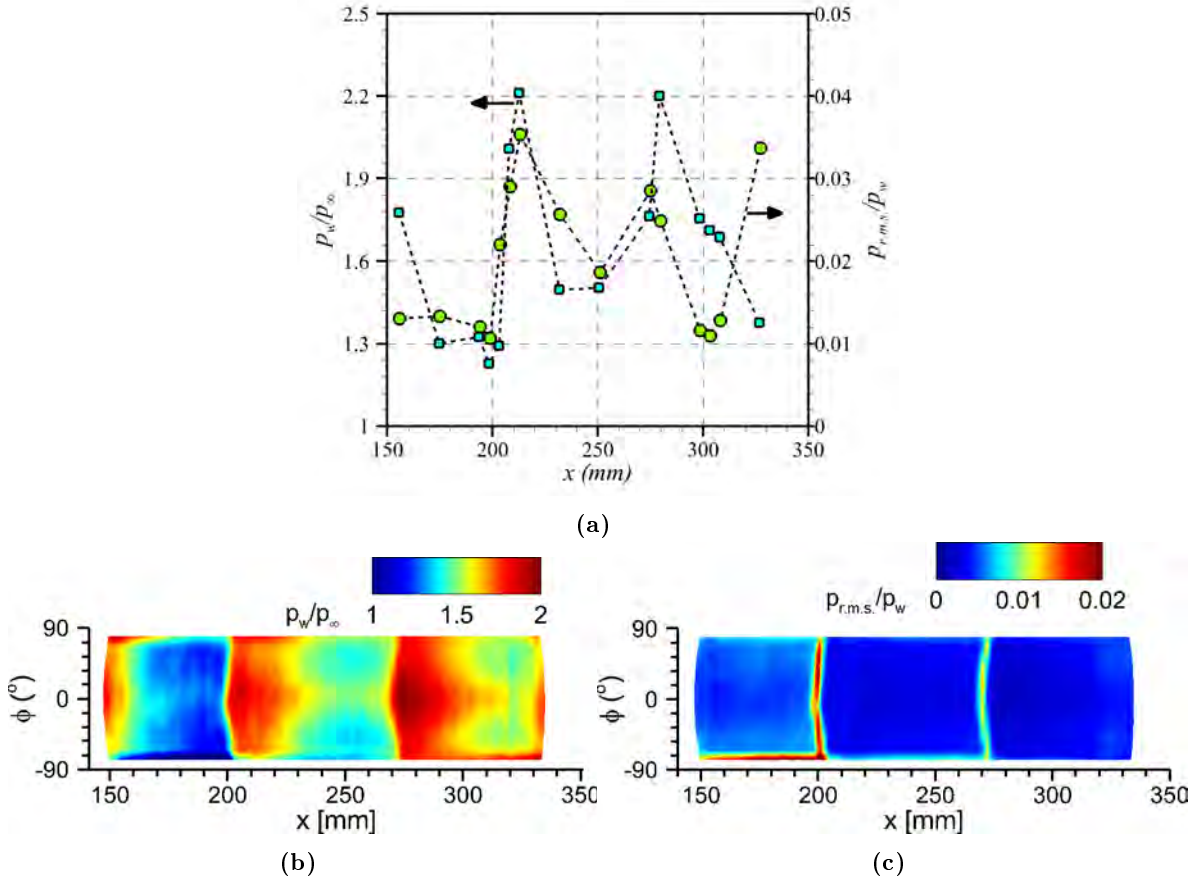


Figure 6: Mean and r.m.s. static pressure distribution along isolator wall: Figure (a): kulite transducer measurements at $\phi = -90^\circ$, figure (b): mean pressure field from PCPSP measurements, and figure (c): r.m.s. pressure field from PCPSP measurements

end-to-end deviation of less than 5 mm in the streamwise direction. Furthermore, the streamwise extent of the elevated normalized $p_{r.m.s.}$ corresponds to the length scale of shock oscillations. Figure 6(c) reveals that both the shock foot imaged by the PCPSP oscillates over a distance of 5 mm. For the subsequent discussions, we consider only the two shock legs that intersects the isolator surface at $x = 205$ mm and 275 mm. Furthermore, the shock impingement located at $x = 205$ mm will be referenced as the ‘upstream shock leg’ and the shock impingement located at $x = 275$ mm will be referenced as the ‘downstream shock leg’.

The fractional rise in pressure due to the individual shock legs on the isolator surface was compared with the empirical inviscid pressure ratio determined by [18] for incipient separation onset. It should be noted that the wall pressure takes longer development length downstream of the shock due to viscous effects and the values are typically depressed compared to the inviscid pressure rise. However, it can be argued that the peak pressure obtained in the downstream vicinity of the shock is still very close to the inviscid pressure rise, thereby justifying the comparison within certain margin. For the freestream Mach number implemented here, Korkegi criterion suggests a pressure rise of 3.7 times across a shock is necessary to separate a turbulent boundary layer. The upstream shock leg (located at $x = 205$ mm) indicates a pressure ratio of 1.8 times, while the downstream shock leg (located at $x = 275$ mm) indicates a pressure ratio of 1.5. Both these values are well below the limit determined by Korkegi, strongly suggesting that the mean flow remains attached across both of these shock impingement locations. Furthermore, the mean surface streakline field (not shown) also evidences the lack of mean separation, and the r.m.s of the planar laser scattering (PLS) fields (not shown) evidences the absence of any

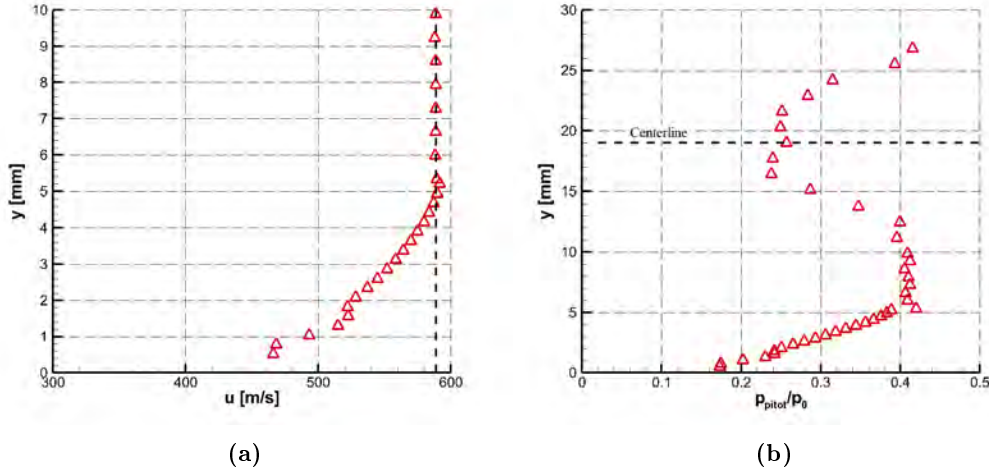


Figure 7: Velocity and pitot pressure profiles across the isolator at $x = 241$ mm: (a) velocity profile; (b) ratio between pitot pressure and stagnation pressure of freestream

instantaneous separation that may be observed with incipient separation conditions. Thus, the shock interactions with the isolator boundary layer do not generate either mean or instantaneous boundary layer separation.

The velocity profile within the inlet/isolator were obtained using pitot pressure sweeps to establish the extent of symmetry in the flowfield as well as provide a determination of the boundary layer thickness at the measurement station. The measurement station was chosen to be $x = 240$ mm which lies in between the upstream and downstream isolator shock legs. Figure 7(a) shows the mean velocity profile for the inlet/isolator, where each velocity value were averaged over 10000 data samples that were highly repeatable. The nearest location to the isolator wall with reliable data was 0.5 mm; this offset resulted only in the outer part of the boundary layer be measurable. It can be observed from figure 7(a) that the freestream velocity in the measurement station was ≈ 590 m/s. The velocity profile starts to decrease at about 5 mm from isolator wall and the velocity drops to 78 % of the freestream value at 0.5 mm from the wall. This provides an estimate on the boundary layer thickness, displacement thickness, and momentum thickness of $\delta = 5$ mm, $\delta^* = 1.84$ mm, and $\theta = 0.31$ mm, respectively, and the fraction of viscous region ($2\delta/d_{\text{iso}}$) at about 0.26. The pitot pressure profile shown in figure 7(b) is fairly symmetric about the model centerline. A deficit in pitot pressure can be observed in the vicinity of the centerline due to a small near-normal-shock stem that was formed around the centerline. Close to the wall, the pitot pressure monotonically decreases within the boundary layer due to viscous dissipation. Overall, a fair amount of mean flowfield symmetry can be observed about the model centerline.

6.1.3 Power Spectra Evolution Across the Isolator Channel Length

The power spectral densities (PSDs) of wall pressure fluctuations at various streamwise locations were calculated to investigate the energy content of the self-sustained shock oscillations and the boundary layer before and after getting processed by the shock wave. Figure 8(a) shows the frequency pre-multiplied PSD of the incoming boundary layer that interacts with the upstream shock leg measured at $x = 196$ mm, which is located 9 mm upstream of the shock impingement location. Figure 8(a) shows that the boundary layer contains a broad range of frequencies that span several decades and the frequency-multiplied PSD exhibits a monotonic increase with frequency over the range measured. This trend is consistent with classical turbulent boundary layer spectrum where the peak energy is located at the boundary layer convection frequencies

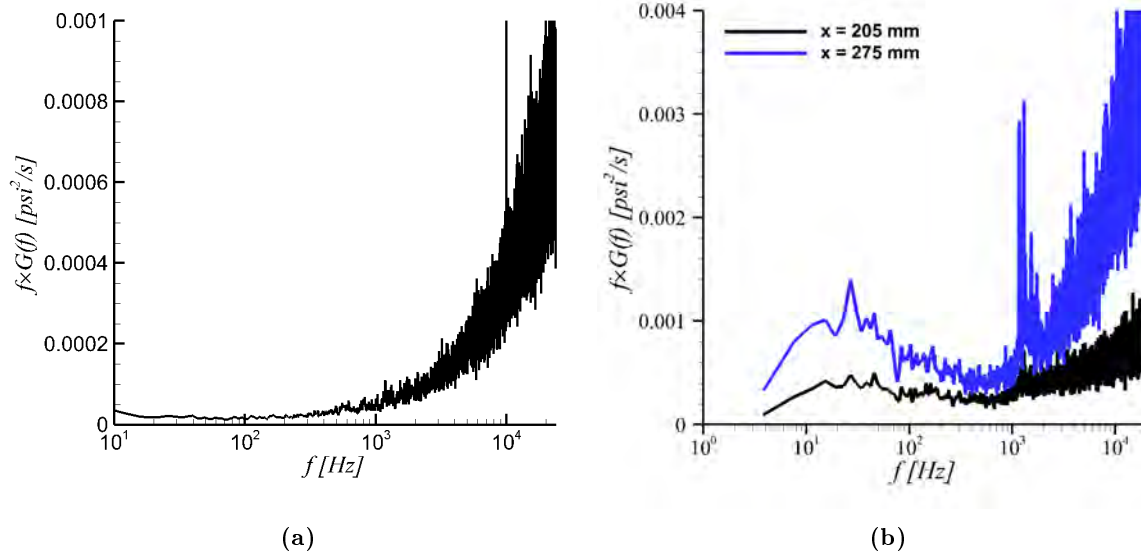


Figure 8: Frequency pre-multiplied pressure spectral density of wall pressure fluctuations measured beneath (a): boundary layer at $x = 196 \text{ mm}$ located 9 mm upstream of upstream shock leg, and (b) upstream and downstream isolator shock legs.

(u_∞/δ) . In the present flow, this peak lies in the excess of 100 kHz, which is greater than the maximum measurable frequency of the transducers used.

The frequency pre-multiplied PSD wall pressure fluctuations beneath the upstream shock leg ($x = 205 \text{ mm}$) and the downstream shock leg ($x = 275 \text{ mm}$) are presented in Figure 8(b). Overall, the PSD beneath both shock foot locations at any given frequency is elevated compared to the corresponding magnitude in the incoming boundary layer. Interestingly, the PSDs beneath both shock foot locations show broadband elevated values between 10 Hz and 500 Hz, with a peak magnitude occurring at around 50 Hz. To provide a context to the hump in the shock oscillation PSD with respect to duct frequency, the characteristic duct frequency based on flow through time, $f_a = u_\infty/L$, (where u_∞ is the freestream velocity and L is the duct length) is 1550 Hz; thus, the shock oscillation hump occurs at about $3\% f_a$. The trough in the PSD at around 500 Hz is followed by an steep increase in PSD with frequency up to the maximum measurable frequency of 25 kHz. This steep increase in PSD with frequency is quite suggestive of the turbulent boundary layer fluctuations that get amplified across the shock wave. Finally, the sharp spikes at around 1 kHz is a remnant of electronic noise and will be ignored in the future analysis. Comparing the PSD magnitudes reveal the downstream shock leg has substantially higher strength compared to upstream shock leg oscillations. However, this difference could all be due to the differences in the relative proximity of the transducer to the respective shock foot.

The power spectral density of the boundary layer that is processed by the shock wave and its subsequent downstream evolution is analyzed to learn if any of the low frequency hump in the shock oscillations has its origins from the downstream boundary layer. For comparison, the PSD of the incoming boundary layer to the shock leg and the PSD beneath the shock foot are also presented. Figure 9(a & b) shows the PSD of the incoming boundary layer (measured 5 mm upstream of the mean shock foot location), shock foot oscillations, and the relaxing boundary layer (measured 5 mm downstream of the mean shock foot location) for the upstream and downstream shock legs, respectively. The power spectra without frequency pre-multiplication is presented to highlight the changes in PSD across all frequencies. The red, blue, and black curves in both figures represent the incoming boundary layer, relaxing boundary layer just downstream of the shock, and the shock foot locations, respectively. Comparing the PSD of the incoming and relaxing boundary layer for a given shock foot, it can be observed that the relaxing boundary layer has noticeably higher energies at frequencies exceeding 100 Hz, while the lower frequency

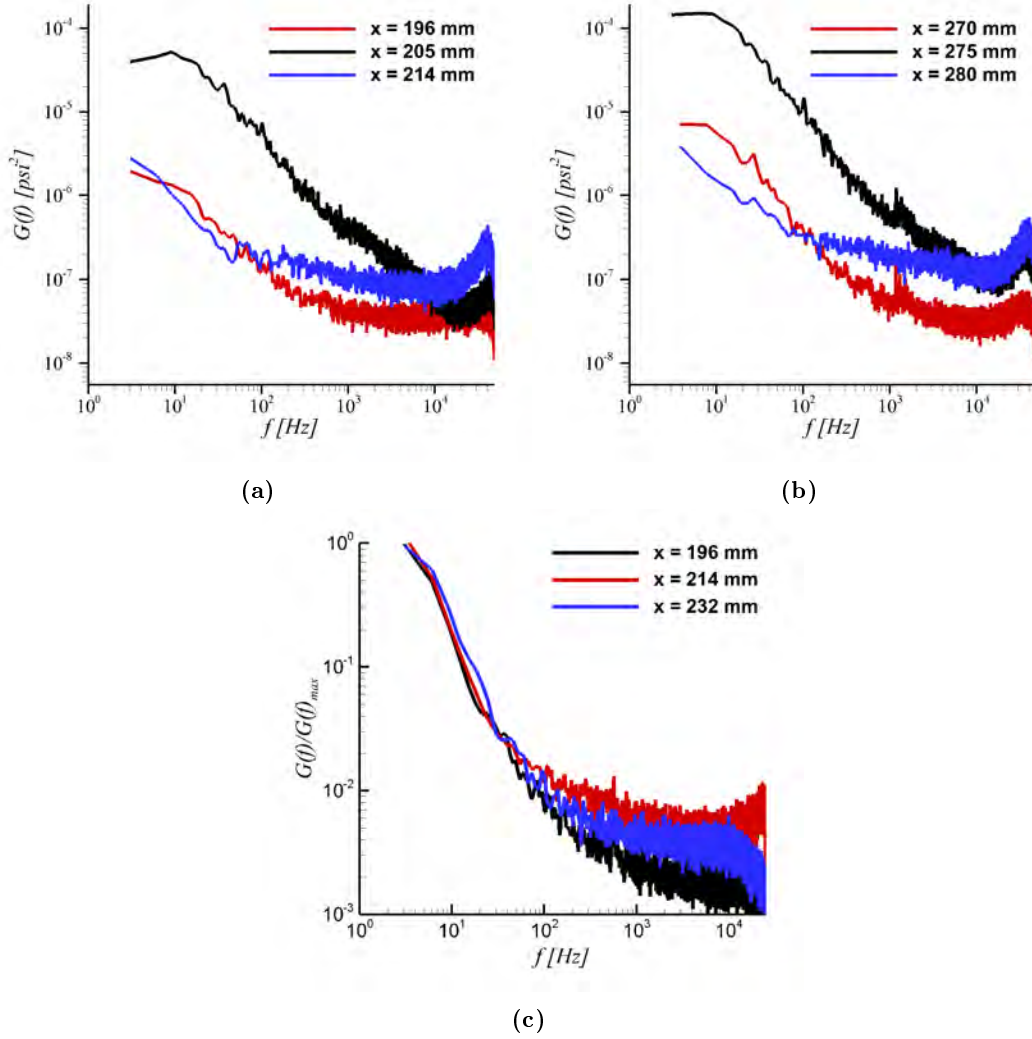


Figure 9: Power spectral densities of wall pressure fluctuations measured (a) in the vicinity of upstream shock isolator impingement location; (b) downstream isolator shock impingement location; and (c) at locations before and after upstream isolator shock impingement location.

have similar or lower strength depending as the incoming boundary layer on the streamwise location. To provide a context on the relative strength of PSD at different frequencies, the PSD of the relaxing boundary layer between 100 Hz through 10 kHz exhibit an amplitude of about 5% of the peak measured at 10 Hz. The corresponding PSD for the incoming boundary layers compared to the shock oscillation are at least an order of magnitude lower. Beyond 10 kHz, both the incoming boundary layer and the relaxing boundary layer exhibit a similar magnitude uptick in their PSD. Overall, the important difference in the PSD of the relaxing boundary layer is an order of magnitude increased energy at the intermediate frequency between 100 Hz through 10 kHz.

The evolution of the PSD of the relaxing boundary layer processed by the upstream shock leg is shown in Figure 9(c). The values are normalized by the peak power to illustrate the relative strength of the PSD at different frequencies. The PSD of the canonical turbulent boundary layer that is incident on the upstream shock leg is also presented in figure 9(c) for comparison. It can be observed from figure 9(c) that the elevated strength at the intermediate frequencies greater than 100 Hz is carried over a considerable streamwise distance. For example, the PSD at $x = 232$ mm (28 mm downstream of the shock foot) shows that the normalized energy content at frequencies greater than 100 Hz lies in between those at $x = 214$ mm and canonical turbulent

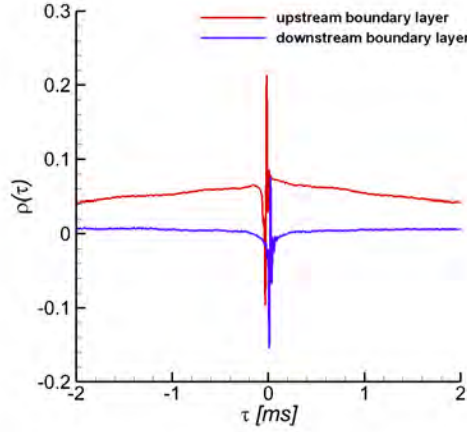
boundary layer (measured at $x = 196$ mm). The gradual dissipation of the high frequency continues with downstream distance, and by the time the downstream shock leg is encountered, the boundary layer has almost recovered to the canonical PSD distribution, as shown in the PSD of $x = 270$ mm location in figure 9(b). Figure 9(b) also exhibits an interesting uptick in the energy content by about five times at low frequencies (< 100 Hz) for the boundary layer incident on the downstream shock leg ($x = 270$ mm) compared to the boundary layer incident on the upstream shock leg ($x = 196$ mm) which may suggest some degree of back scattering in the boundary layer frequencies during the relaxation process. However, it should be noted that the PCPSP-based pressure field imaging shows that $x = 270$ mm lies at the upstream periphery of the $p_{r.m.s.}$ inflation associated with the downstream shock foot (figure 6(c)), which suggests the shock motion extends to $x = 270$ mm. Hence, the strengthening of low frequency pressure fluctuations at $x = 270$ mm could be simply an artifact of intermittent shock foot presence. Though not presented, a very similar downstream evolution was also exhibited by the relaxing boundary layer generated by the downstream shock leg.

