AEROACOUSTIC CHARACTERIZATION OF DUAL-STREAM, SUPersonic, RECTangULAR EXHAUST JETS

A Dissertation in Aerospace Engineering
by
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Submitted in Partial Fulfillment of the Requirements for the Degree of

Doctor of Philosophy

December 2019
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Abstract

A continued desire for high-performing tactical military aircraft motivates the advancement of low-bypass ratio turbofan engine technologies. The high-speed, high-temperature jets exhausting from these engines create dangerously high noise levels in the vicinity of the aircraft and are a source of noise pollution for communities surrounding military bases. Methods to reduce noise without negatively impacting aircraft performance are difficult. Low-bypass ratio, multi-stream exhaust engine architectures for tactical aircraft are currently under development. The concept aims to enhance fuel efficiency while still maintaining the high specific thrusts required to minimize engine size. It is anticipated that the engine nozzles will have low-aspect ratio, rectangular shapes.

The primary goal of this study seeks to gain insight into the aeroacoustic and mean-flow characteristics of low-bypass ratio, rectangular, dual-stream, supersonic jets. Accurate comparisons of the noise radiated from these military-style, dual-stream jets with single-stream jets are lacking. Emphasis is placed on studying the effects of dual-stream nozzle configuration and comparing the noise of dual-stream jets with single-stream jets. Comparisons between jets are made on an equal estimated thrust and mass flow rate basis.

Pitot pressure data acquired at the exit planes of the dual-stream nozzles are used to assess the accuracy of thrust and mass flow rate estimates. It is found that the experimentally estimated thrust values agree to within 6% of the theoretical estimates, while the experimentally estimated mass flow rates agree to within 1% of the theoretical estimates. Stream-wise Pitot pressure measurements and schlieren flow visualizations are used to characterize the dual-stream jet flow fields. Centroid line Mach numbers and shear layer thickness are compared with single-jet data. Fully-developed, dual-stream jet axial velocity profiles collapse to a self-similar shape.

Far-field acoustic measurements aimed at isolating the effects of jet operating conditions are presented. The effects of bypass-to-core jet velocity ratio, and net thrust, on the far-field noise are also studied. Increases in core jet velocity result in an increase in peak noise levels as well as an upstream shift in the peak noise direction. For subsonic core jet
velocities, the thin bypass jet is found to make a 5dB OASPL contribution to the turbulent mixing noise. Turbulent mixing noise decreases with decreasing bypass-to-core jet velocity ratio.

The effects of bypass ratio on noise is are presented for dual-stream jet with a single fluid shield and two, symmetric fluid shield. As bypass ratio is increased, the mixing noise of the symmetric jet is reduced but the mixing noise is invariant for the fluid shield jet. At a bypass ratio of 0.50, the broadband shock-associated noise of the single fluid shield jet is eliminated. Flow field measurements show that the shock noise reduction is due to a weakening of the semi-regular shock-cell system in the jet. This trend is not observed for the symmetric fluid shield jet. The results indicate that nozzle configuration, i.e., the placement of the bypass jet(s) relative to the core jet, affects which noise components are sensitive to changes in operating conditions. The effects of an aft deck installed downstream of the dual-stream nozzle exit plane are also studied.

The noise radiated from the single fluid shield and symmetric fluid shield jets is compared with equivalent single-stream round and rectangular jets. In the minor axis direction, the peak noise levels of dual-stream jets are within 1dB OASPL of the equivalent round jet, but up to 4dB OASPL louder than the equivalent rectangular jet. A second series of comparisons are presented for the single fluid shield jet operating at elevated bypass ratios, up to 0.91. In the minor axis direction, the dual-stream jets reduce overall sound pressure level by 4dB compared with the equivalent round and rectangular jets.
# Table of Contents

List of Figures x

List of Tables xvii

List of Symbols xix

Acknowledgments xxiii

Chapter 1  
Introduction 1

1.1 Motivation for Jet Noise Research ................................. 1

1.2 The Components of Jet Noise and Jet Noise Prediction ............... 3

1.2.1 Turbulent Mixing Noise and Mach Wave Radiation ............... 4

1.2.2 Shock-Associated Noise and Jet Screech ........................ 7

1.3 Noise Reduction Concepts for Single-Stream Jets ..................... 8

1.3.1 Mechanical Chevrons and Fluid Injection .......................... 9

1.3.2 Mechanical and Fluid Nozzle Corrugations ........................ 10

1.3.3 Rectangular Nozzle Designs ..................................... 11

1.3.4 Bypass Flow Effects on Far-Field Noise ........................... 12

1.4 Multi-Stream Exhaust Systems for Jet Noise Reduction ............... 13

1.4.1 Thrust and Mass Flow Rate Considerations for  
Aeroacoustic Measurements of Multi-Stream Jets ...................... 13

1.4.2 Mechanical Chevrons for Multi-Stream Nozzles .................... 15

1.4.3 Mixer-Ejector Nozzles ........................................... 15

1.4.4 Fluid Shield Noise Reduction Concepts ............................ 16

1.4.4.1 Asymmetric Jet Exhaust Concepts .............................. 17

1.4.4.2 Inverted Velocity Profiles .................................... 19

1.5 Adaptive Cycle, Low-Bypass Ratio Turbofan Engines for Tactical Military  
Aircraft ................................................................. 20
<table>
<thead>
<tr>
<th>Section</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.6 Scope of Dissertation</td>
<td>22</td>
</tr>
<tr>
<td>1.6.1 Goals and Original Contribution</td>
<td>22</td>
</tr>
<tr>
<td>1.6.2 Research Objectives</td>
<td>23</td>
</tr>
<tr>
<td>1.6.3 Dissertation Synopsis</td>
<td>25</td>
</tr>
<tr>
<td>Chapter 2</td>
<td>27</td>
</tr>
<tr>
<td>High-Speed Jet Aeroacoustics Facility</td>
<td></td>
</tr>
<tr>
<td>2.1 Anechoic Chamber Description</td>
<td>27</td>
</tr>
<tr>
<td>2.2 High-Pressure Air Delivery and Control System</td>
<td>28</td>
</tr>
<tr>
<td>2.2.1 Overview of the Air Supply And Control System</td>
<td>28</td>
</tr>
<tr>
<td>2.2.2 Upgrades to Air Delivery System for Dual-Stream Jet Operation</td>
<td>30</td>
</tr>
<tr>
<td>2.2.3 Design of the Horizontal, Dual-Stream Jet Plenum</td>
<td>32</td>
</tr>
<tr>
<td>2.3 Dual-Stream, Rectangular Jet Model Designs</td>
<td>35</td>
</tr>
<tr>
<td>2.3.1 Design of the Baseline Dual-Stream Jet Models</td>
<td>35</td>
</tr>
<tr>
<td>2.3.2 Dual-Stream Nozzles with an Aft Deck</td>
<td>44</td>
</tr>
<tr>
<td>2.3.3 Aerodynamic Acoustic Shroud and Acoustic Cover for the Forward Flight Duct</td>
<td>45</td>
</tr>
<tr>
<td>2.4 Single-Stream Equivalent Nozzle Designs</td>
<td>46</td>
</tr>
<tr>
<td>2.4.1 Single-Stream Equivalent Rectangular Nozzle</td>
<td>46</td>
</tr>
<tr>
<td>2.4.2 Single-Stream Equivalent Round Nozzle</td>
<td>48</td>
</tr>
<tr>
<td>2.5 Methods for Estimating Dual- and Single-Stream Jet Thrust and Mass Flow Rate</td>
<td>49</td>
</tr>
<tr>
<td>2.5.1 Dual-Stream Jet Thrust Estimates</td>
<td>49</td>
</tr>
<tr>
<td>2.5.2 Model for Oblique Shock Wave Thrust Losses in Nozzles</td>
<td>51</td>
</tr>
<tr>
<td>2.5.3 Dual-Stream Jet Mass Flow Rate Estimates</td>
<td>53</td>
</tr>
<tr>
<td>2.5.4 Method to Determine Thrust and Mass Flow Rate Matched Dual-Stream Jet Conditions</td>
<td>53</td>
</tr>
<tr>
<td>2.5.5 Determining Single-Stream Equivalent Jet Operating Conditions</td>
<td>55</td>
</tr>
<tr>
<td>Chapter 3</td>
<td>58</td>
</tr>
<tr>
<td>Experimental Methods, Data Acquisition, and Processing</td>
<td></td>
</tr>
<tr>
<td>3.1 Facility A/D Conversion and Data Acquisition System</td>
<td>58</td>
</tr>
<tr>
<td>3.2 Coordinate Systems</td>
<td>59</td>
</tr>
<tr>
<td>3.3 Acoustic Measurements</td>
<td>59</td>
</tr>
<tr>
<td>3.3.1 Far-Field Polar Microphone Array</td>
<td>60</td>
</tr>
<tr>
<td>3.4 Aeroacoustic Simulation of Heated Jets using Helium-Air Mixtures</td>
<td>61</td>
</tr>
</tbody>
</table>
5.1.3 Variation in OASPL with Core Jet Velocity .......................... 127
5.1.4 Effect of Bypass Jet Nozzle Pressure Ratio ............................ 131
5.2 Dual-Stream Jet Noise Variation with Velocity Ratio and Thrust ........ 134
5.3 Effects of Bypass Ratio on Dual-Stream Jet Noise ........................... 136
5.3.1 Experimental Test Plan .................................................. 136
5.3.2 Acoustic Measurements at Baseline Specific Thrust Conditions .......... 138
5.3.3 Acoustic Measurements at Elevated Specific Thrust .................... 143
5.4 Comparisons of Dual-Stream Jets with Single-Stream Equivalent Jets .......................... 146
5.4.0.1 Low Bypass Ratio Comparisons ..................................... 146
5.4.0.2 Elevated Bypass Ratio Comparisons ................................ 148
5.5 Effects of Aft Deck on Far-Field Noise .................................. 151
5.5.0.1 Aft Deck Effects: DSR-FS-BL Nozzle ................................. 153
5.5.0.2 Aft Deck Effects: DSR-Sym-BL Nozzle ............................... 158

Chapter 6
Summary and Conclusions 162
6.1 Summary of Goals and Objectives .......................................... 162
6.2 Summary of Primary Results ................................................. 164
6.2.1 Mean Flow Measurements .................................................. 164
6.2.2 Acoustic Measurements of Dual-Stream Jets with a Single Fluid Shield .................................................. 165
6.2.3 Effects of Bypass Ratio and Nozzle Configuration on Far-Field Acoustics .................................................. 166
6.2.4 Comparison of Acoustic Measurements of Dual-Stream Jets with Single-Stream Jets .................................................. 167
6.3 Recommendations for Future Work ........................................ 168

Appendix A
Photographs of Dual-Stream Jet Facility Upgrades 172
A.1 Photographs of Dual-Stream Plenum Flow Conditioning .................... 172
A.2 Photographs of the Dual-Stream Plenum Assembly and Installation .... 173

Appendix B
Photographs and CAD Drawings of Dual-Stream Nozzle Components 177
B.1 Photographs of Dual-Stream Nozzle Components ........................... 177
B.2 CAD Drawings of Dual-Stream Nozzle Components ........................ 182
# List of Figures

1.1 Measured overall sound pressure levels of a $M_J = 1.36$ jet with a with a helium-air simulated total temperature ratio, $T_0/T_\infty$ ($TTR$) of 3.0 measured by Powers [1]. Theoretical fully-expanded jet velocity is 690 m/s. .................................................. 7

1.2 Schematic diagram defining major and minor axes, and azimuthal angle for a rectangular nozzle. .......................................................... 11

1.3 Conceptual design of a multi-stream, adaptive cycle, low-bypass ratio turbofan with rectangular exhaust proposed for future military tactical aircraft. Image source: [2] .................................................. 20

2.1 Overhead schematic of the Pennsylvania State University High-Speed Jet Aeroacoustics Facility. .......................................................... 28

2.2 Photo schematic of the high-pressure air control piping cabinet in the High-Speed Jet Aeroacoustics Facility. Air control circuit is indicated in blue while red marks the helium control circuit. The photograph was taken in September 2017. .......................................................... 29

2.3 computer-aided design (CAD) model of the piping cabinet assembly in the High-Speed Jet Aeroacoustics Facility with dual-stream jet control upgrades. .......................................................... 31

2.4 Photographs of bypass air line control piping for dual-stream jet experiments. .......................................................... 32

2.5 CAD models of the single jet plenum designed by Veltin [3]. .......................................................... 33

2.6 CAD models of the dual-stream plenum upgrade in the High-Speed Jet Aeroacoustics Facility. .......................................................... 34

2.7 CAD cross-section views of the dual-stream, rectangular jet model. Dimensions are reported in centimeters. .......................................................... 37

2.8 Cross section views of the baseline dual-stream, rectangular nozzles. Red arrows indicate the position where mass flow rates and thrust are estimated. .......................................................... 38

2.9 CAD model showing an isometric cross-sectional view of the DSR-FS-BL nozzle covered with the aerodynamic acoustic shroud. Dimensions are reported in centimeters. .......................................................... 40

2.10 Circular-to-rectangular cross-section transition contours of the core flow. .......................................................... 41
2.11 CAD cross-stream section views of the dual-stream, rectangular model. Cut surfaces are highlighted blue. Axial positions reported under each cross-section view have been non-dimensionalized by the model length, \( L = 37 \text{ cm} \).

2.12 Cross-sectional areas of core and bypass flow along the model axis. Core and bypass flow areas have been non-dimensionalized by their respective throat areas: \( A^*_C = 3.07 \text{ cm}^2 \) and \( A^*_BP = 1.29 \text{ cm}^2 \).

2.13 Distribution of estimated theoretical flow conditions along the model axis. Core and bypass total pressure ratios were set at \( NPR_C = 3.0 \) and \( NPR_{BP} = 2.0 \) to estimate flow properties.

2.14 Aft deck variant dual-stream rectangular nozzles.

2.15 Annotated photographs of the dual-stream plenum before installing dual-stream model and after model has been installed with acoustic foam.

2.16 Drawings of the single-stream equivalent rectangular nozzle with an exit aspect ratio of 2. Dimensions are reported in centimeters.

2.17 CAD model of the single-stream equivalent (SSEQ)-Rnd nozzle.

2.18 Model problem geometry for estimating thrust loss across oblique shock waves. Example oblique shock wave pattern, in red, shown for an \( NPR \) of 3.0 and a turning angle of 4°.

2.19 Results from model problem estimating the change in over-expanded jet thrust through oblique shock waves.

2.20 Example output from MATLAB script used to estimate constant-thrust, constant-mass flow rate, dual-stream jet operating conditions. Output shown for a target thrust of 125 N and target mass flow rate of 0.21 kg/s.

2.21 Example output from MATLAB script used to estimate single-stream equivalent (SSEQ) jet operating conditions. Example output shown for a target thrust of 125 N and target mass flux of 0.21 kg/s.

3.1 Cartesian and spherical coordinate systems.

3.2 Positions of the far-field microphones relative to the dual-stream jet nozzle. Figure is to scale.

3.3 Flow chart detailing the methodology for matching the speed of sound of heated jets using helium-air mixtures.

3.4 Example screen-shot of the LabVIEW code user-interface for acoustic data acquisition.

3.5 Far-field acoustic results for the SSEQ-Rnd and SSEQ-Rect jets. Jets operating at \( M_J = 1.34, TTR = 2.44 \). The characteristic frequencies of the jets are: Rnd: \( f_C = 30.2 \text{ kHz} \), Rect: \( f_C = 25.3 \text{ kHz} \).
3.6 Visualization comparison of schlieren images of pure-air, $M_J$ 1.34 jets exhausting from a $M_D$ 1.73, single-stream, rectangular ($AR = 2$) nozzle. The stream-wise extent of the images is $X/D_{Eq} = 4.5$ (12.4 cm). 73

3.7 Photograph of 1.6 mm gradient filter used for schlieren visualization. Ruler on the bottom is marked in inches. 74

3.8 Z-type schlieren hardware layout for single- and dual-stream jet flow visualizations in the Penn State High Speed Jet Aeroacoustics Facility. Letters mark optical hardware listed in Table 3.2. 74

3.9 Cartoon schematic of a single Pitot probe immersed in a supersonic flow. Subscripts 1 and 2 denote flow quantities upstream and downstream of the shock, respectively. 77

3.10 Photographs of Pitot probe measurement system installed ahead of the DSR-FS-BL nozzle. 79

3.11 Example of Pitot probe measurement locations for dual-stream jet plume survey. Colors indicate individual surveys that are compiled together during data processing. 80

3.12 Example histograms of total plenum pressure during Pitot probe survey of dual-stream jet. 81

3.13 Diagram of error in microphone positioning. Black line indicates the measured position of the microphone used in acoustic data processing. 86

3.14 Out-of-test (top row) and in-test (bottom row) acoustic spectra for pure air and heat-simulated $M_J = 1.36$ jets exhausting from the SSEQ-Rnd nozzle. 88

3.15 Estimated uncertainty in subsonic Pitot pressure measurements. 91

3.16 Example of error in assuming static pressure of over-expanded supersonic jet is equal to atmospheric pressure for Pitot probe data processing. 92

4.1 Summary of baseline dual-stream, rectangular nozzles and coordinate system. 96

4.2 Flow visualization images (top) and Mach number profiles (bottom) of the DSR-FS-BL nozzle operating at TP2: $M_{IC} = 1.38, M_{IBP} = 0.85$. 98

4.3 Flow visualization images (top) and Mach number profiles (bottom) of the DSR-FS-BL nozzle operating at TP4: $M_{IC} = 1.33, M_{IBP} = 1.17$. 100

4.4 Flow visualization images (top) and Mach number profiles (bottom) of the DSR-Sym-BL nozzle operating at TP2: $M_{IC} = 1.38, M_{IBP} = 0.85$. 102

4.5 Flow visualization images (top) and Mach number profiles (bottom) of the DSR-Sym-BL nozzle operating at TP4: $M_{IC} = 1.33, M_{IBP} = 1.17$. 103

4.6 Vorticity thickness of dual-stream jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles. Dotted lines run through the points above the jet centroid line. Solid lines are single-stream vorticity thickness data for an AR 2, $M_J = 1.36$, rectangular jet measured by Akatsuka et al. [4]. 104
4.7 Comparison of centroid line Mach numbers for jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles. Also included are the centerline single-stream centerline velocities for an AR 2, $M_J = 1.36$, rectangular jet measured by Akatsuka et al. [4].

4.8 Comparison of Mach number profiles for jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles. $Z/D_{Eq}$ positions of all profiles have been zeroed to the position of their jet centroid lines.

4.9 Normalized velocity profiles for DSR-FS-BL (top) and DSR-Sym-BL (bottom) nozzles operating at Test Point 2: $M_{J,C} = 1.38, M_{J,BP} = 0.85$.

4.10 Normalized velocity profiles for DSR-FS-BL (top) and DSR-Sym-BL (bottom) nozzles operating at Test Point 4: $M_{J,C} = 1.33, M_{J,BP} = 1.17$.

4.11 Diagram of the half-plane range for the cross-stream Pitot pressure survey.

4.12 Cross-stream Mach number contours measured at $X/D_{Eq} = 0$. Top: DSR-FS-BL nozzle, Bottom: DSR-Sym-BL nozzle. Black dots indicate position of Pitot probe measurements.


4.14 Relationship between measured flow quantities and the area enclosed by the isocontour of the quantities.

5.1 Summary of baseline dual-stream, rectangular nozzles and coordinate system.

5.2 Dual-stream nozzle orientations relative to a far-field microphone for acoustic measurements in the $\phi = 0^\circ$ and $\phi = 270^\circ$ azimuthal directions. Diagrams show a head-on view of DSR-FS-BL nozzle.

5.3 Acoustic summary of the effects of core jet nozzle pressure ratio on the far-field noise, in the $\phi = 0^\circ$ azimuthal direction. Core jet nozzle pressure ratio, $P_0/P_\infty$ ($NPR$) varied while core jet $TTR$ and bypass jet $NPR$ were held constant.

5.4 Acoustic summary of the effects of simulated core jet total temperature ratio on the far-field, in the $\phi = 0^\circ$ azimuthal direction. Core jet $TTR$ varied while $NPR_C$ and $NPR_{BP}$ were held constant.

5.5 Acoustic summary of the effects of simulated core jet total temperature ratio on the far-field, in the $\phi = 270^\circ$ azimuthal direction. Core jet $TTR$ varied while $NPR_C$ and $NPR_{BP}$ were held constant.

5.6 Variation in overall sound pressure level (OASPL) with core jet velocity. OASPL measured at $\theta = 90^\circ$ polar angle in the $\phi = 0^\circ$ azimuthal direction.

5.7 Variation in OASPL with core jet velocity. OASPL measured at $\theta = 45^\circ$ polar angle in the $\phi = 0^\circ$ azimuthal direction.
5.8 Variation in far-field spectra at $\theta = 45^\circ$ with $NPR_{BP}$ for core jet velocities of (a) 325 m/s and (b) 421 m/s. ........................................... 131
5.9 Acoustic summary of the effects of bypass jet nozzle pressure ratio on the far-field noise from dual-stream rectangular jets. Bypass jet $NPR$ varied while core jet $NPR$ and $TTR$ were held constant. Far field noise measured in the $\phi = 0^\circ$ azimuthal direction. ......................... 132
5.10 Acoustic summary of the effects of bypass nozzle pressure ratio on the far-field noise from dual-stream rectangular jets. Bypass jet $NPR$ varied while core jet $NPR$ and $TTR$ were held constant. Noise measured in the $\phi = 270^\circ$ azimuthal direction. ................................. 133
5.11 OASPL at $\theta = 45^\circ$ variation with estimated velocity ratio for jets exhausting from the DSR-FS-BL nozzle. ................................. 135
5.12 OASPL at $\theta = 45^\circ$ variation with estimated thrust for jets exhausting from the DSR-FS-BL nozzle. ................................. 136
5.13 Far-field spectra measured in the $\phi = 0^\circ$ azimuthal direction for jets exhausting from the DSR-FS-BL (left) and DSR-Sym-BL (right) nozzles. 139
5.14 Far-field spectra measured in the $\phi = 0^\circ$, $\theta = 45^\circ$ direction for jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles. ................................. 140
5.15 Directivity of jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles measured in the $\phi = 0^\circ$ azimuthal direction. ......................... 141
5.16 Far-field spectra measured in the $\phi = 270^\circ$ azimuthal direction for jets exhausting from the DSR-FS-BL (left) and DSR-Sym-BL (right) nozzles. 142
5.17 Directivity of jets exhausting from the DSR-FS-BL and DAR-Sym-BL nozzles measured in the $\phi = 270^\circ$ azimuthal direction. ................................. 143
5.18 Far-field spectra, measured in the in the $\phi = 0^\circ$ azimuthal direction, for dual-stream jets operated at an elevated specific thrust of 540 N/(kg/s). 145
5.19 Overall sound pressure levels, measured in the in the $\phi = 0^\circ$ azimuthal direction, for dual-stream jets operated at an elevated specific thrust of 540 N/(kg/s). .................................................... 145
5.20 Far-field spectra, measured in the $\phi = 0^\circ$ azimuthal direction, of dual-stream jets operating at $BPR's \leq 0.5$ compared with SSEQ jets operating at equivalent mass flow rate and thrust conditions. ......................... 147
5.21 Overall sound pressure levels, measured in the $\phi = 0^\circ$ azimuthal direction, for dual-stream jets and single-stream equivalent rectangular and round jets. 148
5.22 Far-field spectra of DSR-FS-BL jets operating at $BPR's$ of 0.65 and 0.91 compared with SSEQ jets operating at equivalent mass flow rate and thrust conditions. .................................................... 150
5.23 Overall sound pressure levels of DSR-FS-BL jets operating at $BPR's$ of 0.65 and 0.91 compared with SSEQ jets operating at equivalent mass flow rate and thrust conditions. .................................................... 151
5.24 Summary of aft deck nozzle geometries and nozzle orientations relative to a far-field microphone for acoustic measurements in the $\phi = 0^\circ$ and $\phi = 270^\circ$ azimuthal directions.

5.25 Far-field spectra for jets exhausting from the DSR-FS-AD and DSR-FS-BL nozzles. Jets operating at Test Point 2, $BPR = 0.30$. Top row: $\phi = 0^\circ$ azimuthal direction. Bottom row: $\phi = 270^\circ$ azimuthal direction.

5.26 Directivity of jets exhausting from the DSR-FS-AD and DSR-FS-BL nozzles. Jets operating at Test Point 2, $BPR = 0.30$.

5.27 Far-field spectra for jets exhausting from the DSR-FS-AD and DSR-FS-BL nozzles. Jets operating at Test Point 4, $BPR = 0.50$. Top row: $\phi = 0^\circ$ azimuthal direction. Bottom row: $\phi = 270^\circ$ azimuthal direction.

5.28 Directivity of jets exhausting from the DSR-FS-AD and DSR-FS-BL nozzles. Jets operating at Test Point 4, $BPR = 0.50$.

5.29 Far-field spectra for jets exhausting from the DSR-Sym-AD and DSR-Sym-BL nozzles. Jets operating at Test Point 1, $BPR = 0.02$. Top row: $\phi = 0^\circ$ azimuthal direction. Bottom row: $\phi = 270^\circ$ azimuthal direction.

5.30 Directivity of jets exhausting from the DSR-Sym-AD and DSR-Sym-BL nozzles. Jets operating at Test Point 2, $BPR = 30\%$.

5.31 Far-field spectra for jets exhausting from the DSR-Sym-AD and DSR-Sym-BL nozzles. Jets operating at Test Point 4, $BPR = 0.50$. Top row: $\phi = 0^\circ$ azimuthal direction. Bottom row: $\phi = 270^\circ$ azimuthal direction.

5.32 Directivity of jets exhausting from the DSR-Sym-AD and DSR-Sym-BL nozzles. Jets operating at Test Point 4, $BPR = 0.50$.

A.1 Photograph and schematic drawing of the core flow conditioning sleeve.

A.2 Photograph of the dual-stream plenum, looking upstream, with focus on honeycomb flow straightening in bypass flow.

A.3 Core flow plenum.

A.4 Assembled, outer, aft end of dual-stream jet plenum.

A.5 Photograph of the bypass flow Pitot tube installed in dual-stream plenum.

A.6 Photograph of the partially assembled plenum. Secondary core plenum supports are labeled.

A.7 Photograph of the fully-assembled and installed dual-stream jet plenum.

B.1 Head-on view, looking upstream, of dual-stream model Stage I, the circular-to-rectangular cross-section transition. Part was rapid-prototyped using the PolyJet HD method with RGD720 as the material.

B.2 Photographs of the core flow Pitot tube installed in Stage I.

B.3 Annotated photographs of dual-stream model Stage III.
B.4 Side view of Stage III with DSR-FS-BL nozzle attached. Approximate contour of bypass jet flow path is outlined by the dotted, blue curves.

B.5 Photograph of the fully-assembled dual-stream jet model looking along the major axis of the nozzle.

B.6 Photograph of the fully-assembled dual-stream jet model looking along the minor axis of the nozzle.