6.1.4 Communication Between and Within a Shockwave Impingement Location

The discussions presented thus far show that there is a clear difference in the PSD of shock foot oscillations for the inlet/isolator when compared to the shock oscillations with 2D SBLI units with shock-induced separation, where the shock oscillations are governed by the separated flow pulsations. Interestingly, the very low frequencies of shock oscillations in unseparated SBLI cannot be directly traced to either incoming or relaxing boundary layer. Very few works so far has investigated the unseparated shock oscillations and none of them have made direct wall pressure measurements. Notable among the relevant works include that of [24] who performed hot wire measurements of an unseparated compression ramp interactions at various wall-normal locations starting from $y/\delta = 0.2$. The authors noted that the turbulent mass flux PSD in the near wall locations of an attached compression ramp interaction exhibited no noticeable spectral peaks at the low frequency range (~ 100 Hz) and the overall spectrum resembles that of the incoming boundary layer PSD due to turbulence induced jitter on the shock oscillations. It is not clear if a direct measurement of the shock oscillations using wall pressure measurements would have revealed a low frequency range oscillations in that study as well.

It should be remarked that the low frequency shock oscillation peaks were reported in the external and internal flows with shock-induced separation wherein the back pressure fluctuations as well as the separated flow pulsations provided the low frequency content to the shock oscillations. Considering there is no boundary layer separation in the flowfield considered in the present study, to the authors' knowledge this is the first study to document these very low frequency oscillations in unseparated flows. Such low frequency shock oscillations can potentially couple with structural resonances or cause hot spots within the ducts, and hence would necessitate additional aero-structural considerations during design.

Of particular interest is what causes the low frequency shock oscillations in these unseparated internal flows. While there are several studies that provide detailed insights into the upstream propagation of pressure fluctuations in separated flows, the issues with extending these ideas to unseparated flow of the current study include: 1) the lack flow separation in the current inlet/isolator flowfield cannot seed the shock oscillations with low frequencies associated with separated flow pulsations; and 2) the sonic line is estimated to lie well within one mm from the isolator wall ($< 0.03D$) based on the boundary layer profile presented in figure 7a, and these near-wall regions are expected to have very high shear and appreciable viscous forces. The latter two factors are expected to cause significant damping and dispersion effects to the upstream propagating acoustic waves in the subsonic region of the boundary layer. It is not clear how far the pressure perturbations can travel upstream in these non-canonical relaxing boundary layers. Hence, to unravel the driving mechanisms of the shock oscillations, we need to first answer if the oscillations arise because of global interactions across the duct length or due to



(a)

Figure 10: Cross-correlation coefficient between upstream isolator shock impingement location and boundary layer in the upstream and downstream vicinity of the shock foot. The reference transducer was located beneath the shock foot.

local interactions around the shock foot. Hence, the subsequent discussions will help pin the communication mechanisms and length-scales of the downstream and upstream propagation of pressure fluctuations that originate within the isolator channel.

6.1.5 Downstream Communication through the Isolator Channel

Knowing how the upstream and downstream shock legs influence each other's dynamics is next in unfolding the dynamics of the isolator system. Two-point cross-correlations are extremely useful in determining information propagation directions and speeds and thus was the method of choice in this study. Figure 10 shows the two-point cross-correlations between the pressure fluctuation emanating from the upstream shock leg and the incoming boundary layer, measured 9 mm upstream of the shock leg. Two clear features are observable in the correlation plots: a sharp maxima that is located at a very short negative time-lag value, and a smooth broader region of decreasing correlation that extends along positive and negative time-lags. The negative time-lag of the sharp maxima shows that the fluctuations in the incoming boundary layer leads the shock fluctuations and converting this lag to a corresponding velocity gives an approximate speed of downstream propagation of 540 m/s ($0.87 \times U_\infty$), which is very close to the boundary layer convection velocity. The relatively modest value of the correlation peak suggests that the shock fluctuations have a small but non-trivial influence from the downstream convection of the turbulent boundary layer structures.

Similarly, the two-point correlation between the shock foot and downstream boundary layer reveals that the shock foot fluctuations leads the downstream boundary layer fluctuations with the same propagation speed as the boundary layer structures. This suggests that the correlation occurs from the amplification of the boundary layer turbulence across the shock wave. Interestingly, the broad decay in the correlation observed with the incoming boundary layer is absent with the relaxing boundary layer. Once again, the cross-correlation magnitudes are quite modest suggesting a weak contribution from the shock processing on the downstream boundary layer.

The two-point cross-correlations between the upstream and downstream shock leg are next considered and presented in Figure 11. A strong negative correlation with negative time lag at the correlation peak is observed. The negative time lag peak signifies that the upstream shock leg pressure fluctuations lead the downstream shock leg fluctuations. Once again, the time lag of the peak correlation was converted to the corresponding velocity of pressure fluctuation propagation,

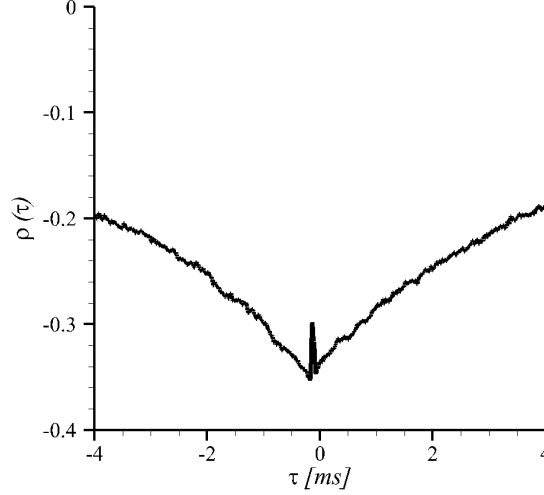


Figure 11: Two-point cross-correlation coefficient between the pressure fluctuations at the upstream and downstream shock impingement locations. The reference transducer is located at the downstream shock impingement location.

which stood at 512 m/s or $0.82 \times U_\infty$. This value being very close to the boundary layer convection velocity strongly suggests that the communication of the perturbations between shock legs occurs through the boundary layer structure convection. In other words, the fluctuations introduced by the upstream shock foot on the boundary layer convects along the isolator and elicits a corresponding response from the downstream shock foot.

The broad spread of the cross-correlation between the shock legs over large time lags, observed in Figure 11, suggests a wide range of shock oscillation frequencies are correlated. Hence, it is important to learn which oscillation frequencies are best correlated and communicated across. The coherence spectrum provides this piece of information as it forms the spectral analog of the cross-correlations. Whereas a coherence of unity represents a linearly coupled system, a value of zero would represent uncoupled system. Figure 12 shows the coherence magnitude between the upstream and downstream shock legs over the measured frequency range. A very high coupling ($C_{xy}(f) = 0.6 - 0.8$) in the shock oscillations are observed at the low frequencies of interest ($f < 100$ Hz) and the values approach zero at high frequencies representative of shock jitter. This shows that the propagation of perturbations between the shock legs through boundary layer convection indeed provide the downstream coupling channel for low frequency shock oscillations. While it was not presented here, essentially identical findings were also obtained with the other isolator configurations that were tested with changing isolator diameter and inlet angle. Summarily, the downstream propagation of pressure fluctuations at low frequencies of interest occurs over large streamwise distances via boundary layer convection.

6.1.6 Upstream communication through the isolator duct

The upstream propagation of pressure fluctuations in the isolator duct with shock-induced flow separation were shown to occur via acoustic propagation through the thick subsonic regions [25]. While the previous studies have shown that such propagation can extend across the entire isolator length, largely facilitated by the thick subsonic regions within the duct, it is not very clear how long such propagation can occur with unseparated boundary layers. Previous study used theoretical analysis to show the upstream propagation of acoustic waves within a canonical supersonic turbulent boundary occur over a length scale of local boundary layer thickness [26, 27]. However, boundary layers that get processed by the shock exhibit significant deviation in their structure and dynamics and it is not clear whether the upstream propagation distances are still

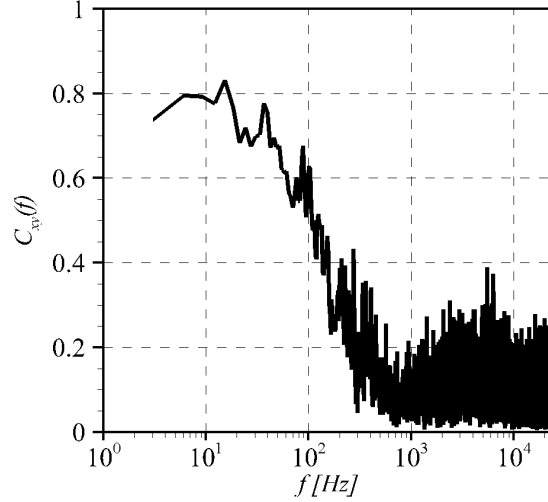


Figure 12: Two-point cross-coherence of pressure traces between the upstream and downstream shock impingement locations to illustrate the shock oscillation coupling at different frequencies.

maintained.

Experimentally unraveling the acoustic propagation length scales in unseparated flows are challenged by the very small acoustic pressure fluctuations amplitudes, anticipated dissipation of these waves by viscous forces with distance, and the limitation in the spacing of the transducers within close distance and the sensitivity of PCPSP measurements. All these factors would substantially diminish the magnitude of statistical correlations and coherences for making meaningful inferences unless one has an estimate of the length scales over which acoustic propagation occurs prior to making cross-correlation studies. Hence, the approach undertaken here is to first demonstrate the length scale of upstream acoustic propagation from an alternate setting.

The alternate setting comprises of having two independent shocks within the isolator and observing at what separation between the shocks would cause the upstream shock to respond to the presence of the downstream shock. The experimental setup to create this situation consists of injecting a sonic jet into the isolator, which creates a jet shock system that could be placed at predetermined downstream distances from the isolator downstream shock foot. Having a jet shock creates strong perturbations to the isolator shock train that will help make meaningful measurements. During the experiments, the isolator shock leg locations were fixed since the inflow conditions and inlet geometry remained unchanged, and the jet port location was always at $x = 289$ mm (14 mm downstream of the undisturbed isolator downstream shock leg). The mass flow rate of the jet is varied to change the jet separation shock location over $x = 285$ mm (10 mm downstream of the isolator downstream shock leg) all the way to physically merging with the isolator downstream shock leg. The effect of having different strength jet on the shock oscillations are quantified by investigating the PSD of pressure fluctuations beneath the upstream and downstream shock leg.

First, the impact of the jet injection on the isolator upstream shock leg, which is separated from the jet separation shock by a streamwise distance of 80 mm - 100 mm or approximately 16δ - 20δ (δ measured in between the upstream and downstream shock legs at $x = 240$ mm) depending on the jet strength, is investigated. Figure 13 presents the calculated PSDs at the two isolator shock foot locations without and with mass injection, where the jet strength is quantified by the mass flow rate ratio between the jet and isolator flow from the inlet capture. Each PSD presented is an average of three highly repeatable test runs taking 10^6 data points (2 seconds) for each run. The range of injection strength implemented here provides an examination of the upstream shock leg under two situations: first, when the jet shock is located far enough downstream of the

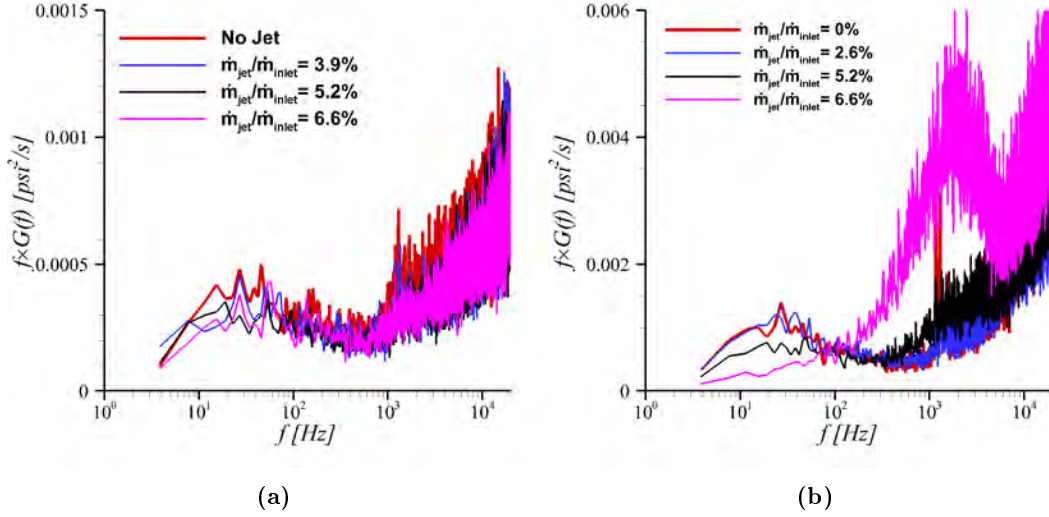


Figure 13: Frequency-multiplied PSD underneath shock impingement locations with various jet mass injection strengths: (a) upstream shock impingement location; (b) downstream shock impingement location

isolator downstream shock leg that there is little to no modification in its unsteady characteristics ($\dot{m}_{jet}/\dot{m}_{iso} < 5.2\%$), and the second when there is a considerable modification of the mean and unsteady characteristics of the isolator downstream shock leg ($\dot{m}_{jet}/\dot{m}_{iso} \geq 5.2\%$) due to physical merging (figure 13(b)). It should be noted that the isolator downstream shock leg after physical merging with the strongest jet shock is located about 16δ downstream of the upstream shock leg. It can be observed from Figure 13(a) that the jet injection of various strengths did not cause any appreciable qualitative or quantitative change in the upstream shock leg oscillation PSD. This indicates that perturbations caused both by the jet injection and gross modification to the downstream shock leg are not felt at the upstream shock foot location, signifying that the acoustic pressure fluctuations cannot propagate $O(10)$ boundary layer thicknesses upstream. It should also be noted that whereas only shock oscillation information is shown here, the observed mean wall pressure as well as r.m.s. and other higher order statistics of pressure fluctuations did not exhibit any statistically significant changes as well.

Having established a first upper bound of upstream acoustic propagation at $O(10)$ boundary layer thickness, a further refinement is obtained by examining the isolator downstream shock leg oscillation response to the jet injection. In this situation, the mean jet shock is located between 10 mm (2δ) through 0 mm (physical merging) from the downstream shock leg. At this close proximity range, it is important to consider the jet separation shock oscillation that can cause instantaneous physical merging with the isolator downstream shock leg, which may be wrongly attributed to acoustic propagation. To obtain an upstream extent of the jet separation shock motion, the r.m.s. of the PLS fields were computed and a threshold value of 20 % above the inviscid core region r.m.s. was chosen to delineate the upstream extent of separation shock motion at a given wall normal distance. This was extrapolated to the isolator wall to obtain an estimate of upstream location of jet separation shock foot motion. As a redundant measurement, the surface streakline images were analyzed to provide the location of the mean upstream influence line which again estimates the upstream travel of the jet separation shock. A more concrete determination of the region of jet separation shock motion was also made using unsteady wall pressure measurements. Despite these redundant measurements, one should exercise abundant caution during data interpretation since the weak compression waves that emanate from the separation shock within the boundary layers are hard to capture using PLS or streakline visualization, which causes the PLS and streakline fields to underestimate the upstream jet separation shock motion amplitude. The wall pressure data also has spatial resolution limitation of 4.5 mm

that precludes an exact determination of upstream extent of shock motion. However, the closest upstream location to jet separation shock where the pressure signal ceases to exhibit the low frequency shock unsteadiness characteristic of separation shock oscillations will help estimate an upstream bound of the jet separation shock motion.

Figure 14 shows the PSD comparison at the isolator downstream shock leg location with and without jet injection (Figure 14(a)), along with the surface streakline visualization field (figure 14(b)) and center-plane mean and r.m.s. PLS field (Figure 14(c,d)) around the isolator downstream shock leg and jet separation shock. The jet injection employed had the lowest strength among the ones tested ($\dot{m}_{jet}/\dot{m}_{iso} = 1.3\%$). The mean PLS field shows that jet separation shock foot is located at around $x = 285$ mm, while the r.m.s. of the PLS field shows that shock foot fluctuations becomes appreciable at $x \approx 284$ mm, which provides an estimate of the upstream extent of jet separation shock motion. This estimated upstream extent is also consistent with the upstream influence location determined from the surface streakline visualization. In addition, the pressure signal at $x = 279$ mm (not shown) did not possess any low frequency unsteadiness from jet separation shock. This places the jet separation shock location at $x = 284$ mm, which is about 1.8δ downstream of the isolator downstream shock leg. With this jet separation shock location, the PSD of the isolator downstream shock leg oscillations (figure 14(a)) reveals no identifiable qualitative or quantitative changes compared to the situation without jet injection. This observation shows that a revised upper bound of the upstream propagation of the perturbation occurs at 1.8δ , which is an order of magnitude smaller than the previous estimate made using upstream shock response. This length scale of $o(\delta)$ is also essentially identical to the estimates provided with canonical boundary layers, which serves to show that the relaxing boundary layer did not significantly augment or inhibit upstream acoustic propagation.

6.1.7 Physical model of shock oscillation mechanism and implications to inlet unstart

From the insights into the propagation of perturbations across the isolator duct, it is clear that the isolator shock foot oscillations have contributions from downstream travelling boundary layer structures (exhibiting diminishing correlations with distance) and upstream propagating acoustic pressure fluctuations within an order of one boundary layer thickness proximity, as illustrated by the schematic in figure 15. These provide compelling evidences that the isolator shock foot oscillations typically occur from local interactions with the surrounding boundary layers and have very little response from the ‘global’ back pressure fluctuations of the duct. With this input, the following physical model is proposed to explain the isolator shock foot unsteadiness. The approach boundary layer to the isolator upstream shock leg as well as the boundary layer immediately downstream of the shock leg provides broadband pressure fluctuations into the shock leg. The shock leg responds to the broadband input by selectively amplifying the low frequencies and the very high frequencies corresponding to boundary layer characteristic frequencies. Whereas the PSD of oscillations above 1 kHz essentially mimic the incoming boundary layer spectrum illustrating a simple amplification of incoming turbulence, the selective amplification at low frequencies needs further explanation and a simple physical model is presented below.

Consider a control volume as shown in Figure 16 that bounds the incident and reflected oblique shock waves of the isolator duct (radius ‘ r ’) over a height of a boundary layer thickness (δ) and cross-sectional area A . The streamwise length-scales of the control volume (L and l) is both chosen on the order of a boundary layer thickness to emulate the lengthscale over which boundary layer fluctuations can make active contributions to the shock motion. The situation of interest is the upstream motion of the shock foot after it is displaced downstream by a high momentum patch of the incoming boundary layer. Let us consider two instances: at $t = t_0$ when the shock foot has a velocity $u_s(l)$ and at a short time later $t = t_0 + dt$ when the shock has moved a short distance l_s and has a velocity $u_s(l - l_s)$. The corresponding change in downstream pressure between the two time instances is Δp_3 . For the sake of simplicity, the pressure and

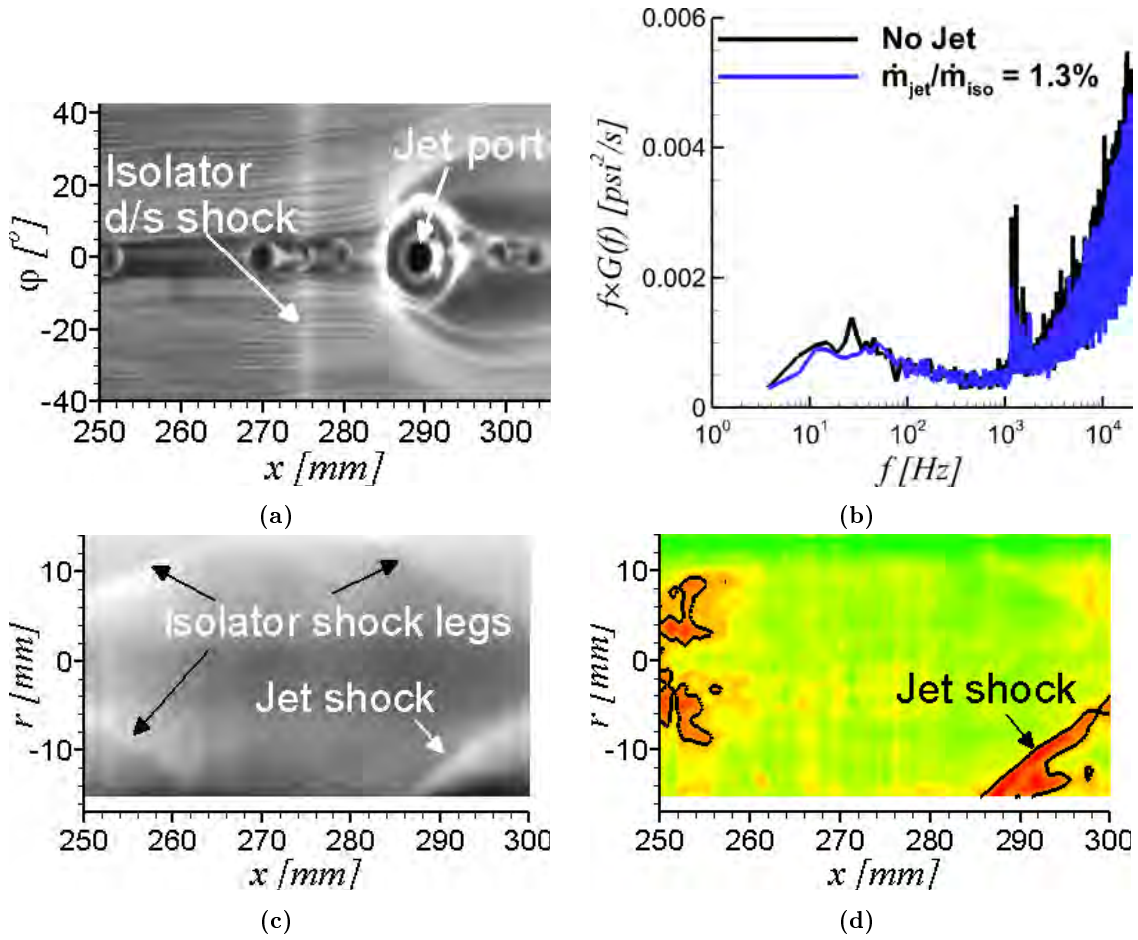


Figure 14: Isolator flow and pressure field with sonic jet injection of mass ratio $\dot{m}_{jet}/\dot{m}_{iso} = 1.3\%$: (a) mean surface streakline visualization field; (b) comparison frequency pre-multiplied PSD of wall pressure fluctuations measured beneath the isolator downstream shock foot ($x = 275$ mm) with and without jet injection; (c) corresponding mean PLS field with jet injection; (d) corresponding r.m.s. PLS field.

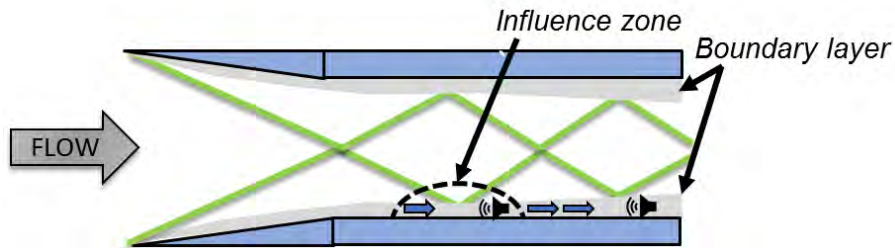


Figure 15: Illustration of the dominant sources that drive the shock oscillation at a given location

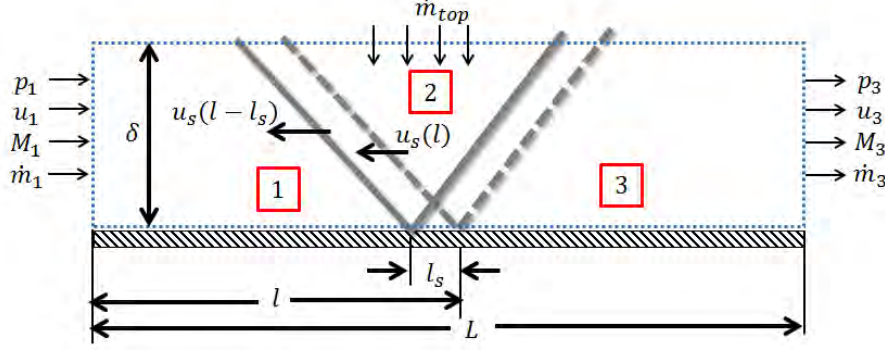


Figure 16: Control volume schematic of proposed shock unsteadiness model

velocity fluctuations within the incoming boundary layer are neglected during this recovery time. Force balance equations in the frame of reference of the shock at these two time instances lead to the following:

$$\text{At } t = t_0: \quad \dot{m}(u_1 + u_s(l)) + \dot{m}_{top}u_{top} + p_1A = \dot{m}(u_{R3}) + p_3A + \tau_w(l)A_w(l), \quad (1)$$

$$\begin{aligned} \text{At } t = t_0 + dt: \quad \dot{m}(u_1 + u_s(l - l_s)) + \dot{m}_{top}u_{top} + p_1A &= \dot{m}(u_{R3} + \Delta u_{R3}) \\ &+ (p_3 + \Delta p_3)A + \tau_w(l - l_s)A_w(l - l_s), \end{aligned} \quad (2)$$

In the above equations, u_{R3} represents the velocity of the flow in region 3 in the shock frame of reference. For very small changes in the shock velocity, $\Delta u_s = u_s(l - l_s) - u_s(l)$, the corresponding change in the flow velocity relative to the shock is almost equal to Δu_s , i.e., $\Delta u_{R3} \approx \Delta u_s$ and the residual difference in momentum flux is much smaller than the unbalanced pressure force (Δp_3A). Hence, for the purpose of this analysis, we assume $\Delta u_{R3} = \Delta u_s$ and $\dot{m}_{top}u_1$ remains unchanged.

Coming to the pressure excursion Δp_3 , it can be shown that for a shock angle of the incident shock of β , the corresponding pressure excursion across the incident shock Δp_2 for an inflow Mach number fluctuation ΔM_1 is given by:

$$\Delta p_2 = p_1 \left(\frac{2\gamma}{\gamma + 1} 2M_{1n} \times \Delta M_{1n} \right) = p_1 M_1^2 \sin^2 \beta \left(\frac{2\gamma}{\gamma + 1} \frac{2\Delta u_s}{u_1} \right), \quad (3)$$

$$\Delta p_2 A = \frac{2}{\gamma + 1} \dot{m} \sin^2 \beta \times 2\Delta u_s, \quad (4)$$

For flow turning angles that are modest, such as the case with unseparated shock boundary layer interactions, the pressure jumps between the two shocks are nearly equally split; hence, the overall pressure excursion Δp_3 is twice Δp_2 . That is,

$$\Delta p_3 A = 2 \times \Delta p_2 A = 2 \times \frac{2}{\gamma + 1} \dot{m} \sin^2 \beta \times 2\Delta u_s, \quad (5)$$

As we approach the isolator SBLI region, the skin friction coefficient (C_f) would theoretically decrease from its incoming boundary layer value. The drop in C_f typically begins about one boundary thickness upstream and extends about one boundary layer thickness downstream of the shock impingement location. Subsequently downstream, C_f recovers to a value that slightly exceeds the incoming boundary layer skin friction coefficient. Let C_{f1} represent an average skin friction coefficient in the upstream region until shock impingement location and C_{f2} represent the average skin friction coefficient downstream of the shock impingement location. With these simplifications, the total friction force on the isolator wall within the control volume at the two time instances are given by:

$$\tau_w(l)A_w(l) = \frac{1}{2}C_{f1}\gamma p_1 M_1^2 2\pi r l + \frac{1}{2}C_{f3}\gamma p_3 M_3^2 2\pi r(L-l), \quad (6)$$

$$\tau_w(l-l_s)A_w(l-l_s) = \frac{1}{2}C_{f1}\gamma p_1 M_1^2 \times 2\pi r(l-l_s) + \frac{1}{2}C_{f3}\gamma p_3 M_3^2 \times 2\pi r(L-l+l_s), \quad (7)$$

Further, for small values of Δu_s , it can be shown by using a Taylor series expansion that:

$$\Delta u_s = \frac{du_s}{dt} \frac{l_s}{u_s} \quad (8)$$

In the above equation, the relation $u_s = dl_s/dt$ was employed during simplification. Subtracting equation 1 from 2, and using the expressions for the terms on the right hand side, one obtains:

$$0 = \frac{4}{\gamma+1} \dot{m}_1 \sin^2 \beta \times 2 \frac{du_s}{dt} \frac{l_s}{u_s} + \frac{1}{2} C_{f1} \gamma p_1 M_1^2 \left(\frac{C_{f3} p_3 M_3^2}{C_{f1} p_1 M_1^2} - 1 \right) 2\pi r l_s, \quad (9)$$

The above equation can be further simplified by noting $\dot{m}_1 u_1 = \gamma p_1 M_1^2 2\pi r \delta$ into:

$$0 = \frac{8}{\gamma+1} \sin^2 \beta \frac{du_s}{dt} + \frac{C_{f1} u_1}{2\delta} \left(\frac{C_{f3} p_3 M_3^2}{C_{f1} p_1 M_1^2} - 1 \right) u_s, \quad (10)$$

which is of the form:

$$\frac{du_s}{dt} = -k u_s, \quad (11)$$

where

$$k = \frac{\gamma+1}{16 \sin^2 \beta} \frac{C_{f1} u_1}{\delta} \times \left(\frac{C_{f3} p_3 M_3^2}{C_{f1} p_1 M_1^2} - 1 \right), \quad (12)$$

This form of equation for shock velocity is essentially identical to the model presented by [28] on the shock oscillations over shock-induced separated flows. Whereas the factor k was experimentally determined in Plotkin's model, a theoretical determination is made here. The corresponding frequency multiplied pressure power spectrum is given by:

$$fG(f) = 4p_{r.m.s} f \frac{k}{k^2 + (2\pi f)^2} \quad (13)$$

Figure 17 shows the predicted PSD using the current formulation beneath the isolator downstream shock leg and the corresponding experimental PSD. The value of $p_{r.m.s.}$ was obtained from the experimental data integrating the PSD between 10 Hz and 100 Hz, and the choice of the frequency limits were made to adjust the predicted amplitudes to match with experimental PSD. The C_{f1} was set at 0.0014 (as measured from the boundary layer profile at $x = 240$ mm) and the ratio C_{f3}/C_{f1} was varied between one through four. Evidently, the proposed physical model shows an excellent agreement with the measured spectrum over the entire band of low frequency oscillations up to 100 Hz for $C_{f3}/C_{f1} \approx 1 - 2$ that are typically observed across SBLI, which provides significant credibility to the analysis and assumptions made. Interestingly, increasing the C_{f3}/C_{f1} to four, which is quite high for attached SBLI situations, still keeps the shock oscillation spectrum in the ball park of the observed low frequency range of shock oscillations, which serves to show that the analysis is fairly robust to the chosen value of the C_{f3}/C_{f1} . It should be noted that the unsteadiness of the pressure fluctuations of the upstream and downstream boundary layers that would serve as forcing functions of the shock oscillations has been ignored since their direct effects were observed only at high frequencies. Based on these observations, it appears that the low frequency shock oscillations are driven by the damping effect of the skin

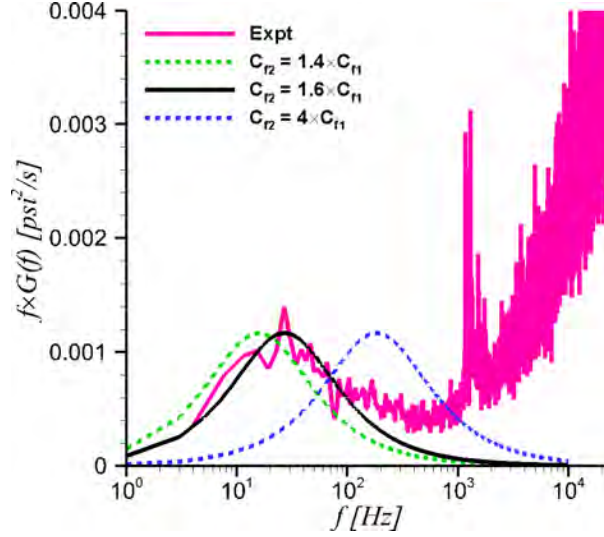


Figure 17: Comparison of PSD of the wall pressure fluctuation from the proposed model and the experimental data for the isolator downstream shock leg

friction to the relaxing shock wave that is displaced by the boundary layer structures. Thus, the shock oscillations are driven rather indirectly by the boundary layers through the changes in skin friction coefficients across the SBLI, which explains the relatively modest coherences between the pressure fluctuations beneath the shock foot and boundary layers at these low frequencies.

6.1.8 Comparison with other configurations

Among the assumptions made in the previous analysis, the choice of the height of the control volume to be a boundary layer thickness needs to be re-examined as it ignores the possible dependence of the duct diameter of the shock oscillations at very low frequencies. Furthermore, simplifying assumptions made on the shock jump relations as well as the shock velocities in relation to the relative velocities need to be further validated. Two further experimental configurations were tested to evaluate the above assumptions. The first configuration is an external flow SBLI where a 6° shock generator placed in a Mach 3 flow generated the shock interactions with the wind tunnel boundary layer that naturally transitioned to turbulence. This flow is an effectively unconfined flow and without the presence of a shock train; hence, this configuration corresponds to an "infinite" duct diameter. The second configuration is that of the inlet/isolator used so far, but with smaller inlet compression angle of 8° , which provides a smaller pressure rise across the shock waves. Both these measurements were made using PCPSP technique, which provided spectra at frequencies up to 4 kHz.

Figure 18(a) shows the comparison of the experimentally obtained PSD beneath the shock foot of the external flow SBLI and the predicted PSD using the physical model. First and foremost, the unconfined SBLI with unseparated boundary layer also exhibits the very low frequency oscillations at frequencies below 100 Hz, which is similar to the inlet/isolator flows, as well as a sharp increase in PSD due to the amplification of the incoming turbulent boundary layer fluctuations. The predicted power spectra from the physical model also matched very well with the experimental dataset with the skin friction ratio C_{f3}/C_{f1} exhibiting very similar values as the inlet/isolator configuration. This excellent agreement provides compelling evidence that the shock oscillation phenomenon is indeed driven by the near-wall dynamics with very little contribution from the duct acoustics. A tight agreement was also obtained with the inlet/isolator flow fitted with an 8° inlet turn angle shown in figure 18(b), which further validates the simplifying assumptions made regarding the shock jumps and shock velocities.

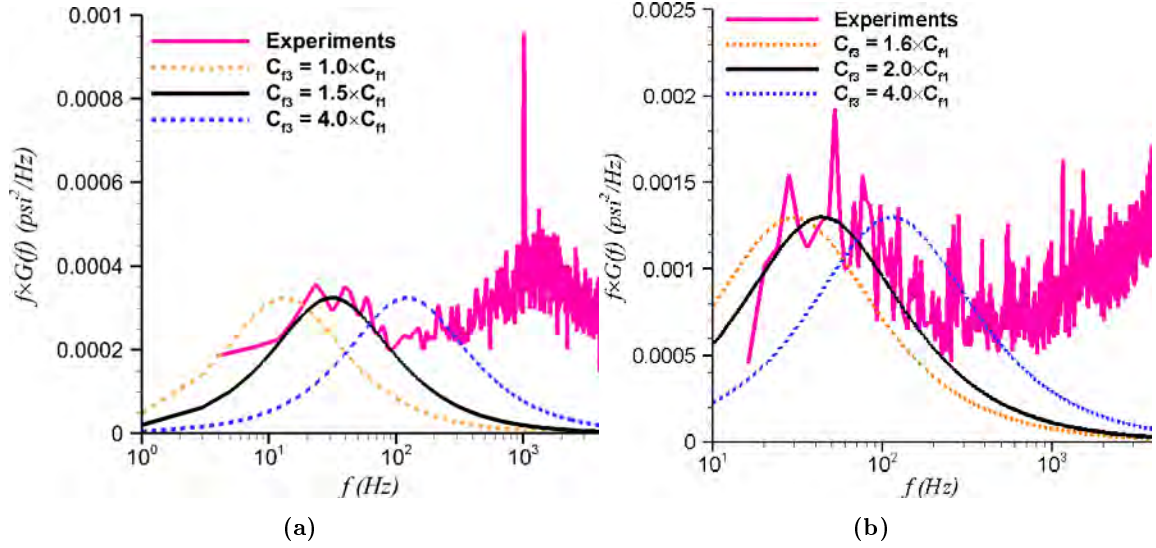


Figure 18: Comparison of wall pressure fluctuation PSD from the proposed model and the measured PSD in (a) an external unseparated shock boundary layer interaction unit; and (b) inlet/isolator shock foot with 8° inlet compression angle.

6.2 Investigation of Negative Surface Curvature Effects in an Axisymmetric SBLI

6.2.1 Executive Summary

Experiments and computations were performed for a negatively curved compression ramp SBLI at a freestream Mach number of $M_\infty = 2.5$ to determine the extent to which negative surface curvature affected the shock-induced separation size. For the purposes of comparison, auxiliary experiments were performed for a canonical planar (2D) configuration with similar inflow conditions. Surface streakline and wall static pressure measurements indicated significantly increased values of L_{sep} for the negatively curved H-I interaction compared to the 2D SBLI for a given compression ramp angle. Reynolds-averaged Navier Stokes simulations and planar laser scattering confirmed outer shock strengths in excess of the 2D limit for the H-I SBLI, which occur due to the combined influence of a streamwise area contraction effect and shock-shock interactions. The severity of these mechanisms scales with a confinement parameter, h/R , as validated by experiments at an additional ramp height for the H-I interaction. While these negative curvature effects do not significantly alter the static pressure rise across the separation from that of a 2D case, they do cause a stronger adverse pressure gradient downstream of the compression corner. This increased adverse pressure gradient may necessitate the development of a higher peak velocity on the dividing streamline and a larger separation length, similar to what has been observed for SBLI in rectangular channels [29, 30]. An additional constriction of the virtual throat at the H-I SBLI exit was also ascertained. The effect of this area reduction is in an increase in the mass being carried toward spillage by the separation bubble, which will also result in larger L_{sep} values given similar wall pressure distributions across the interactions. The details of this study are elaborated below.