B.7 Dimensioned drawing of the aluminum outer component of dual-stream model Stage 0. The outer component threads on to the outer, 15.24 cm national pipe taper (NPT) plenum pipe. Stage 0 is the adapter that attached models to the dual-stream plenum.

B.8 Dimensioned drawing of the aluminum inner component of dual-stream model Stage 0. The inner component threads on to the inner, 10.16 cm NPT plenum pipe. Stage 0 is the adapter that attached models to the dual-stream plenum.

B.9 Dimensioned drawing of the aluminum attachment plate for dual-stream model Stage 0. The plate bolts on to the outer Stage 0 component and holds the dual-stream model in place.

B.10 Dimensioned drawing of the aluminum flange plate between dual-stream model Stages I and II. Stage I is the circular-to-rectangular cross-section transition and Stage II is the contraction to final width.

B.11 Dimensioned drawing of the aluminum flange plate between dual-stream model Stages II and III. Stage II is the contraction to final width and Stage III is the core converging-diverging section as well as bypass converging section.

B.12 Dimensioned drawing of the stainless steel Stage 3A. Two Stage 3A parts are used to form the upper and lower surfaces of the core nozzle converging-diverging section.

B.13 Dimensioned drawing of the stainless steel Stage 3B. Stage 3B forms the outer converging surface of the bypass jet.

B.14 Dimensioned drawing of the aluminum Stage 3C. Two Stage 3C parts form left and right straight sidewalls dual-stream model Stage III.

B.15 Hole dimensioning for the aluminum Stage 3C.

B.16 Dimensioned drawing of the aluminum nozzle attachment plate. Two attachment plates are used to the attach the interchangeable dual-stream nozzles to model Stage III.
List of Tables

1.1 Takeoff conditions for high-bypass ratio engines installed on commercial aircraft. ......................................................... 2
1.2 Data for low-bypass ratio engines installed on military fighter aircraft. .......................................................... 3
1.3 Single-stream rectangular nozzle designs reported in the open literature .................................................. 12
1.4 Overview of thrust and multi-stream capabilities of jet aeroacoustic facilities. .............................................. 15
1.5 Low-bypass ratio, dual-stream round nozzle designs surveyed ................................................................. 19
1.6 Low-bypass ratio, multi-stream, rectangular nozzle designs reported in the open literature ................................................................. 21

2.1 Summary of dual-stream, rectangular nozzle dimensions .......................................................... 39
2.2 Material and Function of Dual-Stream Rectangular Jet Model Components .................................................. 40
2.3 Examples of dual-stream jet operating conditions with a thrust of 125 N and mass flow rate of 0.21 kg/s ................................................................. 55

3.1 Positions of the far-field microphones .......................................................... 60
3.2 Optical hardware and distances between components used in Z-type schlieren setup .......................................................... 75
3.3 Accuracies of laboratory gauges and transducers .......................................................... 83
3.4 Jet velocity and sound pressure uncertainties .......................................................... 85
3.5 List of uncertainties for far-field acoustic measurements .......................................................... 87
3.6 Test matrix for single-stream reference jets .......................................................... 93
3.7 Test matrix for baseline (BL), dual-stream rectangular (DSR) nozzles .......................................................... 94
3.8 Test matrix for dual-stream rectangular (DSR) nozzles with an aft deck (AD) .......................................................... 95

4.1 Dual-stream jet operating conditions for Pitot probe and flow visualization measurements .......................................................... 97
4.2 Comparison of measured single and dual-stream jet shock cell spacing with single-stream jet model .......................................................... 108
4.3 Comparison of estimated jet mass flow rate and thrust levels from Pitot pressure measurements and theoretical values .......................................................... 116
5.1 DSR-FS-BL jet operating conditions for $NPR_C$ variation 123
5.2 DSR-FS-BL jet operating conditions for $TTR_C$ variation 125
5.3 DSR-FS-BL jet operating conditions for $U_{JC}$ variation 129
5.4 Jet operating conditions for bypass ratio, $\dot{m}_{BP}/\dot{m}_C$ ($BPR$) variation 131
5.5 DSR-FS-BL jet operating conditions for velocity ratio and thrust variation 134
5.6 Dual-stream jet operating conditions for $BPR$ variation 138
5.7 Test program for baseline dual-stream rectangular nozzle acoustic measurements 138
5.8 Jet operating conditions for comparing the far-field acoustics of SSEQ jets with low bypass ratio dual-stream jets 146
5.9 Jet operating conditions for comparing the far-field acoustics of SSEQ jets with elevated bypass ratio DSR-FS-BL jets 149
5.10 Dual-stream jet operating conditions to study aft deck effects 153
5.11 Dual-stream aft deck nozzle test program 153

6.1 Example of dual-stream jet test conditions at elevated core jet total temperature ratios 170
List of Symbols

\( A \)    area
\( AR \)  aspect ratio, \( W/H \)
\( c \)   local speed of sound
\( c_p \) specific heat at constant pressure
\( c_v \) specific heat at constant volume
\( D \) nozzle diameter based on area
\( D_{Eq} \) equivalent nozzle diameter based on area, \( D_{Eq} = \sqrt{\frac{4A}{\pi}} \)
\( D_J \) fully expanded jet diameter
\( f \) frequency (Hz)
\( f_c \) characteristic frequency (in Hz), \( f_c = \frac{U_J}{D} \)
\( H \) height of rectangular nozzle along measured along the minor axis
\( k \) wavenumber, \( k = \frac{2\pi}{\lambda} \)
\( M_a \) acoustic Mach number
\( M_C \) convection Mach number of the jet large scale structures
\( M_D \) nozzle design Mach number
\( \dot{m} \) mass flow rate (kg/s)
\( M_J \) jet Mach number
\( MW \) molecular weight
\( N_{FFT} \) number of data points used to calculate the double-sided power spectral density
\( P \) pressure
\( r \) radial distance measured from the center of the nozzle exit plane
\( Re \) Reynolds number
\( R \) gas constant, \( R = c_p - c_v \). For air, \( R = 287 \text{ J/kg} \cdot \text{K} \)
\( \bar{R} \) universal gas constant, \( 8.314 \, kJ/K \cdot mol \)

\( St \) Strouhal Number, \( \frac{f}{f_c} \)

\( F_s \) specific thrust \( (N/(kg/s)) \), \( T/\dot{m} \)

\( T \) thrust (N)

\( U \) axial velocity

\( \tilde{U} \) mean axial velocity

\( U_J \) fully-expanded jet velocity

\( V \) pressure transducer output voltage

\( V_a \) plenum pressure transducer output voltage for pure-air flow

\( V_{mix} \) plenum pressure transducer output voltage for helium-air mixture flow

\( W \) width of rectangular nozzle along measured along the major axis

\( w \) mass fraction

\( X \) downstream axis oriented in the jet exhaust direction

\( Y \) cross-stream axis oriented along the major axis of a rectangular nozzle

\( Z \) transverse-stream axis oriented along the minor axis of a rectangular nozzle

**Greek**

\( \Delta \) change in quantity

\( \Delta C_{act} \) microphone actuator response correction (in dB)

\( \Delta C_{atm} \) microphone atmospheric attenuation correction (in dB)

\( \Delta C_{ff} \) microphone free-field correction (in dB)

\( \delta_w \) shear layer/vorticity thickness: \( \delta_w = \frac{U(z)_{max}}{\partial U(z)/\partial z}_{max} \)

\( \eta^* \) Nondimensional radius: \( \eta^* = \frac{z-0.5}{\delta_w} \)

\( \gamma \) ratio of specific heats, \( c_p/c_v \)

\( \lambda \) wavelength

\( \mu \) dynamic viscosity

\( \nu \) kinematic viscosity

\( \omega \) radian frequency, \( \omega = 2\pi f \)

\( \phi \) azimuthal angle measured from the cross-stream jet axis, \( Y \)
$\rho$  density
$\sigma$  uncertainty
$\sigma_{u_j}$  uncertainty of the jet velocity
$\theta$  polar angle measured from the downstream jet axis

Subscript
0  total or stagnation quantity
$\infty$  ambient quantity
$a$  pure-air quantity
$BP$  bypass jet quantity
$C$  core jet quantity

exit  quantity relative to the nozzle exit
$J$  fully-expanded jet quantity
$max$  maximum measured value
$mix$  helium-air quantity
$raw$  quantity relative to the raw signal data
$RMS$  root-mean square quantity

Acronyms

AD  aft deck
AR  aspect ratio, $AR = W/H$
$BBSAN$  broadband shock-associated noise
$BPR$  bypass ratio, $\dot{m}_{BP}/\dot{m}_C$
CAD  computer-aided design
CD  converging-diverging
CFD  computational fluid dynamics
DAQ  data acquisition
DNS  direct numerical simulation
FFT  fast Fourier transform
FMEQ  fully-mixed equivalent
<table>
<thead>
<tr>
<th>Abbreviation</th>
<th>Description</th>
</tr>
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<tbody>
<tr>
<td>LES</td>
<td>large-eddy simulation</td>
</tr>
<tr>
<td>MOC</td>
<td>method of characteristics</td>
</tr>
<tr>
<td>NPR</td>
<td>nozzle pressure ratio, ( P_0/P_{\infty} )</td>
</tr>
<tr>
<td>NPT</td>
<td>national pipe taper</td>
</tr>
<tr>
<td>OASPL</td>
<td>overall sound pressure level</td>
</tr>
<tr>
<td>PDF</td>
<td>probability density function</td>
</tr>
<tr>
<td>PIV</td>
<td>particle image velocimetry</td>
</tr>
<tr>
<td>PSD</td>
<td>power spectral density</td>
</tr>
<tr>
<td>RANS</td>
<td>Reynolds-Averaged Navier-Stokes</td>
</tr>
<tr>
<td>SNR</td>
<td>signal-to-noise ratio</td>
</tr>
<tr>
<td>SPL</td>
<td>sound pressure level</td>
</tr>
<tr>
<td>SSEQ</td>
<td>single-stream equivalent</td>
</tr>
<tr>
<td>STOVL</td>
<td>short take-off and vertical landing</td>
</tr>
<tr>
<td>TTR</td>
<td>total temperature ratio, ( T_0/T_{\infty} )</td>
</tr>
</tbody>
</table>
Acknowledgments

I would like to graciously thank my co-advisors Dr. Dennis K. McLaughlin and Dr. Philip J. Morris for providing me the opportunity to work with them for the past six years, and guide me with their practical insight, experience, and wisdom. As I look back, I feel truly lucky, and honored to have been given such a remarkable opportunity. Their innumerable lessons will continue to guide and inspire me for years to come. I greatly appreciate the other members of my Ph.D. committee: Dr. Cengiz Camci, Dr. Robert Kunz, and Dr. Jose Palacios for their time, expertise, and helpful feedback.

This research was supported, in part, by Naval Air Warfare Center - Aircraft Division Section 219 funding, Project # 219WFD-HE-17-014, with Dr. James Sheehy serving as Program Officer and Mr. Jerry Rubinsky serving as Technical Monitor. The views and conclusions contained herein are those of the authors and do not represent the official opinions of the Office of Naval Research, or the U.S. government. I wish to thank the Department of Defense SMART program for their sponsoring. I’m grateful to my mentor Mr. Terence Ghee for his support and guidance as well as Branch Head Mr. Frank Taverna, who was a tireless advocate of mine and I always enjoyed our bi-weekly telecons.

I want to acknowledge the numerous colleagues in the Aerospace Department. In particular, I want to thank Dr. Russel Powers and Dr. Leighton Myers, for coaching me on laboratory procedures, planner-making, and good research practices. I’m also grateful to Nicholas Rudenko, Christopher Shoemaker, James Falcone, Stephen Willoughby, Nick Scupski, Dr. Jessica Morgan, Dr. Junichi Akatsuka, and Dr. Chitrarth Prasad for their research assistance. I also want to thank department staff who assisted with experimental design and operations, and computer support, specifically, Rich Auhl, J.D. Miller, Keith Miska, Kirk Heller, and Mark Catalano.

Lastly, I want to thank my family (Ralph, Carol, Kelcie, and Brian) and the many friends at Penn State and beyond who supported me throughout my studies. I particularly want to acknowledge my closest friends Adam, Brian, Carina, Dan, Erika, Greg, Jay, Matt, Nicole, Sam, Tony, and my girlfriend Kassandra Garza who are the greatest people I know and always a source of fun and laughter. I could not have done it without you.
1 | Introduction

1.1 Motivation for Jet Noise Research

A continued desire for high performing military tactical aircraft motivates the development of increasingly powerful low-bypass ratio \( BPR = \frac{\dot{m}_{BP}}{\dot{m}_C} \leq 1 \) turbofan engines. The high-speed, high-temperature jets exhausting from these engines generate dangerously high noise levels. Full scale acoustic measurements of military aircraft engines demonstrate noise levels in excess of 140 dB OASPL within the vicinity of the aircraft [5] and up to 120 dB OASPL at a distance 300 m away from the aircraft [6]. Studies have shown an increase in hearing loss among Naval personnel on board aircraft carriers compared with personnel at other duty stations [7]. Shipboard personnel on the flight decks of aircraft carriers are often in close proximity (within 50 ft.) to aircraft operating at the high power levels required for shipboard take-off. This comes at high risk to sailors who may be exposed a high number of launches and recoveries during a shift [8,9]. Field measurements on-board aircraft carriers have been made to quantify noise exposure levels at different locations on the carrier deck [9]. At military power, noise levels within 50 ft. of the aircraft often exceed 140 dB. Maximum noise levels during afterburner operation can be in excess of 150 dB [6,8,9]. During an aircraft launch, prior to catapult triggering, the aircraft spends nearly 30 sec at military power [8,9]. This prolonged, repeated exposure to these dangerously high noise levels often exceed the safe exposure limits.

Aircraft noise is a community issue as well. There is increased public pressure on the U.S. Department of Defense to reduce community noise pollution surrounding military bases [8]. This has led to restrictions to both training times and flight paths to minimize the noise impact on nearby communities. Research into the generation and mitigation of
supersonic jet noise is driven by the need to reduce the acoustic impact of current and future aircraft [8].

Community concerns over aircraft noise date back to the 1960’s with the increasing popularity and accessibility of commercial aviation. Over the past several decades, community noise pollution has prompted the FAA and international organizations to put into place rigorous noise certification standards for commercial aircraft and airports [10]. In general, aircraft noise can be sub-divided into three major components: turbomachinery noise, airframe noise, and jet exhaust noise. The relative contributions of these components differ between commercial and tactical aircraft, with the hot, high-speed jet exhaust noise being the dominant source of noise radiated from military aircraft [10]. Engine sizing on commercial aircraft is less restrictive compared with fighter aircraft. Publicly-available data for 10 high $BPR$ engines installed on commercial aircraft are listed in Table 1.1. Engine data at takeoff conditions are publicly available courtesy of General Electric Aviation and Pratt and Whitney through the referenced websites. Commercial aircraft are powered by larger, high-bypass ratio ($BPR > 1$) turbofan engines. These turbofan engines primarily generate thrust by exhausting a large mass of bypass air to lower velocities (compared with military fighter jet exhaust). The cooler, lower speed bypass exhaust improves the propulsion efficiency of the engine [11]. As well, due to the shielding effect of the lower-speed bypassed air, exhaust noise is not the dominant source of noise for commercial aircraft.

Table 1.1: Takeoff conditions for high-bypass ratio engines installed on commercial aircraft.

<table>
<thead>
<tr>
<th>Engine</th>
<th>$BPR$ ($\dot{m}/\dot{m}_{BP}$)</th>
<th>Thrust (kN)</th>
<th>$T/\dot{m}_{air}$ ($N/(kg/s)$)</th>
<th>Fan Diameter (m)</th>
<th>Aircraft</th>
<th>Ref.</th>
</tr>
</thead>
<tbody>
<tr>
<td>CF6</td>
<td>4.32</td>
<td>184.6-310.5</td>
<td>255.6</td>
<td>2.7-2.9</td>
<td>Airbus A330 [12,13]</td>
<td></td>
</tr>
<tr>
<td>JTD9</td>
<td>4.8-5.0</td>
<td>213.5-249.1</td>
<td>253.4</td>
<td>2.40</td>
<td>Boeing 747 [12,14]</td>
<td></td>
</tr>
<tr>
<td>CFM56-7B</td>
<td>5.4</td>
<td>121.4</td>
<td>–</td>
<td>1.55</td>
<td>Boeing 737 [13]</td>
<td></td>
</tr>
<tr>
<td>PW4000</td>
<td>5.4</td>
<td>121.4</td>
<td>–</td>
<td>1.55</td>
<td>Boeing 737 [14]</td>
<td></td>
</tr>
<tr>
<td>GEnx-1B</td>
<td>7.9-9.0</td>
<td>310.5-338.5</td>
<td>267-280</td>
<td>2.80</td>
<td>Boeing 787 [13]</td>
<td></td>
</tr>
<tr>
<td>GP7200</td>
<td>8.8</td>
<td>311</td>
<td>–</td>
<td>2.96</td>
<td>Airbus A380 [13,14]</td>
<td></td>
</tr>
<tr>
<td>GE90</td>
<td>9.0</td>
<td>416.8-512.9</td>
<td>–</td>
<td>3.43</td>
<td>Boeing 777 [13,15]</td>
<td></td>
</tr>
<tr>
<td>PW1700G</td>
<td>9.0</td>
<td>62.3-76.6</td>
<td>–</td>
<td>1.42</td>
<td>E175-E2 [14]</td>
<td></td>
</tr>
<tr>
<td>PW1900G</td>
<td>12.0</td>
<td>84.5-102.3</td>
<td>–</td>
<td>1.85</td>
<td>E190-E2 [14]</td>
<td></td>
</tr>
<tr>
<td>PW1500G</td>
<td>12.0</td>
<td>84.5-111.2</td>
<td>–</td>
<td>1.85</td>
<td>Airbus A220 [14]</td>
<td></td>
</tr>
</tbody>
</table>

The military jet noise issue is exasperated by the practical engineering challenges associated with reducing the acoustic footprint of tactical aircraft. The low-$BPR$, turbofan engines...
powering fighter aircraft must be small to minimize weight and aircraft size. Data for 7 low BPR engines installed on military fighter aircraft are listed in Table 1.2. For similar amounts of thrust, the low BPR engines are about half the diameter of their high BPR counterparts. Because of the reduced engine size, inlet mass flow is reduced, and thus the high thrust required for take-off and supersonic flight is generated by accelerating the hot exhaust gas to supersonic speeds. This results in the low BPR engines having higher specific thrust levels (thrust per unit of mass flow, \( T/\dot{m} \)) compared with high BPR engines. Jet exhaust noise levels are highly influenced by the exhaust jet velocity. Prior research has shown the peak noise levels to scale with the 8\(^{th}\) power of velocity [16–18]. Military aircraft noise levels are less restricted by federal regulations. A top priority is that fighter aircraft must be able to successfully carry out their designed missions. It is imperative that noise reduction technologies not compromise aircraft performance. This limits the feasibility of retrofittable technologies which may add weight and/or impact thrust. However, these challenges inspire advanced engine designs that combine both high-performance and low-noise technologies.

Table 1.2: Data for low-bypass ratio engines installed on military fighter aircraft.

<table>
<thead>
<tr>
<th>Engine</th>
<th>BPR (\dot{m}<em>c/\dot{m}</em>{BP})</th>
<th>Thrust (kN)</th>
<th>(T/\dot{m}_{air}) (\text{N/(kg/s)})</th>
<th>Maximum Diameter (m)</th>
<th>Aircraft</th>
<th>Ref.</th>
</tr>
</thead>
<tbody>
<tr>
<td>TF-41</td>
<td>0.76</td>
<td>63.4</td>
<td>50.8</td>
<td>–</td>
<td>LTV A-7D</td>
<td>[12,19]</td>
</tr>
<tr>
<td>F110-GE-132</td>
<td>0.68</td>
<td>142</td>
<td>1139</td>
<td>1.2</td>
<td>F-16A</td>
<td>[20]</td>
</tr>
<tr>
<td>F100-PW-229</td>
<td>0.36</td>
<td>129.7</td>
<td>–</td>
<td>1.18</td>
<td>F-15E</td>
<td>[21]</td>
</tr>
<tr>
<td>RM12</td>
<td>–</td>
<td>80.5</td>
<td>1167</td>
<td>0.89</td>
<td>JAS 39 Gripen</td>
<td>[20]</td>
</tr>
<tr>
<td>F404-GE-402</td>
<td>–</td>
<td>78.7</td>
<td>1192</td>
<td>0.89</td>
<td>F-18C/D</td>
<td>[20]</td>
</tr>
<tr>
<td>F414-GE-400</td>
<td>–</td>
<td>98</td>
<td>1271</td>
<td>0.89</td>
<td>F-18E/F</td>
<td>[20]</td>
</tr>
</tbody>
</table>

1.2 The Components of Jet Noise and Jet Noise Prediction

The hot, supersonic exhaust the dominant source of noise for a military fighter jet. A comprehensive understanding of noise radiated from these turbulent exhaust jets does not yet exist, but numerous studies have illuminated the underlying physical mechanisms responsible for the high-intensity, directive sound radiated from subsonic and supersonic
turbulent jets. This section provides a summary of the components of jet exhaust noise as well as an overview of the current techniques available to model and predict the far-field acoustics.

1.2.1 Turbulent Mixing Noise and Mach Wave Radiation

Beginning with the seminal work on jet noise by Sir James Lighthill [16, 17], turbulence in the jet shear layer was identified as a predominant source of aerodynamic noise. Lighthill rearranged the continuity and momentum equations into the form of an inhomogeneous wave equation with equivalent source terms. This is commonly referred to at the Lighthill acoustic analogy. Lighthill’s analogy found that fluctuations in pressure, density, and Reynolds’ stresses due to turbulent fluctuations in the jet generate a series of convecting sound sources that behave as quadrupoles. Ffowcs Williams [22] expanded on Lighthill’s work to consider the amplification of sources convecting towards an observer while Lilley [23] considered mean flow refraction effects of sounds waves.

Lighthill estimated that the acoustic power radiated by a jet scales with the 8th power of jet velocity, $U_J$. Ffowcs Williams built off of Lighthill’s work to consider the effects of source convection and convective amplification of noise sources [24]. For higher speed jets, Ffowcs Williams predicted that the far-field noise scales with $U_J^3$ [24]. Experimental evidence by Viswanathan [18] showed that the velocity exponent increases with jet temperature, with the power scaling by $U_J^8$ for unheated jets and up to $U_J^{8.74}$ for heated jets with a TTR of 3.2. Viswanathan [25] later demonstrated that the OASPL also scales with velocity but the value of the exponent is a function of polar angle, $\theta$, and TTR. In the direction of max noise emission, OASPL scales approximately with $U_J^9$ over a range of temperature ratios up to TTR= 3.2.

Lighthill correctly predicted that noise radiated by turbulent structures in the jet was directive in nature with a peak emission angle of

$$\theta_{max} = \sin^{-1}(1/M_C)$$

(1.1)

where $M_C$ is convective Mach number of the large scale turbulent eddies in the jet shear layer with respect to the ambient speed of sound, $M_C = U_c/c_\infty$. $U_C$ is the convective velocity of turbulent eddies in the jet shear layer. Tam and Hu [26] identified three families of instability waves supported by supersonic jets. The most prevalent mode is associated
with the Kelvin-Helmholtz instability and has a convective Mach number given by

\[ M_C = \frac{U_C}{c_\infty} = \frac{U_J + c_J}{c_J + c_\infty} \]  

(1.2)

where \(c_J\) and \(c_\infty\) are the speeds of sound in the jet and in ambient air, respectively, and \(U_J\) is the jet velocity. Kelvin-Helmholtz instability waves are responsible for the development of large-scale turbulent structures in jet flows [26]. Hot wire measurements of a low density \(M_J = 2.1\) jet by Troutt and McLaughlin [27] found the convection velocity of disturbances to be approximately \(0.8U_J\). Lau [28] notes that heating causes a decrease in \(U_C/U_J\) and indeed laser Doppler velocimetry measurements of a heated (\(TTR= 1.6\) and 2.0) \(M_J = 1.65\) jet reported by Ecker et al. showed the convection velocity to between \(0.52U_J\) and \(0.66U_J\). Papamoschou and Bunyajitradyula [29] discuss that one-dimensional correlations do not discern between the different turbulence scales. Tinney et al. [30] applied proper orthogonal decomposition to pressure field data of a cold \(M_J = 0.85\) jet to show the convective Mach number decreases from \(0.8U_J\) to \(0.6U_J\) with increasing mode number, while optical deflectometer measurements by Veltin et al. showed that the convection velocities of instabilities waves increases with frequency [31].

The spatially-coherent structures in high Reynolds number jet shear layers form a train structures gradually growing in scale in the axial direction. These large-scales structures are generated at intermittent times and correlate with positive far-field pressure fluctuations [32–34]. The growth of these large-scale structures saturates towards the end of the potential core and they being to decay. This growth, saturation, and decay of these structures has been referred to as amplitude modulation [35,36]. Hot-wire and acoustics measurements by Troutt and McLaughlin [27] determined the collapse of the large-scale structures to be a noise radiating event while a linear stability analysis by Morris and Tam [37] identified amplitude modulation to be an important aspect of the noise radiation process. The series of intermittently generated, amplitude-modulated structures may be modeled as a linear superposition of instability waves of different wavelengths, \(\lambda\), (or wavenumbers, \(k = 2\pi/\lambda\)) and frequencies, \(\omega\) [35,38]. Even if the average convective Mach number is subsonic, a portion of these instability waves have supersonic phase velocities, \(\omega/k > c_\infty\), that radiate noise [35–37,39].

When the convective Mach number of large-scale structures is supersonic relative to the ambient speed of sound, \(U_C/c_\infty > 1\), the large turbulent structures will generate Mach waves radiation [35,36,40]. This Mach wave radiation is directive noise source that has a peak radiation angle, measured from the downstream jet axis, given by Equation (1.1),
and is responsible for the highest sound levels [40]. Optical measurements of jets with supersonic turbulence convection velocities have observed Mach waves being radiated outward within the first few diameters downstream of the nozzle exit. Some examples of this phenomena are shown in Krothapalli et al. [40] and Tam [36]. Linear stability analysis by Tam and Burton [35] predicted the near-field sound pressure levels due to Mach waves at different frequencies. Their predictions compared well with experimental measurements by Troutt and McLaughlin [27]. Studies on low and moderate Reynolds number jets by McLaughlin et al. [41], Morrison and McLaughlin [42], and Troutt and McLaughlin [27] provided early experimental evidence linking the Mach wave mechanism to the dominant radiated noise levels.