6.2.2 Comparison of Near-Wall H-I and 2D Interaction Flowfields

Figure 30 shows time-averaged images from the surface streakline recordings (top) and PSP (bottom) for the H-I and 2D SBLI configurations at $\alpha = 20^\circ$; the flow is in the positive x -direction (left to right). The H-I images of Fig. 30(a) have been unwrapped by a coordinate transformation; therefore, the scaling of the ϕ -axis is linear. Upstream influence (U), separation (S), and reattachment (R) points are marked with dashed black lines, and some example surface

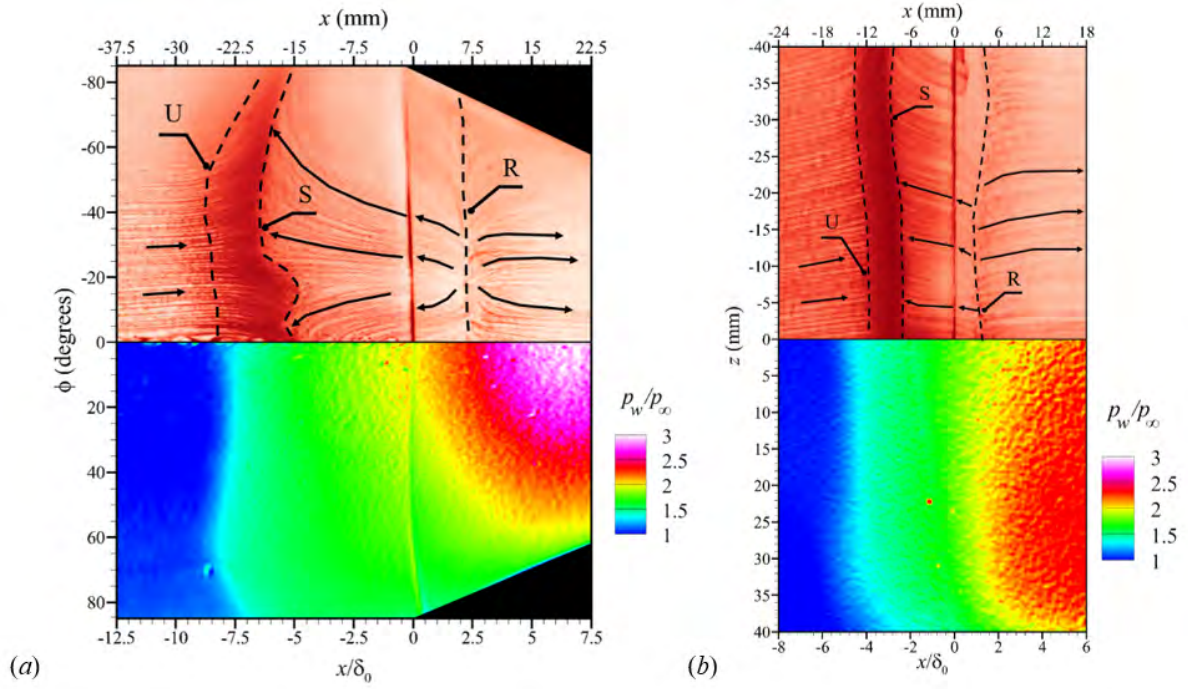


Figure 19: Top-view of surface streakline visualization and averaged PSP pressure field for (a) 20° H-I interaction, $h/R = 0.246$ (b) 20° 2D interaction.

streaklines trajectories (as discerned from the videos) are depicted using solid black arrows.

Figure 30 affords a good deal of information about the flow in the near-wall regions of the interactions, virtually all of which is consistent with the well-documented structure of separated planar ramp SBLI [7]. The streaklines in the incoming boundary layer are nominally parallel to the longitudinal axes of the models, exhibiting little curvature. As the boundary layer moves downstream, it encounters the adverse pressure gradient induced by the smeared oblique shock foot near the upstream influence line. The wall shear stress is reduced as a result, forming the dark vertical bands observed in the figure. The boundary layer then separates, and the downstream flow recirculates in a closed separation bubble, as evidenced by the reversed streaklines along the floor. A reattachment line is clearly visible on each ramp face, and the flow downstream of this line exits the interaction altogether.

The corresponding mean wall pressure fields exhibit values close to the freestream pressure in the incoming boundary layer. The pressure begins to increase near the upstream influence line, which results in the blue/green interface in the PSP images. Downstream of S , the flow is gradually compressed across the separation region until the reattachment line is reached. After reattachment, a sharp pressure rise can be observed in the PSP fields that continues until the compression ramp shoulder. The H-I flowfield in Fig. 30(a) exhibits a reasonable degree of axisymmetry with the exception of the separated region nearest the model edge ($\phi < -60^\circ$).

While the interactions of the H-I and 2D SBLI models shown in Fig. 30(a) and Fig. 30(b) are qualitatively similar, closer inspection of the streakline visualizations and wall pressure fields reveals considerable differences in their streamwise extents. Whereas the spanwise-mean separation line of the H-I case in Fig. 30(a) occurs at $x/\delta_0 \approx -5.9$ (i.e. $L_{sep}/\delta_0 \approx 5.9$, when measured from the leading edge of the compression ramp), the corresponding planar separation length is only $L_{sep}/\delta_0 \approx 2.7$. This inflation of separation size for the H-I model prompted further investigation into L_{sep} values at additional ramp angles. Figure 20(a) shows a comparison of the separation length scales of Fig. 30 alongside values at ramp angles of 16° and 24° for both interactions. The authors note that based on the Korkegi criterion [18], the incipient separation angle for a planar weak-solution shock at $M_\infty = 2.5$ is $\alpha \approx 18^\circ$. Therefore, no separation is expected for

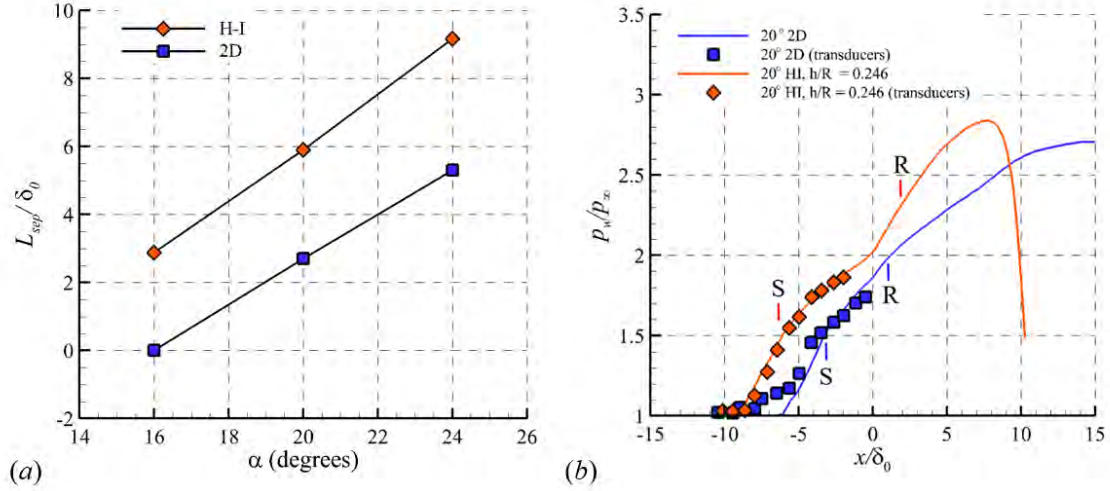


Figure 20: Comparison of (a) separation length scale between H-I and 2D interactions for various compression ramp angles (b) smoothed PSP wall pressure profiles alongside transducer data at $\phi = 5^\circ$ for the H-I interaction, and $z = 0$ mm for the 2D interaction.

a 16° planar compression ramp. It can be observed from the figure that while the 16° 2D case is consistent with this expectation, a sizeable separation exists for the H-I configuration at the same turn angle. Further, the H-I separation sizes at 20° and 24° are both around twice as large as the corresponding 2D values. Thus, the H-I model experiences separation at an earlier ramp angle, and generates considerably larger separation sizes as compared to the corresponding 2D SBLI for a given value of α .

Figure 20(b) compares the smoothed wall pressure profiles at the centerline of the 20° interactions in an attempt to explain the mean enlargement of the H-I separations observed in Fig. 20(a). Transducer measurements are included in the figure as symbols, there is generally good agreement between the transducer data and PSP-derived curves. It can be discerned that the wall static pressure of the H-I SBLI is larger than that of the 2D interaction for a given streamwise location across the separation shock and ramp face. In keeping with ideas of free interaction theory [31, 32], the pressure rise at separation is similar between the two cases, occurring in the vicinity of $p_w/p_\infty \approx 1.5 - 1.6$. At the mean reattachment location, the wall pressure of $p_w/p_\infty \approx 2.3$ for the H-I interaction is slightly higher than that of the 2D SBLI, which is closer to $p_w/p_\infty \approx 2.0$. Thus, the magnitude of compression across the separated flow region is larger in the H-I SBLI than for the 2D case by about 15%.

Downstream of the mean reattachment, the wall pressure continues to increase in both SBLI, with the H-I interaction exhibiting a considerably steeper gradient. The wall pressure reached at the most downstream location on the ramp face of the H-I model is $p_w/p_\infty \approx 2.8$, which is around 20% higher than the 2D value of $p_w/p_\infty \approx 2.4$ at the same streamwise location (as noted previously the 2D ramp face is longer than for the H-I case, with a shoulder closer to $x/\delta_0 \approx 17$). The 2D wall pressure eventually achieves a peak level of $p_w/p_\infty \approx 2.7$ near the shoulder. While this value is comparable to the maximum wall pressure of the H-I interaction, it is important to note that an additional streamwise distance of nearly $10\delta_0$ was required to attain it. The elevated downstream wall pressures and adverse pressure gradient of the H-I model suggest that some aspect of the negative curvature may result in back pressures that exceed the 2D values. If this notion is valid, the elevated back pressures would at least partially explain the inflated separation size observed for the H-I model.

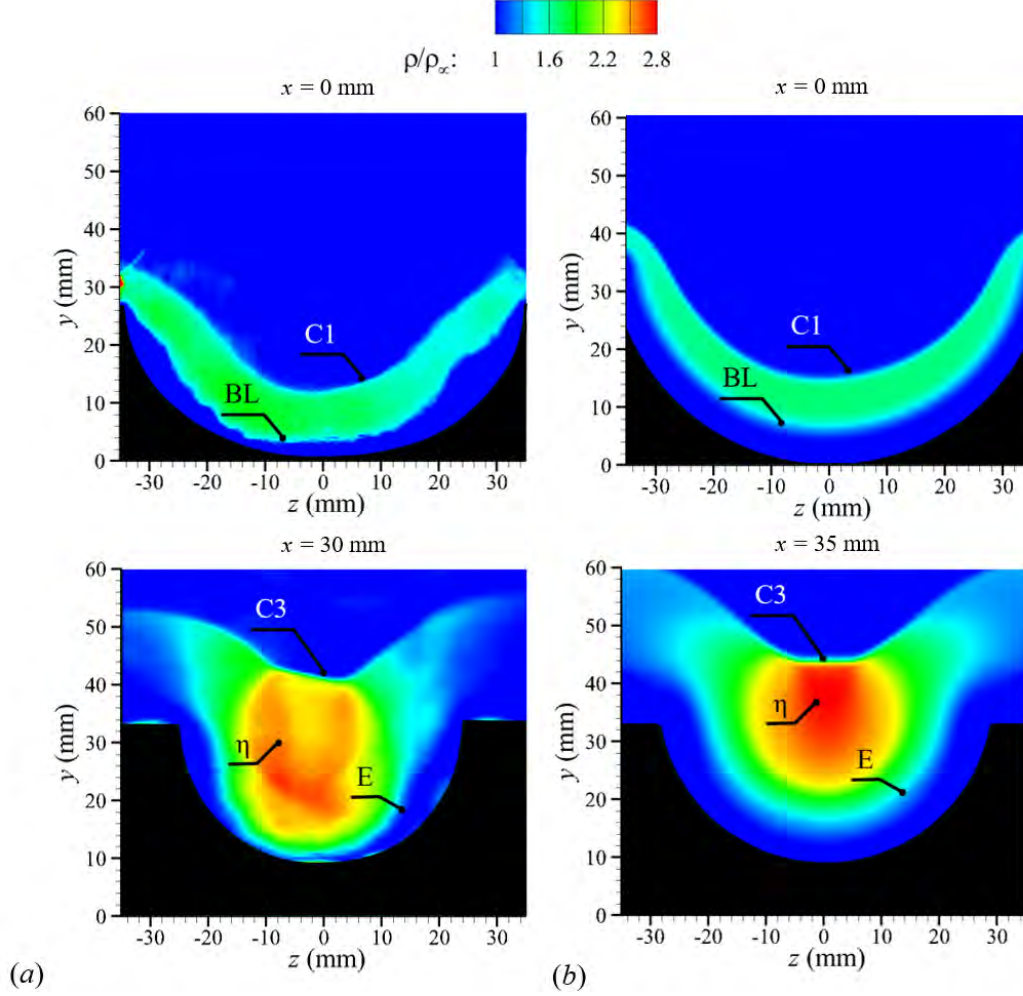


Figure 21: Transverse/wall-normal slices for the 20° H-I interaction, $h/R = 0.246$ (a) experimental PLS density contours (left) (b) RANS density contours (right).

6.2.3 H-I SBLI Flowfield Structure

Planar laser scattering (PLS) based density fields were acquired in transverse/wall-normal planes at two streamwise locations within the 20° H-I SBLI ($h/R = 0.246$) to determine if overcompression could be observed in the outer shock structure. As the strength of a shock is uniquely defined by the incoming Mach number and the jump of a single thermodynamic property across it, these measurements also provide an estimate of the interaction back pressure. The PLS-derived density fields are displayed alongside Reynolds-averaged Navier Stokes (RANS) contours in Fig. 21. Meaningful PLS-based density fields could only be obtained outside the viscous regions of the flow (above the separation bubble) due to signal loss due to ice particulate evaporation near the model surface.

At the compression ramp leading edge ($x = 0$ mm), a nominally axisymmetric oblique shock is visible (C1). This wave separates the freestream flow from the higher density interaction zone, and it will later be shown that this is the upper boundary of the separation shock. The upper boundary of the separated boundary layer (BL) is visible as a green/blue transition moving toward the floor. The maximum experimental density ratio at this location is $\rho/\rho_\infty \approx 1.8$, which is below the planar shock density ratio of $\rho/\rho_\infty = 2.2$. However, by $x = 30$ mm in the PLS (and $x = 35$ mm in the RANS solution) the outer shock structure has changed considerably, with flat shock C3 developing above a high density core region (η). The maximum PLS-based density ratio in the core at this streamwise position is $\rho/\rho_\infty = 2.5$, which is around 10% lower

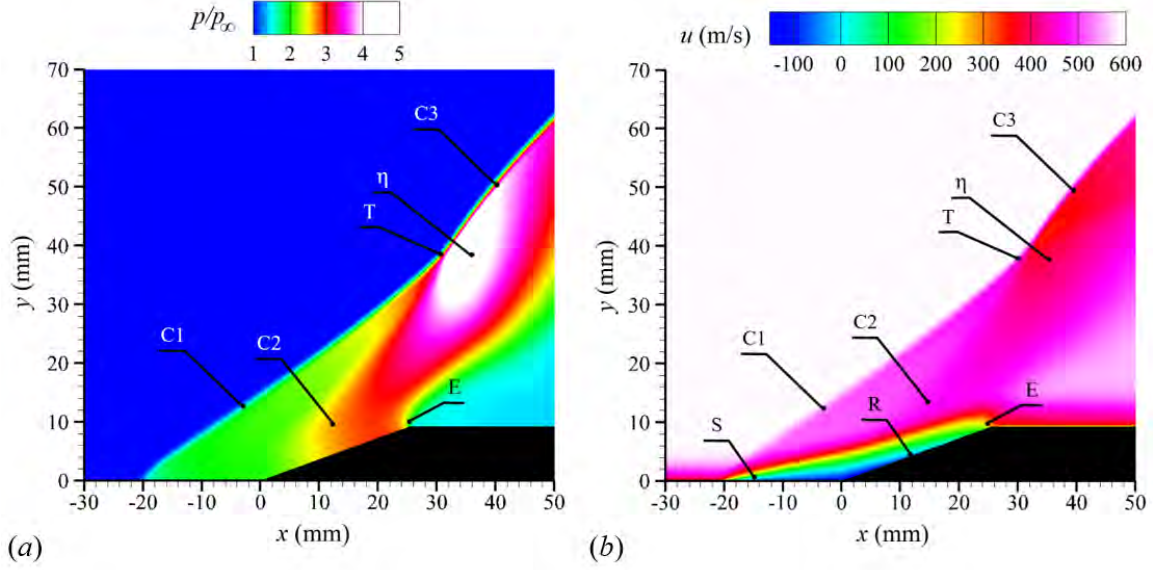


Figure 22: RANS simulation contours for the 20° H-I interaction at $\phi = 0^\circ$, $h/R = 0.246$ (a) static pressure (b) streamwise velocity.

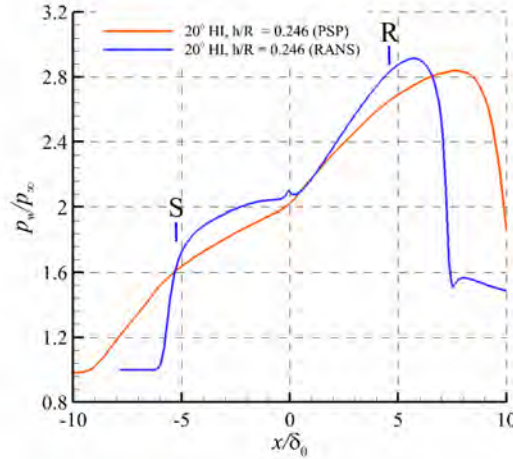


Figure 23: Comparison of PSP and RANS wall pressure at $\phi = 5^\circ$ for the 20° H-I interaction ($h/R = 0.246$).

than the RANS value of $\rho/\rho_\infty = 2.8$. From oblique shock theory for an inflow Mach number of 2.5, the experimental density ratio provides a pressure rise of $p_b/p_\infty \approx 4.0$ at this streamwise location. This represents an increase of around 25% from the 2D limit. In summary, Fig. 21 reveals that evidence of overcompression can be observed in the outer shock structure of the H-I SBLI, confirming an elevated back pressure ratio.

The qualitative agreement between the experiments and CFD in Fig. 21 suggests that the numerical solution effectively captures the features of the interaction. As the curvature of the H-I model makes PLS impractical within the model height in all but transverse/vertical planes, the RANS becomes particularly useful for gaining insights into the mechanisms causing the elevated interaction strength. Normalized static pressure and streamwise velocity contours from the 20° H-I SBLI computation at $\phi = 0^\circ$ are presented in Fig. 22. Boundary layer separation can be observed in Fig. 22(b) near $x = -19$ mm ($L_{sep}/\delta_0 \approx 5.4$). The outer flow perceives the leading edge of the separation bubble as an impediment and deflects upwards in response,

resulting in the formation of smeared separation shock $C1$. Downstream of $C1$, the flow within the separation bubble recirculates in a clockwise direction as the fluid abutting the free shear layer gains momentum through turbulent mixing. The strength of $C1$ is fixed in the upstream portion of the SBLI, but begins to increase downstream of the ramp corner as it intersects a series of compression waves ($C2$) generated along the ramp face. $C2$ occurs as a result of the flow being subjected to a curvature-induced area reduction as it is squeezed by the ramp (this authors also refer to this effect as streamtube compression in the subsequent discussion). While the boundary layer reattaches at around $x = 16 \text{ mm}$, no distinct reattachment shock is discernable. As the reattachment shock is also significantly smeared by viscosity [7], it is probable that it has been integrated into $C2$. The separation and reattachment compression waves generated around the cylinder circumference all propagate radially inwards, intersecting near the center of curvature. The subsequent shock-shock interaction results in the formation of triple-point T , and outer shock wave $C3$. The strength of $C3$ gradually diminishes in the vertical direction due to a 3D relieving effect from the open top of the H-I model and the arrival of the centered expansion E , as can be observed in Fig. 22(a). It is apparent from Fig. 22(a) that the flow downstream of the triple point experiences compression in excess of the planar limit ($p/p_\infty = 3.2$), with a maximum of $p_b/p_\infty = 4.6$ in the RANS. This represents an approximately 15% higher interaction strength than observed in the PLS. Figure 23 compares the wall static pressure profiles at $\phi = 5^\circ$ for the PSP and RANS. It can be observed that the RANS has a smaller upstream influence and stronger separation shock than the experiments, both of which can be attributed to the higher Reynolds number of the incoming boundary layer in the computation. Despite the difference in back pressure ratios, the two methods both predict maximum wall pressures in the vicinity of $p_w/p_\infty \approx 2.8 - 2.9$. This indicates that the region of peak pressure does not permeate the interaction to the wall. However, a comparison of Fig. 21(c) and Fig. 21(d) reveals that the high pressure core extends lower in the experiments than in the RANS. This enhanced proximity to the model surface would explain how the lower experimental back pressure ratio translates into a peak wall pressure similar to that of the CFD.

It is important to note that the shock-shock interference and area contraction noted for the H-I interaction are inviscid effects, which arise from the negative curvature of the model. By the Edney classification [33] an inviscid 20° turn in a full axisymmetric duct at $M_\infty = 2.5$ will result in the formation of a Type II interaction (a Mach reflection with a strong solution outer shock wave). However, the observed H-I interaction strengths indicate weak solution outer waves. It is hypothesized that this reduction in strength is actually due to viscosity, as the influence of the SBLI gradually turns the flow over several boundary layer thicknesses, effectively contouring the compression ramp and smearing the shocks that undergo interference near the center of curvature. As a result, the outer H-I shock-shock interaction falls within the scope of a Type I interference. The authors further point out that the effects of shock-shock interaction and area contraction on the back pressure ratio are not mutually exclusive, and are tightly coupled when compression is induced by a negatively swept ramp (as one will inherently accompany/influence the other). In fact, the area contraction results in what could be effectively described as a continuous series of right-running Type VI interactions that occur as $C2$ strengthens $C1$ prior to the triple-point; this increases the severity of the eventual Type I interaction and the pressure rise across $C3$.

6.2.4 Effect of Confinement on H-I SBLI

To this point, the results of the present study have shown that the experimental wall static pressure values and separation length scales of the H-I interaction are larger than those of a planar compression ramp SBLI for a given ramp angle. PLS and RANS simulations for the H-I configuration suggested that a causal mechanism for these differences is an elevated back pressure ratio as a result of negative curvature effects, namely shock-shock interference and streamtube contraction. This elevated back pressure ratio leads to a larger adverse pressure gradient along

the floor than a 2D SBLI of the same ramp angle. Subsequently, we investigate the effect of the compression ramp height on the back pressure ratio of the H-I SBLI.

As previously noted, the focusing of $C1/C2$ near the center of the model results in a shock-shock interaction, which will inherently impose a larger compression than a single planar wave. For a constant area duct at a given Mach number, the strength of this interference is a function of incident wave angle only. In the H-I configuration, another important consideration is the proximity of the centered expansion fan, E . As illustrated in Fig. 24, the height of the ramp (h) and the cylinder radius of curvature (R) govern the relative positions of the shock focusing point and the expansion waves; therefore the strength of the outer shock-shock interaction also depends on these parameters. For $h \ll R$, the expansion fan will arrive at and weaken shock $C1$ before the shock-shock interaction occurs, resulting in a weaker outer shock $C3$ as shown in Fig. 24(a). As ramp height increases (Fig. 24(b)), the expansion fan moves rearward and eventually ceases to weaken the shock-shock interaction. In this way, the strength of the interaction for a given value of α is a function of ramp height normalized by the radius of curvature of the inner model surface (h/R). The aforementioned area reduction mechanism is also related to h/R ; in a fully confined axisymmetric duct, the area contraction ratio can be reduced to a function of h/R only. As the captured streamtube of the H-I model is not fully confined, h/R instead serves as a qualitative indication of the amount of streamtube compression induced, as taller ramps will result in more constriction downstream of the separation shock.

To validate the effects of the confinement ratio h/R on the interaction pressure rise, experiments were performed for an additional 20° H-I ramp at $h/R = 0.194$. The results are displayed in Fig. 25. Figure 25(a) displays the streakline patterns and wall pressure field of the interaction. It can be observed that the separation length for $h/R = 0.194$ is $L_{sep}/\delta_0 \approx 4.8$, which represents a reduction of over 20% from the value of $L_{sep}/\delta_0 \approx 5.9$ at $h/R = 0.246$. PLS density contours at $x = 30 \text{ mm}$ (just downstream of the triple-point at $z = 0 \text{ mm}$) are displayed in Fig. 25(b) for the lower ramp height. The physical features of the density field at this streamwise position are qualitatively identical to those of the $h/R = 0.246$ configuration, with outer shock $C3$ and high pressure core η clearly distinguishable in the figure. The PLS-based density ratio yields a pressure rise of $p/p_\infty \approx 3.5$ across $C3$, down from the value of $p/p_\infty \approx 4.0$ observed at $h/R = 0.246$. These results reveal that in negative curvature situations, the back pressure ratio and L_{sep} exhibit a dependence on confinement level (ramp height) for a given compression ramp angle. However, the authors point out that the length scales of the 20° H-I separations ($L_{sep}/h \approx 2$) are considerably less than the forward facing step limit proposed by Zheltovodov [34, 35], and that the strength of $C3$ is still below the attached shock limit. Further, the 16° H-I

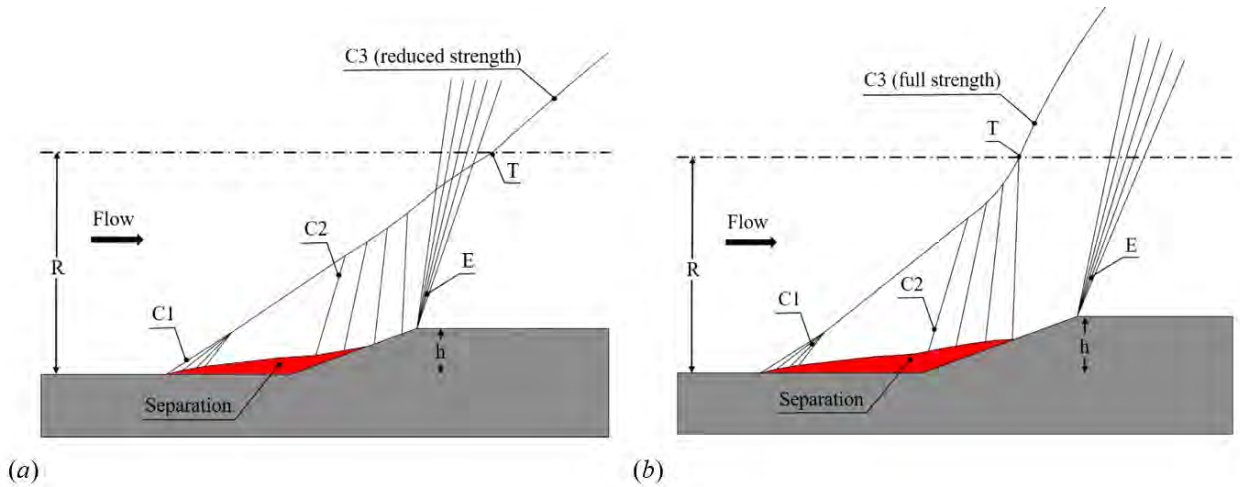


Figure 24: Illustration showing effect of confinement ratio h/R on outer shock strength (a) small h/R (b) large h/R .

interaction from Fig. 20 also has a confinement ratio of $h/R = 0.194$, and a lesser separation extent than the 20° case at $L_{sep}/\delta_0 \approx 3.0$. Hence, the separation length of the H-I configuration exhibits a combined dependence on ramp angle and height. This contrasts a dependence on ramp angle alone for planar SBLI.

The smoothed wall static pressure profiles for both 20° H-I cases are shown in Fig. 26. The figure reveals a streamwise compression for the lower confinement ratio that is smaller than that of the taller ramp, which is consistent with the weaker outer shock (*C3*). It is interesting that the differences in peak wall pressures (and separation sizes) between the two cases at different ramp heights seemingly contradict the aforementioned infinite ramp criterion [19]. However, herein lies an important distinction that must be made between the H-I configuration and zero/positive curvature compression ramp interactions. In the zero or positive curvature case [7], the infinite ramp boundary layer (once supercritical [19]) will have gained enough momentum while crossing the separation to not only reattach, but also to withstand the additional smeared pressure rise up to the constant inviscid value (regardless of if it actually occurs on the ramp face [36]). In other words, increasing ramp height past the infinite value should not affect separation size or the upstream surface pressure distribution, although the peak wall pressure prior to the ramp shoulder may actually rise. By contrast, for the H-I case the level of wall pressure rise across the interaction (and the back pressure ratio itself) have also been shown to be function of the ramp height. Therefore, the authors posit that the infinite ramp criterion cannot be applied to the H-I interactions, as increasing ramp height inherently increases the area contraction effect (larger h/R) and the outer shock strength.

It is perhaps intuitive that larger constrictions will engender a larger pressure rise across the SBLI, which will in turn lead to higher wall pressures and inflation of the separation length scale. However, it is interesting to note that the elevation of the back pressure ratio of the H-I interaction above the 2D value for a given ramp angle does not manifest as an increase of the same level in the wall pressure rise across the separated flow (from the pressure level in the incoming boundary layer up to the reattachment point). A useful concept for understanding shock-induced separation size is that of the dividing streamline, which can be thought of as dividing the SBLI into the separation bubble and outer flow regions. The authors define this

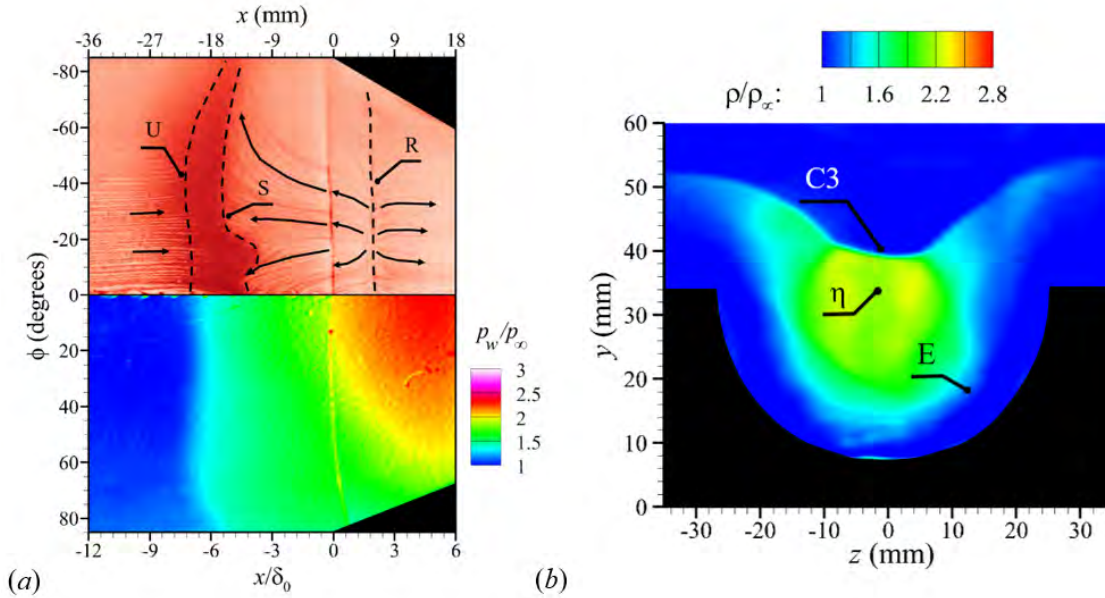


Figure 25: Experimental results for 20° H-I interaction, $h/R = 0.194$ (a) Top-view of surface streakline visualization and averaged PSP pressure field (b) transverse/vertical PLS density contours at $x = 30$ mm.

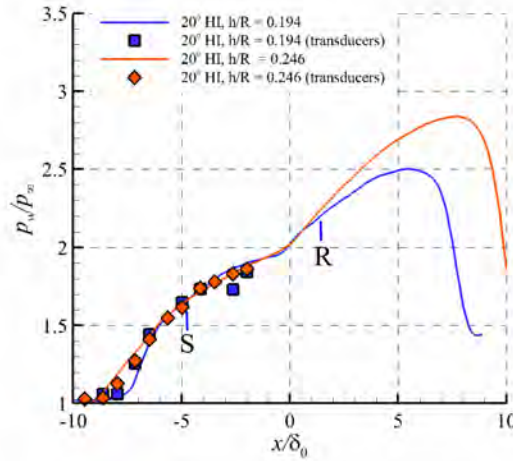


Figure 26: Comparison of smoothed PSP wall pressure profiles at $\phi = 5^\circ$ for 20° H-I SBLI cases.

streamline as the one closest to the wall that passes over the ramp shoulder. By definition, all the streamlines below the dividing streamline are entrained in the separation bubble, and all those above pass out of the interaction. From classical 2D arguments [7], the dividing streamline is first decelerated across the separation shock wave. It must then re-accelerate through turbulent mixing across the free shear layer up to a sufficient velocity to navigate the pressure rise due to deceleration as it approaches reattachment (which is by definition a stagnation point). A larger effective reattachment pressure imposes an increased force on the dividing streamline as it approaches the wall, hence it must attain a higher peak velocity (more momentum) prior to the deceleration process [7] which begins at the compression corner. This necessitates additional shear layer length, and a larger L_{sep} . As was noted previously, the 20° H-I case with the larger confinement ratio ($h/R = 0.246$) has only a marginally larger pressure at the reattachment point than the 20° 2D case (by around 15%). The reattachment pressure of $p_w/p_\infty \approx 2.2$ in the H-I SBLI with less downstream confinement is within 10% of the planar value, but the separation length scale is over 70% larger. This suggests that some intrinsic characteristic of the H-I configuration will result in larger separations than a zero or positive curvature case even when the magnitude of the pressure rise the boundary layer undergoes up to reattachment is similar; the following section explores this hypothesis in greater detail.

6.2.5 Mechanisms Contributing to Inflation of H-I SBLI

The previous discussion has shown definitively that the H-I SBLI flowfield is considerably more complex than that of a 2D compression ramp interaction. It is therefore highly improbable that a sole cause is responsible for the swelling of the separation bubble that has been observed with negative surface curvature, compared to 2D SBLI, in spite of similarities in the magnitudes of the wall pressure rise across the separations. The authors instead endeavor to discuss several potential mechanisms.

6.2.6 Increased Magnitude of Streamwise Pressure Gradient

While the levels of wall pressure in the vicinity of reattachment may be similar between the H-I and 2D interactions, the H-I configuration spurs a significantly increased adverse pressure gradient downstream of the compression corner (as was noted in Section 6.6.2.2). To further illustrate this point, Fig. 27 shows wall pressure profiles for the 16° and 20° H-I interactions at $h/R = 0.194$ alongside the 20° 2D wall pressure. The x -axis of the figure has been offset such

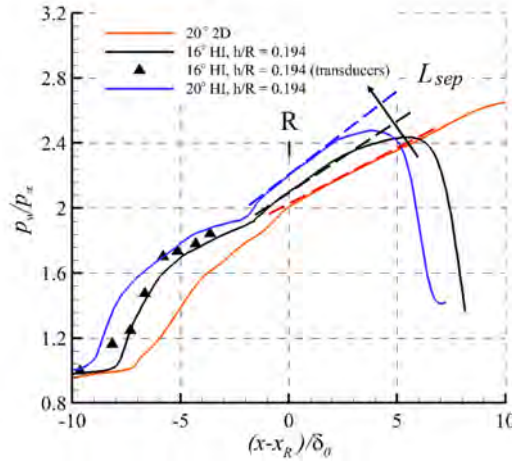


Figure 27: Comparison of smoothed PSP wall pressure profiles for H-I and 2D interactions with similar reattachment pressure magnitudes.

that the origin is located at the reattachment point, we note that the scale of the axes has been maintained such that the slopes of the pressure profiles are unchanged. For each of these cases, reattachment occurs within $\pm 5\%$ of $p_w/p_\infty = 2.1$. The approximate slopes (local pressure gradients) in the vicinity of reattachment are indicated by dashed lines. It is apparent that the slope of the wall pressure downstream of the ramp leading edge is greater for the H-I configurations. This is attributable to the streamwise area reduction effect of the geometry discussed at length previously. Therefore, a fundamental difference between the ideal 2D interaction and the H-I SBLI is the presence of an additional adverse pressure gradient downstream of the reattachment location in the H-I SBLI.

Titchener & Babinsky [29] performed an experimental investigation of a normal SBLI in a rectangular channel upstream of a subsonic diffuser. They noted a significant increase in separation length as the normal shock wave was moved rearward (toward the diffuser); this increase was attributed to the inability of the separated boundary layer to recover from the adverse pressure gradient of the shock prior to exposure to additional compression. The work of Babinsky *et al.* [30] extended this idea to supersonic impinging/reflected SBLI, where shocks emanating from the corner flow arriving within the separation bubble at the centerline were observed to increase the streamwise separation length of the interaction. We argue that for the present H-I SBLI case, the compression wave series associated with the area contraction of the H-I SBLI (*C2* in Fig. 22) is analogous to the subsonic diffuser in the case of Titchener & Babinsky [29] and the corner waves in the case of Babinsky *et al.* [30]. It is logical that the stronger the compression wave series (or the larger the slope of the wall pressure rise downstream of the ramp leading edge), the larger the separation becomes.