To illustrate the directive nature of jet noise, the far-field OASPL of a $M_J = 1.36$ jet with a helium-air simulated $TTR$ of 3.0 measured by Powers [1], is shown in Figure 1.1. The theoretical, fully-expanded jet velocity, $U_J = M_J c_J$, based on isentropic relations is 690 m/s, where $c_J$ is the speed of sound in the jet. Polar angles are measured from the downstream jet axis; polar angles less that 90° are downstream of the nozzle exit. The peak noise emission angle agrees well with that predicted using Equations (1.1) and (1.2). While the highest noise levels and peak emission angle are governed by the amplitude modulation and Mach wave radiation emanating coherent large-scale turbulent structures in the jet shear layer, Tam et al. [43,44] used experimental evidence to show that the noise radiated to higher polar angles is increasingly due to that generated by fine-scale turbulence. Due to the stochastic nature of fine-scale turbulence, the noise radiated is nearly omni-directional but in the downstream direction its contribution is minimal compared with the noise generated by the large-scale structures.
1.2.2 Shock-Associated Noise and Jet Screech

Unique to imperfectly expanded jets ($M_D \neq M_J$) is the presence of a semi-periodic shock cell structure in the jet that extends downstream of the nozzle exit for several diameters. Acoustic measurements of these jets exhibit a noise component referred to as broadband shock-associated noise (BBSAN). Tam and Tanna [45] offer the following physical explanation for this noise component. The large-scale turbulent structures in jets are coherent and essentially periodic over several jet diameters, and thus the jet shear layer may be thought of as being wave-like. As these periodic structures convect downstream they interact with the semi-periodic shock cells. This interaction gives rise to coherent, wave-like disturbances. Because the range of turbulent scales is broad, the wavenumber range of the generated disturbances is broad as well. The phase velocities of these disturbances are different - most of them being subsonic, i.e. $\omega/k < c_\infty$, and are thus evanescent. However, some of these disturbances have supersonic phase velocities that can efficiently radiate noise to the far-field. These outgoing waves are a form of Mach wave radiation. Each of these outward propagating waves has a different phase velocity and thus radiate most effectively to different angles [46]. This led Tam and Tanna [45] to hypothesize that BBSAN is the superposition of disturbances with supersonic phase velocities generated by the weak interaction of large-scale turbulent structures with the shock cell pattern. Tam and Tanna [45] further went on to perform a theoretical analysis that concluded with fairly simple relationships for the peak frequency and intensity of the BBSAN. Even if a large-scale structure is convecting subsonically with respect to the ambient speed of sound, it can generate a disturbance with a supersonic phase velocity.
Several studies, including [47–49], have shown $BBSAN$ levels to saturate with increasing jet temperature. Experimental work by Kuo et al. [49] showed that peak $BBSAN$ levels, for a constant jet Mach number, plateau to constant levels by $TTR = 2.2$. For overexpanded jets, $M_D > M_J$, peak $BBSAN$ levels increase with increasing jet temperature while the converse is true for underexpanded jets, $M_D < M_J$ [49]. Other studies have shown the broadband shock associated noise to be omni-directional [48,50,51], however, similar to the noise generated by fine-scale turbulence, its contribution to the noise radiated to downstream angles is minor compared with the noise generated by large-scale turbulence.

The interaction of the large-scale turbulent structures with the shock cells generates waves that can travel upstream [45,52]. These upstream traveling waves can interact with the nozzle and flow at the nozzle exit plane to excite a downstream traveling instability wave of the same frequency. This feedback loop generates a high-amplitude, acoustic tone referred to as screech [52]. Tam et al. [52] found that this feedback loop is only maintained through upstream traveling waves of sufficient strength to excite an instability wave large enough to continue the loop. This series of instabilities constitute a narrow band of frequencies at the lower limit of the $BBSAN$ peak [46,52]. Tam et al. [52] developed a semi-empirical prediction for the fundamental screech tone frequency. The amplitude and frequency of the screech tone is dependent on the nozzle geometry and operating conditions. Norum [53], Ponton and Seiner [54], and Shen and Tam [55] found that the nozzle lip thickness affects the receptivity of the flow to upstream traveling waves. Reducing the nozzle lip thickness was found to decrease the amplitude of the screech tone while Shen and Tam [55] also found that increasing jet temperature decreases the amplitude of the tone. Screech is not typically present in full-scale military aircraft exhausts [1,6].

1.3 Noise Reduction Concepts for Single-Stream Jets

The motivation for studying the exhaust jet noise problem is to develop pragmatic jet noise reduction technologies for aircraft. Studies into the fundamental physics of jet noise inspire innovations in propulsion systems designed to target and eliminate these sources. Noise reduction technologies must be proven effective before they are implemented on production aircraft. But therein lies the challenge: in addition to effectiveness, these technologies must not be detrimental to aircraft performance e.g. weight addition, thrust loss, and/or power or mass flow requirements. There is an added level of concern in terms of retrofitting technologies to existing aircraft. Many aircraft or their engines cannot support much of the proposed noise reduction technologies and would require
re-designed engines which may be prohibitively expensive. Morris and McLaughlin provide a review of noise reduction concepts considered for tactical military aircraft [56]. The noise reduction concepts reviewed are: mechanical and fluid chevrons, mechanical and fluid nozzle corrugations, rectangular nozzle designs, and the effects of an interior bypass stream.

1.3.1 Mechanical Chevrons and Fluid Injection

One of the more popular technologies that have been implemented on full-scale aircraft is mechanical chevrons. Chevrons are (usually) triangular-shaped tabs that penetrate a small distance into the jet flow to generate stream-wise vorticity that causes a more rapid decay of the noise-producing large-scale turbulent structures in the jet [56]. Small-scale measurements of chevrons on converging-diverging (CD) nozzles demonstrated OASPL reductions across all polar angles [57]. An large-eddy simulation (LES) study by Liu et al. [58], using the same nozzle geometry as [57], found the chevrons can increase the high-frequency noise generated near the nozzle exit, but are effective at reducing the noise in the mid-frequency range, near $St = 0.45$. More recently, chevrons have been retrofitted to F/A-18 fighter aircraft with measured noise reduction benefits of approximately 2 dB OASPL at afterburner conditions [8].

Fluid injection, of either air or water, into the jet exhaust has been considered as a means of noise reduction. Similar to mechanical chevrons, fluid injection is designed to introduce stream-wise vorticity into the jet to accelerate the break-down of the noise-generating, large structures in the turbulent shear layer. Henderson [59] reviews fifty years of research into fluid injection. Using water as the injection medium has been considered using systems attached to and separate from the the aircraft nozzle [59]. Microjet injection of water at the nozzle exit has been seen to scale with the ratio of the injector momentum to the core jet momentum [60]. The optimum injection angle measured with respect to the jet flow direction was experimentally determined to be approximately 60° [59,61]. Greska et al. [61] and Seiner et al. [62] reported on aqueous microjet injection experiments using a full-scale F-404 engine. Noise reductions of 2-3 dBA were achieved in the peak noise direction. However, the reductions required injection flow rates of 8 – 11% of the core jet mass flow at pressures nearly twenty-eight times atmospheric pressure. The use of microjet water injection systems installed on military aircraft is not practical because of the need to carry and pressurize the water. Water injection systems not installed on vehicles are used for launch systems [59] where injection flow rates are not a concern.
Microjet injection using high-pressure has been considered in a number of studies including [63–67]. Krothapalli et al. [63] reported noise reductions up to 4.5 dB OASPL in the peak radiation direction but included the elimination of screech tones and required air injection pressures more than $30 \times$ atmospheric pressure. Such high pressures are impractical for full-scale aircraft. Henderson et al. [64] and Henderson and Norum [66] studied single stream jets as well as axisymmetric, high-$BPR$, dual-stream nozzles with fluid chevrons. The fluid chevrons had little effect on the far-field acoustics of the dual-stream jets when both streams were operated supersonically.

1.3.2 Mechanical and Fluid Nozzle Corrugations

Full-scale acoustic measurements of an un-installed F-404 engine by Seiner et al. [62] demonstrated hard-walled interior nozzle corrugations to be an effective means of noise reduction. However the permanence of the corrugations within the nozzle would be detrimental to aircraft performance outside of take-off conditions. Alternatively, fluid corrugations are designed to aerodynamically recreate the effects of hard-walled corrugations, with the added benefit of being able to be turned off when not needed. The concept uses a small amount ($\leq 5\%$ of the core mass flow) of bypass air for distributed blowing within the diverging section of the nozzle. Small-scale experiments reported by Powers [1] demonstrated the potential for fluid corrugations to produce noise reductions over 5 dB OASPL. It is important to note that because full-scale aircraft exhausts do not exhibit screech tones, Powers [1] electronically removed screech tones from spectra prior to calculating changes in overall sound pressure levels. McLaughlin et al. [68], and later Morgan [69], further proved the noise reduction capabilities of the fluid corrugation technology through a series of five times larger, industry-scale testing conducted at G.E. Aviation Cell 41 anechoic test facility. Small-scale fluid corrugation results by Cuppoletti et al. [70] reported $BBSAN$ reductions as large as 7 dB OASPL, although the authors included the elimination of screech tones in their results. The works of Powers [1] and Cuppoletti et al. [70] highlight the sensitivity of the noise reductions to the design and operation of the injectors. Powers et al. [71] and Akatsuka et al. [72] considered rectangular, converging-diverging nozzles with fluid corrugations. Modest noise reductions of at most 2 dB OASPL were achieved in the peak noise directions of jets operating at a simulated total temperature ratio of 3.0.
1.3.3 Rectangular Nozzle Designs

For tactical applications, engines with rectangular nozzles have key performance benefits compared to round nozzles including easier thrust-vectoring [73], as well as, less-complicated moving components due to simpler geometry, and lower observability and reduced drag through integration with the aircraft body [74–76]. The details of single-stream, rectangular nozzles reported in the open literature are listed in Table 1.3. The review of rectangular nozzles is focused on low-aspect ratio, \( \text{AR} = W/H \) (AR) nozzle designs of two or less, where the width, \( W \), and height, \( H \), are the nozzle dimensions in the major and minor axis directions, respectively. The definitions of the height, width, and major and minor axes are shown schematically in Figure 1.2. It is conjectured that higher aspect ratio nozzles would not be practical for fighter aircraft as they would be too wide considering the tight spatial constraints on board aircraft carriers. The design Mach numbers \( M_D \), and exit areas, \( A_E \), of the nozzles are listed in the table. Variations in nozzle size are unsurprising as nozzles are sized based on the mass flow rate limitations of their respective facilities. Contour refers to the design of the diverging section wall contour of CD nozzles and can either be method of characteristics (MOC) [77] or Linear, which means linearly diverging, non-parallel walls. Also listed is the AR of the nozzles. Consistent among the nozzles surveyed is that the major axis width remains constant downstream of the nozzle throats. The method for contouring the diverging section of the nozzles is split between MOC and linear, although, based on round nozzle designs for military aircraft, such as [1,62,78], diverging sections with linear walls are expected to be more applicable to full-scale aircraft nozzles because of the more simple design.

![Figure 1.2: Schematic diagram defining major and minor axes, and azimuthal angle for a rectangular nozzle.](image)

Veltin and McLaughlin [79] compared the measured far-field noise of heat-simulated, supersonic, rectangular and circular jets. Rectangular jet noise was measured in both the major and minor axis directions. In the peak noise direction, the overall sound pressure
levels of over-expanded \((M_D > M_J)\), circular and rectangular (both directions) jets were within 2 dB of each other. The highest noise levels were radiated by the rectangular jet in the minor axis direction. At upstream polar angles, the rectangular jets has greater levels of \(BBSAN\). For under-expanded jets \((M_D < M_J)\), the peak noise levels varied by less than 1 dB OASPL. Viswanathan \textit{et al.} [76] compared the predicted far-field noise levels of ideally-expanded \((M_D = M_J)\), rectangular and circular jets. For jets heated to a \(TTR\) of 3.0, the peak noise levels of all jets were within 2 dB OASPL, but the highest noise levels were radiated by the rectangular jet in the minor axis direction.

Table 1.3: Single-stream rectangular nozzle designs reported in the open literature

<table>
<thead>
<tr>
<th>Facility</th>
<th>Ref.</th>
<th>(M_D)</th>
<th>Contour</th>
<th>(AR)</th>
<th>(A_E) ((cm^2))</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cincinnati</td>
<td>[73,76]</td>
<td>1.50</td>
<td>Linear</td>
<td>2.0</td>
<td>3.36</td>
</tr>
<tr>
<td>FSU</td>
<td>[80]</td>
<td>1.44</td>
<td>MOC</td>
<td>4.0</td>
<td>3.98</td>
</tr>
<tr>
<td>NASA Glenn</td>
<td>[81–85]</td>
<td>1.00</td>
<td>Linear</td>
<td>2, 4, 8</td>
<td>23.0</td>
</tr>
<tr>
<td>PSU (Veltin &amp; Goss)</td>
<td>[79,86]</td>
<td>1.50</td>
<td>MOC</td>
<td>1.75</td>
<td>2.43</td>
</tr>
<tr>
<td>PSU (Powers)</td>
<td>[71]</td>
<td>1.65</td>
<td>Linear</td>
<td>2.0</td>
<td>3.98</td>
</tr>
<tr>
<td>GTRI</td>
<td>[87]</td>
<td>1.60</td>
<td>Both</td>
<td>1.5</td>
<td>2.85, 20.3</td>
</tr>
</tbody>
</table>

1.3.4 Bypass Flow Effects on Far-Field Noise

Traditional low-\(BPR\) turbofan engines installed on military aircraft have a primary core flow for thrust and power generation plus a secondary bypass flow used for combustor, turbine, and nozzle cooling. These core and cooling flows mix prior to exhausting from the aircraft [11]. Typically, the bypass fan flow has mass flow rates approximately 30\% that of the core air flow [11,88]. Small-scale experiments typically consider single-jet flows where the bypass flow is assumed to be fully-mixed with the core flow prior to exhausting from the nozzle [88]. This simplifies the experimental hardware and avoids the potential of low-Reynolds number viscous effects associated with trying to replicate the bypass flow effects with small experimental models typical of university research labs. Recent LES simulations of jets exhausting from an F404 nozzle with a 12.9 cm diameter by Liu \textit{et al.} [89] found less than 1 dB OASPL of difference in the peak noise levels between jets with the bypass flow and jets without a bypass flow, but with a turbulent boundary layer along the nozzle wall. A continuation of this work by Liu \textit{et al.} [90] found that the state of the shear layer between the core and bypass flow (laminar vs. turbulent) changes the peak far-field noise by less than 0.5 dB OASPL for core jet \(TTR\)'s up to 2.9.
1.4 Multi-Stream Exhaust Systems for Jet Noise Reduction

This section reviews some of the multi-stream exhaust concepts that have been proposed for low- and high-$BPR$ turbofan engines for jet noise reduction. As well as thrust and mass flow rate considerations for dual-stream jet aeroacoustic testing are discussed. Seiner and Kresja [91] and Martens and Haber [92] review proposed noise reduction technologies for multi-stream, high-speed civil transport aircraft. The topics reviewed in this section: thrust and mass flow considerations for acoustic testing, mechanical chevrons, mixer-ejector nozzles, fluid shields, asymmetric jet including exhaust plume deflection and reshaping, and inverted velocity profiles. The section concludes with a discussion on adaptive cycle, low-$BPR$ turbofan engines in consideration for future tactical military aircraft and their potential acoustic benefits.

1.4.1 Thrust and Mass Flow Rate Considerations for Aeroacoustic Measurements of Multi-Stream Jets

The interpretation of dual-stream jet acoustic data can prove difficult. Tanna [93] remarks that noise reduction results for dual-stream jets may be misleading in their presentation. The example given by Tanna is the reporting of changes in noise levels due to a simply changing the velocity ratio between two streams. This can be a misleading interpretation of noise reduction results because the net thrust and net mass flow rate can change with velocity ratio if jet operating conditions are not chosen carefully. To obtain meaningful results, the noise levels of dual stream jets should be assessed by accounting for critical aircraft engine design and performance parameters. Such parameters include total thrust, total mass flow rate, total enthalpy, and nozzle exit area. Tanna [93] and Tanna and Morris [94] suggest the concept of a fully-mixed equivalent (FMEQ) jet as an appropriate acoustic baseline. The FMEQ jet is defined as having the same total exit area, mass flow rate, and thrust as a dual stream jet. In a parametric study of high-$BPR$ concentric jets, Viswanathan [18], chose operating points by giving practical consideration to full-scale, in-flight, high-$BPR$ aircraft engine operation. The concept of a "mixture velocity" was used to explain changes in far-field noise. The concept of a mixture velocity has also been discussed by other authors, notably Fisher et al. [95,96] and Khavaran and Bridges [97], in modeling coaxial jets. The jets merge downstream into a fully-mixed jet with a single
shear layer between the jet centerline and the ambient air [98]. Using the one-dimensional conservation equations and the equation of state, the area, velocity, and temperature of the mixed jet can be estimated [97]. The properties of this mixed jet vary slightly compared with Tanna’s fully-mixed equivalent jet.

A number of studies including those conducted by [93,94,99,100] presented data on an equal-thrust basis but the work was restricted to subsonic jets. This reduces the difficulty of estimating thrust as the exit pressure is known and non-isentropic thrust losses such as shock waves need not be considered. Previous dual-stream studies involving supersonic jets, including [18,101–104] only considered nozzle configurations in which each jet exhausted to atmosphere, making the estimation of thrust, using a one-dimensional flow assumption and isentropic relations, more accurate and straightforward.

Recently, several authors, including [74,75,105–109], reported on aeroacoustic measurements of low-\(BPR\), dual-stream jets that are embedded upstream in a wall-bounded, secondary diverging section, and partially interact prior to exiting the nozzle. The authors did not report data at thrust or mass flow rate equivalent conditions. The difficulty in estimating the thrust values for these types of nozzle geometries compounded supersonic jets operating at off-design pressure ratios (\(M_D \neq M_J\)). The jets will likely be subject to oblique shock wave in the nozzles. Supersonic flow in planar nozzles can be subject to complex shock waves patterns within the nozzle. This inevitably causes thrust losses and can lead to flow separation within the nozzle [110] as well. Accurately predicting these complicated phenomena, and their impact on thrust, requires validated computational fluid dynamics (CFD) data.

Another option is to measure jet thrust using a thrust stand. Valdez and Tinney [111] recently documented the development of a thrust stand for jet aeroacoustic testing. Although thrust stands can produce reliable measurements of thrust, they can also require facilities separate from anechoic test facilities [87] or require the development of new stands [111], which may be cost-prohibitive. A list of the current experimental aerodynamics facilities and their thrust measurement capability is provided in Table 1.4. The last column indicates whether the facility is also equipped for multi-stream jet measurements. Currently, U.T. Austin is only the research group capable of thrust and acoustic measurements of multi-stream jets [75,111].
Table 1.4: Overview of thrust and multi-stream capabilities of jet aeroacoustic facilities.

<table>
<thead>
<tr>
<th>Facility</th>
<th>Ref.</th>
<th>Acoustic</th>
<th>Thrust</th>
<th>Multi-Stream</th>
</tr>
</thead>
<tbody>
<tr>
<td>A.S.E. Fluidyne</td>
<td>[112,113]</td>
<td>✓</td>
<td>✓</td>
<td></td>
</tr>
<tr>
<td>Boeing LSAF</td>
<td>[18,114]</td>
<td>✓</td>
<td></td>
<td>✓</td>
</tr>
<tr>
<td>Florida State University</td>
<td>[115,116]</td>
<td>✓</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Georgia Tech Research Institute</td>
<td>[87]</td>
<td>✓</td>
<td>✓</td>
<td></td>
</tr>
<tr>
<td>NASA Glenn CE-22</td>
<td>[117]</td>
<td></td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>NASA Glenn SHJAR</td>
<td>[118,119]</td>
<td>✓</td>
<td>✓</td>
<td></td>
</tr>
<tr>
<td>Ohio State University</td>
<td>[120,121]</td>
<td>✓</td>
<td></td>
<td>✓</td>
</tr>
<tr>
<td>Penn State University</td>
<td>[71,122]</td>
<td>✓</td>
<td></td>
<td>✓</td>
</tr>
<tr>
<td>Syracuse University</td>
<td>[105]</td>
<td>✓</td>
<td></td>
<td>✓</td>
</tr>
<tr>
<td>University of California, Irvine</td>
<td>[123]</td>
<td>✓</td>
<td></td>
<td>✓</td>
</tr>
<tr>
<td>University of Cincinnati</td>
<td>[73]</td>
<td>✓</td>
<td></td>
<td></td>
</tr>
<tr>
<td>University of Florida</td>
<td>[124]</td>
<td>✓</td>
<td></td>
<td></td>
</tr>
<tr>
<td>University of Mississippi</td>
<td>[125]</td>
<td>✓</td>
<td></td>
<td></td>
</tr>
<tr>
<td>University of Texas, Austin</td>
<td>[75,111]</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
</tbody>
</table>

1.4.2 Mechanical Chevrons for Multi-Stream Nozzles

Mechanical chevron designs have also been considered for high-\(BPR\), dual-stream nozzle [126, 127]. Both studies only considered subsonic core and bypass jets. Small-scale experiments by Rask et al. [127] found that chevrons on both the core and fan nozzles reduced the noise of heated jets by up to 5 dB OASPL. Moderate-scale acoustic measurements conducted at NASA Glenn Research Center, by Saiyed et al. [126], also studied the effects of chevrons on the fan nozzle only, on the core nozzle only, and on both nozzles. The greatest effective perceived noise reductions, over 2 EPNdB, when corrected for thrust losses, were measured with chevrons installed on the core nozzle only. Today, the use of chevrons are commonplace on commercial, high-\(BPR\) aircraft engines [56, 58].

1.4.3 Mixer-Ejector Nozzles

Mixer-ejector nozzles are designed to mix the high velocity core jet flow with entrained ambient air within a secondary shroud downstream of the primary nozzle prior to exhausting to atmosphere [91, 100, 128]. The objective is to increase the mass flow rate through ambient air entrainment but decrease the mixed jet velocity to reduce the noise, while providing minimal impact on thrust [91]. Earlier studies have indicated the mixer-ejector could produce noise reductions of up to 8 dB [91, 100]. The great noise reductions were achieve
when the entrained flow fully mixed with the core jet prior to exhausting from the nozzle [100]. Kinze et al. [129] considered elliptic jets with a rectangular mixing shroud. Rectangular mixer-ejector nozzles have been studied by Raman and Taghavi [130] and more recently by Zaman et al. [128]. Henderson et al. [100] applied the mixer-ejector concept to a three-stream axisymmetric nozzle. Although the mixer-ejector concept has shown the potential for noise reduction benefits, existing concepts would be prohibitively heavy for full-scale aircraft [91,128].

1.4.4 Fluid Shield Noise Reduction Concepts

High-\textit{BPR} turbofan engines installed on commercial aircraft have a cooler, low-speed bypass exhaust flow surrounding the hot, high-speed core jet exhaust. The renewed interest in the development of supersonic civil transport aircraft [91,92,131,132] has inspired research into multi-stream turbofan engines. As well, the U.S. Military is considering multi-stream exhaust engine architectures for future generations of tactical aircraft [2]. These examples multi-stream exhausts are anticipated to feature a secondary or tertiary stream surround the hot, high-speed core jet. The outer stream can act as a 'fluid shield' to suppress the noise generated by the hot engine core exhaust. The outer secondary and/or tertiary flows reduce the relative convective Mach number of the large turbulent structures in the core jet shear layer to subsonic velocities which severely impacts their ability to radiate noise to the far-field. [131,133,134].

The aeroacoustic characteristics of high-\textit{BPR}, co-annular exhaust jets have been studied over the past several decades by a number of researchers including [18,93,99,102,123,132,135–137]. Henderson [99] compared the aeroacoustics of two and three-stream, high-\textit{BPR} jets. It was found that the third stream had little impact on the large-scale mixing noise peak in the maximum noise emission direction but the third stream was effective at reducing the high-frequency noise levels in the peak noise direction. Reducing the velocity of the core jet (highest speed exhaust) had the greatest impact on radiated noise levels but for equivalent thrust, both dual and three-stream jets had comparable noise levels in peak noise direction and at a polar angle of 90°. In a follow-on study, Henderson and Huff [132] considered geometric variations on the noise radiated from high-\textit{BPR}, three-stream jets. Shortening the cowl length of the core nozzle increased low-frequency noise levels.

Recently, Papamoschou et al. [101] demonstrated the noise reduction potential of low-\textit{BPR}, three-stream jets. Noise reduction in circular multi-stream jets is achieved by decreasing the radiation efficiency of the high-speed core jet. The presence of the surrounding jets
reduces shear stresses and the relative convective Mach number of turbulent eddies in the high-speed core shear layer [134]. A Reynolds-Averaged Navier-Stokes (RANS)-based acoustic analogy model developed for multi-stream jets provides evidence that it is the outer-most shear layer that governs the intensity of noise radiated to the acoustic far field [138]. Papamoschou et al. [101] note that the thickness of the bypass jet is a key factor in the effectiveness of the bypass stream in reducing the far field noise. When the bypass jet(s) are relatively thin, they become fully-mixed with the core jet upstream of the end of the core jet’s potential core which makes them ineffective at shielding/reducing the noise radiated by the large turbulent structures in the core jet shear layer. The objective is to maintain the low-speed secondary jet’s presence downstream of the core jet’s potential core so it can effectively shield the noise radiated by noise radiated by the core jet. [98].

Papamoschou et al. [103] reported on a series of mean velocity measurements of ideally-expanded, low-BPR jets exhausting from dual-stream, rectangular nozzles. The measurements were obtained using a Pitot probe. The study considered 8 high-aspect ratio dual-stream rectangular nozzles: the primary nozzle had an aspect ratio of eight while the bypass jet dimensions were varied. Four of the nozzles had a single bypass stream exhausting to one side of the core jet, while the other four nozzles had two bypass streams exhausting on both sides of the core jet. It was found that increasing the area ratio of the bypass jets decreased the potential core length of the core jet, but that increasing the bypass ratio has little effect reducing the length of the potential core.

### 1.4.4.1 Asymmetric Jet Exhaust Concepts

Asymmetric multi-stream nozzle designs have shown promising noise reductions for high-BPR [123,139–142]. Asymmetric designs allow the secondary (and tertiary if applicable) to persist farther downstream and more effectively shield/eliminate the noise radiated by the core jet. Papamoschou [98,140] proposed a simple concept for "on-demand" directive noise reduction for circular, high-BPR jets, by introducing airfoil-shaped turning vanes in the bypass jet. In principle, the turning vanes can be angled to deflect the bypass exhaust downward by a few degrees with minimal impact on thrust. To the thick side of the bypass jet, the radiated noise levels were found to be reduced by up to 4.5 dB OASPL. Papamoschou et al. [123], Papamoschou and Phong [142], and Henderson and Wernet [143] considered asymmetric three-stream jets. The best results showed up to 15 dB reductions at the mid-to-high frequencies at downstream polar angles [123]. Viswanathan [141] proposed a retrofittable concept for high-BPR, dual-stream nozzles that features a beveled core.
nozzle. Initial experiments showed reductions of 3-4 EPNdB in effective perceived noise levels at takeoff flight speeds.

Asymmetric, low-BPR jets have also received some consideration. Details of the round, dual-stream, low-bypass ratio nozzles reviewed are listed in Table 1.6. The number of bypass streams present in dual-stream model is abbreviated to 'BP'. The ratio, \( A_{BP}/A_C \), is the area of the bypass jet relative to the area of the core jet and \( A_{tot} \) is the total exit area of both the core and bypass nozzles. Much of details on the core nozzle used by Martens and Haber [92] were not reported but the authors did note that the nozzle is a CD nozzle.

Debiasi and Papamoschou [139] performed a rigorous thermodynamic analysis of a multi-stream Brayton cycle to determine realistic jet operating conditions for asymmetric, round nozzles. Martens and Haber [92] reported on a series of industry-scale measurements of a supersonic, round jet with a fluid shield (FS). The fluid shield wrapped halfway around the perimeter of the nozzle and, conceptually, is designed to reduce the noise by reflecting or refracting the acoustic waves due to the impedance change caused by the shield air flow [144]. Martens and Haber [92] considered fluid shield total pressure ratios between 1.3 and 2.5, but the greatest noise reductions were measured for the fluid shield operating at total pressure ratios \( \geq 2.0 \). The shield was effective at reducing the perceived noise (PN) by 2-2.5 PNdB across most polar directivity angles using bypass flow ratios \( (\dot{m}_{BP}/\dot{m}_C) \) typically less than 20%. The authors comment that "a variable bypass ratio engine architecture would represent a near ideal candidate for application of fluid shield technology" [92]. Experiments conducted at NASA Glenn Research Center by Henderson et al. [119] studied a similar fluid shield concept using an offset, circular bypass jet. The authors report on flyover acoustic data of a LearJet 25 aircraft featuring a nacelle with air 'scoops' that direct low-speed air to the bypass jet. Offsetting the bypass jet allows for noise reductions to be directed towards observer positions [101]. Papamoschou et al. [101] report on a series of acoustic measurements of axisymmetric and asymmetric nozzles with two bypass streams. Their study considered core jet pressure and temperature ratios above 4.0, supersonic secondary bypass pressure ratios, and both subsonic and supersonic tertiary bypass pressure ratios. The authors determined nozzle operating conditions using a thermodynamic analysis of a concept variable bypass engine. Up to 3 dB EPNL of noise reduction was measured in the direction of the thickened side of the offset bypass stream.
Table 1.5: Low-bypass ratio, dual-stream round nozzle designs surveyed

<table>
<thead>
<tr>
<th>Facility &amp; Ref.</th>
<th>Core (C)</th>
<th>Bypass (BP)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$M_{D_C}$</td>
<td>$M_{D_{BP}}$</td>
</tr>
<tr>
<td>GE Cell 41 [92]</td>
<td>–</td>
<td>–</td>
</tr>
<tr>
<td>NASA Glenn [119]</td>
<td>1</td>
<td>–</td>
</tr>
<tr>
<td>U.C. Irvine I [139]</td>
<td>1.00 &amp; 1.50</td>
<td>–</td>
</tr>
<tr>
<td>U.C. Irvine II [101]</td>
<td>1.50</td>
<td>MOC</td>
</tr>
</tbody>
</table>

*a Estimated from Figure 14 in [92]

1.4.4.2 Inverted Velocity Profiles

Inverted velocity profiles have been proposed as a means of reducing jet noise. The inverted velocity profile is defined to have a outer flow with higher velocity than the inner flow. This could be achieved in full-scale aircraft exhaust in one of several ways which include: diverting the core flow to the outside of the bypass exhaust, introducing an afterburner to the bypass exhaust, or by operating only the outer rings of the core flow afterburner. Tanna [93] studied the noise radiated for normal and inverted velocity profile subsonic, high-$BPR$, dual-stream jets and compared with the FMEQ jet. Noise reductions were observed when the velocity of the bypass stream exceeded the velocity of the core jet. Martens and Haber [92] explain that noise reductions are achieved by increasing by mixing the high velocity flow stream with the ambient air thus reducing the axial extent of the highest velocities. Experimental work by Daniel et al. [145,146] and large-eddy simulations by Bres et al. [147] studied inverted velocity profile jets in the form of jets with thermal non-uniformities. In practice, this could be implemented relatively simply on a full-scale aircraft by turning off the inner rings of the afterburner. Daniel et al. [145,146] reported noise reductions of 2 dB OASPL in the peak emission direction.
1.5 Adaptive Cycle, Low-Bypass Ratio Turbofan Engines for Tactical Military Aircraft

Throughout the past decade there has been an interest in low-$BPR$, multi-stream exhaust engine architectures for tactical aircraft applications [2, 101, 138]. Inclusion of a tertiary, fan stream in tactical aircraft engine exhaust systems has been inspired by the use of high $BPR$ turbofan engines on commercial aircraft for efficiency and low noise.

The development of three-stream engine concepts for next generation fighter aircraft is supported by the U.S. Air Force Research Laboratory’s (AFRL) Adaptive Engine Transition Program (AETP) [148, 149]. The adaptive cycle engines are being designed to be in the same thrust class as the F135-PW-100 engine [20, 148]. Inclusion of a tertiary bypass stream would provide an added level of control and variability over engine operation, and would allow the engine cycle to adapt between high-thrust or high-efficiency cycle points depending on the demands of the flight profile to maximize performance and range [149]. Moreover, the addition of a cooler, tertiary fan flow in fighter aircraft exhaust may reduce the spectral infrared radiation and improve aircraft stealthiness [150]. A concept for a rectangular, three-stream exhaust engine architecture is shown in Figure 1.3. The concept features a primary core flow (red arrow), a secondary bypass flow pulled from the compressor stages (yellow arrows), and a tertiary fan flow (blue arrows). In 2016, the AFRL awarded contracts of $1 billion to Pratt & Whitney, a United Technologies Corporation, and General Electric Aviation, to continue development of this engine technology [148]. The U.S. Navy is expected to leverage these adaptive cycle engines technologies for future carrier-based aircraft through the Variable Cycle Advanced Technology (VCAT) program [151]. Simulations could be performed at temperature ratios above the limits of helium-air mixture experiments.

![Figure 1.3: Conceptual design of a multi-stream, adaptive cycle, low-bypass ratio turbofan with rectangular exhaust proposed for future military tactical aircraft. Image source: [2]](image-url)

Recent, small-scale studies, including [74, 75, 105–109], have begun studying noise and flow field characteristics of low-$BPR$, multi-stream, rectangular jets. Details of the nozzle
designs are listed in Table 1.6. A common trait among the nozzles is that the bypass stream(s) are positioned along the minor axis direction of the core jet so as to not change the overall width, $W$, of the nozzle. Murakami and Papamoschou [152] and Papamoschou et al. [103] used dual-stream nozzle that had a core nozzle aspect ratio, $AR$, of 8.0 and the bypass nozzles could change size to alter bypass ratio. Their nozzle could be operated with either a single bypass jet exhausting along one side of the core jet, acting as a single fluid shield, or in a symmetric configuration in which the bypass air exhausted along either side of the core jet. The Syracuse nozzle features a single bypass jet exhausting to one side of the core jet while the bypass jet in the U.T. Austin nozzle exhausts along either side of the core jet. Both designs feature a converging bypass nozzle while the core nozzles have similar design Mach numbers: 1.6 for Syracuse versus 1.71 for U.T. Austin.

Table 1.6: Low-bypass ratio, multi-stream, rectangular nozzle designs reported in the open literature

<table>
<thead>
<tr>
<th>Facility &amp; Ref.</th>
<th>Core (C) $M_{D,C}$ Contour</th>
<th>Bypass (BP) $M_{D,BP}$ # BP $A_{BP}/A_C$</th>
<th>$AR$</th>
<th>$AE (cm^2)$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Syracuse [105–109]</td>
<td>1.60 MOC</td>
<td>1.0 1 0.13$^b$</td>
<td>2.74</td>
<td>24.7</td>
</tr>
<tr>
<td>U.C. Irvine [103,152]</td>
<td>1.50 MOC</td>
<td>1.0-1.71 1-2 0.20-0.65</td>
<td>8.0</td>
<td>3.20</td>
</tr>
<tr>
<td>U.T. Austin [74,75]</td>
<td>1.71 MOC</td>
<td>1.0 2 0.32$^c$</td>
<td>2.0</td>
<td>12.9</td>
</tr>
</tbody>
</table>

$^b$ Estimated from Figure 3 in [153], $^c$ Estimated from Figure 2a in [75]

Berry et al. [105] reported far field acoustic measurements of an unheated rectangular supersonic dual-stream jet with a single bypass flow exhausting over an aft deck. The aft deck is designed to simulate the aircraft body for nozzles integrated with the aircraft fuselage. The far-field measurements also showed the presence of a high-amplitude acoustic tone. Using the same model geometry, a LES investigation by Stack and Gaitonde [108] found the source of the tone to be periodic vortex shedding from the trailing edge of the splitter plate between the core and bypass streams within the nozzle. Stack and Gaitonde [109] further demonstrated that this vortex shedding was the dominant source of pressure fluctuations on the aft deck. Stack et al. [154] showed that the shedding frequency scales with the splitter lip thickness. When the splitter lip was thinner than the boundary layer, the periodic vortex shedding no longer observed in the most energetic proper orthogonal decomposition modes. Another LES study by Ruscher et al. [106] applied proper orthogonal decomposition to the unsteady jet exhaust to identify the
modes that contributed most to the radiated noise. Dual-stream, rectangular jets with two identical bypass streams studied by Tinney [74] and Tinney et al. [75]. Tinney [74] investigated the unsteady aft deck surface pressures and, in a follow-up study, Tinny et al. [75] reported on far-field acoustic measurements of unheated jets.

1.6 Scope of Dissertation

Previous work in the Penn State High-Speed Jet Aeroacoustics facility have demonstrated successful noise reduction concepts for single-stream supersonic jets that typically exhaust from round, military-style turbofan engines. Recently, variable cycle point engines with low-aspect ratio rectangular shaped nozzle concepts have been in consideration for future military aircraft. These exhaust concepts feature an additional exhaust stream that may offer an acoustic benefit over the typical military-style jet exhausts.

1.6.1 Goals and Original Contribution

The primary goal of this study is to improve the understanding of the noise radiated from low-$BPR$ ($BPR \leq 1$), rectangular, dual-stream supersonic jets. Particular emphasis is placed on studying the effects of dual-stream nozzle configuration and comparing the noise of dual-stream jets with single-stream jets. It is hypothesized that, for equivalent thrust and mass flow rates, the supersonic, dual-stream jets will offer a noise reduction benefit compared with single-stream jets representative of those installed on tactical military aircraft.

Prior measurements have focused on the far-field noise radiated from multi-stream jets exhausting from high-$BPR$ nozzles. These studies have applications for commercial aviation. In particular, many of the studies reported on noise measurements of subsonic, round jets (e.g. [93, 94, 97, 99]). Asymmetric and rectangular exhausting configurations have received less consideration. While low-$BPR$, multi-stream exhaust concepts have been considered by prior researchers, the focus again was placed primarily on round nozzle designs.

Current studies on rectangular, low-$BPR$, supersonic, dual-stream jets have investigated the turbulent structures of these complex jet flows [107]. Other work has investigated the effects of adding an aft deck and side walls on the far-field noise [75] and unsteady pressure loading on the nozzle [74]. Initial comparisons reported by Berry et al. [105] indicate that
the peak noise levels of military-style dual-stream jets can potentially be 2 dB OASPL quieter single-stream jets in the peak noise direction. However, the dual-to-single-stream comparison of Berry et al. [105] did not account for differences in jet thrust and mass flow rates.

Thorough comparisons of the noise radiated from rectangular, military-style, dual-stream jets with single-stream military-style jets are lacking. The focus of this work is placed on studying the noise from relevant nozzle geometries currently in consideration by the U.S. military for future tactical aircraft. The primary goal of this study is to quantify the potential noise reduction benefit of these military-style, dual-stream jets over equivalent single-stream jets. As a means to this end, the secondary goal of this study aims to improve the understanding of the operating parameters, including Mach number, velocity, thrust, and bypass ratio, on the noise radiated from military-style dual-stream jets. The goals of this study are accomplished through a series of far-field acoustic measurements paired with mean velocity measurements, and flow visualizations. Flow-field measurements give initial insight into the changes in these complex jet flows responsible for the changes in the radiated noise.

1.6.2 Research Objectives

The goals of this dissertation are achieved through the completion of the following research objectives:

1. Design and fabricate a series of variable geometry, rectangular, dual-stream nozzles that are a generic representation of those in consideration for use on future military fighter aircraft [75, 107, 148, 149]. The design process included a survey of the relevant literature on current low-\(BPR\), dual-stream rectangular jet models as well as rectangular nozzle design to understand and overcome the engineering challenges. The model has a modular design to allow for quick interchange of dual-stream nozzles with different bypass jet geometries.

2. Design and install upgrades to the Penn State High-Speed Jet Aeroacoustics facility for experimental flow field and acoustic measurements of dual-stream jets. Facility upgrades include the installation of a new co-annular high-pressure air delivery plenum into the anechoic chamber as well as upgrades to the high-pressure air delivery system to control and monitor secondary air flows with mass flow rates higher than have previously been possible in the Penn State facility.
3. Mean flow measurements and schlieren visualization of the jets exhausting from two baseline dual-stream nozzles. Mean Pitot pressure measurements acquired the nozzle exit plane give insight into the flow uniformity of the nozzle exhausts and are used to provide an experimental estimate of jet thrust. Stream-wise flow field surveys give insight into the changes to the complex, dual-stream jet flow field due to changes in jet operating conditions and nozzle geometry. The stream-wise extent of the shock cells, in addition to jet spreading rates, assist in the interpretation and understanding of far-field acoustic measurements. Flow visualization is used to confirm measured deflections/angularity of the jet exhaust as well as captures additional details of the shock cell structure and jet spreading. Together, the Pitot pressure measurements and flow visualizations begin to form a necessary experimental database to verify future CFD simulations.

4. Perform a series of fundamental acoustic experiments of jets issuing from a dual-stream nozzle with a single fluid shield to gain insight into the effects of jet operating conditions on the noise radiated from dual-stream jets. Parameter variations include core jet Mach number, core jet temperature (simulated with helium-air mixtures), and bypass jet Mach number, and total thrust.

5. Acquire acoustic measurements of jets exhausting from two different baseline dual-stream nozzles operating at constant estimated net thrust and constant, estimated net mass flow rate to quantify the effects of jet bypass ratio on the far-field noise.

6. Conduct acoustic measurements of round and rectangular single-stream equivalent jets. Far-field noise measurements of single-jet are compared with dual-stream jets operating at the same estimated mass flow rate and thrust values to quantify the acoustic benefits of dual-stream jets over single-stream jets.

7. Measure noise changes due the the addition of an aft deck installed downstream of the dual-stream nozzle exit planes to simulate installed nozzle effects. Aft deck effects assessed for jets exhausting from two different geometry dual-stream nozzles.

The objectives are completed using measurements of jets exhausting from two baseline dual-stream, rectangular nozzles. The two baseline dual-stream nozzles considered are a dual-stream nozzle with a single fluid shield and a dual-stream nozzle with two, symmetric fluid shields. The nozzle designs were inspired by relevant geometries, reported in the literature to be in consideration by the U.S. military for use on future tactical aircraft with multi-stream exhaust engines. The noise benefits (reductions) of jets exhausting from
these nozzles is assessed through comparisons with single-stream round and rectangular jets operating at the same thrust and mass flow rates.

1.6.3 Dissertation Synopsis

The remainder of this dissertation addresses the research goal and objectives. Chapter 2 provides a description of the Penn State High-Speed Jet Aeroacoustics facility in which all measurements were conducted. Details on the high-pressure air delivery controls and air delivery plenum into the anechoic chamber are provided. The focus of this discussion is particularly placed on upgrades to the air controls and development of the dual-stream plenum to support dual-stream jet measurements in the facility. Because simulating heated jets is an integral part of the far-field noise measurements, an in-depth discussion on the methodology to acoustically simulate heated jets using helium-air gas mixtures is provided. Chapter 2 then details the design and fabrication process for the dual-stream jet models. A review of the relevant literature surveyed, as well as all pertinent details of the model development and fabrication, are addressed. Following this is an explanation on the design and development of the single-stream equivalent nozzles. The jets exhausting from these nozzles will serve as the single-stream acoustic baseline jets. Chapter 2 concludes with a detailed discussion of the reasoning for, and methodology used, to determine dual- and single-jet operating conditions that maintain constant estimated net thrust and mass flow rate.

Chapter 3 describes the experimental methods, data acquisition, and data processing methodology for the far-field acoustic, schlieren visualization, and mean total Pitot pressure measurements. Attention is paid to the differences in processing methods applied to single and dual-stream jet data including non-dimensionalization of the data. The discussion surrounding Pitot pressure measurements includes the design of the probe traversing system and details the limitations and difficulties regarding intrusive total pressure measurements in supersonic flow. The experimental uncertainties of acoustic and Pitot probe measurements are then reviewed. Chapter 3 concludes with the test matrices for single- and dual-stream jet experiments.

Chapter 4 presents mean flow and flow visualization data acquired for jets exhausting from the two baseline dual-stream nozzles. The data are designed to support the acoustic measurements presented in Chapter 5. The first half of Chapter 5 focuses on the influence of dual-stream jet operating conditions, including core jet Mach number, core jet (simulated) total temperature, bypass jet Mach number, and thrust, on the radiated noise. To this end,
the parameters are varied while maintaining the other two constant. The chapter then transitions to investigate the influence thrust and velocity on the radiated noise. Beginning with Section 5.3, the remainder of Chapter 5 is focused on comparing the acoustics of dual-stream jets on a constant mass flow rate and constant thrust basis. Additional interpretation of this acoustic data is drawn from the flow-field measurements of Chapter 4 and then the effects of an aft deck on the radiated noise are addressed. Chapter 5 concludes with comparing the acoustics of dual-stream jets with single-stream equivalent jets.

Chapter 6 reviews the stated goals and objectives and offers final conclusions of the experiments conducted. Chapter 6 closes with recommendations for further work including outlining a series of experiments designed to reinforce and expanding upon the results of this dissertation, as well as future experimental and computational studies aimed at elucidating the underlying mechanisms responsible for the radiated noise.
This chapter presents details of the anechoic High-Speed Jet Facility at Penn State University. First, an overview of the facility in the horizontal, free jet configuration is provided, followed by a description of the high-pressure air control and delivery system. In particular, focus is placed on the upgrades made to the facility for this study. The details on the designs of the dual- and single-stream jet models are then presented. The chapter concludes with methodologies for estimating the total thrust and mass flow rates for dual- and single-stream jets.

2.1 Anechoic Chamber Description

The Pennsylvania State University High Speed Jet Aeroacoustics Laboratory was used for the experiments presented in this study. The facility is a 5.02 m × 6.04 m × 2.8 m anechoic chamber covered with fiberglass wedges. The theoretical cutoff frequency of the room is 500 Hz. A top-down schematic view of the facility is shown in Figure 2.1. The piping cabinet and horizontal plenum, which are labeled in the picture, allow for controlled, high-pressure air delivery into the chamber. An exhaust collector is installed in the wall opposite the plenum which prevents helium accumulation in the chamber during heat-simulated jets experiments. Further technical details of the anechoic chamber, including the development and prior upgrades, and forward flight simulation, can be found in dissertations by Powers [1], Veltin [3], and Doty [155].

The laboratory is equipped with the capability to simulate jet exhaust of aircraft in forward flight. The facility can generate a co-flow, up to a Mach number of 0.17, that exhausts
parallel to the jet plenum to mimic a full-scale aircraft at takeoff speeds. Technical details regarding the forward flight capability are found the dissertation by Powers [1].

Figure 2.1: Overhead schematic of the Pennsylvania State University High-Speed Jet Aeroacoustics Facility.

2.2 High-Pressure Air Delivery and Control System

2.2.1 Overview of the Air Supply And Control System

Compressed air is supplied to the facility by a CS-121 compressor manufactured by Kaeser Compressors. The air is filtered and dried by a KAD-370 dryer that was also manufactured by Kaeser Compressors. The dried, compressed air is stored in two tanks, each with a 28.3 m$^3$ capacity. At full capacity the tanks are pressurized to 1.24 MPa. The piping system from the tanks to the anechoic chamber is opened using a shop air controlled 2.54 cm pneumatic valve. This pneumatic valve represents an overall choke point in the supply to the facility. The maximum mass flow rate of air into the anechoic chamber when the supply tanks are at full pressure is approximately 0.43 kg/s. The supply piping routes the air from the tanks into a piping cabinet outside of the anechoic chamber. The approximate location of the piping cabinet in the facility is indicated in Figure 2.1. Within the piping cabinet, a series of pressure regulators, ball valves, and gate valves, allow the air flow into the plenum and jet models to be manually controlled during testing.

A photo schematic of the piping cabinet controls is shown in Figure 2.2. The photograph shows all of the valves used to control the high-pressure air and helium flows into the
anechoic chamber. Helium gas can be mixed with the air flow downstream of the control circuit for acoustically simulating heated jets. Further details on the use of helium-air mixtures for aeroacoustic testing are reserved for Section 3.4.

The piping cabinet features both "large" and "small" control lines. The large control line was fabricated from 3.81 cm NPT iron pipe and runs straight through the cabinet. It is attached to ball valve #1 (BV1) and gate valve #2 (GV2). The small control line was fabricated from 2.54 cm NPT iron pipe and branches off of the large line and runs through ball valve #2 (BV2). The small control line is used for controlling nozzles with exit diameters of 2.25 cm up to a NPR of 3.0. For higher mass flow rate requirements, the large control line is used. Gate valve #2 (GV2) is used for coarse adjustment of the jet pressure while GV1, attached to smaller 2.54 cm NPT piping, is used for fine adjustments of the pressure.

![Photo schematic of the high-pressure air control piping cabinet in the High-Speed Jet Aeroacoustics Facility. Air control circuit is indicated in blue while red marks the helium control circuit. The photograph was taken in September 2017.](image)

The facility also has the ability to operate secondary air flows in tandem with the primary air flow. Two smaller air supply lines break off from the main line to allow for separately controlled fluid insert air. Technical details on the design and use of fluid inserts for supersonic jet noise reduction can be found in dissertations by Powers [1] and Morgan [69].
The maximum mass flow rate of air through the fluid insert control lines is approximately 8% of the main air flow.

The main line has the option to route high-pressure air or helium-air mixtures to the top of the anechoic chamber for downward-exhausting, dual-impinging jet experiments. A secondary impinging jet line breaks away from the main line to deliver air to a second nozzle in the impinging jet model. However the secondary air flow was binary in operation: it was either off or delivered air with a total pressure of twice atmospheric pressure. More details on the dual-impinging jet capabilities can be found the dissertation by Myers [156] and thesis by Rudenko [157].

2.2.2 Upgrades to Air Delivery System for Dual-Stream Jet Operation

Operation of dual-stream jet models required upgrades to the facility’s secondary flow control capabilities. Secondary, or bypass, air control piping was retrofitted into the piping cabinet to allow for precise control of bypass air flow pressures with flow rates similar to that of the existing primary control line. A CAD model of the piping cabinet assembly with dual-stream jet control upgrades is shown in Figure 2.3. Dashed pink and green arrows highlight the primary (or core) and secondary (or bypass) flow control paths, respectively.

In the current configuration, helium can only be introduced into the primary air flow for heat-simulated acoustic tests. However, at the time of writing, additional upgrades have been completed that expand this capability so that either the primary or secondary stream can receive helium, although not simultaneously.

The original line feeding the secondary impinging jet (see Figure 2.2), was removed and replaced with the bypass flow control line that runs along the bottom of the piping cabinet. Head loss through pipe elbows can be non-negligible so the bypass piping was designed to be as linear as possible to minimize the number of bends. The bypass control line was fabricated from schedule 40, 2.54 cm NPT, iron pipe with brass control valves. The bypass flow line connects to the dual-stream plenum (discussed in Section 2.2.3) by a 5.08 cm rubber hose and 5.08 cm NPT pipe unions. The unions allow the bypass line to be quickly and easily disconnected from the dual-stream plenum and reattached to the secondary impinging jet piping, if so desired.

Photographs of the bypass air control piping for dual-stream jet experiments are shown in Figure 2.4. The bypass flow is controlled using a 2.54 cm NPT globe valve, for coarse
pressure adjustments, in parallel with a 4.75 mm (inner diameter) needle valve for precise pressure adjustments. Upstream of the control valves is a pressure regulator to maintain steady flow pressure. The regulator was set to maintain a total gauge pressure of 0.93 MPa (135 psig). Bypass jets with a throat area of $1.29 \, \text{cm}^2$ have been successfully tested up to $NPR_{BP} = 4.8$ with these settings. It is not advised to decrease the regulator pressure as testing at lower regulator pressures has caused the control valves to become un-choked when opened too far. This led to strong oscillating pressure instabilities in the bypass control piping.
2.2.3 Design of the Horizontal, Dual-Stream Jet Plenum

The plenum leading into the anechoic chamber, designed by Veltin [3], was only capable of operating as a single jet. CAD models showing isometric and cut views of the plenum are shown in Figure 2.5. The plenum was made of a 1.83 m long, 10.16 cm $NPT$ schedule 40 aluminum pipe. This plenum has been replaced with a concentric, dual-flow plenum to supply the high-pressure source air (and helium) to dual-stream jet models. CAD models showing an isometric and a cut view of the dual flow plenum are shown in Figure 2.6. The plenum consists of a 15.24 cm $NPT$, schedule 80 aluminum pipe concentric with a 10.24 cm $NPT$ schedule 40 aluminum pipe. The pipes are held concentric by a tank fitting on the upstream end of the plenum as well as three semi-adjustable supports halfway down the plenum. Source air for the core jet flows through the center of the 10.16 cm $NPT$ pipe while the bypass air enters the plenum from two sides and flows in the 1.90 cm gap between the two pipes. The core plenum was designed to mimic Veltin’s single jet plenum [3]. The 15.24 cm pipe size was chosen to keep the cross-sectional flow area of both streams equal as well as minimize the overall cross-sectional area of the dual-stream plenum in the forward flight duct so as to not compromise the forward flight capability, [3]. Two aluminum adapters on the downstream end of the plenum are used to attach models to the plenum. Dual-stream
nozzles attach the adapter by way of a flange connection. O-rings in the adapters ensure an air-tight seal between the core and bypass air sources and atmosphere. Photographs of the dual-stream plenum are shown in Appendix A.2 and engineering drawings for the aluminum adapters are shown in Appendix B.2.