An important distinction between the H-I SBLI and the interaction of Babinsky *et al.* [30] is that compression wave series *C2* in the present work extends well beyond the mean reattachment location of the interaction. In an ideal 2D compression ramp SBLI [7], the dividing streamline will attain the majority of the inviscid pressure rise before the mean reattachment location, with nominally zero pressure gradient conditions downstream. In other words, the separation length is fairly independent of the streamwise evolution of the reattached boundary layer for a 2D interaction. From the previous discussion the H-I configuration has no infinite ramp length; it follows logically that the separation size of the H-I interaction may also be influenced by the continued pressure rise downstream of reattachment. Stated differently, whilst traversing the separation, the dividing streamline of the H-I interaction must obtain enough momentum to not only reattach, but also to continue competing with an imposed adverse pressure gradient. Hence,

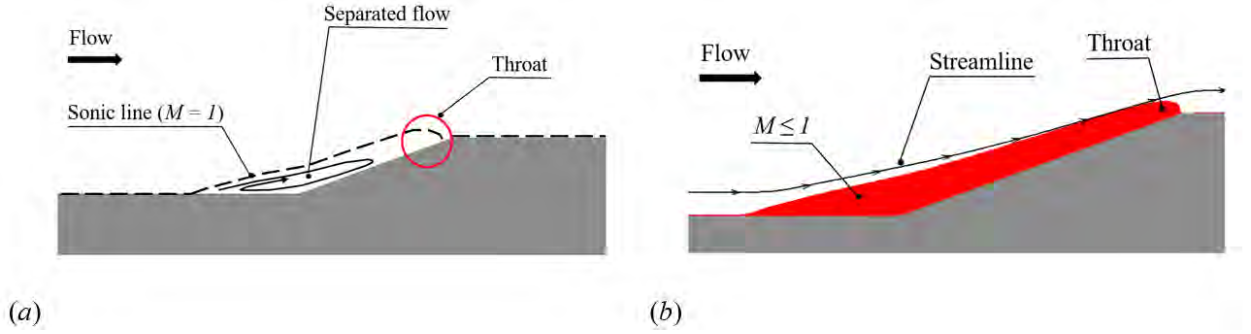


Figure 28: Illustration of virtual throat formation at outflow of 2D SBLI (a) schematic showing separated flow region (b) 2D RANS results, with streamline coincident to throat.

the larger the adverse pressure gradient downstream of reattachment, the larger the separation should become. This hypothesis is supported by the cases in Fig. 27, which are the interactions observed with reattachment wall pressures that fall within 10% of one another. Furthermore, the wall pressures at the ramp shoulder are within 3% for the two H-I cases. It can be observed that the 20° 2D SBLI has the most gradual downstream pressure rise, and also the smallest separation length. Of the two H-I cases in Fig. 27, the 16° ramp angle provides a more gentle geometric area contraction than the 20° case. This causes a more gradual pressure rise downstream of the compression corner for the 16° H-I interaction, and a reduced separation size. Hence, we conclude that for a given reattachment pressure, the magnitude of L_{sep} is a positive function of the local pressure gradient in the vicinity of the reattachment line. This dependence of L_{sep} on the local pressure gradient in the vicinity of reattachment line also potentially explains why L_{sep} for ideal 2D compression ramp SBLI are same for different ramp heights (exceeding the infinite ramp criterion) wherein the wall pressure at the ramp shoulder can be substantially different from one another.

6.2.7 Reduced Separation Outflow Area

An interesting effect of the large scale reversed flow for compression ramp SBLI is a lifting of the sonic line, which encompasses the separation bubble in the manner observable in Fig. 28(a). As the boundary layer thins post-reattachment, it forms a virtual throat where the flow is locally choked and it accelerates back to supersonic velocities [19]. Figure 28(b) demonstrates the existence of this throat at the centerline of the 2D RANS simulation by tracing the path of a streamline coincident with the top of the sonic line. By definition this streamline is impermeable to mass, hence for a truly 2D flow all the fluid below it must pass through the throat to exit the interaction. In this way, the flow out of the SBLI is effectively throttled by the throat area, which in 2D is approximately equal to the height (arc length) of the throat multiplied by the span.

The situation becomes more complex when considering a 3D interaction, given that spillage can provide an additional source of mean mass removal from the separation; we note that both the 2D and H-I SBLI cases in the present study are effectively 3D by this classification. Fig. 29(a) shows the RANS results of the 2D interaction, where the streamline shown in Fig. 28(b) has been converted into a stream surface that spans the model. The areas below this stream surface at the inflow plane (which was arbitrarily selected to be 10 mm upstream of the nominal separation location in the RANS simulations) and throat of the interaction are highlighted in red. Because the sides of the model are open to the freestream, a subsonic channel exists within the separation bubble that provides an alternative exit to the throat for the entrained fluid. In a mean sense, the mass flow rate into the SBLI (that is, the fluid passing below the stream surface at the inflow) must now be equivalent to the sum of the mass flow rate of the fluid passing

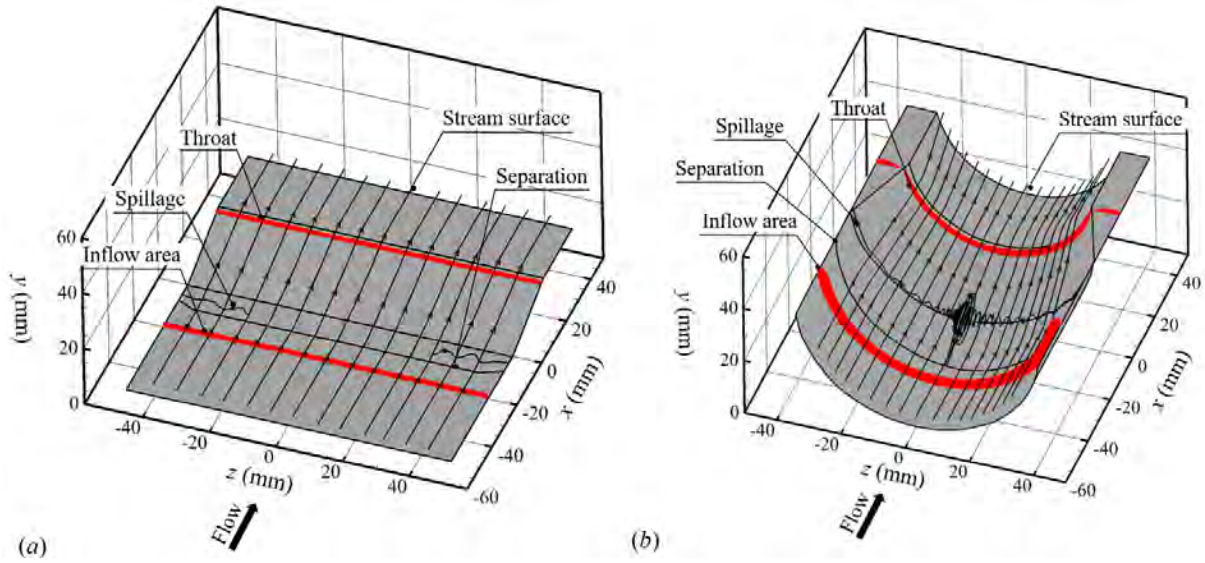


Figure 29: RANS results illustrating virtual throat formation at outflow of (a) 2D SBLI (b) H-I SBLI. Inflow and throat areas are red slices, stream surface and spillage streamlines also shown.

out of the interaction (through the throat) and the mass flow lost through spillage. Therefore, any local restriction of the throat area would theoretically result in increased spillage from the subsonic region in a manner similar to that of a one-dimensional Fanno type flow. The authors point out that the spillage effects for the planar case are fairly minimal, as the throat-to-inflow area ratio is only diminished by the relative height of the stream surface at each location. From the 2D RANS computation this value is $A_t/A_i \approx 0.93$, which qualitatively corresponds to the level of throttling.

In the case of the H-I SBLI, the RANS simulations indicate that the wall normal distance to the stream surface coincident with the top of the throat is the same between $\phi = \pm 80^\circ$ for all streamwise locations; expressly the behavior of the near wall flow is virtually axisymmetric. Further, as the boundary layer thickens and thins at similar rates between the H-I and 2D interactions (as observed from the RANS simulations), the inflow/throat heights of the stream surfaces are also highly similar between the two configurations (around $0.60\delta_0$ at the inflow, and $0.53\delta_0$ at the throat). Figure 29(b) shows a representation of the inflow and outflow areas beneath the stream surface for the H-I configuration, which are effectively rings. Due to the constriction effect of the H-I compression ramp, for any streamwise throat location an additional area loss will be generated due to the decreased radius/circumference of the ring. The 20° H-I RANS simulation indicates a throat-to-inflow area ratio of $A_t/A_i \approx 0.73$, down over 20% from the 2D A_t/A_i value. Therefore, the H-I SBLI throat is able to accommodate a lesser percentage of the captured mass flow rate, and more fluid must be supported by the bubble until it can be spilled over the sides; it follows that this additional fluid would contribute to a swelling of the separation bubble and an increase in L_{sep} . This finding is supported by the streakline visualizations of Fig. 30, which reveal larger streakline trajectory components in the circumferential direction for the H-I separation than in the transverse direction for the 2D case.

6.3 VG Control of Axisymmetric SBLI

To date, several studies have effectively documented the structure of the planar microramp VG flowfield [20, 21, 37, 29, 38, 39]. While it is expected that the flowfield of the axisymmetric microramp utilized in the present work will be highly similar to those of planar VGs, some key features are included here in the interest of completeness and CFD validation. Particular emphasis is placed on the mean/unsteady wall pressure fields and the development of the VG-

induced flow features.

6.3.1 Executive Summary

An experimental campaign was performed at $M_\infty = 2.5$ and $Re/m = 5.3 \times 10^7 \text{ m}^{-1}$ to examine the flowfield of an axisymmetric microramp VG with a height equal to 85% of the surrounding boundary layer thickness. Through surface streakline visualizations and mean PSP measurements, the device was shown to produce flow features entirely consistent with previous accounts of planar microramps [21, 37, 39]. The existence of turbulent vortex ring structures suggested by LES [39] was verified in the wake of the VG using ice-particulate PLS, and high-frequency wall static pressure measurements revealed a significant increase in post-VG turbulent energy that agrees with the production of these rings.

The effects of single and multiple devices on the mean and unsteady characteristics of a compression ramp SBLI in a half-cylindrical duct were studied. Surface streakline visualizations and mean PSP revealed results consistent with planar SBLI control research [21, 37]. Local minimums in upstream influence were observed in the wake of the VGs, but the off-wake upstream influence increased from the uncontrolled interaction. The microramps were also seen to generate larger pressure values both within the separated flow and downstream of the compression corner. Through additional PLS measurements, it was discovered that the reduced smearing of the separation shock wave caused by the VGs increased the interaction pressure rise. The authors suggest that these elevated pressures negatively impact the portions of the flow where no microramps are applied. Through a two-point cross-coherence analysis, it was shown that the unsteady characteristics mirror these mean changes; the devices minimize the sensitivity of the separation shock to disturbances in the wake, but increase the susceptibility of the off-wake separation shock to fluctuations of the enlarged downstream bubble.

Due to the uniquely coupled nature of the near-wall and outer flowfields presented here, it is not immediately obvious how relevant the observed results are to planar cases. In the equivalent rectangular situation, reducing the smearing of the interaction should not inherently change the attained interaction pressure rise to the degree seen here. However, in impinging/reflected interactions with VG control [21, 37], larger maximum wall pressures are observed when arrays are placed upstream of the interactions. Further, the mean alterations to the off-wake flow observed in CFD [37] indicate that these changes in the wake are communicated laterally through the separation. We therefore hypothesize that while the off-wake changes observed here with VGs are exaggerated due to the elevated interaction pressure rise, the same ideas should be applicable to a lesser extent in the planar case. The details of the experimental campaign are elaborated below.

6.3.2 Surface Streakline Patterns of VG-effected boundary layer interactions with axisymmetric SBLI

Figure 30(a) depicts a time-averaged image of the surface streakline patterns for the baseline (uncontrolled) SBLI. The freestream flow direction is from left to right, with the origin at the center of the compression ramp leading edge ($x = 0 \text{ mm}$, $\phi = 0^\circ$). The upstream influence, separation, and reattachment line locations are denoted as U, S, and R, respectively. The authors point out that the actual point of separation should not be taken strictly as S, which is the downstream limit of attached (or non-reversed) flow; the actual separation line falls somewhere between U and S. Some example streakline trajectories are also provided (black arrows) as discerned from video. The taps for the surface mounted Kulite pressure transducers are apparent at $\phi = 0^\circ$ in some of the images.

The features of the uncontrolled interaction are highly consistent with those observed in planar 2D and axisymmetric compression ramp SBLI [36, 40, 41, 42]. The streaklines in the incoming boundary layer (left edge of the figure) are parallel to the freestream flow direction.

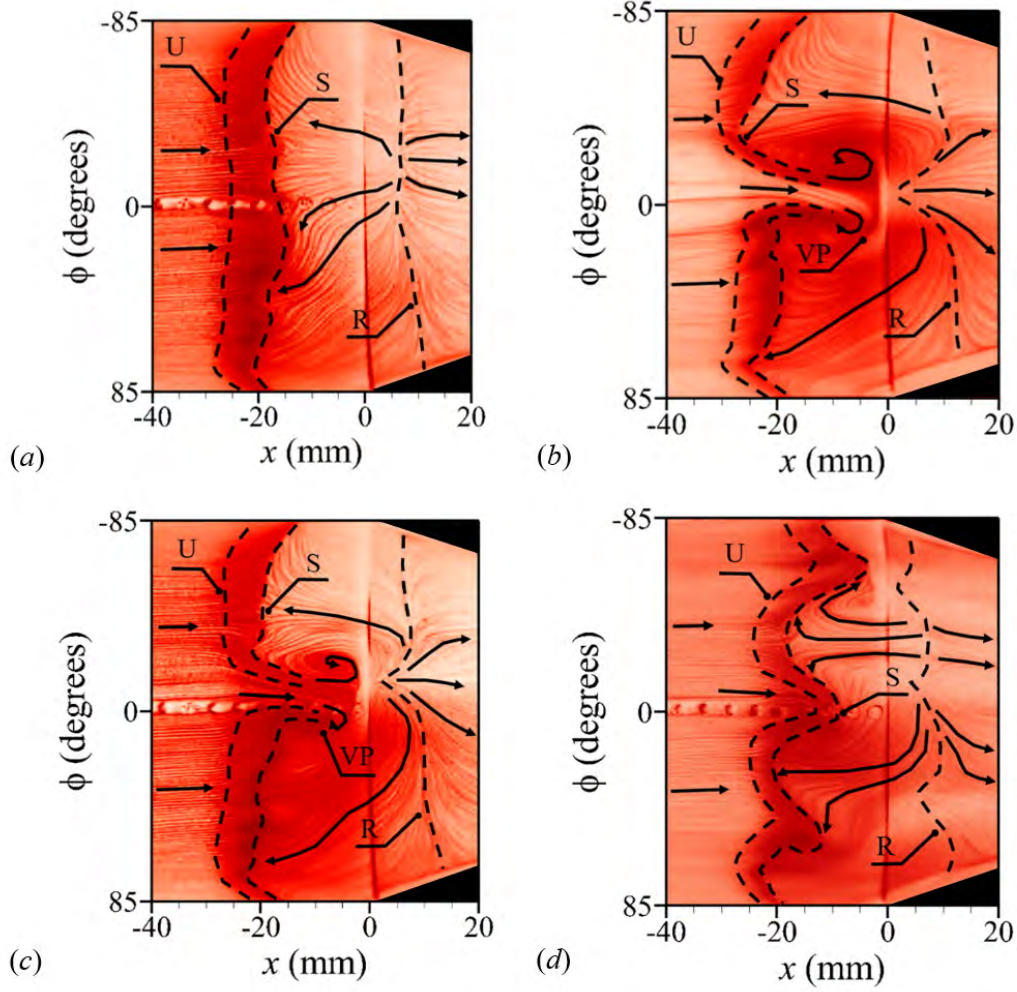


Figure 30: Time-averaged surface streakline visualizations (a) baseline interaction (b) single VG, $x_{TE} = -50 \text{ mm}$ (c) single VG, $x_{TE} = -100 \text{ mm}$ (d) VG array, $x_{TE} = -100 \text{ mm}$.

The oblique shock wave generated by the curved compression ramp interacts with this boundary layer, inducing a pocket of reversed flow (right to left in the figure) which spans the arc of the model from the separation line to the reattachment point. The bright band between the upstream influence and separation lines is formed as the flow decelerates beneath the smeared oblique shock foot, reducing shear stress prior to the separation point. The separation length of the baseline case, $\overline{L_{sep}}$ (here defined as the streamwise distance between the compression ramp leading edge and separation line, averaged over the circumference) is 17.7 mm . The local separation length is seen to vary little in the spanwise/circumferential direction.

The surface streakline patterns of the controlled case with a single VG placed 50 mm upstream of the compression corner ($x_{TE} = -50 \text{ mm}$) are shown in Fig. 30(b). The introduction of the upstream microramp results in some considerable differences from the baseline case. Notably, the flow directly downstream of the VG remains attached far longer than the off-wake flow. This results in a V-shaped separation front that agrees with the simulations of Ghosh *et al.*[37] and Yan *et al.*[39]. It is interesting that while the separation length is significantly reduced in the wake of the microramp, the off-wake separation length actually increases by around a boundary layer thickness from that of the uncontrolled SBLI; this effect was also observed by Ghosh *et al.*[37]. This leads to a circumferential average of $\overline{L_{sep}} = 17.8 \text{ mm}$, which is marginally larger than the baseline. A counter-rotating vortex pair (VP) exists where the microramp vortex system meets the SBLI shock front. The rotation direction of this vortex pair is consistent with

the energized boundary layer having a fuller shape, and subsequently generating a higher pressure rise across the separation shock than the off-wake boundary layer. Similar structures were observed by Babinsky *et al.*[21]. Downstream of the compression corner, the reattachment line approximately mirrors the separation front, with an upstream kink in the vicinity of the device wake.

Figure 30(c) displays the streakline results for the more upstream single VG placement ($x_{TE} = -100 \text{ mm}$). These results are qualitatively similar to those of Fig. 30(b), although the off-wake separation length of around 18.5 mm is improved from the $x_{TE} = -50 \text{ mm}$ case. However, this is still a marginal increase from the baseline, which contradicts the nearly 25% reduction in separation length away from the microramp wake observed by Yan *et al.*[39]. They simulated placing a VG approximately 20 device heights away from the upstream influence location of a planar compression ramp SBLI. The present $x_{TE} = -100 \text{ mm}$ case corresponds to a similar streamwise placement, at around 25 device heights from the upstream edge of the baseline interaction. This suggests that single microramp VGs may not be as effective at off-wake separation suppression in situations with negative surface curvature, or that the reduction observed in the LES is somewhat overstated. The average separation length (including the device wake) for $x_{TE} = -100 \text{ mm}$ is $\overline{L_{sep}} = 16.5 \text{ mm}$, which is an approximately 7% improvement over the baseline.

As placing the VG 100 mm upstream of the compression ramp yielded superior results in terms of reducing separation length, this streamwise location was selected for the array trial. Figure 30(d) depicts the streakline visualization results with three devices, offset by $\phi = 45^\circ$. The qualitative effect of each microramp is similar to the single VG cases, and clear dips (local minimums) in the separation length are visible behind each device centerline. Interestingly, the vortex pairs where the VG wakes meet the separation front are less intense. We hypothesize that this difference is caused by the additional devices, which would act to reduce smearing more uniformly across the span leading to lesser circumferential pressure gradients near each wake. The corrugated separation front in Fig. 30(d) is highly similar to what was observed by Manisankar *et al.*[38]. However, no explicit information on separation length is provided from streakline visualization in their experiments. In the present work, the periodic reduction in separation length caused by each device leads to a spanwise mean separation length of $\overline{L_{sep}} = 14.4 \text{ mm}$, down 18% from the baseline. However, between the VGs the flow can be seen to separate as early as $x = -21 \text{ mm}$. This observation is in agreement with the larger separations observed between devices in RANS simulations[37].

From these results, we posit that the effect of microramp VGs on the axisymmetric compression ramp SBLI is similar to what was observed by Babinsky *et al.*[21, 37], in that the effect of the VG is not to remove shock-induced separation altogether, but to reduce the total separated flow area. It follows that arrays of VGs are more effective than a single device in suppressing the total separation size further[21]. The effect of the microramp on the mean pressure distributions is investigated in the following section through PSP and diaphragm transducer wall static pressure measurements.

6.3.3 Mean Wall Pressure Distributions

Figure 31 shows the mean freestream-normalized pressure fields from the PSP measurements. The uncontrolled interaction results are presented as Fig. 31(a). The upstream influence of the SBLI is clearly visible upstream of the compression ramp leading edge, resulting in the interface near $x = -25 \text{ mm}$. In line with the surface streakline results, the separation shock front is nominally straight across the span, the circumferential average of upstream influence is $\overline{L_u} = 23.7 \text{ mm}$. The pressure increases up to the separation point and then plateaus until the compression corner is reached, where an immediate increase in pressure is clear. The pressure continues to rise until the ramp shoulder.