Flow conditioning in the dual-stream plenum was designed to be similar to that used in Veltin’s plenum design [3]. Photographs and a schematic diagram of the flow conditioning components are shown in Appendix A.1 and the approximate location of the flow conditioning is noted by the yellow boxes in Figure 2.6. The core flow conditioning consists first of a perforated, stainless steel cone with 6.35 mm holes to encourage complete mixing of air and helium mixtures as well as to "mix out" any flow non-uniformities. Located 12.7 cm downstream of the cone is 7.62 cm of honeycomb to straighten the flow and break down the largest turbulent structures. Both flow conditioning pieces were attached to a polycrylic sleeve that tightly fits inside the upstream end of the 10.16 cm NPT core
plenum. The bypass plenum has 7.62 cm of honeycomb flow conditioning installed at approximately the same downstream position as the core flow honeycomb. Due to the relatively narrow annulus thickness ($H_{annulus} = 1.9$ cm), the bypass flow plenum does not have a perforated cone. Mixing in the bypass plenum occurs over the relatively long length of the bypass plenum ($L_{plenum}/H_{annulus} \approx 63$) which mitigates the potential for any flow non-uniformities at the downstream end of the plenum. A total pressure Pitot probe is mounted in the bypass plenum 7.62 cm from the downstream end of the 15.24 cm pipe. The total pressure of the core air is monitored by a Pitot probe installed in the dual-stream jet model. Photographs of the dual-stream plenum, flow conditioning, Pitot probe, and pipe supports can be found in Appendix A.
2.3 Dual-Stream, Rectangular Jet Model Designs

This section details the design of the rectangular, dual-stream jet models. Specifically, the discussion includes key design objectives used to guide the design of the models, the model interface with the plenum air supply, details of the individual model components, and descriptions of the different dual-stream nozzle geometries. The section concludes with overviews of the aerodynamic shroud and forward flight duct cover that were designed for acoustic testing.

2.3.1 Design of the Baseline Dual-Stream Jet Models

The literature regarding both single- and dual-stream nozzle designs was studied to understand how engineering design challenges were overcome and to determine nozzle geometries that are representative of those expected to be installed on future tactical aircraft. To guide the design of the Penn State experimental model, three key design objectives were identified:

1. Design the model to be generic representation of those in consideration for use on future military fighter aircraft, such as that depicted in Figure 1.3
2. Make the model as modular as possible to expedite model changes and minimize cost to develop different model geometries
3. Maximize the use of rapid-prototyped plastic components that allow for complex flow geometries with relatively low-cost and short manufacturing lead times compared with machined (or rapid-prototyped) metal components.

To meet the first design objective, the design of the models is based on the single- and dual-stream nozzles listed in Tables 1.3 and 1.6, respectively. Particular focus was placed on the most-recent dual-stream nozzle designs by the research teams of Syracuse University [105–107, 153] and U.T. Austin [74, 75]. The development of those models was supported by the U.S. Air Force and are considered to be relevant, practical representations of nozzle geometries in consideration for future aircraft. Dimensioned CAD cross-section drawings of the Penn State dual-stream rectangular jet model are shown in Figure 2.7.

It is emphasized that the Penn State model, as well as those at Syracuse University and U.T. Austin are two-stream nozzles that approximate a three-stream engine exhaust configuration. The aim is to recreate the mean-flow and acoustic effects of the core and
tertiary fan flows. The core and secondary fan flows are assumed to be fully-mixed prior to entering the engine exhaust [107]. As was discussed in Section 1.3.4, the secondary fan flow has been found to have a minimal affect on the far-field noise. Therefore, the Penn State model has also been designed as a dual-stream jet nozzle to reduce the model complexity, and to simplify future comparisons with data from other dual-stream jet facilities.

The Penn State dual stream, rectangular (DSR) nozzle model has the ability to operate in two configurations. In the first configuration, the bypass stream exhausts along one side of the core jet to act as a single fluid shield (FS). This configuration is referred to as the DSR-FS-BL nozzle and was inspired by the work of the research team led by Professor M. Glauser at Syracuse University [105–107,153]. A cross-sectional view, looking along the major axis of the DSR-FS-BL nozzle, is shown in Figure 2.7a. A plug, which is labeled in Figure 2.7a, is inserted in the model to block off the upper half of the bypass air flow. In the second dual-stream model configuration, the bypass stream is split and exhausts on both minor axis sides of the core jet. This exhaust configuration follows that of Tinney [74] and Tinney et al. [75] and is referred to as the DSR-Sym-BL nozzle. A cross-sectional view, looking along the major axis of the nozzle, is shown in Figure 2.7b.

Detailed views of the two nozzle exhaust configurations are shown in Figure 2.8. Cross-sectional views are shown looking along the major axis direction. In both configurations, the major axis width (into the plane of the page), \( W \), is a constant 3.54 cm. This width is the same for the core and bypass flows. Therefore, area changes are accomplished only through changes in the height, \( H \). Nozzle dimensions for each configuration are summarized in Table 2.1.

In both nozzle configurations, the core (subscript C) nozzle is identical. The core nozzle area varies linearly through the CD section. This is expected to be more applicable to military aircraft nozzles. The core nozzle has an area ratio, \( A_C/A_C^* \), of 1.295 corresponding to a design Mach number, \( M_{D,C} \), of 1.65, where the "exit" of the core nozzle is highlighted by the red arrows in Figure 2.8. The "exit" is defined to be the stream-wise position just upstream of where the core and bypass jets meet. "Exit" dimensions of the core nozzle, \( H_C \times W \), are 1.15 cm \( \times \) 3.45 cm. These dimensions correspond to an equivalent diameter, \( D_C \), based on area, of 2.25 cm. The core nozzle size was chosen to facilitate comparisons with previous acoustic and mean velocity measurements of jets of this scale [1]. At \( TTR_C = 3.0, NPR_C = 3.0 \), the core jet Reynolds number, \( Re_C \), is estimated to be \( 6.00 \times 10^5 \).
(a) Dual-stream rectangular (DSR) jet model in fluid shield (FS) baseline (BL) configuration. Abbreviation: DSR-FS-BL model. Section view is looking along the major axis.

(b) Dual-stream rectangular (DSR) jet model in twin, symmetric bypass (Sym) baseline (BL) configuration. Abbreviation: DSR-Sym-BL model. Section view is looking along the major axis.

(c) Section view of the core air flow path looking along the minor axis.

Figure 2.7: CAD cross-section views of the dual-stream, rectangular jet model. Dimensions are reported in centimeters.

Internal contours for the bypass streams (BP) were generated with third-order polynomials in MATLAB to ensure the flow path is strictly converging. The models of Syracuse and
U.T. Austin also feature choked flow bypass jet designs. Because the DSR-Sym-BL nozzle features two symmetric bypass streams, the choked flow area, $H_{BP}^\ast \times W$, of each stream is half of the bypass jet area in the DSR-FS-BL nozzle. Thus the total bypass jet area is constant between the two configurations. Bypass stream dimensions in the DSR-FS-BL nozzle configuration are 0.374 cm × 3.45 cm. Using $H_{BP}^\ast = 0.374$ cm as the reference length, with bypass jet operating conditions of $TTR_{BP} = 1$ and $NPR_{BP} = 2.5$, the estimated Reynolds number of the bypass jet is $Re_{BP} = 1.37 \times 10^5$. For the DSR-Sym-BL nozzle, the dimensions of each bypass stream are 0.187 cm × 3.45 cm. Using $H_{BP}^\ast = 0.187$ cm as the reference length, with bypass jet operating conditions of $TTR_{BP} = 1$ and $NPR_{BP} = 2.5$, the estimated Reynolds number of each bypass stream is $Re_{BP} = 0.684 \times 10^5$. Aeroacoustic studies of jets at comparable Reynolds numbers have been reported by Troutt and McLaughlin [27] and Kibens [158].

In all configurations, the core and bypass jets are embedded within a secondary diverging section where they partially mix prior to exhausting to atmosphere. The designs of Syracuse and U.T. Austin also include similar features. In a full-scale, tactical aircraft, this feature would likely be included to further shield the hot core jet exhaust from infrared-based detection. This secondary expansion section allows the two jets to partially mix as they are expanded to the desired exit dimensions. Exit plane dimensions, $H_E \times W$, of the dual-stream nozzles are 1.73 cm × 3.54 cm, corresponding to an equivalent exit diameter $D_E$, based on area, of 2.76 cm. The exit area of the dual-stream nozzles has been designed to closely match the "Gen2" scale nozzles ($D_E = 2.7$ cm) tested at Penn State by Morgan [159].
The final two design objectives were to design the model to be modular and maximize the use of rapid-prototyped hardware. To meet these objectives, the dual-stream model was designed as a series of rapid-prototyped and machined metal sections. Table 2.2 summarizes the model sections, fabrication materials, and component function of each numbered section in Figure 2.7a. Each section bolts together through mating flanges while tongue-and-groove joints between the stages ensure proper alignment of all components. The joints and flanges can be seen in the isometric cross-sectional view of the DSR-FS-BL CAD model shown in Figure 2.9. Additionally, channels in the mating faces are filled with room-temperature vulcanizing silicone, to ensure air-tight seals. Photographs of the individual model components are shown in Appendix B.1 and the engineering drawings used to fabricate all metal components are shown in Appendix B.2. The Polyjet HD RGD720 material mentioned in Table 2.2 has previously been referred to by its other name, 'Polyjet Amber Clear' by Powers [1]. Rapid-prototyped components were designed to have at minimum 1.27 cm thick walls to ensure they would withstand internal pressures more than five times atmospheric pressure.

Section 0 is the aluminum plenum adapter that was previously discussed in section 2.2.3. The model attaches to the adapter by a press-fit flange connection. Both the model and the adapter are round at this point the provides a rotational degree of freedom for the model to change the azimuthal orientation for acoustic measurements.

Section 1 is the start of the dual-stream jet model and transitions flow from a circular cross-sectional shape to a rectangular one. The manner in which the cross-sectional area changes

---

**Table 2.1: Summary of dual-stream, rectangular nozzle dimensions**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>DSR-FS-BL Value</th>
<th>Value/$H_C^*$</th>
<th>DSR-Sym-BL Value</th>
<th>Value/$H_C^*$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$H_C^*$</td>
<td>0.89 cm</td>
<td>1</td>
<td>0.89 cm</td>
<td>1</td>
</tr>
<tr>
<td>$W$</td>
<td>3.45 cm</td>
<td>3.89</td>
<td>3.45 cm</td>
<td>3.89</td>
</tr>
<tr>
<td>$H_C$</td>
<td>1.15 cm</td>
<td>1.295</td>
<td>1.15 cm</td>
<td>1.295</td>
</tr>
<tr>
<td>$W/H_C$</td>
<td>3.0</td>
<td>–</td>
<td>3.0</td>
<td>–</td>
</tr>
<tr>
<td>$L_1$</td>
<td>1.78 cm</td>
<td>2</td>
<td>1.78 cm</td>
<td>2</td>
</tr>
<tr>
<td># Bypass</td>
<td>1</td>
<td>–</td>
<td>2</td>
<td>–</td>
</tr>
<tr>
<td>$H_{BP}^*$</td>
<td>0.374 cm</td>
<td>0.42</td>
<td>0.187 cm</td>
<td>0.21</td>
</tr>
<tr>
<td>$H_{BP}/H_C$</td>
<td>0.32</td>
<td>–</td>
<td>0.16</td>
<td>–</td>
</tr>
<tr>
<td>$W/H_{BP}$</td>
<td>9.25</td>
<td>–</td>
<td>18.5</td>
<td>–</td>
</tr>
<tr>
<td>$H_E$</td>
<td>1.72 cm</td>
<td>1.94</td>
<td>1.72 cm</td>
<td>1.94</td>
</tr>
<tr>
<td>$W/H_E$</td>
<td>2.0</td>
<td>–</td>
<td>2.0</td>
<td>–</td>
</tr>
<tr>
<td>$L_2$</td>
<td>1.78 cm</td>
<td>2</td>
<td>1.78 cm</td>
<td>2</td>
</tr>
</tbody>
</table>

---
shape from round to rectangular can have significant effects on internal flow separation and swirl [160]. Frate and Bridges [81] and Powers et al. [71] used the 'flat wrap' design [161] to transition the cross-section shape. This method is fairly simple to implement and is relatively easy to fabricate when machining is a concern. However, this design can be coarse and rapidly introduce corners into the flow.

Figure 2.9: CAD model showing an isometric cross-sectional view of the DSR-FS-BL nozzle covered with the aerodynamic acoustic shroud. Dimensions are reported in centimeters.

<table>
<thead>
<tr>
<th>Section</th>
<th>Material</th>
<th>Summary of Function</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>Aluminum (Al)</td>
<td>Plenum piping adapter and attachment point for dual-stream jet models</td>
</tr>
<tr>
<td>I</td>
<td>Polyjet HD RGD720</td>
<td>Circular-to-rectangular cross section transition</td>
</tr>
<tr>
<td>II</td>
<td>Polyjet HD RGD720</td>
<td>Contraction to final (major axis) width</td>
</tr>
<tr>
<td>Plug</td>
<td>Polyjet HD RGD720</td>
<td>Removable upstream plug for fluid shield (bypass along one side only) model configuration</td>
</tr>
<tr>
<td>III</td>
<td>Al and Stainless Steel</td>
<td>Core stream converging-diverging section. By-pass converging section. Area changes in minor axis dimension only.</td>
</tr>
<tr>
<td>IV</td>
<td>Polyjet HD RGD720</td>
<td>Interchangeable dual stream, rectangular nozzles</td>
</tr>
</tbody>
</table>
Another option is to use a family of ellipsoids to create a smooth transition [162]. This was the method chosen to transition the flow cross sectional shape from circular to rectangular. A series of ellipsoid curves are generated in MATLAB, imported into SolidWorks, and lofted together to form the duct shape. Example contours for the core transition are shown in Figure 2.10. The core flow transitions from a 5.72cm diameter circle to a 7.00 cm × 2.54 cm rectangle over 12.7 cm. The area reduces by 30% over this transition to maintain a favorable pressure gradient and mitigate the potential for flow separation. The bypass stream transitions over the same distance as the core flow and is simultaneously diverted around the core. A series of cross-stream cross section images showing the circular-to-rectangular cross section transition of the core and bypass streams are shown in Figure 2.11.

![Figure 2.10: Circular-to-rectangular cross-section transition contours of the core flow.](image)

Section II is contracts the core and bypass flows to their final widths. The contractions were designed in SolidWorks using spline curves. The flow area contracts over a length of 7.62 cm. Downstream of Section II the flow widths remain a constant 3.54 cm and area changes are accomplished solely through changes in duct height. A rapid prototyped plug is inserted into one of the bypass flow channels in Section II to block off one of the bypass jets for the DSR-FS-BL model configurations.

Section III forms the converging-diverging section of the core nozzle as well as the upstream half of the bypass flow contraction. This section is comprised of a series of machined aluminum and stainless steel parts. The engineering drawings used for part fabrication are shown in Appendix B.2. Section III was fabricated from metal components to ensure
Figure 2.11: CAD cross-stream section views of the dual-stream, rectangular model. Cut surfaces are highlighted blue. Axial positions reported under each cross-section view have been non-dimensionalized by the model length, $L = 37 \text{ cm}$.

sufficient material strength. The splitter plate between the core and bypass streams has a trailing edge lip thickness of 0.38 mm. This was the minimum thickness possible by the wire electrical discharge manufacturing process used to fabricate the part. A thin trailing edge is used to mitigate the potential for any flow instabilities and acoustic tones that could result with a thicker trailing edge. The acoustic data presented by Berry et al. [105] was compromised by a strong acoustic tone that was eventually confirmed to be associated with the Kármán vortex street formed at the trailing edge of the thick splitter plate in their model [108].

Section IV is the interchangeable dual-stream nozzles. The nozzles attach to the model by a press-fit flange connection. The plates that hold the nozzle to Section III can be removed by undoing 10 screws. This allows quick interchange of different dual-stream nozzles during testing.

The stream-wise area distributions of the core and bypass flow paths for the dual-stream models are plotted in Figure 2.12a and Figure 2.12b, respectively. The areas in Figure 2.12 are non-dimensionalized by their respective throat areas. The horizontal, dashed line
indicates \( A/A^* = 1.0 \). Vertical, dashed-dot lines mark the ends of the model sections. Straight-walled regions with constant flow area were incorporated between model sections to simplify the mating geometry. Frate and Bridges [81] note that constant area sections clean up the flow to achieve a more uniform flow profile. The dual-stream models have been designed to maintain as gentle of an area reduction as possible to minimize the potential for flow separation and pressure losses.

![Figure 2.12: Cross-sectional areas of core and bypass flow along the model axis.](image)

(a) Core flow cross-sectional area along the model axis.

(b) Bypass flow cross-sectional area along the model axis.

Figure 2.12: Cross-sectional areas of core and bypass flow along the model axis. Core and bypass flow areas have been non-dimensionalized by their respective throat areas: \( A^*_C = 3.07 \text{ cm}^2 \) and \( A^*_BP = 1.29 \text{ cm}^2 \). Theoretical Mach number and pressure ratio distributions for the core and bypass jets have been calculated using isentropic relations and plotted in Figure 2.13a and Figure 2.13b, respectively. The horizontal line in Figure 2.13a marks \( M = 0.3 \) above which compressibility effects are traditionally considered to be non-negligible [163]. The flows remains effectively incompressible through the dual-stream model until the core and bypass flows are within the converging section of their nozzles. This helps to ensure a minimal pressure gradient, and hence a negligible pressure loss in either stream as the air flows through the dual-stream model.
Figure 2.13: Distribution of estimated theoretical flow conditions along the model axis. Core and bypass total pressure ratios were set at $NPR_C = 3.0$ and $NPR_{BP} = 2.0$ to estimate flow properties.

2.3.2 Dual-Stream Nozzles with an Aft Deck

Integration of the nozzle with the aircraft fuselage can potentially shield the noise and other aircraft observable parameters from ground-based detection [164,165]. Blended nozzle-body designs with rectangular-shaped nozzle exhausts are featured in existing tactical aircraft such as the Northrop-Grumman B-2 Spirit bomber, Northrop-Grumman X-47B unmanned demonstrator, and Lockheed Martin F-22 Raptor fighter. In such designs a portion of the aircraft fuselage protrudes downstream of the nozzle exit and the nozzle exhaust flows over this aft deck. The effect of an aft deck on the noise radiated from rectangular, multi-stream exhaust jets have received little attention. To this end, two aft deck (AD), dual-stream nozzles, derived from their baseline counterparts, the DSR-FS-BL and DSR-Sym-BL nozzles, have also been designed.

Cross-sections along the major axis of the aft deck nozzles are shown in Figure 2.14. Aft deck extensions have been designed to be a generic representation of those found on military tactical aircraft. The aft deck is a constant width extension that matches the
slope of the lower surfaces of the nozzle and extends one equivalent nozzle exit diameter, 2.76 cm, downstream of the nozzle exit plane ($L_{AD} = 1D_{Eq}$).

(a) Dual-stream, rectangular nozzle with a single fluid shield and an aft deck (AD). Abbreviation: DSR-FS-AD
(b) Dual-stream, rectangular nozzle with 2 symmetric bypass jets and an aft deck (AD). Abbreviation: DSR-Sym-AD

Figure 2.14: Aft deck variant dual-stream rectangular nozzles.

2.3.3 Aerodynamic Acoustic Shroud and Acoustic Cover for the Forward Flight Duct

In order to obtain repeatable, high-quality acoustic data, proper care must be taken to address and minimize the potential for acoustic reflections within the anechoic chamber. To this end, an aerodynamic shroud, with swept-back, contoured surfaces, was designed to cover the dual-stream jet model during acoustic testing to shield the flat, reflective surfaces on the model. A CAD model, showing an isometric cross-sectional view of the DSR-FS-BL model covered with the aerodynamic shroud is presented in Figure 2.9. During static acoustic testing (i.e. no forward flight), the aerodynamic shroud is covered with approximately 6 cm of acoustic foam to further minimize the potential for acoustic reflections. Additionally, for acoustic testing, a new acoustic cover has been designed to shield the facility’s forward flight duct. The cover consists of alternating foam wedges positioned around the perimeter with 10 cm thick acoustic foam in the center around the plenum piping. Photographs of the forward flight duct and plenum with and without acoustic treatment are shown in Figure 2.15.
2.4 Single-Stream Equivalent Nozzle Designs

A key objective to this work is to determine whether, low-bypass ratio, dual-stream rectangular exhaust jets offer an acoustic benefit (lower noise) compared with single-stream jets. To meet this objective, the far-field noise radiated by jets exhausting from two single-stream equivalent (SSEQ) nozzles was also measured.

The single-stream equivalent SSEQ jet is defined to be the jet exhausting from a single-stream nozzle with that produces the same net mass flow rate and thrust, or the same thrust and mass flow per unit area, as the dual-stream jets. This section describes the design of the single-stream equivalent nozzles. The details of the SSEQ rectangular nozzle specifically fabricated for this study are presented first and are then followed by the details of the SSEQ round nozzle which has been in use in the Penn State facility for 5 years.

2.4.1 Single-Stream Equivalent Rectangular Nozzle

A SSEQ rectangular (Rect) nozzle was designed based on the dual-stream nozzles. The nozzle is designed to exhaust an equivalent single-stream rectangular jet that served as
the one of acoustic baselines for dual-stream jet acoustic measurements. The SSEQ-Rect nozzle has the same throat and exit areas as the dual-stream nozzles. The design of the SSEQ-Rect nozzle is inspired by Tanna’s fully-mixed equivalent jet [93]. The FMEQ jet is a dual-stream jet that is defined as having a uniform exit velocity profile, and the same exit area, same mass flow rate, and same thrust as other dual-stream jets.

The SSEQ-Rect nozzle has the same exit dimensions as the dual-stream nozzles \((H_{SSEQ,E} \times W_{SSEQ,E})\) of 1.73 cm \(\times\) 3.45 cm, with a corresponding aspect ratio \((W_{SSEQ,E}/H_{SSEQ,E})\) of 2.0. Moreover, the throat area of the SSEQ-Rect nozzle, \(A_{SSEQ}^*\), is equal to the sum of the throat areas of the core, \(A_C^*\), and bypass, \(A_{BP,i}^*\), nozzles. That is,

\[
A_{SSEQ}^* = A_C^* + \sum_i A_{BP,i}^*
\]

so both the throat and nozzle exit areas are constant between the dual and single-stream nozzles. The SSEQ-Rect nozzle has a design Mach number \((M_{D,SSEQ})\) of 1.73.

Dimensioned cross-section engineering drawings of the SSEQ-Rect nozzle are shown in Figure 2.16. The flow cross-section undergoes a gradual circular-to-rectangular transition over a length of 7.62 cm. Downstream of this transition is a 2.54 cm length of constant area.

Figure 2.16: Drawings of the single-stream equivalent rectangular nozzle with an exit aspect ratio of 2. Dimensions are reported in centimeters.
flow that helps to achieve a more uniform flow profile. Similar to the dual-stream nozzle design, the width, $W$, of the SSEQ-Rect nozzle remains a constant 3.45 cm downstream of the circular-to-rectangular transition and through the linear converging and diverging sections of the nozzle. The SSEQ-Rect nozzle was fabricated using the PolyJet HD additive manufacturing method with RGD720 as the material.

### 2.4.2 Single-Stream Equivalent Round Nozzle

In addition to the SSEQ-Rect nozzle, a single-stream, convergent-divergent round nozzle, referred to as the SSEQ-Rnd nozzle, is also used to exhaust an equivalent single-stream round jet. Together with the rectangular nozzle, these will serve as the single-stream baselines for comparison with dual-stream jet data. Single, round jets have been the focus of experimental and theoretical jet noise research for a number of decades (for example [25, 27, 37, 45, 68, 92]) due their relatively simple nozzle geometries. They only have two defining parameters: diameter and area ratio. As such, most military fighter aircraft have round exhaust nozzle geometries (e.g. F-14, F-18, F-35). Compared to jets of other shapes, the aeroacoustics characteristics of round jets have been extensively studies and are comparatively well-understood. Therefore, single, round jets are an appropriate acoustic baseline to compare with different geometry and dual-stream jets.

The round nozzle used in this work is a scale model of the GE F404-style nozzle installed on F-18 aircraft. The nozzle geometry was previously provided to Penn State by General Electric Aviation under a Strategic Environmental Research and Development Program (SERDP) contract. This nozzle served as the baseline nozzle in the fluid insert work reported by Powers [1].

The full-scale nozzles are capable of varying their geometry to produce different exit to throat area ratios based on the flight regime. The full-scale nozzle is manufactured using a series of overlapping flaps and seals. The configuration consists of twelve flat flap segments that are interleaved to allow for area adjustment. The seals cover the gaps between the flaps. The scale model nozzle at Penn State was fabricated to represent the nozzle in one typical takeoff configuration. The area ratio chosen was 1.295, which results in a design Mach number, $M_D$, of 1.65. A CAD cross-section of the SSEQ-Rnd nozzle is shown in Figure 2.17. The SSEQ-Rnd nozzle was fabricated using the PolyJet HD additive manufacturing method with RGD720 as the material.
The exit area of the round nozzle is 4 cm$^2$ and matches the area of the core nozzle ($H_C \times W_C$) in the dual-stream jet model. In fact, the core dual-stream nozzle was specifically designed to match the size and area ratio of the pre-existing SSEQ-Rnd nozzle. The SSEQ-Rnd nozzle is operated at the same pressure ratios and simulated total temperature ratios as the SSEQ-Rect nozzle to produce approximately the same thrust and mass flow rate per unit area. In Section 3.4.3, it is shown that the peak far-field noise levels measured for jets exhausting from both SSEQ nozzles match, with less then 0.5 dB OASPL difference, when the data are non-dimensionalized based on the characteristic frequencies of the jets.

![Figure 2.17: CAD model of the SSEQ-Rnd nozzle.](image)

### 2.5 Methods for Estimating Dual- and Single-Stream Jet Thrust and Mass Flow Rate

This section details the methods used to estimate the thrust and mass flow rates of dual- and single-stream jets in tested at Penn State. The development of a thrust stand at Penn State was restricted by the other costs associated with upgrading the dual-flow capabilities. A model problem for oblique shocks is considered in order to attempt to account for thrust losses in the dual-stream nozzles. The methods for determining thrust and mass flow rate matched jet conditions for dual- and single-stream jets are also explained.

#### 2.5.1 Dual-Stream Jet Thrust Estimates

The objective is to estimate the thrust and mass flow rates of dual-stream, supersonic jets operating at off-design pressure ratios. In order to estimate these quantities for jets
exhausting from dual-stream nozzles with different geometries (e.g. the DSR-FS-BL and DSR-Sym-BL nozzles) in a timely manner, and with reasonable accuracy, the following approach was adopted. The thrust and mass flow rate values for the core and bypass jets were estimated using the theoretical flow conditions at the exit planes of the core and bypass nozzles. The position for these estimates is marked by the red lines in Figure 2.8. The thrust of the core, $C$, and bypass, $BP$, jets are calculated using Equations (2.1) and (2.2), respectively.

\[
T_C = \dot{m}_C U_C + (P_C - P_\infty) A_C = \left( \frac{P_C}{P_0} \left[ M_{D,C}^2 \gamma_C + 1 \right] NPR_C - 1 \right) A_C P_\infty \tag{2.1}
\]

\[
T_{BP} = \dot{m}_{BP} U_{BP} + (P_{BP} - P_\infty) A_{BP} = \left( \frac{P_{BP}}{P_0} \left[ M_{BP}^2 \gamma + 1 \right] NPR_{BP} - 1 \right) A_{BP} P_\infty \tag{2.2}
\]

The areas of each nozzle are calculated as $A_C = H_C \times W$ and $A_{BP} = H_{BP} \times W$. The individual dimensions are listed in Table 2.1. The specific heat ratio of the core jet, $\gamma_C$, accounts for changes due to the addition of helium but generally does not exceed 1.6. The core jet expands to the design Mach number of the core nozzle, $M_{D,C}$, that varies slightly based on the value of $\gamma_C$. For example, the design Mach number of the core nozzle is $M_{D,C} = 1.65$ for $\gamma_C = 1.4$, but increases to $M_{D,C} = 1.71$ for $\gamma_C = 1.6$. Because both the core and bypass nozzle exit planes are at the same stream-wise position within the dual-stream nozzle, the exit static pressure of the bypass jet was assumed to be equal to the exit static pressure of the core jet at subsonic pressure ratios.

Using the equations for core and bypass thrust, Equations (2.1) and (2.2), respectively, the net thrust for a dual-stream jet is calculated as:

\[
T_{net} = T_C + \sum_{i=1}^{N} T_{BP,i} \tag{2.3}
\]

where $N$ is the number of bypass streams in the model. $N = 1$ for the DSR-FS-BL nozzle and 2 for the DSR-Sym-BL nozzle.
2.5.2 Model for Oblique Shock Wave Thrust Losses in Nozzles

It is anticipated that oblique shock waves will exist in the core jet flow in the secondary diverging sections of the dual-stream nozzles where the core and bypass jet partially mix. An attempt is made to estimate the impact of these oblique shocks on dual-stream thrust. To this end, a two-dimensional model problem is considered. The geometry for the problem is shown in Figure 2.18. To simplify the task at hand, thrust is estimated for a single-stream stream, over-expanded jet with a Mach number of 1.65 \((A/A^* = 1.295)\) at Plane A. Downstream of this expansion, a turning wedge of angle \(\theta\), with respect to horizontal, is imposed on one side of the jet. On the opposite side of the jet the wall remains horizontal. An oblique shock turns the flow through the angle \(\theta\). The oblique shock reflects off of the upper wall of the upper wall of the nozzle and turns the flow back to horizontal. The jet then exhausts to atmosphere \((P_\infty = 98000 \text{ Pa}, T_\infty = 293 \text{ K})\) at Plane B. Plane B is set to the downstream position where the reflected oblique shock intersects the turning wedge. The example shown in Figure 2.18 shows the theoretical oblique shock wave pattern, in red, for an \(NPR\) of 3.0 and a turning angle of 4°.

\[
T = \dot{m}U + (P - P_\infty) A = \left( \left[ M^2 \gamma + 1 \right] P - P_\infty \right) A
\]

Figure 2.18: Model problem geometry for estimating thrust loss across oblique shock waves. Example oblique shock wave pattern, in red, shown for an \(NPR\) of 3.0 and a turning angle of 4°.

Changes in flow quantities across the oblique shock waves are calculated using oblique shock relations [77]. The thrust at Planes A and B is calculated as
where the pressure, $P$, Mach number, $M$, and area, $A$, quantities are calculated at each plane from isentropic and oblique shock relations. The specific heat ratio, $\gamma$, is taken to be 1.4 for pure air. The oblique shock waves are assumed to be weak i.e. the flow downstream of the shock waves remains supersonic [77]. Changes in thrust are considered on a percentage-wise basis as,

$$\Delta T = \frac{T_A - T_B}{T_A} \times 100\%$$

where the subscripts A and B denote the thrust values at Planes A and B, respectively.

The model problem was solved in MATLAB for over-expanded nozzle pressure ratios of $2.5 \leq NPR \leq 3.5$ and turning angles between $0^\circ \leq \theta \leq 6^\circ$. Figure 2.19a shows a contour plot of the percent change in thrust as a function of turning angle (abscissa) and nozzle pressure ratio (ordinate). The black curve marks a 0% change in thrust. To the left of this curve, the thrust at Plane B is less than that at Plane A, and to the right, the thrust at Plane B is greater than Plane A. Thrust increases are due to the increase in pressure ('pressure recovery') through the shock waves. A histogram of all of the changes in thrust levels is plotted in Figure 2.19b. For the model problem considered, the average thrust loss was found to be $\Delta T_{avg} = 8\%$. Therefore, a thrust loss of 8% is applied to the net thrust estimates, given by Equation (2.3), to account for the losses in all dual-stream nozzles.

Figure 2.19: Results from model problem estimating the change in over-expanded jet thrust through oblique shock waves.
2.5.3 Dual-Stream Jet Mass Flow Rate Estimates

The mass flow rate of the core jet is calculated as:

\[ \dot{m}_C = A_C \sqrt{\frac{\gamma C}{R_C}} \left( T_0 \left( \frac{T_\infty}{T_0} \right) \right) \frac{1}{T_\infty} \] (2.6)

where the ratios \( P_C/P_0 \) and \( T_C/T_0 \) are calculated from isentropic relations for a given core jet exit Mach number and specific heat ratio. Importantly, it must be remembered that the actual total temperature ratio, \( TTR = T_0/T_\infty \) is always 1.0 as the high pressure air and helium are stored at room temperature. For acoustic experiments, the effect of higher total temperature ratios is simulated by the addition of helium which causes the specific heat ratio and gas constant of the core jet, \( R_C \) to vary. Core jet mass flow rate estimates are anticipated to be reasonably accurate as the core jet is only operated at total pressures above the minimum required to choke the core jet at the throat.

The mass flow rate of the bypass jet is approximated as:

\[ \dot{m}_{BP} = A_{BP} \sqrt{\frac{\gamma}{R}} \left( \frac{T_0}{T_{BP}} \right) (1.0) \frac{1}{T_\infty} \] (2.7)

where \( \gamma = 1.4 \) and \( R = 287 \text{ J/(kg \cdot K)} \) for pure air jets and the total temperature ratio, \( TTR = T_0/T_\infty \) has been replaced with 1.0. From Equations (2.6) and (2.7), the net mass flow rate for the dual-stream jets is calculated as:

\[ \dot{m}_{net} = \dot{m}_C + \sum_{i} \dot{m}_{BP,i} \] (2.8)

where \( N \) is again the number of bypass streams in the model.

2.5.4 Method to Determine Thrust and Mass Flow Rate Matched Dual-Stream Jet Conditions

A MATLAB script was developed to determine the dual-stream jet operating conditions with constant thrust and mass flow rate. Equations (2.1) to (2.3) and (2.6) to (2.8) are solved over a range of potential operating conditions: \( 2.0 \leq NPR_C \leq 3.5 \), \( 1.0 \leq TTR_C \leq 3.6 \), and \( 1.2 \leq NPR_{BP} \leq 3.0 \), where \( TTR_C \) is the simulated total temperature ratio using
helium-air mixtures. This generates three-dimensional matrices of net thrust and net mass flow rate. The "isosurface" function is used to generate isosurfaces of user-specified target net thrust and target net mass flow rate. The intersection of these isosurfaces is the locus of dual-stream jet operating conditions \((NPR_C \cdot TTR_C \cdot NPR_{BP})\) pairs that have both constant thrust and constant mass flow rate values that match user-specified target values. Example outputs from the MATLAB script, including isosurfaces of thrust and mass flow rate, and the locus of test points, are shown in Figure 2.20. Table 2.3 lists six example dual-stream jet operating conditions, with a constant thrust of 125 N and mass flow rate of 0.21 kg/s, taken from the curve shown in Figure 2.20. Also listed in the table are the bypass ratios, bypass-to-core jet velocity ratio, \(U_{J,BP}/U_{J,C}\), and the bypass-to-core jet thrust ratio, \(T_{BP}/T_C\). The general trend is that as \(BPR\) decreases, \(NPR_C\) increases, while \(TTR_C \cdot NPR_{BP}\) velocity ratio, and thrust ratio all decrease. The accuracy of the thrust and mass flow rates estimates from this analysis are quantified in Section 4.2.1. The estimated quantities are compared with thrust and mass flow rate values determined from Pitot pressures measured at the exit plane of the dual-stream nozzles.

Figure 2.20: Example output from MATLAB script used to estimate constant-thrust, constant-mass flow rate, dual-stream jet operating conditions. Output shown for a target thrust of 125 N and target mass flow rate of 0.21 kg/s.
Table 2.3: Examples of dual-stream jet operating conditions with a thrust of 125 N and
mass flow rate of 0.21 kg/s.

<table>
<thead>
<tr>
<th>#</th>
<th>Operating Conditions</th>
<th>BPR</th>
<th>$U_{J, BP}/U_{J, C}$</th>
<th>$T_{BP}/T_{C}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>3.39 4.38 2.64</td>
<td>0.60</td>
<td>0.50</td>
<td>0.26</td>
</tr>
<tr>
<td>2</td>
<td>3.50 4.08 2.35</td>
<td>0.50</td>
<td>0.49</td>
<td>0.21</td>
</tr>
<tr>
<td>3</td>
<td>3.63 3.77 2.02</td>
<td>0.40</td>
<td>0.46</td>
<td>0.16</td>
</tr>
<tr>
<td>4</td>
<td>3.79 3.49 1.63</td>
<td>0.30</td>
<td>0.42</td>
<td>0.10</td>
</tr>
<tr>
<td>5</td>
<td>3.96 3.20 1.23</td>
<td>0.20</td>
<td>0.33</td>
<td>0.05</td>
</tr>
</tbody>
</table>

### 2.5.5 Determining Single-Stream Equivalent Jet Operating Conditions

The SSEQ jet has been defined to be the jet exhausting from a single-stream nozzle with
the same exit and choked flow areas as the dual-stream nozzle that produces the same net
mass flow rate and thrust as the dual-stream jet. Because the jet is a single-stream jets
exhausting from a converging-diverging nozzle, the estimation of jet operating conditions is
more straightforward compared with dual-stream jets. The SSEQ mass flow rate, $\dot{m}_{SSEQ}$,
and thrust, $T_{SSEQ}$, levels are calculated using Equations (2.9) and (2.10), respectively.

$$\dot{m}_{SSEQ} = \dot{m}_{target} = \left( \frac{P_E}{P_0} \right) (NPR) P_\infty A_{SSEQ,E} M_D \sqrt{\frac{\gamma}{R} \left( \frac{T_0}{T_E} \right) \left( \frac{T_\infty}{T_0} \right)} \frac{1}{T_\infty} \quad (2.9)$$

$$T_{SSEQ} = T_{target} = \left( \frac{P_E}{P_0} \right) \left[ M_D^2 \gamma + 1 \right] NPR P_\infty A_{SSEQ,E} \quad (2.10)$$

The pressure and temperature ratios, $P_E/P_0$ and $T_E/T_0$, respectively, are calculated from
isentropic relations at the nozzle exit plane assuming the flow remains supersonic through
the diverging section of the nozzle. The mass flow rate values, $\dot{m}_{SSEQ}$, are known based
on the values to match the target dual-stream jet values. The objective then becomes
to determine the appropriate SSEQ jet operating conditions, $NPR_{SSEQ}$ and $TTR_{SSEQ}$
(simulated with helium), that satisfy Equations (2.9) and (2.10). Because of the iterative
nature of determining the appropriate amount of helium required to simulate the effects
of $TTR_{SSEQ}$ with helium-air mixtures, Equations (2.9) and (2.10) cannot be solved
simultaneously. The challenge is further compounded by the fact that the design Mach
number of the SSEQ nozzle, as well as the values for the ratios $P_E/P_0$ and $T_E/T_0$, change as $\gamma$ varies with the addition of helium.

A MATLAB script has been written to determine the appropriate SSEQ jet operating conditions, $NPR_{SSEQ}$ and $TTR_{SSEQ}$, that satisfy Equations (2.9) and (2.10). The script uses the SSEQ nozzle geometry, target thrust and mass flow rate values as inputs. The right hand sides of Equations (2.9) and (2.10) are solved over a range of possible jet operating conditions: $1.0 \leq NPR_{SSEQ} \leq 3.5$ and $1.0 \leq TTR_{SSEQ} \leq 3.25$. The errors between the calculated and target values are:

$$\sigma_\dot{m} = \frac{|\dot{m}_{target} - \dot{m}|}{\dot{m}_{target}} \quad (2.11)$$

$$\sigma_T = \frac{|T_{target} - T|}{T_{target}} \quad (2.12)$$

The goal then becomes to simultaneously minimize the thrust and mass flow rate errors. To this end, the combined error is defined as

$$\sigma_{SSEQ} = \sqrt{\sigma_\dot{m}^2 + \sigma_T^2} \quad (2.13)$$

which is a function of $NPR_{SSEQ}$ and $TTR_{SSEQ}$. The minimum value of $\sigma_{SSEQ}$ gives the SSEQ jet operating conditions, $NPR_{SSEQ}$ and $TTR_{SSEQ}$, that satisfy Equations (2.9) and (2.10). Example outputs from the SSEQ MATLAB script are shown in Figure 2.21. Contours of the mass flow rate and thrust errors, defined in Equations (2.11) and (2.12), are shown in the left and center panels of the figure, respectively, and a contour of the combined error is shown in the right panel. The SSEQ jet operating conditions that satisfy Equations (2.9) and (2.10), for a target thrust of 21 N/cm$^2$ and target mass flux of 0.035 (kg/s)/cm$^2$, are $NPR_{SSEQ} = 2.93$, $TTR_{SSEQ} = 2.44$, and are noted in the figure and marked by the white tee on the contour.
Figure 2.21: Example output from MATLAB script used to estimate single-stream equivalent (SSEQ) jet operating conditions. Example output shown for a target thrust of 125N and target mass flux of 0.21 kg/s.
3 Experimental Methods, Data Acquisition, and Processing

This chapter describes all experimental methods, data acquisition, data processing techniques, and measurement uncertainties used for this study. The chapter is setup as follows. First, a review of the analog-to-digital (A/D) conversion and data acquisition system is presented. Following this is a review of the experimental methods for far-field acoustic measurements, schlieren flow visualization, and Pitot probe measurements. Experimental uncertainties are then discussed for acoustic and Pitot probe measurements. The chapter concludes an overview of the test matrices for single and dual stream jet experiments.

3.1 Facility A/D Conversion and Data Acquisition System

Transducer output voltage signals are acquired and digitized using a PXIe-1073 National Instruments data acquisition (DAQ) chassis equipped with two PXIe-6358 and one PXIe-6356 (A/D) conversion modules. The A/D modules have a maximum input voltage range of ±10 V. The DAQ system is interfaced with the main facility computer running Windows 10. Simultaneous, 16-bit samples can be acquired for forty channels at a maximum rate of 500 kHz. Transducers and sensors within the anechoic chamber can be connected to the data acquisition systems using a series of forty, 15 ft long, BNC cables pre-run through a small hole in the chamber wall and bundled into eight groups of five.

Available for use are two, two-channel, Krohn-Hite filter/amplifiers that can be digitally changed per channel. In addition, there is a forty channel Krohn-Hite Model 3932s filter/amplifier with a high-pass frequency of 500 Hz and gain settings of +20 dB or +40 dB.
3.2 Coordinate Systems

The coordinate systems used in present study are shown in Figure 3.1. A CAD cross-section of the DSR-FS-BL nozzle is shown in the figure. The radial distance, \( r \), is measured from the geometric center of the nozzle, \( \theta \) is the polar angle between the radius and downstream jet axis, \( X \), and \( \phi \) is the azimuthal angle measured from the cross-stream axis, \( Y \), directed along the major axis of the nozzle. The transverse stream coordinate, \( Z \), is subsequently defined, using a right-handed Cartesian coordinate system, to be oriented along the minor axis of the nozzle. Acoustic data are presented using the spherical coordinate system while mean velocity data obtained from Pitot probe measurements are presented using the Cartesian coordinate system.

![Figure 3.1: Cartesian and spherical coordinate systems.](image)

3.3 Acoustic Measurements

Acoustic measurements are performed with 3.2 mm diameter, pressure field, model 40DP, *GRAS* microphones. These microphones were selected due to their high frequency response (up to 120 kHz), high dynamic range, and high signal-to-noise ratio. Seventeen microphones are mounted on a semi-permanent rotating microphone boom for faster microphone positioning, increased position accuracy, and measurement repeatability. Microphone calibrations are performed with a *B&K* acoustic calibrator, model 4231. The calibrator outputs a constant 1 kHz sine wave with a 94 dB or 114 dB amplitude.
3.3.1 Far-Field Polar Microphone Array

The High-Speed Jet Aeroacoustics Facility is equipped with a semi-permanent, far-field polar microphone array. The array is fixed in place in the $\phi = 0^\circ$ azimuthal direction ($+Y$ direction), but it can freely rotate in the $XY$ plane. The dual-stream jet model can be rotated to measure the noise in different azimuthal directions. The microphone array was originally fabricated by Veltin [3] to hold six microphones. The array was expanded by Powers [1] to hold up to twenty-three microphones.

Seventeen microphones, distributed between polar angles of $24^\circ$ and $133^\circ$, were in place on the far-field microphone array for acoustic measurements. The locations of the microphones relative to the exit plane of the dual-stream jet model are shown in Figure 3.2 and listed in Table 3.1. The array is a semi-circular arc with an average radius of 1.86 m. The center of rotation is located approximately 37.8 cm downstream of the dual-stream nozzle exit. The distance to the microphones is measured using a tape measure to an accuracy of 6.4 mm. The angular position is determined using a laser pointer mounted on an analog angle dial with $1^\circ$ increments. In the arc of peak noise emission (approximately microphones 3-8), the microphones are located at an average radial position of $75 D_{Eq}$ ($D_{Eq} = 2.76$ cm or $92 D_C$, where $D_C = 2.25$ cm) away from the dual-stream nozzle exit plane.

<table>
<thead>
<tr>
<th>Mic #</th>
<th>$r$ (m)</th>
<th>$\theta$ (deg)</th>
<th>Mic #</th>
<th>$r$ (m)</th>
<th>$\theta$ (deg)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
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<td>1.81</td>
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<td>3</td>
<td>2.11</td>
<td>42</td>
<td>12</td>
<td>1.76</td>
<td>100</td>
</tr>
<tr>
<td>4</td>
<td>2.10</td>
<td>45</td>
<td>13</td>
<td>1.73</td>
<td>105</td>
</tr>
<tr>
<td>5</td>
<td>2.06</td>
<td>47</td>
<td>14</td>
<td>1.72</td>
<td>110</td>
</tr>
<tr>
<td>6</td>
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<td>50</td>
<td>15</td>
<td>1.68</td>
<td>116</td>
</tr>
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<td>7</td>
<td>2.05</td>
<td>56</td>
<td>16</td>
<td>1.66</td>
<td>120</td>
</tr>
<tr>
<td>8</td>
<td>2.08</td>
<td>60</td>
<td>17</td>
<td>1.64</td>
<td>133</td>
</tr>
<tr>
<td>9</td>
<td>1.93</td>
<td>70</td>
<td>–</td>
<td>–</td>
<td>–</td>
</tr>
</tbody>
</table>

The array is equipped with individual microphone mounts that hold the microphones in grazing ($90^\circ$) incidence to the jet. In this orientation the microphone diaphragms are parallel with the $XY$ plane so the wavefronts from the noise sources distributed along the jet axis are oriented at $90^\circ$ to the microphone diaphragm minimizing the effects of diffraction around the microphone. Gabrielson et al. [166] have shown grazing incidence to increase the frequency bandwidth of the microphones. In fact, it is now industry standard.
to mount pressure field microphones in grazing incidence for acoustic measurements of full-scale aircraft [167].

Figure 3.2: Positions of the far-field microphones relative to the dual-stream jet nozzle. Figure is to scale.

### 3.4 Aeroacoustic Simulation of Heated Jets using Helium-Air Mixtures

To accurately simulate the acoustics of jets exhausting from full-scale aircraft engines, the temperatures and velocities of these jets must be replicated. The peak noise levels are highly dependent on the jet velocity, so it is imperative to replicate the increased velocities of heated jets. Instead of heating the high-pressure air, the Penn State facility uses helium-air jet mixtures to acoustically simulate the total temperature ratio, $TTR = T_0/T_\infty$, of heated air jets. To date, $TTR'$s up to 3.6 have been simulated in the Penn State Facility [168]. Helium-air mixtures are advantageous for small-scale testing because they avoid the need for costly heaters and allow models to be fabricated from plastic and aluminum hardware which are less expensive than stainless steel. As an added benefit, for the same nozzle sizing, Mach number, and density, helium-air jets have higher Reynolds numbers than heated jets. For the same jet density, the viscosity of a helium-air mixture jet is less than that of a heated jet, thus a helium-air jet has a higher Reynolds number than a heated jet.
The helium-air mixture technique was developed in the High-Speed Jet Aeroacoustics Facility by Kinzie [169] and Doty and McLaughlin [170]. Kinzie and McLaughlin [171] and Papamoschou [137] have shown that mixtures of helium and air (heat-simulated) can appropriately replicate the noise of heated jets to within 1.0 dB by matching the speed of sound of the mixture to that of the heated gas. A comparison between experimental data from heat-simulated jets in the High Speed Jet Aeroacoustics Facility at Penn State and the moderate scale (12.5 cm nozzle diameter) heated jet noise facility at NASA Glenn Research Center has been made. McLaughlin et al. [88] documented how the acoustic data measured in the two facilities agreed to within ±1.5 dB OASPL. Helium-air mixtures for jet aeroacoustic experiments are currently used by four facilities in the U.S.: 1) Penn State University [169,170], U.C. Irvine [137], U.T. Austin [75,111], and A.S.E. FluiDyne [112,113].

The partial pressures of both helium and air are manually regulated in the control piping cabinet to produce a helium-air mixture jet that replicates the increased fully-expanded speed of sound of heated jets. More specifically, the objective is to equate the left and right hand sides of Equation (3.1)

\[
\frac{c_{J,a}}{\sqrt{\gamma_a R_a T_{J,a}}} = \frac{c_{J,mix}}{\sqrt{\gamma_{mix} R_{mix} T_{J,mix}}}
\]

where the subscripts \(a\) and \(mix\) denote pure-air and helium-air mixture quantities, respectively. Helium addition lowers the density, and alters the specific heat ratio, \(\gamma_{mix}\), and gas constant, \(R_{mix}\), of the mixture. For typical heat-simulated jet conditions, the specific heat ratio changes from 1.4 (pure air i.e. \(TTR = 1\)) up to approximately 1.6 (\(TTR = 3\)), while the gas constant varies from 287 J/kg-K (pure air) to 1000 J/kg-K (\(TTR = 3\)).

Knowing the desired nozzle pressure ratio, \(NPR\), and the target \(TTR\), the theoretical speed of sound of the fully-expanded jet and jet Mach number, \(M_J\), are calculated from isentropic relations. For the range of total temperature ratios considered, the specific heat ratio and gas constant of heated air are taken to be: \(\gamma_a = 1.4\) and \(R_a = 287\) J/kg-K, respectively. The theoretical speed of sound of a heated air jet at fully-expanded conditions is set as the target value to match with the helium-air mixture following Equation (3.1). The temperature of the helium-air jet mixture is a function of \(M_J\) and specific heat ratio of the mixture, \(\gamma_{mix}\). Since \(M_J\) is already known from the choice of \(NPR\), the only two remaining unknowns in Equation (3.1) are \(\gamma_{mix}\) and the gas constant of the mixture, \(R_{mix}\). These quantities are only a function of the mass fraction of helium in the mixture. The goal then becomes to determine the appropriate mass fraction of helium in the mixture to satisfy Equation (3.1).
The mass fraction of helium in the mixture is determined iteratively in MATLAB by incrementally increasing the mass fraction of helium until the speed of sound of the mixture matches the target value. A flow chart detailing the helium-air speed of sound matching process is shown in Figure 3.3. The iteration loop is within the dashed box in the figure. The inputs to the routine are the nozzle pressure ratio and desired $TTR$ to simulate. Once the iteration process converges, the MATLAB routine determines an updated nozzle pressure ratio based on the desired jet Mach number and new mixture specific heat ratio. Knowing the appropriate mass fraction of air, the associated mass flow rate of air and the required partial pressure of air are also calculated. The plenum pressures of the air-only and the helium-air mixture flows are converted to voltages, $V_a$ and $V_{\text{mix}}$, respectively, that are monitored both manually and by the LabVIEW data acquisition code during testing.

In practice, 3-4 helium cylinders pressurized up to 14.5 MPa (2100 psig) feed into manifold connected to the pressure control cabinet. The helium control line is noted by the red arrows in Figures 2.2 and 2.3. During tests, with the helium controls closed, first the partial pressure of air is set to the appropriate value, $V_a$, by manually monitoring the core plenum pressure transducer output voltage on a multimeter. The helium pressure regulator is then set to an estimated value and the helium ball valve is manually opened to introduce helium into the core air stream. Near the target voltage, the operator should open the valve slowly so the voltage rise through the target value is as shallow as possible. The total plenum pressure of the mixture is continuously monitored by LabVIEW. Data is automatically acquired once the plenum transducer reaches the target voltage of the mixture, $V_{\text{mix}}$. The acquisition code holds a buffer of 102400 sample points on each active channel so the saved data files have approximately 0.3 sec of data prior to the target voltage being reached. Total pressure of the mixture is controlled to within 3% of the target value during data acquisition. Only the core flow of the dual-stream jet model was able to be operated with helium-air mixtures.
Figure 3.3: Flow chart detailing the methodology for matching the speed of sound of heated jets using helium-air mixtures.
3.4.1 Acoustic Data Acquisition

The analog time-domain signals from the microphones are routed through a GRAS model 12AG power module and amplified thus enabling their accurate digital conversion in the following data processing. A high-pass filter is set to 500 Hz to remove any undesirable, low-frequency noise that could contaminate the data. The microphones have a steep "roll-off" in their frequency response above 120 kHz that omits the need for an anti-aliasing filter as higher-frequency content is naturally damped. All spectra shown in this dissertation have a frequency range of 0.5 kHz < f < 100 kHz. The sampling rate of the analog-to-digital converter is set to 300 kHz for data acquisition. The raw data are processed using a series of MATLAB codes.

The data processing settings are different between pure-air and helium-air acoustic measurements. For pure-air acoustic measurements, 409,600 data points are collected and split into 199, \( N_{FFT} = 4096 \) point segments with 50% overlap. A Hann window is applied to each segment to minimize spectral leakage. The fast Fourier transform (FFT) is calculated on each segment and the individual spectral densities are averaged together to estimate the power spectral density (PSD). Spectra are corrected for energy lost due to applying the Hann window to ensure that each integrated spectrum yields the mean-square of the time series segment used. This yields power spectral densities with a bin spacing, \( \Delta f \), of 73 Hz.

For heat-simulated, helium-air acoustic measurements, 204,800 data points are collected and split into \( N_{FFT} = 1024 \) point segments. Fewer data points are acquired to shorten the acquisition time to limit the amount of helium used during testing. This returns power spectral densities with \( \Delta f = 293 \) Hz. The spectral density per bandwidth is converted to spectral density per unit Hz, \( SPL(f) \), by dividing the spectra by \( \Delta f \). Spectral density is reported as sound pressure level (SPL) in decibels (dB) using the standard reference pressure of 20\( \mu \)Pa.