Comparing the baseline PSP field to those of the single VG cases with $x_{TE} = -50 \text{ mm}$ and

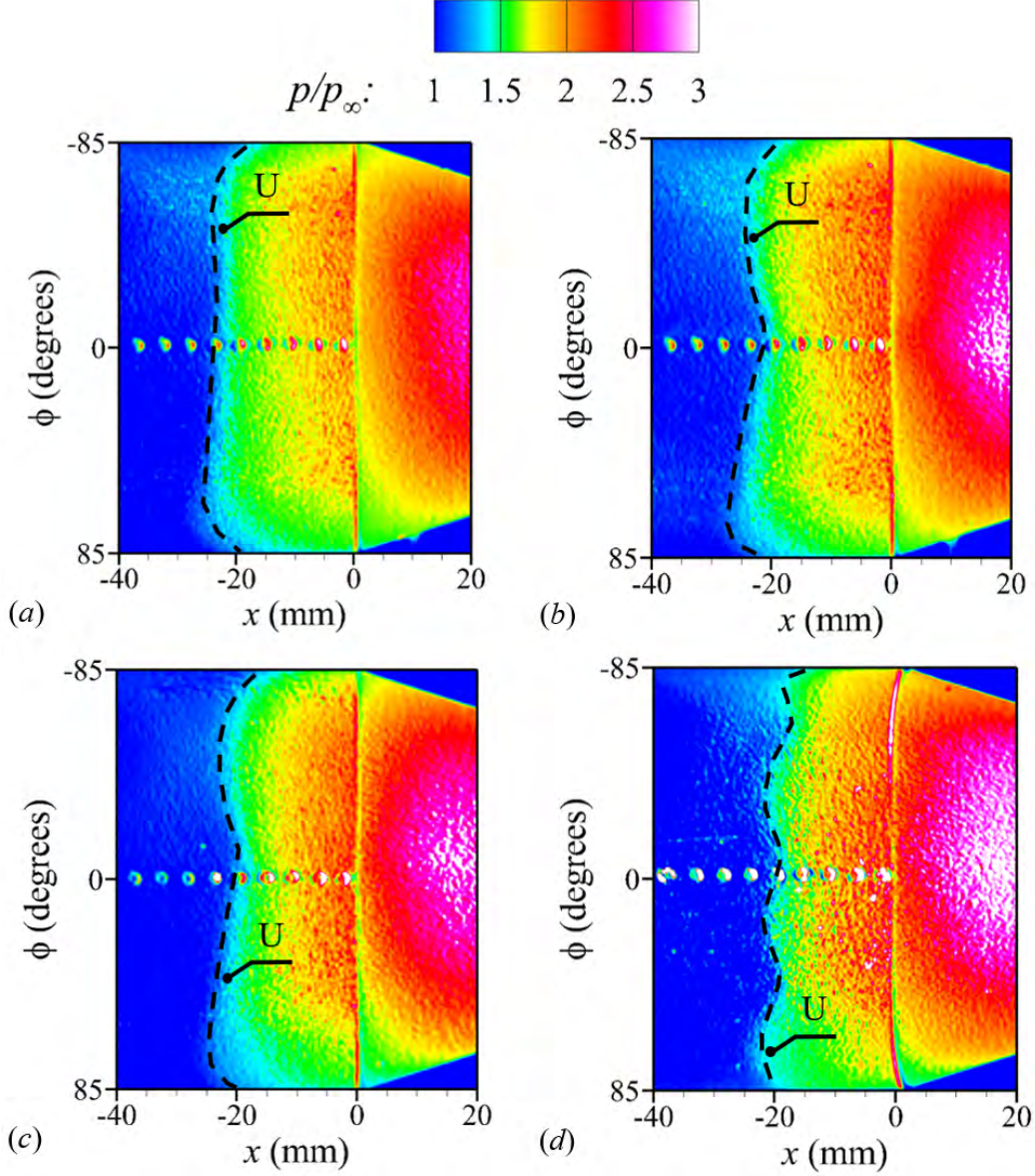


Figure 31: Mean PSP pressure fields (a) baseline interaction (b) single VG, $x_{TE} = -50 \text{ mm}$ (c) single VG, $x_{TE} = -100 \text{ mm}$ (d) VG array, $x_{TE} = -100 \text{ mm}$.

$x_{TE} = -100 \text{ mm}$ in Fig. 31(b) and Fig. 31(c), several differences can be observed. The upstream influence of the SBLI dips inward in the immediate wake of the VG, in agreement with the streakline visualizations. However, the separation front appears to be less chaotic; the counter-rotating vortex pair observed on the floor at the VG wake/separation shock front intersection does not have any mean pressure signature. As discerned from Fig. 31(c), the $x_{TE} = -100 \text{ mm}$ case has an average upstream influence of $\overline{L_u} = 22.3 \text{ mm}$, a decrease from both the baseline and $x_{TE} = -50 \text{ mm}$ configurations ($\overline{L_u} = 24.2 \text{ mm}$). Downstream of the compression ramp leading edge, it seems that both single VG-controlled cases achieve a larger static pressure rise than the baseline interaction, with higher pressure regions downstream of $x \approx 10 \text{ mm}$.

The upstream influence line of the array-controlled interaction in Fig. 31(d) corroborates the corrugated nature of the separation line observed in the streakline visualizations. These periodic dips in upstream influence reduce the spanwise average to $\overline{L_u} = 20.0 \text{ mm}$, down 16% from the baseline. Ghosh *et al.*[37] reported a decrease of approximately 15% in the upstream influence of pressure with microramp array control, in good agreement with the present work. Finally,

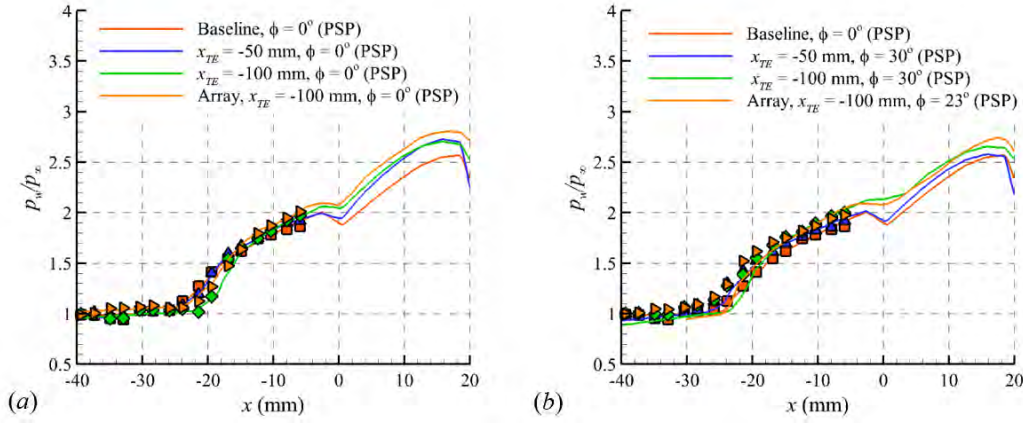


Figure 32: Smoothed PSP wall pressure profiles alongside transducer data for baseline and VG-controlled interactions (a) device wake, $\phi = 0^\circ$ (b) off-wake.

the mean pressure on the face of the compression ramp is elevated from the earlier cases. The reasons for this increase are discussed subsequently.

The smoothed mean pressure curves extracted from the PSP fields are displayed alongside transducer data (same color symbols) for all test configurations in Fig. 32. The two subfigures correspond to the pressure profiles in the wake (Fig. 32(a)) and off-wake (Fig. 32(b)) regions of the interactions. As seen in the figure, the profiles are all qualitatively similar, with pressures equal to the freestream in the incoming boundary layer. A portion of the interaction pressure rise is achieved across the separation shock, behind which the pressure plateaus between around $-15 < x < 0 \text{ mm}$. The remaining pressure rise takes place downstream of the compression corner, due to the combined action of reattachment and an area contraction effect caused by the ramp curvature[1]. Some disagreement exists in the upstream influence locations of the interactions between the PSP and transducer data. The reasons for this discrepancy are not certain, but it is believed that the PSP (which is temperature sensitive) lags the transducers somewhat due to the relatively low temperature near the upstream influence location. For this reason, the upstream influence locations discussed subsequently are based on transducer data only.

Comparing the mean wall pressures in the wake of the devices at $\phi = 0^\circ$ in Fig. 32(a), it can be seen that the single VG with $x_{TE} = -50 \text{ mm}$ has a similar upstream influence location to the baseline interaction, at $x = -24 \text{ mm}$. The single device and array cases with $x_{TE} = -100 \text{ mm}$ have lesser upstream influence locations of $x = -22 \text{ mm}$. The separation pressure levels with single microramps are somewhat elevated from the baseline, while the array case exhibits the largest separation pressure levels. This is consistent with expectation, given that a more energetic boundary layer should be able to sustain a larger pressure gradient when separating. In the experiments of Babinsky *et al.*[21] the same trend was observed, with single devices inducing small increases in separation pressure levels and arrays causing larger differences. It is evident from Fig. 32(a) that the VG-controlled interactions also exhibit increased streamwise pressure gradients downstream of the compression corner at $\phi = 0^\circ$, with the array-controlled case having the largest overall pressure rise. Funderburk & Narayanaswamy[1] showed that the separation length of the H-I SBLI is a positive function of the streamwise pressure gradient downstream of the compression corner. The microramp-controlled cases of the present work directly defy this trend, with similar or lesser separation lengths than the baseline SBLI for larger streamwise pressure gradients. The mechanisms behind the increasing pressure rise with reduced separation size are discussed subsequently.

The mean pressure profiles from the off-wake locations are shown in Fig. 32(b). It can be

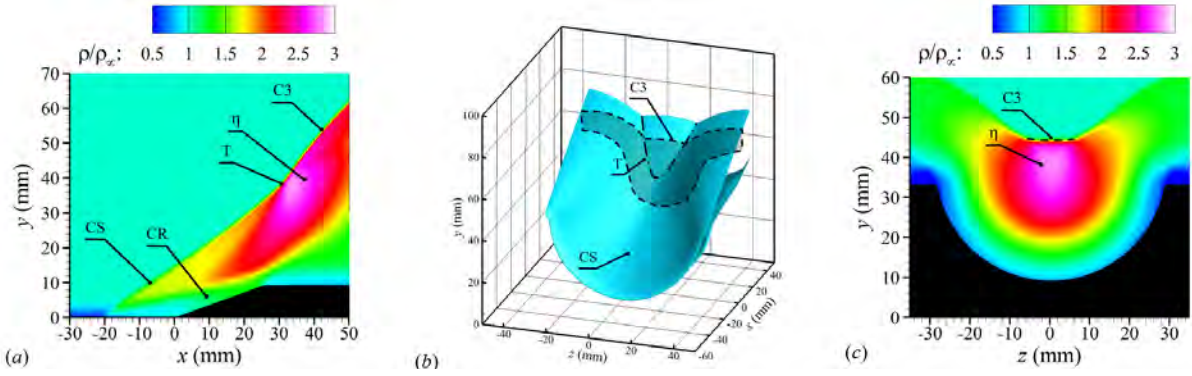


Figure 33: RANS results for baseline interaction (a) streamwise/vertical density contour, $z = 0 \text{ mm}$ (b) outer shock structure (c) transverse/vertical density contour, $x = 30 \text{ mm}$.

observed that the separation shock strength of the VG-controlled cases is elevated from the baseline strength by a slightly lesser degree than at $\phi = 0^\circ$. Downstream of the compression corner the single VG-controlled cases have pressure distributions which are more in line with (albeit still slightly elevated from) the baseline interaction. This suggests that the increased streamwise pressure gradient is generated in the device wake, but that high pressures spread outward circumferentially to a slight degree. Larger differences exist in the off-wake upstream influence location of the interactions, which occurs at $x = -26 \text{ mm}$ for the $x_{TE} = -50 \text{ mm}$ case (larger upstream influence than baseline). The upstream influence of the $x_{TE} = -100 \text{ mm}$ at $\phi = 30^\circ$ appears to be elevated somewhat from the closer VG placement, at $x = -28 \text{ mm}$. The array configuration (profile taken from halfway between two microramps at $\phi = 22.5^\circ$) has a similar upstream influence to the single device case, but exhibits the largest separation pressure levels and highest wall pressures downstream of the compression ramp leading edge. This indicates that the spanwise repetition of device placement provides a more global increase in downstream compression (i.e. increased pressure over a larger portion of the model circumference) as compared to a single microramp. It is interesting that the off-wake upstream influence scales proportionally with the peak pressure on the compression ramp face, a finding that is consistent with those of Funderburk & Narayanaswamy[1] for uncontrolled H-I interactions.

In summary, the wall pressure profiles of Fig. 32 provide two valuable insights. First, the microramps induce elevated wall pressures downstream of the compression ramp leading edge. These increased pressures are localized, but not entirely confined to, the microramp wake. Second, the way in which the on/off-wake interaction regions respond to this enlarged compression are different. The flow in the wake has a smaller upstream influence for larger downstream pressure, whereas the off-wake flow exhibits increased upstream influence size. While these findings initially appear contradictory, they will be reconciled in the discussion to follow.

6.3.4 Outer H-I SBLI Flow Structure

To determine the mechanisms behind the elevated interaction wall pressures observed with VG control, an examination of the outer shock structure of each SBLI is instructive. However, the unmodified outer H-I interaction flowfield has been shown to be considerably more complex than a 2D interaction[1]. In order to improve reader comprehension, the abbreviated results of earlier RANS simulations for the uncontrolled case are repeated here. For a full description of the simulation parameters the reader is directed to Funderburk & Narayanaswamy[1].

Figure 33(a) displays streamwise/vertical density contours at the centerline of the model ($\phi = 0^\circ$) for the baseline case, the freestream flow direction is from left to right. The separation bubble begins near $x = -20 \text{ mm}$, in agreement with the streakline visualizations and wall pressure measurements. The outer flow perceives the sudden thickening of the boundary layer

at this location as an obstruction, and separation shock wave CS forms as a result. Beginning at the compression corner, the negative curvature of the ramp induces an area contraction. The waves formed by this constriction and the reattachment process form compression wave series CR. The separation and reattachment compression waves would normally completely coalesce in a planar compression ramp interaction, forming the inviscid shock. However, due to the negative surface curvature of the model all waves propagate radially inward toward the center of curvature, resulting in a shock-shock interaction. This occurs prior to the formation of an effective inviscid shock wave (i.e. CS and CR do not fully coalesce before the interaction between waves originating at different circumferential locations occurs). Funderburk & Narayanaswamy[1] showed via Euler simulations that in the inviscid case this interaction results in the formation of a strong outer shock wave (i.e. Mach reflection), which agrees with cursory oblique shock calculations. However, due to the smearing induced by the SBLI, the strength of the shock formed at each circumferential location is lessened, as the ramp is effectively contoured by the separation. This leads to a weaker shock-shock interference (regular reflection), and the formation of swept triple-line T and outer shock C3. The flow downstream of C3 has a density and pressure well in excess of the planar limit for a shock wave induced by a 20° ramp; the area downstream of C3 is referred to as a high pressure core (η in Fig. 33(a)). Figure 33(b) depicts an isometric view of the outer shock structure via isocontours of density. It can be observed that the triple-line has the shape of an inverted parabolic arc, the sides of which are joined by C3. The projection of this shock structure onto a transverse/vertical plane at $x = 30 \text{ mm}$ is illustrated in the figure, and also as a density contour in Fig. 33(c). Transverse/vertical PLS fields were acquired at the same streamwise location for each of the experimental test cases, the results are discussed in the subsequent section. We note that the RANS indicates a peak density of $\rho/\rho_\infty \approx 2.8$, which corresponds to a peak interaction pressure of $p/p_\infty \approx 5.0$.

6.3.5 Planar Laser Scattering Fields

To determine the mechanisms behind the elevated interaction wall pressures observed with VG control, ice particulate laser scattering fields were acquired for each case. Transverse/vertical planes were imaged at $x = 30 \text{ mm}$ in order to best visualize the outer shock structure of the interactions; this location is 5 mm downstream of the compression ramp shoulder. Figure 34 shows the instantaneous and time-averaged results. The freestream flow direction is into the page. The top two subfigures correspond to the baseline interaction. A comparison between the earlier RANS density contour of Fig. 33(c) and the baseline PLS in Fig. 34(b) reveals excellent agreement in the shape of the shock structure; both the high pressure core η and flat outer shock C3 are clearly visible in the PLS images, as is the reattached boundary layer (BL). Using an oblique/spherical bow shock intensity to density calibration[6, 1] the peak density in the core region is slightly less than that of the simulation, at $\rho/\rho_\infty = 2.5$. We note that this relation is only valid in cooler regions of the flowfield, as melting will affect the intensity of scattered light. The baseline C3 is at the upper limit of shock strength without the occurrence of downstream ice particulate melting. However, interpolating the height of the shock at $z = 0 \text{ mm}$ between this location and $x = 35 \text{ mm}$, one obtains a wave angle estimate of $\beta = 48.0^\circ$. Through oblique shock relations, this gives a density rise of $\rho/\rho_\infty = 2.45$ (in good agreement with the density calibration) and pressure rise of $p/p_\infty = 3.86$.