Acoustic data are acquired using a LabVIEW code developed by Powers [1]. The code allows for real-time monitoring of the NPR’s in the core and bypass plenums as well as the microphone PSD’s. The user inputs to the LabVIEW code are: 1) the current ambient conditions, 2) the consecutive sequence of channels to be acquired, 3) the desired sample rate and number of sample points, 4) the transducer calibration constants, 5) the save path and file name for the data, and 6) the current test identification number. An example screen-shot of the user-interface is shown in Figure 3.4.

Raw time and voltage data are acquired when the user clicks on the "Write" toggle switch. However, for helium-air acoustic measurements, the process is altered. The "Trigger"
toggle, in the upper right hand box in Figure 3.4, is turned on (pointing up). This trigger toggle arms the LabVIEW acquisition code to monitor the user-specified trigger channel. When the user clicks the 'Write' toggle, the code does not acquire immediately. Instead it monitors the trigger channel until the trigger voltage 'level' is reached, once this voltage is reached, the code automatically acquires the data. During helium-air tests, the operator sets the partial pressure of air then sets the 'Trigger' to on and inputs the target mixture voltage, $V_{mix}$. The methods to determine $V_{mix}$ were previously explained in Section 3.4. The operator then opens the helium ball valve (BV3 in Figure 2.2) to introduce helium into the core air flow. Once $V_{mix}$ is reached the correct amount of helium has been introduced to simulate the desired TTR, and the LabVIEW code automatically acquires the data.

### 3.4.2 Acoustic Spectra Corrections

The sound pressure level (SPL) is calculated for each microphone and converted to a non-dimensional, lossless spectrum. The correction procedure conforms to the ANSI Military Aircraft Measurement Standard established in 2014 [167]. The corrections include the
actuator correction, $\Delta C_{act}(f)$, and the appropriate free-field response, $\Delta C_{ff}(f)$. Spectra are made lossless by correcting for the daily variations in atmospheric quantities by calculating the atmospheric attenuation of the radiated noise (ISO 9613-2:1996) for each microphone using measured ambient pressures, humidities, and temperatures, $\Delta C_{atm}(f)$ following the work of Shields and Bass [172] and Bass et al. [173]. The sound power lost due to the atmospheric attenuation from the jet to the microphone is added back to the spectrum. Equation (3.2) summarizes the steps that lead the corrected, lossless, SPL per unit Hz as explained in Kuo et al. [174].

$$SPL(f) = SPL_{meas}(f) - \Delta C_{act}(f) - \Delta C_{ff}(f) + \Delta C_{atm}(f) \quad (3.2)$$

The microphones on the far-field array are $60 D_{Eq}$ to $80 D_{Eq}$ away from nozzles with a 2.76 cm equivalent diameter (and up to $98 D_{Eq}$ for nozzles with a 2.25 cm diameter). Jet noise sources are known to be located at frequency-dependent positions distributed along the jet axis [78,175–177]. Low-frequency noise is generated towards the end of the potential core, where the shear layer is largest, and higher-frequency noise is generated near the nozzle exit plane, where the shear layer is thin and turbulence scales are smallest. The microphone array is focused on the nozzle exit plane and does not account for the source distribution. This leads to some discrepancies between the assumed microphone locations and the actual polar angle and distance relative to the location of the noise sources for each frequency. For supersonic, heated (or heat-simulated) jets, Kuo et al. [177] found that far-field measurements performed at a non-dimensional distance of 70 $D$ are not completely accurate for predicting the geometric far-field. Acoustic experiments in the Penn State facility are limited by the fixed radial length of the far-field microphone array. Fabricating and testing smaller-scale dual-stream nozzles, which would increase the relative distances to the microphones, was not feasible because the jet noise data would be at risk of being compromised by low Reynolds number effects [178]. The scale of the model and microphone positions represent the best compromise between these limiting factors. Section 3.7.2 attempts to quantify the errors associated with the noise source distribution. Although imperfect, the microphones are assumed to be in the geometric far-field. Under this assumption, spherical spreading is applied in the data processing to propagate the acoustic data further back to a different far-field radial position. Because the microphones are at different distances to relative to the nozzle exit, all acoustic data are propagated further out (or back) to a uniform radius of $100 D_{Eq}$, measured from the nozzle exit plane.
The 'back' propagated SPL's are calculating using Equation (3.3).

\[
SPL_{r_{100}}(f) = SPL_{r_{mic}}(f) + 20 \log_{10} \left( \frac{r_{mic}}{r_{100}} \right)
\]  

(3.3)

where, \( r_{mic} \) is the 'as-measured' distance to the far-field microphone and \( r_{100} \) is the 100\( D_{Eq} \) distance the acoustic data are being propagated back to. From the back propagated acoustic spectra, at intervals of \( \Delta f \), the OASPL is calculated using Equation 3.4.

\[
OASPL = 10 \log_{10} \left[ \frac{N_{FFT} \Delta f}{\Delta f} \sum_{f=\Delta f}^{N_{FFT} \Delta f} 10^{SPL(f)/10} \right]
\]  

(3.4)

Because full-scale aircraft acoustic data rarely exhibit jet screech tones, screech tones present in acoustic spectra are numerically removed, and the spectra levels are interpolated over the range of frequencies, prior to calculating overall sound pressure levels.

### 3.4.3 Non-Dimensionalization of Single-Stream Jet Spectra

Experimental, lossless acoustic spectra are non-dimensionalized to allow for easier comparison to acoustic spectra measured for jets exhausting from different scale nozzles. Spectra are non-dimensionalized to SPL per unit Strouhal number, \( St = f / f_C \), using the characteristic frequency, \( f_C \), of the jet.

For single-stream jets, characteristic frequency is determined from the the reference length and velocity of the jet. These reference length and velocity scales are the fully-expanded equivalent jet diameter, \( D_{J,Eq} \), and fully-expanded jet velocity, \( U_J \), calculated from isentropic relations. The fully-expanded equivalent jet diameter can be estimated by equating the mass flow rate at the nozzle exit to a point downstream where the jet has expanded to the atmospheric pressure-matched jet Mach number, \( M_J \). The expression to estimate the fully-expanded jet diameter is given by Equation (3.5)

\[
D_{J,Eq} = D_{E,Eq} \sqrt{\frac{M_D}{M_J} \left[ 1 + \frac{\gamma - 1}{2} M_J^2 \right]^{\frac{\gamma + 1}{\gamma - 1}}} \]  

(3.5)

where \( D_{E,Eq} \) is the equivalent exit diameter of the nozzle based on area and \( \gamma \) is the specific heat ratio of air or the helium-air mixtures. For over or under-expanded supersonic jets, Equation (3.5) is only an estimate that does not account for the area modulation of the jet due to the shock cell structure, nor does Equation (3.5) account for ambient air...
entainment. If $M_D = M_J$ or the jet is subsonic the jet diameter is simply the nozzle exit diameter. Once $D_{J, Eq}$ is estimated, the characteristic frequency of the jet is calculated using Equation (3.6).

$$f_C = \frac{U_J}{D_{J, Eq}}$$  \hspace{1cm} (3.6)

Finally, the spectra for single-stream jet acoustic measurements are made non-dimensional using Equation (3.7).

$$SPL(St) = SPL(f) + 10 \log_{10}(f_c)$$  \hspace{1cm} (3.7)

This non-dimensionalization and scaling is known as Strouhal number scaling. McLaughlin et al. [88] demonstrated that by using this scaling the small-scale heat-simulated data acquired at PSU agrees to within 1.5 dB of 5× larger scale heated jet data acquired at NASA Glenn Research Center.

A comparison of the lossless, non-dimensional acoustic spectra measured for jets exhausting from the SSEQ-Rnd and SSEQ-Rect nozzles is shown in Figure 3.5a. The spectra have been non-dimensionalized using the characteristic frequency for each jet. The characteristic frequency of the SSEQ-Rnd jet is 30.2 kHz, and for the SSEQ-Rect jet it is 25.3 kHz. Rectangular jet spectra are shown for acoustic measurements acquired in the $\phi = 0^\circ$ (major axis) and $90^\circ$ (minor axis) azimuthal directions. The spectra show agreement, to within in 1 dB on average, in the mixing noise peak, up to a polar angle of $50^\circ$. The differences in the $BBSAN$ levels are due to the differences in the shock cell structures of the jets because of the different nozzle geometries. Overall sound pressure levels for the SSEQ jets are shown in Figure 3.5b. The radiated noise levels agree to within 1 dB OASPL up to $\theta = 90^\circ$. In the peak noise direction, the difference in noise levels is less than 0.5 dB OASPL.
Figure 3.5: Far-field acoustic results for the SSEQ-Rnd and SSEQ-Rect jets. Jets operating at $M_J = 1.34$, $TTR = 2.44$. The characteristic frequencies of the jets are: Rnd: $f_C = 30.2$ kHz, Rect: $f_C = 25.3$ kHz.

3.4.4 Non-Dimensionalization of Dual-Stream Jet Spectra

The challenge of non-dimensionalizing spectra becomes increasingly difficult for dual-stream jets. The issue of having two compressible jets, with different velocities, pressures and sizes, that only partially-mix prior to exhausting from the nozzle must be addressed. Much of the dual-stream acoustic data presented to date, including [75,93,94,101,114,123,139,141], has been presented on a dimensional basis.

While Berry et al. [105] and Magstadt [107] present spectra as a function of Strouhal number for supersonic jets exhausting from a dual-stream, rectangular nozzle, the authors do not report the reference length and velocity scales they used to calculate the characteristic frequency. Papamoschou and DeBiasi [133] and DeBiasi and Papamoschou [104] also present non-dimensional acoustic data for dual-stream, concentric jets. The authors maintained a constant core jet Mach number and core thrust, during testing. They based their Strouhal number scaling on the characteristic frequency of the core jet and subsequently
scaled the dual-stream data, based on thrust, to the thrust of their core jet. Rask et al. [127] non-dimensionalized subsonic, dual-stream jet acoustic data using a characteristic frequency based on the fully-mixed velocity [97] and the equivalent diameter based on the sum of the core and bypass jet areas.

Two approaches for non-dimensionalizing dual-stream jet acoustic data are adopted in this work. Sections 5.1 and 5.2 report on a series of experiments aimed at better understanding the effects of changing jet operating conditions, $NPR_C$, $TTR_C$, $NPR_{BP}$, core jet velocity, and thrust on the far-field noise. In these sections acoustic spectra are nondimensionalized using the characteristic frequency of the core jet.

The second half of Chapter 5, starting with Section 5.3, focuses on comparing the acoustics of dual-stream jets on a constant-thrust, constant-mass flow rate basis. Because a major goal of this work is to determine whether dual-stream jets offer an acoustic benefit over single-stream jets, dual-stream jet acoustic data are non-dimensionalized using the same characteristic frequency as the SSEQ-Rect jet operating at equivalent thrust and mass flow rate conditions. This ensures that the Strouhal scaling is the same between all jets, so the changes in SPL are only associated with changes in the radiated noise, and eliminates any ambiguity that may be associated with different Strouhal scaling factors. On this basis, it is effectively the same as comparing dimensional spectra, but with the added benefit that the spectra have been made non-dimensional to facilitate comparisons with jets issuing from different size nozzles, such as the SSEQ-Rnd jet, that has a smaller exit area than the dual-stream nozzles.

### 3.5 Schlieren/Shadowgraph Flow Visualization

Schlieren and shadowgraph imaging are techniques used to visualize density gradients in compressible flows. These technique exploit the change in the index of refractivity of air with changes in density. Parallel rays of light are refracted at different angles based on the density gradient of the flow field resulting in an image with light and dark regions highlighting the different features within the field of view (e.g. shock waves, expansion fans, and mixing layers). In practice, the schlieren imaging technique is similar to shadowgraphy but includes the addition of a knife edge or gradient filter at the focusing point of the light beam ahead of the camera lens.

Shadowgraph imaging is better suited for visualizing strong density gradients, such as shock waves or expansion fans. The addition of the knife edge in schlieren imaging adds
contrast and increases the sensitivity to weaker density gradients, such as sound waves and fine-scale mixing. Schlieren imaging is proportional to the first derivative of density, and shadowgraphy is proportional to the second derivative of density [179]. The knife edge emphasizes density gradients perpendicular to the knife orientation. The use of a gradient filter in place of a knife edge allows a wider range of gradients to be observed at the expense of a decrease in system sensitivity to small density fluctuations.

Schlieren images of a pure-air, $M_J = 1.34$ overexpanded jet exhausting from a $M_D = 1.73$, aspect ratio of 2, single-stream, rectangular nozzle are shown in Figure 3.6. The images were acquired during the setup of the schlieren system. All camera and computer acquisition settings were the same in each case; only the knife edge and/or gradient filter was changed between tests. A single image acquired using a vertically-oriented knife edge is shown in Figure 3.6a and a image acquired using a vertically-oriented gradient filter is shown in Figure 3.6b. The filter transitions from transparent to opaque over a gradient length of approximately 3.2 mm. The use of a gradient filter shows more details of the expansion fans downstream of the shock waves compared with the knife edge. However, the added sensitivity of the knife edge allows more details of the shear layer to be visualized. A single image acquired using a gradient filter with a gradient length of 1.6 mm is shown in Figure 3.6c. Images acquired using the 1.6 mm gradient filter strike a balance between the knife edge and 3.2 mm gradient filter. The 1.6 mm gradient filter shows details of the expansion fans downstream of the shock waves while still retaining enough sensitivity to provide good visualization of the shear layer.

Both the schlieren and shadowgraph imaging techniques involve an integration effect of the entire path of light, so that the camera image is an integration of the density gradients through the entire span of the flow. The images are therefore subject to three-dimensional effects and can show features outside of the plane the camera is focused on.
Figure 3.6: Visualization comparison of schlieren images of pure-air, $M_J = 1.34$ jets exhausting from a $M_D = 1.73$, single-stream, rectangular ($AR = 2$) nozzle. The stream-wise extent of the images is $X/D_{E_q} = 4.5$ (12.4 cm).

### 3.5.1 Optical Hardware Setup

All flow visualization images acquired for this dissertation were acquired using a Z-type schlieren setup. A schematic of the setup is shown in Figure 3.8. This arrangement uses a lens to focus the light from an LED source, with an iris in front, to a point. A narrow slit was placed at the focal point of the lens to create an approximate point source of light. The point source is created at the focal point of a 20.32 cm diameter parabolic mirror with a 1.62 m focal length. The parabolic mirror collects the expanding cone of light from the point source, directs a parallel column of light across the anechoic chamber, through desired observation section within the jet. A second parabolic mirror on the opposite side of the anechoic chamber refocuses the light to a point. In the schlieren technique, a knife edge or gradient filter, is placed at this focal point. For this work, a vertically-oriented, 1.6 mm gradient filter was used in place of a knife edge as it provided a favorable balance between system sensitivity and the range of gradients that could be visualized. A photograph of the gradient filter used is shown in Figure 3.7.

As the parallel column of light passes through the jet, density gradients in the flow deflect the light rays off of parallel. Depending on the direction of the density gradient, some of the light rays are deflected towards the light side of the gradient filter while others are
deflected towards the dark side of the filter. These deflections cause the resulting image to have associated light and dark regions.

A focusing lens and high-speed digital camera are set up behind the gradient filter to acquire the light. Settles [179] describes how, when using a digital camera, it may be necessary to separate the camera from the focusing lens to give more control over the linear distance between the lens and the CMOS chip within the camera. This allows the image to be brought into sharp focus or allows one to magnify a region of interest, such as a boundary layer, if desired.

A scaled schematic diagram of the Z-type schlieren configuration used in this work is shown in Figure 3.8. The positions of all optical hardware are noted with letters. Table 3.2 lists the hardware component associated with each letter. The distance between components is also tabulated in Table 3.2. Distances under 30 cm were measured with a ruler to
an accuracy of 0.32 cm. Larger distances were measured using a measuring tape to an accuracy of 1.9 cm. The Z-type schlieren setup had an 8° angle between branches. This angle was found to maximize the field of view diameter while still keeping the sending and receiving optical hardware outside of the light column. The field of view diameter was 14.6 cm.

Table 3.2: Optical hardware and distances between components used in Z-type schlieren setup

<table>
<thead>
<tr>
<th>Letter</th>
<th>Component</th>
<th>Segment</th>
<th>Length (cm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>LED light source and iris</td>
<td>AB</td>
<td>8.25</td>
</tr>
<tr>
<td>B</td>
<td>Focusing lens</td>
<td>BC</td>
<td>11.10</td>
</tr>
<tr>
<td>C</td>
<td>Slit</td>
<td>CD</td>
<td>157.50</td>
</tr>
<tr>
<td>D</td>
<td>'Sending' parabolic mirror</td>
<td>DE</td>
<td>551.20</td>
</tr>
<tr>
<td>E</td>
<td>'Receiving' parabolic mirror</td>
<td>EF</td>
<td>157.50</td>
</tr>
<tr>
<td>F</td>
<td>Gradient filter</td>
<td>FG</td>
<td>8.90</td>
</tr>
<tr>
<td>G</td>
<td>Focusing lens</td>
<td>GH</td>
<td>29.20</td>
</tr>
<tr>
<td>H</td>
<td>Camera</td>
<td>–</td>
<td>–</td>
</tr>
</tbody>
</table>

3.5.2 Image Acquisition and Processing

The light source used for schlieren flow visualization was a Thorlabs model MCWHL5 white light LED. The LED was controlled using a Thorlabs LEDD1B LED driver. The LED driver was set to maximum power while the LED was set to continuously emit light. The LED was turned off between tests. During testing, the LED was not observed to undergo an any appreciable fluctuations in luminosity.

A Phantom Miro 310 digital, high-speed, CMOS camera was used to acquire schlieren images. The camera software, Phantom PCC 2.8, allows control over the resolution, frame rate, exposure time, and image histograms. All images were acquired with the camera set to its full resolution of 3200 × 800 pixels and at a frame rate of 3200 Hz. The exposure time for each image was set to 3 µs. Additionally, the gain on the camera was set to 2.5. The gain setting modifies the histogram of pixel intensities to improve brightness and contrast. It was necessary to modify this setting when low-exposure times are used due to the low amount of light absorbed. Frames were acquired until the internal memory of the camera was full. In total, 8310 frames were acquired during each test. In addition to the 8310 frames, a single background, or "no flow" image, with the jet turned off, is
also acquired for post-processing images. The background image helps to digitally remove imperfections due to dust, fingerprints, etc. that might be on optical hardware.

Images are post-processed using MATLAB. The basic assumption is that each individual image that has been acquired, \( I_i \), is the superposition of three sub-images: 

\[
I_i = I_B + \bar{I} + I_i'
\]

where \( I_B \) is the background ("no flow") image, \( \bar{I} \) is the time-mean image, and \( I_i' \) is the time-varying image due to turbulent fluctuations within the jet. \( I_i' \) is assumed to have zero mean. Images are imported into MATLAB and their histograms of pixel intensities are modified to improve brightness and contrast. The mean flow image, \( \bar{I} \), is then calculated as

\[
\bar{I} = \frac{1}{N} \sum_{i=1}^{N} (I_i - I_B)
\]

where \( N \) is the total number of frames.

### 3.6 Pitot Probe Flow Field Measurements

Mean flow Pitot pressure measurements of jets can be performed in the Penn State High-Speed Jet Aeroacoustics Facility using either a five-probe Pitot rake or a single Pitot tube. A series of mean Pitot pressure measurements of jets exhausting from the DSR-Sym-BL and DSR-FS-BL nozzles were obtained using the single Pitot probe. The measurements reported in this work compare the mean flow fields of imperfectly-expanded, dual-stream, rectangular jets exhausting from the DSR-Sym-BL and DSR-FS-BL nozzle configurations.

Mean velocities are estimated from the Pitot pressure measurements in order to help characterize and better understand the complex flow fields of low-area ratio, low-bypass ratio, supersonic, dual-stream, rectangular jets. Information derived from velocity measurements such as potential core length, jet deflection angle, and vorticity thickness will be used to assist in interpreting far-field acoustic data. Velocity data will also serve as a necessary database for verifying future RANS CFD simulations of these jets. Pitot measurements have been made in pure-air jets but the jets were operated at the same pressure ratios and Mach numbers as the acoustic experiments that used helium-air mixtures.
3.6.1 Pitot Pressure Measurement Methodology in Supersonic Jets

There are a number of challenges associated with calculating mean velocity from Pitot pressure measurements of imperfectly-expanded, supersonic jets. This subsection addresses the nuances and assumptions that are made to calculate the velocity from Pitot measurements.

As the flow is supersonic, inserting any obstruction into the flow, including a Pitot probe, causes a detached shock wave to set up upstream of the object. Figure 3.9 shows a cartoon schematic of a single Pitot probe in a supersonic flow. Flow quantities upstream and downstream of the shock are noted by the subscripts 1 and 2, respectively. In the case of a small diameter Pitot probe, a bow shock stands in front of the tube. Along the stagnation streamline the shock wave is normal to the flow direction. Downstream of the shock the local static pressure increases while the stagnation Pitot pressure and flow Mach number decrease [77].

![Figure 3.9: Cartoon schematic of a single Pitot probe immersed in a supersonic flow. Subscripts 1 and 2 denote flow quantities upstream and downstream of the shock, respectively.](image)

In a supersonic flow, the measures the total Pitot pressure \( (P_{0,2}) \) downstream of the shock wave. Combining the isentropic relation for pressure,

\[
\frac{P_{0,1}}{P_1} = \left(1 + \frac{\gamma - 1}{2} M_1^2\right)^{\frac{\gamma}{\gamma - 1}}
\]  

(3.9)

and the Rankine-Hugoniot relation for stagnation pressure ratio across a normal shock,

\[
\frac{P_{0,2}}{P_{0,1}} = \left[1 + \frac{2\gamma}{\gamma + 1} (M_1^2 - 1)\right]^{\frac{\gamma}{\gamma - 1}} \left[\frac{(\gamma + 1)M_1^2}{(\gamma - 1)M_1^2 + 2}\right]^{\frac{\gamma}{\gamma - 1}}
\]  

(3.10)
yields the following expression

\[
\frac{P_{0,2}}{P_1} = \left(\frac{\gamma + 1}{2} M_1^2\right)^{\frac{\gamma}{\gamma - 1}} \left(\frac{\gamma + 1}{2\gamma M_1^2 - (\gamma - 1)}\right)^{\frac{1}{\gamma - 1}}
\]

(3.11)

where \( P_{0,2}/P_1 \) is the ratio of the stagnation pressure downstream of the shock wave to the static pressure in the jet upstream of the shock wave, and the Mach number upstream of the normal shock. Equation (3.11) is commonly referred to as the Rayleigh-Pitot formula.

If the static pressure upstream of the normal shock, \( P_1 \), is known, \( M_1 \) can be determined from Equation (3.11). However, measurement of the static pressure in supersonic jet using a Pitot probe can be difficult and inaccurate [3]. The probe introduces a complicated shock wave and expansion fan structure that originates near the probe tip and reflects off the shear layer as well as off the probe farther downstream. Therefore, to use the Rayleigh-Pitot formula, the static pressure in the jet must be assumed to be equal to the ambient pressure, \( P_1 \approx P_\infty \). This assumption is accurate for ideally-expanded jets, as well as in imperfectly-expanded jets downstream of the shock cells where the static pressure has equated to ambient pressure.

Mach number and velocity estimates from Pitot pressure measurements within the supersonic, shock-containing region of the jet have unavoidable inaccuracies but the trends and comparisons of different jets are still of use.

In subsonic regions of the jet, where \( P_1 = P_\infty \), the Mach number, \( M_1 \), can be calculated directly from Equation (3.9). Once \( M_1 \) is determined, the jet velocity, \( U \), can be calculated using Equation (3.12),

\[
U = M_1 \sqrt{\gamma R \left(\frac{T_1}{T_0}\right) \left(\frac{T_0}{T_\infty}\right) T_\infty}
\]

(3.12)

where the temperature ratio \( T_0/T_\infty \) is unity for unheated jets, and the temperature ratio, \( T_1/T_0 = (P_{0,1}/P_1)^{(\gamma - 1)/\gamma} \), can be calculated once \( M_1 \) is determined.

### 3.6.2 Pitot Probe Measurement System

The Pitot probe has an inner diameter of 1.65mm with an outer diameter of 3.2mm. The probe is mounted to a three-dimensional traversing system. Probe movements in the stream-wise, \( X \), direction and one cross-stream direction are automated using Velmex stepper motor-controlled traversing stages in the LabVIEW data acquisition program. Photographs of the Pitot probe and traversing system installed in front of the DSR-FS-BL
nozzle are shown in Figure 3.10. Major dimensions of the traversing system are noted in the photographs. The traversing arrangement has the benefit of being rigidly fixed in place once it is installed and oriented. Misalignments with the dual-stream model have been measured to be less than $1^\circ$.

Figure 3.10: Photographs of Pitot probe measurement system installed ahead of the DSR-FS-BL nozzle.
The stream-wise traversing stage has a resolution of 4000 steps/in and a maximum down-stream range of 30.5 cm ($X/D_{Eq} = 13$). The cross-stream traversing stage has a resolution of 10160 steps/in with a cross-stream range of $\pm 8.90$ cm. The third dimension of the traversing system is manually controlled by a scissors jack to an accuracy of 1.6 mm. In order to fully exploit the fine traversing resolution of the cross-stream traversing stage, dual-stream models are oriented such that their minor axis direction is aligned with traverse direction. That is, the models are turned to the $\phi = 270^\circ$ azimuthal orientation so that the Pitot probe moves in the transverse cross-stream direction, $Z$ (see Figure 3.1). This allows for a high spatial resolution survey through both the core and (thin) bypass jets. A head-on photograph, looking in the upstream direction, of the probe in front of the DSR-FS-BL nozzle is shown in Figure 3.10b. The core and bypass jets are oriented vertically in the photograph and the bypass jet is on the right side of the nozzle in the photograph.

To conduct two-dimensional, stream-wise total Pitot pressure measurements of the dual-stream jets, a two-step process was employed. The first step involved a coarse, stream-wise survey of the jet, with Pitot probe measurements acquired at increments of $\Delta X/D_{Eq} = 1$ from $X/D_{Eq} = 0$ to 10. At each axial station, cross-stream measurements were acquired between $-1.25 \leq Z/D_{Eq} \leq 1.25$. This coarse survey was used to inform a second, fine jet plume survey. The coarse survey yielded approximate insight into jet deflection and spread rate. This allowed, a second, more spatially-resolved, stream-wise survey to be tailored to the jet exhaust without acquiring extraneous measurements in regions without flow or miss regions with flow. The fine jet plume surveys were usually comprised of nine sub-surveys with individual cross-stream ranges in the $Z$-direction tailored to the jet spread rate. An example of the measurements positions for a stream-wise survey is shown in Figure 3.11. The individual sub-surveys are noted by the different colored circles.