Figure 34(c) and Fig. 34(d) show the instantaneous and mean PLS intensity images for the single VG case with $x_{TE} = -100 \text{ mm}$. The results with a single device at $x_{TE} = -50 \text{ mm}$ are highly similar, and as a result are not pictured here. When comparing Fig. 34(c) with the instantaneous baseline image, the observed features are qualitatively identical with the notable exception of a ring of turbulence above the ramp at $z = -3 \text{ mm}$, $y = 17 \text{ mm}$. The LES of Yan *et al.*[39] also showed the persistence of these vortex rings well after the reattachment point in a planar compression ramp SBLI, past the point at which the primary vortex pair generated by the VG disintegrated. With respect to the mean outer shock, C3 is marginally higher (2%) than the

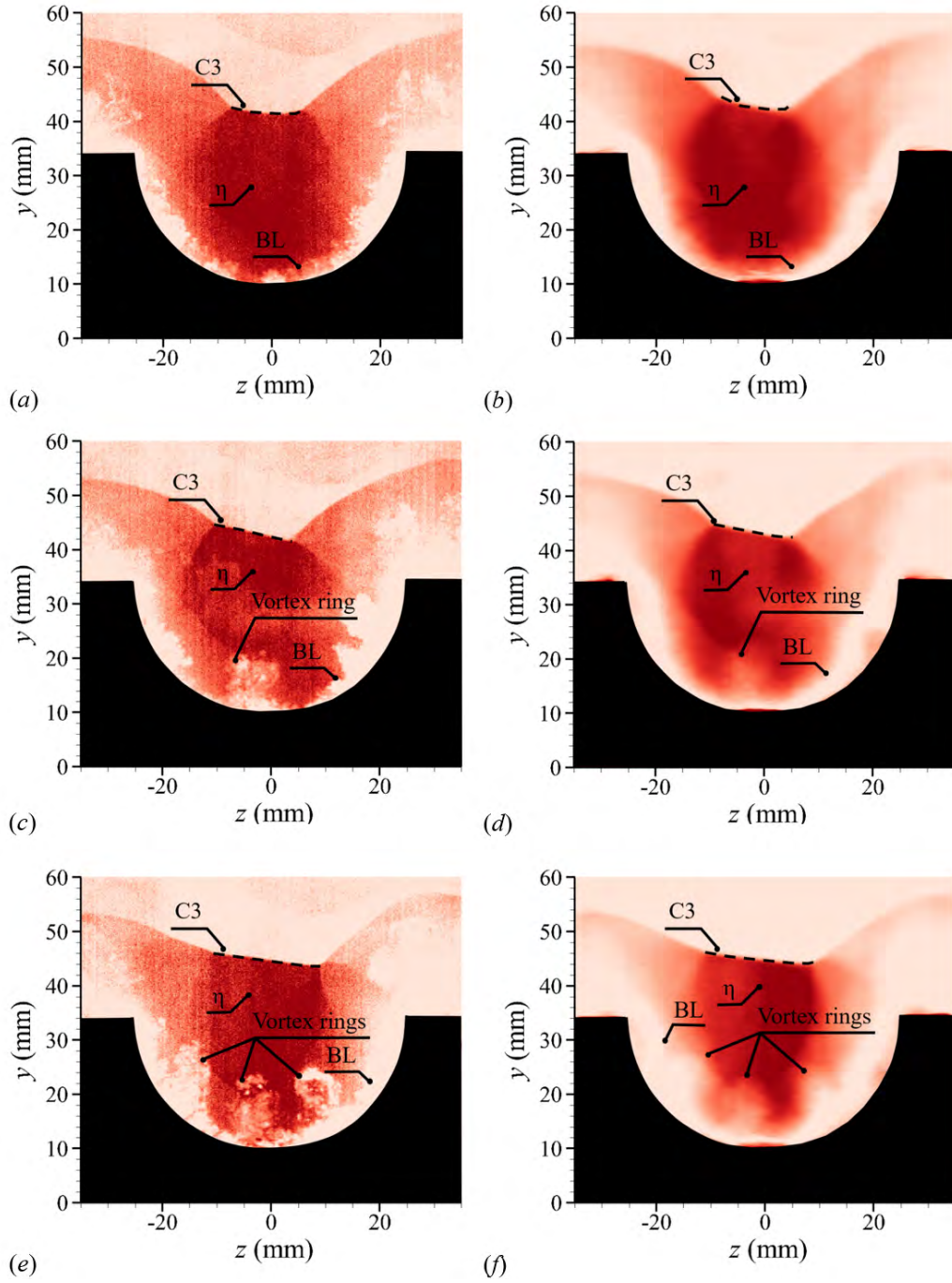


Figure 34: Transverse/vertical planar laser scattering intensity fields at $x = 30 \text{ mm}$ (a) baseline interaction, instantaneous field (b) baseline interaction, mean field (c) single VG, $x_{TE} = -100 \text{ mm}$, instantaneous field (d) single VG, $x_{TE} = -100 \text{ mm}$, mean field (e) VG array, $x_{TE} = -100 \text{ mm}$, instantaneous field (f) VG array, $x_{TE} = -100 \text{ mm}$, mean field.

baseline case in Fig. 34(d), and has virtually the same width. This suggests a slightly stronger compression near the centerline with a single VG, in agreement with the PSP measurements. Using the aforementioned interpolation method, we compute a wave angle estimate of $\beta = 52.3^\circ$. This provides a density rise of $\rho/\rho_\infty = 2.63$ and pressure rise of $p/p_\infty = 4.40$.

The bottom row of subfigures in Fig. 34 corresponds to the array case with $x_{TE} = -100 \text{ mm}$. Again, the qualitative features of the outer flow are similar to the baseline. However, three vortex rings are now visible beneath the high pressure core, consistent with the number and

placement of microramp devices. Moreover, the mean height and width of outer shock C3 have increased substantially, by 5.5% and 33% respectively. The large width increase is consistent with the larger circumferential control region with multiple devices, and supports the wider high pressure zone observed on the ramp face in the PSP. The array C3 has a centerline wave angle of $\beta = 55.7^\circ$, and density/pressure rises of $\rho/\rho_\infty = 2.76$ and $p/p_\infty = 4.81$ respectively. These findings confirm that the VGs increase both near wall pressures within the SBLI and the strength of the outer shock wave. The physical mechanism inducing these changes is addressed in the following section.

6.3.6 Physical Model of H-I SBLI Pressure Increase with Microramp VGs

The preceding results have shown that the use of microramp VGs for control of the H-I SBLI leads to a general reduction of separation size in the wake of the device. This reduction is associated with an elevated wall pressure post-reattachment on the compression ramp face, and the formation of a stronger outer shock wave compared to the baseline. In the context of a planar compression ramp interaction[36, 43], the strength of the outer shock wave is determined purely by the incoming Mach number and turn angle. For a sufficiently tall ramp[19] the separation and reattachment shocks will always coalesce to form the inviscid shock wave, which is of fixed strength. The H-I interaction of the present work differs fundamentally from the planar case in that the strength of the outer shock wave is coupled to the separation size. In the absence of an incoming boundary layer the focusing effect of the H-I compression ramp would result in the formation of a strong oblique shock (Mach reflection)[1]. However, due to the smearing of the SBLI, the strength of separation shock CS is limited by the gradual turn, reducing the strength of the interference between the waves originating around the body circumference, which occurs at the triple-line T (Fig. 35(a) depicts this situation, which is intentionally exaggerated). When flow control is introduced upstream of the interaction (see Fig. 35(b)), the local energization of the boundary layer results in a reduced separation size, as confirmed by streakline visualizations. It follows that the reduced smearing of the separation shock will lead to a stronger shock-shock interaction at T, and an elevated outer shock strength. With increasing energization of the boundary layer CS will strengthen, gradually approaching the inviscid wave corresponding to a 20° turn[1]. This effect is observable in the steepening of C3 with VG control in the PLS of Fig. 34. It is possible that a portion of the resulting pressure increase of the outer flow is communicated upstream to the separation region through the subsonic portion of the boundary layer near the wall. If this occurs, it would explain the elevated wall pressures observed with VGs

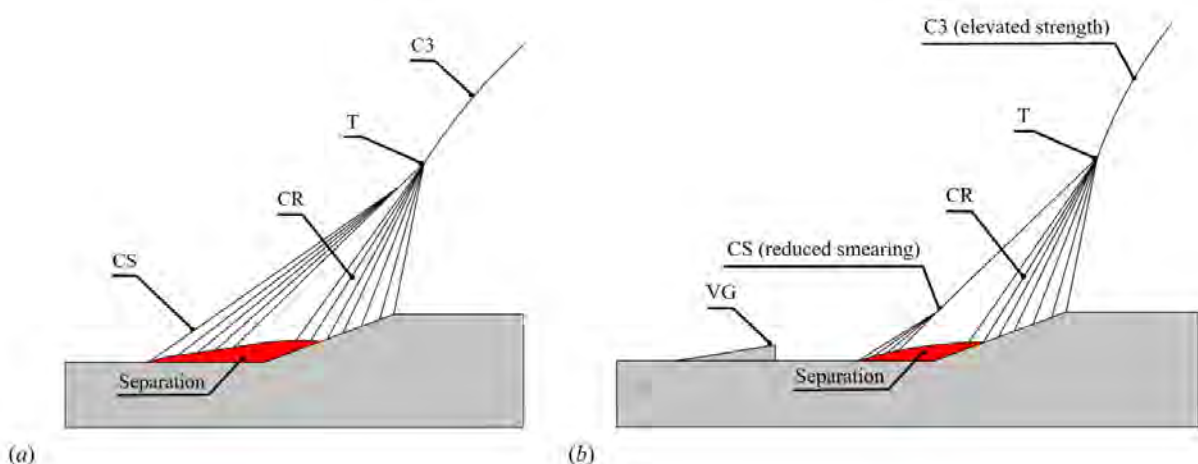


Figure 35: Schematic illustration of outer shock formation (a) uncontrolled interaction (b) VG-controlled interaction.

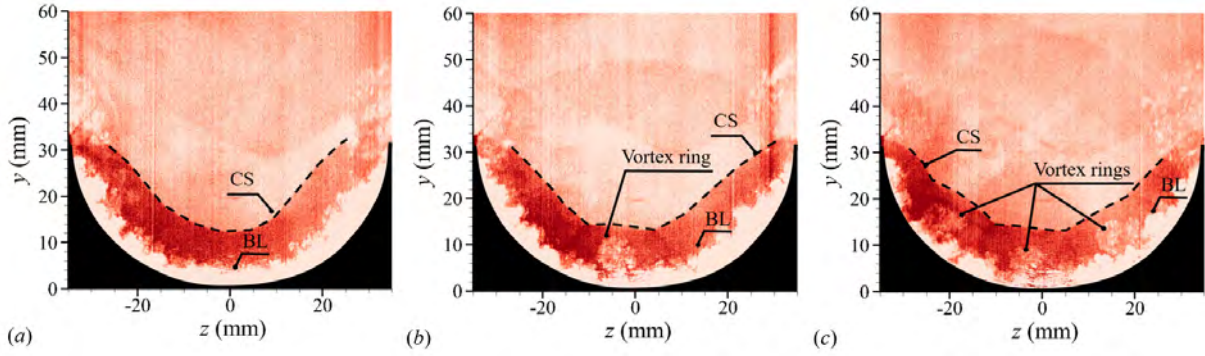


Figure 36: Instantaneous transverse/vertical planar laser scattering intensity fields at $x = 0 \text{ mm}$ (a) baseline interaction (b) single VG, $x_{TE} = -100 \text{ mm}$ (c) VG array, $x_{TE} = -100 \text{ mm}$.

in the PSP. Further, it may adversely affect regions of the interaction where no boundary layer charging occurs (i.e. the off-wake locations) as compared to the baseline. This would explain the discrepancy in upstream influence vs. downstream wall pressure observed in the PSP curves.

Additional transverse/vertical PLS was performed in the vicinity of the compression ramp leading edge ($x = 0 \text{ mm}$) for each test case in order to validate the strengthening of CS in the microramp wake and explain the increased compression observed with VGs. The resulting instantaneous intensity fields are displayed in Fig. 36. In the uncontrolled case of Fig. 36(a), CS is seen to be fairly symmetric about the radius of curvature. In other words, the separation shock has a similar strength at each circumferential location. However, in the single VG and array-controlled cases of Fig. 36(b) and Fig. 36(c), it is apparent that the separation shock is higher above each device-generated vortex ring than it is in the adjacent uncharged boundary layer. This result is impressive given that the separation shock wave behind the microramps begins well downstream of the off-wake locations; that it has outrun the separation shock in the uncharged areas by $x = 0 \text{ mm}$ indicates a considerable increase in strength. It follows that the greater the percentage of the incoming boundary layer with reduced smearing, the larger the increase in outer shock strength will be. This is confirmed by the increasing strength/width of C3 observed for the array case in Fig. 34(f). Hence, by reducing the smearing of the H-I SBLI, the VGs have the unintended and potentially detrimental effect of increasing the interaction pressure rise. The effect of this mean situation on the unsteady characteristics of the interactions is investigated subsequently.

6.3.7 RMS Wall Pressure Distributions

RMS wall pressure distributions were computed from the transducer data to gain insights into the magnitude of the unsteady mechanical loading generated by the interactions. Fig. 37 shows the streamwise evolution of RMS pressure normalized by the corresponding (transducer-based) mean value in Fig. 32 in and out of the wake. From the figure, it can be observed that each profile is qualitatively consistent with those of 2D planar interactions [40]. There is an initially lower value of σ_p/p_w associated with the turbulent energy of the incoming boundary layer, followed by a sharp increase at the upstream influence. This increase is due to the irregular passage of the separation shock foot, which oscillates at low frequency in the streamwise direction due to the “breathing” motion of the separation bubble that has been extensively documented[8]; this region of increased unsteady energy due to shock oscillation is known as the intermittent region[8]. There is a peak in σ_p/p_w in the vicinity of separation (which approximately corresponds to the location of maximum unsteady mechanical loading), followed by a decrease beneath the separation bubble.

In Fig. 37(a), the previously discussed increase in the turbulent energy of the post-VG

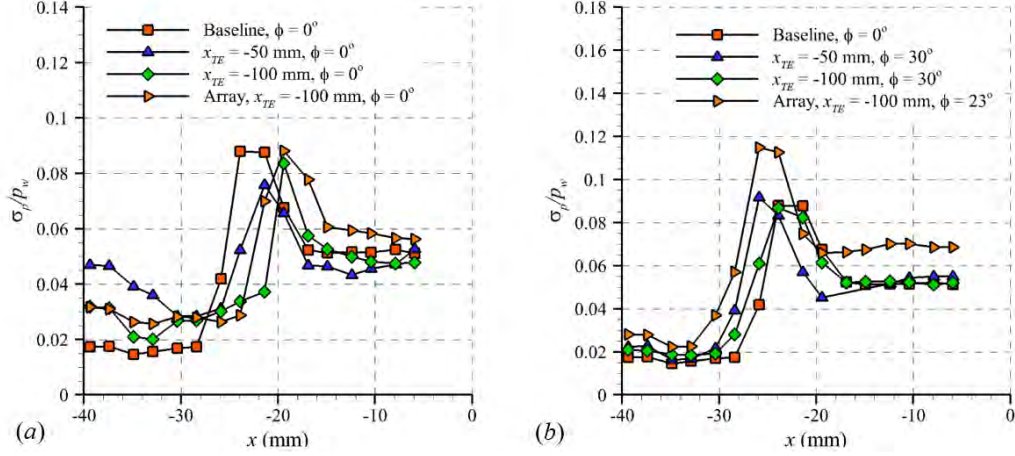


Figure 37: RMS pressure profiles for baseline and VG-controlled interactions (a) wake ($\phi = 0^\circ$) (b) off-wake.

boundary layer at $\phi = 0^\circ$ is apparent. The values of pre-interaction σ_p/p_w are highest for the $x_{TE} = -50 \text{ mm}$ case due to the closer proximity of the microramp, while the single device and array cases with $x_{TE} = -100 \text{ mm}$ have middling levels. The intermittent region occurs farthest upstream for the uncontrolled interaction, at $x = -26 \text{ mm}$. The onset of shock oscillations begins farther downstream for the single VG-controlled cases at $x = -24 \text{ mm}$ and $x = -19 \text{ mm}$ for $x_{TE} = -50 \text{ mm}$ and $x_{TE} = -100 \text{ mm}$, respectively. The array-controlled case with $x_{TE} = -100 \text{ mm}$ exhibits detectable shock excursions slightly upstream of the single VG case, at $x = -21 \text{ mm}$. Hence, it can be said that the energized boundary layer limits the extent of upstream excursions by the separation shock wave. With respect to the peak magnitudes, the cases with single microramps upstream exhibit slightly less severe maximums than the baseline. The array case has a peak σ_p/p_w which is of virtually the same level as the uncontrolled interaction. These results suggest that while the VGs do help to reduce the overall length of the intermittent region, they only marginally lower the intensity of unsteady mechanical loading there. Since the strength of the separation shock wave has been shown to increase in the VG wake, this may lead to larger amplitude pressure fluctuations and decreased benefits directly behind the devices.

The off-wake RMS pressure distributions of Fig. 37(b) all exhibit similar values of σ_p/p_w in the incoming boundary layer, indicating lesser modification outside of the VG span. The intermittent regions of the microramp-controlled cases all begin upstream of the baseline, with the $x_{TE} = -50 \text{ mm}$ and array-controlled case exhibiting the earliest peaks in unsteady loading. The microramp-controlled cases can further be observed to have similar or larger maximum magnitudes of σ_p/p_w as compared to the uncontrolled case. This indicates that the separation shock oscillations lead to larger unsteady mechanical loads outside the VG wake; these more severe separation shock pulsations are also consistent with an increased interaction pressure rise[43]. The authors point out that the pressure time-series from these peak locations were taken as the representative intermittent region signals for the subsequent power spectral density analysis.

6.3.8 Unsteady Wall Pressure Energy Content

Pressure power spectral densities were computed for transducers of peak σ_p/p_w in each of the RMS curves, the results are displayed in Fig. 38. The ordinate, $G(f) \times f$, effectively represents the energy content at each frequency such that its integral is equal to the RMS pressure squared. In this way, the contributions to the observed pressure fluctuations at each frequency can be directly compared. Universally, the characteristics of the spectra in Fig. 38 are consistent with compilations of intermittent region shock fluctuations in 2D SBLI [8]. In contrast to the boundary

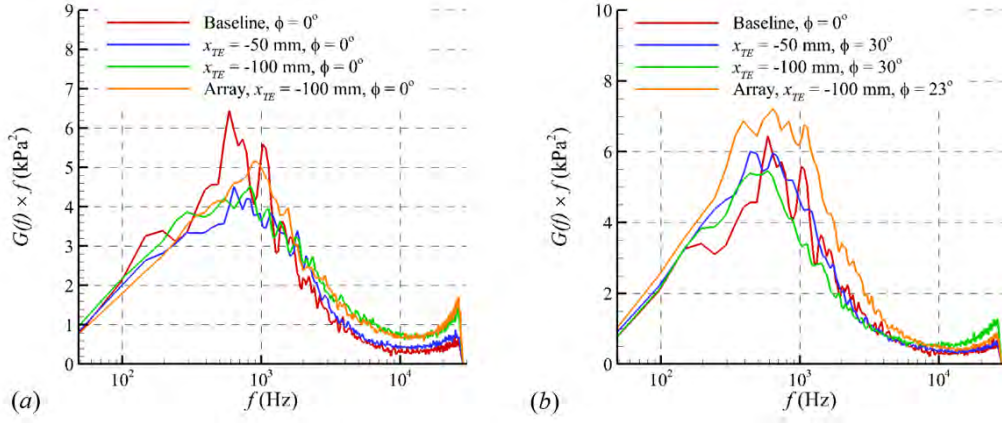


Figure 38: Frequency pre-multiplied pressure power spectra at intermittent region for baseline and VG-controlled interactions (a) wake, $\phi = 0^\circ$ (b) off-wake.

layer power spectra that peaks at very high frequencies ($\sim 100 \text{ kHz}$), the majority of the unsteady energy content occurs over a low-frequency broadband range from $f \approx 100 - 3000 \text{ Hz}$. This is consistent with irregular motion of the separation shock foot across the intermittent region transducer, which is induced by the unsteady expansion/contraction of the separation bubble.

When the shock motions in the direct wake of the VGs at $\phi = 0^\circ$ are compared to the baseline case in Fig. 38(a), the energy distributions are seen to be highly similar. The portions of the SBLI with an energized incoming boundary layer still exhibit low-frequency oscillations. The only notable differences are a decrease in the magnitude of peak power behind the microramps and marginally increased energy content between $f \approx 2 - 3 \text{ kHz}$. The first finding is in keeping with the slight reduction in unsteady loading observed in Fig. 37(a). The second finding is consistent with the observed reduction in separation size aft of the VG, as a smaller separation bubble leads to higher frequency pulsations[43]. The off-wake spectra in Fig. 38(b) also have dominant frequency ranges which are similar to the baseline. A slight reduction in the peak power from the baseline can be observed for the single VG cases at $\phi = 30^\circ$. However, more power is contained in a band from $f \approx 100 - 500 \text{ Hz}$. This shift toward lower frequency shock oscillations is typically observed in interactions with larger inviscid pressure ratios (i.e. larger separation bubbles)[43]. The off-wake energy distribution for the array case exhibits the largest peak power and dominant frequency band, in agreement with the high interaction strength observed in the PSP/PLS.

In conclusion, the observed effect of the VGs on the unsteady separation shock motions is minor, and appears to occur through the mean influence of the devices on separation length. There is no evidence at the intermittent region (or at any other streamwise location, for that matter) which would suggest that the microramps have a distinct spectral signature that reaches the wall within the SBLI at either of the circumferential locations tested. Subsequently, the manner in which the separation shock front responds to upstream and downstream disturbances is investigated.

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