![Figure 3.11: Example of Pitot probe measurement locations for dual-stream jet plume survey. Colors indicate individual surveys that are compiled together during data processing.](image-url)
3.6.3 Pitot Probe Data Acquisition and Processing

Total pressures measurements using a Setra, model 205-2, gauge pressure transducer with a 690 kPa range. The transducer has a response time of 1 millisecond, as reported by the manufacturer. At each measurement location, the Pitot traversing system is paused for 0.2 sec to allow the total pressure in the 3.2 mm (inner) diameter, 35 cm long, tube connecting the Pitot probe and transducer to adjust. 1000 total Pitot pressure samples were acquired at a rate of 1 kHz before the probe is traversed to the next location. The core and bypass plenum pressure transducer voltages are monitored on digital voltmeters during the probe surveys. During testing, the NPR’s of both the core and bypass jets are usually maintained to within ±0.01 of their target values. Caution had to be taken when planning the extent of the sub-surveys shown in Figure 3.11. The duration of each survey is dictated by the number of measurement points. Should the time to complete a survey extend long than approximately 7 min, the high-pressure air reserves risk being depleted to the point where steady jet conditions cannot be maintained.

![Histograms](image)

Figure 3.12: Example histograms of total plenum pressure during Pitot probe survey of dual-stream jet.

Example histograms of the total core and bypass plenum pressure ratios over the duration of a survey are shown in Figure 3.12. Indicated in each panel are the target NPR’s of each stream as well as the standard deviation, \( \sigma \) of each stream throughout the course of the survey. The standard deviations are reported in both dimensional pressure (Pa) and as a fraction of ambient pressure, \( \sigma_{NPR} = \sigma_P / P_\infty \). Figure 3.12a shows an example in which the core jet pressure maintained closest to the target pressure throughout the Pitot probe survey (smallest \( \sigma_{NPR} \)) while Figure 3.12b shows an example in which the core jet...
pressure varied the most throughout the survey (maximum $\sigma_{NPR}$). Both core and bypass plenum pressures were maintained to within 0.6% ($3\sigma_{NPR}/NPR \times 100\%$) of their target values during the Pitot probe surveys.

### 3.7 Experimental Uncertainty

Proper analysis of experimental data and the conclusions deciphered from the results are contingent upon a proper understanding of the precision and accuracy of the measurements themselves. Such is especially true when analyzing the differences between measured quantities. For differences to truly have meaning, the differences must be greater than the unavoidable errors, limitations, and uncertainties associated with the measurement technique and hardware. To this end, this section aims to quantify the uncertainty and repeatability of each measurement technique used in this dissertation. Uncertainties are calculated following the methods described in [180]. The general uncertainties associated with transducers are discussed in Section 3.7.1. The uncertainties of acoustic measurements are estimated in Section 3.7.2 and the uncertainties of Pitot probe measurements are discussed in Section 3.7.3.

#### 3.7.1 Facility Uncertainties

The accuracies of all gauges and transducers in the laboratory pertinent to high-speed jet operation are listed in Table 3.3. Atmospheric pressure, temperature, and relative humidity are read off of analog gauges mounted within the anechoic chamber. The uncertainty in the transducers used to measure total pressures within the nozzles, $P_0$, is reported by the manufacturer: Setra, model 205-2. Microphone radial positions were measured using a tape measure. The angular positions of the microphones are measured using a laser pointer mounted on top of an analog angle dial. Angles and distances are measured from the nozzle exit plane.

A critical parameter for experiments is the nozzle pressure ratio, $NPR = P_0/P_\infty$, as this dictates the fully-expanded jet Mach number, $M_J$, and jet thrust value. The uncertainty of this parameter, $\sigma_{NPR}$, can be calculated analytically from Equation (3.13). The average ambient pressure in the anechoic chamber is 98kPa and a typical $NPR$ is 3.0. Using these values, along with the transducer and gauge uncertainties in Table 3.3, the estimated uncertainty in nozzle pressure ratio is $\sigma_{NPR} = 0.008$. 

82
\[\sigma_{NPR}^2 = \left(\frac{\partial (NPR)}{\partial P_0}\right)^2 \sigma_{P_0}^2 + \left(\frac{\partial (NPR)}{\partial P_\infty}\right)^2 \sigma_{P_\infty}^2\]

\[\sigma_{NPR}^2 = \left[\sigma_{P_0}^2 + (NPR)^2 \sigma_{P_\infty}^2\right] \left(\frac{1}{P_\infty}\right)^2\]  

(3.13)

Table 3.3: Accuracies of laboratory gauges and transducers

<table>
<thead>
<tr>
<th>Measurement</th>
<th>Accuracy, (\sigma)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ambient Temperature</td>
<td>(\sigma_{T_\infty}) ±1 K</td>
</tr>
<tr>
<td>Ambient Pressure</td>
<td>(\sigma_{P_\infty}) ±100 Pa</td>
</tr>
<tr>
<td>Relative Humidity</td>
<td>(\sigma_{RH}) ±1 %RH</td>
</tr>
<tr>
<td>Pressure Transducer</td>
<td>(\sigma_{P_0}) ±690 Pa</td>
</tr>
<tr>
<td>Radial Mic Position</td>
<td>(\sigma_{r_{mic}}) ±6.4 mm</td>
</tr>
<tr>
<td>Angular Mic Position</td>
<td>(\sigma_{\theta}) ±0.5°</td>
</tr>
</tbody>
</table>

3.7.2 Acoustic Measurement Uncertainty

As acoustic measurements are a central focus of this thesis, meaningful comparisons between measurements can only be made once the uncertainty and repeatability of these data have been established. The uncertainty in the measured overall sound pressure levels, \(\sigma_{OASPL}\), is assumed to be dependent on the uncertainties in jet sound pressure level (\(\sigma_{JSP}\)), microphone position (\(\sigma_{pos}\)), ambient atmospheric conditions (\(\sigma_{atm}\)), the microphone output (\(\sigma_{mic}\)), and additional random error (\(\sigma_{rand}\)) term to account for any additional systematic uncertainties. Powers [1] previously showed that the uncertainty due the data acquisition system i.e. quantization error compared with the other uncertainties is negligible for a 16-bit system. The total sound pressure level uncertainty is given by Equation (3.14).

\[\sigma_{OASPL} = \sqrt{\sigma_{JSP}^2 + \sigma_{pos}^2 + \sigma_{atm}^2 + \sigma_{mic}^2 + \sigma_{rand}^2}\]  

(3.14)

Experimental evidence by Viswanathan [25] showed that the OASPL scales with velocity but the value of the exponent is a function of polar angle, \(\theta\), and \(TTR\). In the direction of max noise emission, OASPL scales approximately with \(U_J^9\) over a range of temperature ratios up to \(TTR= 3.2\).
The uncertainty in the jet sound pressure level is estimated based on the ninth power of velocity. This yields a conservative estimate for $\sigma_{JSP}$ as it represents the expected upper limit of uncertainty in acoustic measurements due to variations in the sound power of the jet. For a dual-stream jet then, the OASPL in decibels is assumed to scale with the sum of the core and bypass jet velocities each raised to the ninth power:

$$OASPL \propto 10 \log_{10} \left( U_{J,C}^9 + U_{J,BP}^9 \right)$$

(3.15)

And so the uncertainty in the jet sound pressure is given by

$$\sigma_{JSP}^2 = \left( \frac{\partial OASPL}{\partial U_{J,C}} \right)^2 \sigma_{U_{J,C}}^2 + \left( \frac{\partial OASPL}{\partial U_{J,BP}} \right)^2 \sigma_{U_{J,BP}}^2$$

(3.16)

where

$$\frac{\partial OASPL}{\partial U_{J,C}} = \frac{90 U_{J,C}^8}{(U_{J,C}^9 + U_{J,BP}^9) \ln[10]}$$

(3.17)

and

$$\frac{\partial OASPL}{\partial U_{J,BP}} = \frac{90 U_{J,BP}^8}{(U_{J,C}^9 + U_{J,BP}^9) \ln[10]}$$

(3.18)

so all that remains is to determine estimates for the uncertainties in the core and bypass jet velocities, $\sigma_{U_{J,C}}$ and $\sigma_{U_{J,BP}}$ respectively, before Equation (3.16) can be evaluated.

For any jet, assuming isentropic expansion, the fully-expanded velocity, $U_J$, is given by:

$$U_J = \sqrt{\frac{2\gamma}{\gamma - 1}} R T_0 \left[ 1 - (NPR)^{\frac{\gamma - 1}{\gamma}} \right]$$

(3.19)

where the uncertainty in the velocity is dependent on the uncertainty of each term in Equation (3.19). The equation for the uncertainty in jet velocity is shown in Equation (3.20).

$$\sigma_{U_J}^2 = \left( \frac{\partial U_J}{\partial (NPR)} \right)^2 \sigma_{NPR}^2 + \left( \frac{\partial U_J}{\partial T_0} \right)^2 \sigma_{T_0}^2 + \left( \frac{\partial U_J}{\partial R} \right)^2 \sigma_{R}^2 + \left( \frac{\partial U_J}{\partial \gamma} \right)^2 \sigma_{\gamma}^2$$

(3.20)

where the last 2 terms in Equation (3.20) arise due to the addition of helium into the jet flow. These terms are negligible for pure-air jets. The partial derivatives are evaluated analytically, and given by:

$$\frac{\partial U_J}{\partial (NPR)} = \left( \frac{RT_0}{U_J} \right) (NPR)^{\frac{1 - \gamma}{\gamma}}$$

(3.21)

$$\frac{\partial U_J}{\partial T_0} = \frac{U_J}{2 T_0}$$

(3.22)
\[
\frac{\partial U_J}{\partial R} = \frac{U_J}{2R}
\]

\[
\frac{\partial U_J}{\partial \gamma} = \left( \frac{U_J}{2\gamma} \right) \left( \frac{1}{1-\gamma} + \frac{\ln[\text{NPR}]}{\gamma \left( \frac{1}{\text{NPR}^{\frac{-1}{\gamma}}} - 1 \right)} \right)
\]

The uncertainty in nozzle pressure ratio was determined from Equation (3.13) to be \( \sigma_{\text{NPR}} = 0.008 \). Because both the high-pressure air and helium are stored at room temperature, then \( T_0 = T_\infty \) and \( \sigma_{T_0} = \sigma_{T_\infty} = 1 \text{ K} \).

The uncertainties in the gas constant, \( \sigma_R \), and specific heat ratio, \( \sigma_\gamma \), due to the addition of helium must be estimated. Due to the manual opening of the helium ball valve during experiments, there is inevitably some overshoot (subscript 'ov') in the target NPR during testing. This overshoot is kept within 3% of the target NPR during testing, and is output the MATLAB Command Window during post-processing. During testing, the mass flow rate of the air is constant as it is set by the operator prior to the addition of helium. To estimate \( \sigma_R \) and \( \sigma_\gamma \) of the helium-air gas mixtures first the target values (subscript 'target') of \( R \) and \( \gamma \) were calculated along with the requisite mass flow rate of air. Keeping the mass flow rate of air constant, the helium-air MATLAB routine (outlined in Figure 3.3) was run iteratively with increasing amounts of helium until the nozzle pressure ratio increased by 3%, i.e. \( \text{NPR}_{\text{ov}} = 1.03 \text{NPR}_{\text{target}} \). Finally, the uncertainties in gas constant and specific heat ratio were simply calculated as \( \sigma_R = R_{\text{ov}} - R_{\text{target}} \) and \( \sigma_\gamma = \gamma_{\text{ov}} - \gamma_{\text{target}} \). These values are approximately \( \sigma_R \approx 20 \text{ J/kg} \cdot \text{K} \) and \( \sigma_\gamma \approx 0.003 \) but vary slightly based on the NPR and simulated TTR chosen. Table 3.4 lists the jet velocity and jet sound pressure uncertainties for four sets of dual-stream jet operating conditions.

<table>
<thead>
<tr>
<th>( NPR_C )</th>
<th>Core Jet (C) ( U_{JC} ) ( \sigma_{U_{JC}} )</th>
<th>Bypass Jet (BP) ( U_{JP} ) ( \sigma_{U_{JP}} ) ( \sigma_{\text{JSP}} )</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.5</td>
<td>369 m/s 0.57 m/s</td>
<td>2.30 354 m/s 0.65 m/s 0.05 dB</td>
</tr>
<tr>
<td>3.0</td>
<td>399 m/s 0.44 m/s</td>
<td>1.90 315 m/s 0.92 m/s 0.04 dB</td>
</tr>
<tr>
<td>3.5</td>
<td>422 m/s 0.37 m/s</td>
<td>1.60 272 m/s 1.30 m/s 0.03 dB</td>
</tr>
<tr>
<td>2.5</td>
<td>639 m/s 11.5 m/s</td>
<td>2.30 354 m/s 0.65 m/s 0.70 dB</td>
</tr>
<tr>
<td>3.0</td>
<td>691 m/s 11.2 m/s</td>
<td>1.90 315 m/s 0.92 m/s 0.63 dB</td>
</tr>
<tr>
<td>3.5</td>
<td>730 m/s 11.1 m/s</td>
<td>1.60 272 m/s 1.30 m/s 0.60 dB</td>
</tr>
</tbody>
</table>

The uncertainty in the measured sound pressure levels due to the errors in the microphone position, \( \sigma_{\text{pos}} \), are due to uncertainties in the noise source location in tandem with uncertainties in the measured radial and polar position of the microphone. To make
a conservative estimate, the uncertainty in sound pressure in the peak noise direction, \( \theta = 45^\circ \), is estimated. Experimental measurements of supersonic jets have shown that the highest intensity noise levels originate from a position near the end of the potential core of the jet, \( X/D \approx 6 \) [27,78]. However, the microphone array was focused at the nozzle exit and spherical spreading (Equation (3.3)) does not account for the noise source distribution. A diagram for the error in microphone positioning is shown in Figure 3.13.

To estimate the positioning uncertainty on sound pressure level, a microphone with a nominal position of \( r/D = 75, \theta = 45^\circ \) measured from the nozzle exit plane is considered. Assuming a noise source location 6 diameters downstream of the nozzle exit \( \Delta X/D \approx 6 \), the effective position of the microphone relative to the noise source is found to be \( r'/D = 70.9 \) and \( \theta' = 48.5^\circ \). Around \( \theta' = 48.5^\circ \), the OASPLs shown in Figure 3.5b vary by approximately 0.5 dB/deg. So, the uncertainty in sound pressure levels due to the microphone position is given by:

\[
\sigma^2_{pos} = \left( \frac{20}{r \ln[10]} \right)^2 \sigma^2_{r_{mic}} + \left( \frac{20}{r \ln[10]} \right)^2 \sigma^2_{r'} + (0.5 \text{ dB/deg})^2 \sigma^2_{\theta} \tag{3.25}
\]

where \( \sigma_{r_{mic}} \) and \( \sigma_{\theta} \) are the uncertainties in the nominal position of the microphone, \((r, \theta)\), measured with the tape measure and angle dial and are listed in Table 3.3. \( \sigma_{r'} = r - r' \) is the uncertainty in the microphone position due to a noise source located downstream of the nozzle exit. The change in OASPL with \( r \), given by the first 2 terms on the right hand side of Equation (3.25), are derived analytically from the spherical spreading relation given in Equation (3.3). The estimated positioning uncertainty is \( \sigma_{pos} = 0.63 \text{ dB} \)

The effects of variations in atmospheric conditions on the measured overall sound pressure levels were calculated using the equations for sound absorption in air [173]. The partial

Figure 3.13: Diagram of error in microphone positioning. Black line indicates the measured position of the microphone used in acoustic data processing.
derivatives in Equation (3.26) were numerically evaluated by perturbing the atmospheric absorption calculation around typical laboratory atmospheric conditions ($P_\infty = 980 \text{ mbar}$, $T_\infty = 293 \text{ K}$, $RH = 60\%$). Noise sources at higher frequencies are more susceptible to atmospheric attenuation. As an example, at 1 kHz the atmospheric correction is 0.005 dB but at 100 kHz the correction is 3.61 dB. To make an upper-limit estimate of the uncertainty in the atmospheric attenuation correction, the numerical derivatives were evaluated for a frequency of 100 kHz. The atmospheric uncertainty is estimated to be $\sigma_{\text{atm}} = 0.14 \text{ dB}$.

\[
\sigma_{\text{atm}}^2 = \left( \frac{\partial (\text{ATM})}{\partial P_\infty} \right)^2 \sigma_{P_\infty}^2 + \left( \frac{\partial (\text{ATM})}{\partial T_\infty} \right)^2 \sigma_{T_\infty}^2 + \left( \frac{\partial (\text{ATM})}{\partial RH} \right)^2 \sigma_{RH}^2 \tag{3.26}
\]

Microphones are calibrated using a B&K model 4321 calibrator. The uncertainty in the pressures measured by each microphone is due to the accuracy of the calibrator. The uncertainty in the calibrator is given by the manufacturer as $\sigma_{\text{mic}} = 0.2 \text{ dB}$. Finally an additional 1\% uncertainty to the core jet velocity is considered. This uncertainty is used to account for other potential sources of error not otherwise considered such as geometry imperfections with in the nozzles or total pressure losses within the dual-stream model.

The individual and total uncertainties for a range of dual-stream jet operating conditions are listed in Table 3.5. Unheated, pure-air jet acoustic measurements have estimated uncertainties of approximately 0.75 dB. The additional complexity of simulating heated jets with helium-air mixtures increases the experimental uncertainty to around 1.00 dB.

<table>
<thead>
<tr>
<th>Operating Conditions</th>
<th>Individual Uncertainties (dB)</th>
<th>Total (dB)</th>
</tr>
</thead>
<tbody>
<tr>
<td>NPRC TTRC NPRBP</td>
<td>$\sigma_{\text{ISP}}$ $\sigma_{\text{pos}}$ $\sigma_{\text{atm}}$ $\sigma_{\text{mic}}$ $\sigma_{\text{bias}}$</td>
<td>$\sigma_{\text{OASPL}}$</td>
</tr>
<tr>
<td>2.5 1 2.30</td>
<td>0.05 0.63 0.14 0.20 0.23</td>
<td>0.72</td>
</tr>
<tr>
<td>3.0 1 1.90</td>
<td>0.04 0.63 0.14 0.20 0.35</td>
<td>0.76</td>
</tr>
<tr>
<td>3.5 1 1.60</td>
<td>0.03 0.63 0.14 0.20 0.38</td>
<td>0.78</td>
</tr>
<tr>
<td>2.5 3 2.30</td>
<td>0.70 0.63 0.14 0.20 0.39</td>
<td>1.05</td>
</tr>
<tr>
<td>3.0 3 1.90</td>
<td>0.63 0.63 0.14 0.20 0.39</td>
<td>1.00</td>
</tr>
<tr>
<td>3.5 3 1.60</td>
<td>0.60 0.63 0.14 0.20 0.39</td>
<td>0.98</td>
</tr>
</tbody>
</table>

Sample spectra highlighting experimental repeatabilities for out-of-test and in-test acoustic measurements are shown in Figure 3.14. Spectra are shown for both pure air ($TTR = 2.0$) and heat-simulated ($TTR = 3.0$) $M_f = 1.36$ jets exhausting from the SSEQ-Rnd nozzle. Polar angles are noted in the upper-right corner of each figure. The data shown in Figure 3.14 span a 4 year period of time between 2013 and 2017, with the dates of each test
noted in the legends. Data acquired prior to 2015 were taken with different test operators and different microphone array arrangements. The out-of-test unheated jet spectra, shown in the top row of Figure 3.14, compare well with each other, with differences less than 1.5dB at most frequencies. Out-of-test heat-simulated data, shown in Figure 3.14b were acquired by two different operators. Although there are some discrepancies at low-frequencies, the difference in the overall sound pressure levels are 0.9 dB which is within the uncertainty estimate. Differences between in-test acoustic data for both pure air (Figure 3.14c) and heat-simulated (Figure 3.14d) measurements fall below the estimated measurement uncertainty. In-test overall sound pressure levels agree to within 0.5 dB OASPL.

Figure 3.14: Out-of-test (top row) and in-test (bottom row) acoustic spectra for pure air and heat-simulated $M_J = 1.36$ jets exhausting from the SSEQ-Rnd nozzle.
3.7.3 Total Pitot Pressure Measurement Uncertainty

The uncertainty in Pitot probe measurements arises from the uncertainty associated with the pressure transducers used to measure the jet flow field in addition to uncertainty in the transducers monitoring the total pressure in the jet plenums. These pressure uncertainties translate in uncertainties in the Mach numbers determined from these measurements. First, the uncertainties associated with subsonic jet measurements are discussed in Section 3.7.3.1. Following this, Section 3.7.3.2 discusses the errors associated with Pitot probe measurements of supersonic jets. It will be shown that the uncertainties due to random and systematic errors decrease with increasing jet Mach number and that for measurements of imperfectly-expanded supersonic jets, the dominant source of error arises from not precisely knowing the jet static pressure.

3.7.3.1 Uncertainty in Measurements of Subsonic Jets

Uncertainties in the plenum pressure lead to uncertainties in the Mach number of the jet exhausting from the nozzle ($\sigma_{M,NPR}$). Simultaneously, uncertainties in the total Pitot pressure measurement ($\sigma_{M,P}$) along with uncertainties in the measured ambient pressure ($\sigma_{M,P\infty}$) lead to uncertainties in the jet Mach number calculated from the measurements. Because Pitot pressure measurement surveys have durations of about 5-7 min additional error arises from the ability of the operator to consistently maintain a constant plenum pressure throughout the test ($\sigma_{opp}$). The sources of error and the total error in jet Mach number ($\sigma_M$) are summarized in Equation (3.28).

$$\sigma_M = \sqrt{\sigma_{M,NPR}^2 + \sigma_{M,P_0}^2 + \sigma_{M,P\infty}^2 + \sigma_{opp}^2}$$

(3.27)

To limit the complexity of this uncertainty analysis, only a single, subsonic jet is considered. The isentropic relation for the static-to-total pressure ratio, which is given in Equation (3.9), is rearranged to solve to Mach number

$$M = \sqrt{\left[(NPR)^{\frac{\gamma-1}{\gamma}} - 1\right] \left(\frac{2}{\gamma-1}\right)}$$

(3.28)

$$= \sqrt{\left[\left(\frac{P_0}{P\infty}\right)^{\frac{\gamma-1}{\gamma}} - 1\right] \left(\frac{2}{\gamma-1}\right)}$$

89
where top row is expressed in terms of NPR while the bottom row is expressed in terms of the total and ambient pressures. Now, the partial derivatives of Mach number with respect to $P_0$, $P_\infty$, and $NPR$ are

\[
\frac{\partial M}{\partial P_0} = \frac{1}{M\gamma} \left( P_0 \right)^{\frac{\gamma - 1}{\gamma}} \left( \frac{1}{P_\infty} \right) \tag{3.29}
\]

\[
\frac{\partial M}{\partial P_\infty} = -\left( \frac{P_0}{P_\infty} \right) \left( \frac{\partial M}{\partial P_0} \right) \tag{3.30}
\]

\[
\frac{\partial M}{\partial (NPR)} = \left( \frac{(NPR)^{\frac{\gamma - 1}{\gamma}}}{M\gamma} \right) \tag{3.31}
\]

The individual uncertainties, $\sigma_{M,P_0}$, $\sigma_{M,P_\infty}$, $\sigma_{M,NPR}$, and $\sigma_{opp}$ are

\[
\sigma_{M,P_0} = \left( \frac{\partial M}{\partial P_0} \right) \sigma_{P_0} \tag{3.32}
\]

\[
\sigma_{M,P_\infty} = \left( \frac{\partial M}{\partial P_\infty} \right) \sigma_{P_\infty} \tag{3.33}
\]

\[
\sigma_{M,NPR} = \left( \frac{\partial M}{\partial (NPR)} \right) \sigma_{NPR} \tag{3.34}
\]

\[
\sigma_{opp} = \left( \frac{\partial M}{\partial P_0} \right) \sigma_{P_0,opp} \tag{3.35}
\]

where the total and ambient pressure uncertainties, $\sigma_{P_0}$ and $\sigma_{P_\infty}$, are listed in Table 3.3, and the uncertainty in nozzle pressure ratio was determined to be $\sigma_{NPR} = 0.008$ from Equation (3.13). The only remaining unknown is the error in the operator’s ability to maintain constant plenum pressure throughout the test, $\sigma_{P_0,opp}$. For this, attention is directed to the plenum pressure histograms in Figure 3.12. The standard deviations of the core and bypass plenum pressures are noted in each panel. The 95\% confidence interval (2× standard deviation) in the plenum pressure with highest calculated standard deviation is used to conservatively estimate the operator control error. This leads to $\sigma_{P_0,opp} = 2 \times 527 \text{ Pa} = 1054 \text{ Pa}$.

The uncertainties in Mach number for a range of pressure ratios between 1 and 1.89 are shown in Figure 3.15. Error bars representing $\sigma_M$ are superimposed on the $M$ vs. $P_0/P_\infty$ curve in Figure 3.15a while the ratio of $\sigma_M$ to $M$, expressed as a percentage, is presented in Figure 3.15b. The uncertainty quickly drops to less than 5\% for Mach numbers above 0.4. In low speed flow, the output voltage from the Pitot probe pressure transducer is low, so
the pressure measurements are compromised by the noise floor of the transducer. However, the uncertainties plotted in Figures 3.15a and 3.15b represent the uncertainty, due to random errors, of a single total pressure measurement. In practice, 1000 total pressure samples are acquired and averaged prior to calculating Mach number. The standard error of the mean decreases by a factor inversely proportional to the square root of the number of samples acquired, that is, \( \sigma_{M,\text{mean}} = \sigma_M / \sqrt{1000} \approx \sigma_M / 31.6 \). This quantity is plotted in Figure 3.15c. The large sample size is effective at reducing the uncertainties in the Mach number calculated from the average measured total pressure to less than 2\% for subsonic jets.

![Graphs](image_url)

(a) Estimated uncertainty in jet Mach numbers. Error bars indicate \( \pm \) uncertainty.

(b) Estimated uncertainty as a percent of jet Mach number.

(c) Percent uncertainty of the mean as a percent of jet Mach number.

Figure 3.15: Estimated uncertainty in subsonic Pitot pressure measurements.

### 3.7.3.2 Error in Pitot Probe Measurements of Imperfectly-Expanded Supersonic Jets

An attempt is made to partially-quantify the error in the Pitot pressure measurements of over-expanded supersonic jets. A theoretical analysis was carried out for a single, over-expanded jet exhausting from a \( M_D = 1.65 \) nozzle. This theoretical jet exhausts from the nozzle with an over-expanded Mach number \( M = 1.65 \). The atmospheric pressure-matched nozzle pressure ratio for a \( M = 1.65 \) nozzle is 4.6. The over-expanded jet has nozzle pressure ratios between 1.89 and 4.6. This jet was "jumped" across a normal shock as if a Pitot probe were in the flow. The total pressure downstream of the normal shock, \( P_0,2 \), was then calculated using Equation (3.10). Assuming the static pressure upstream of the shock is equal to atmospheric pressure, Equation (3.11) was solved to estimate the Mach number at the nozzle exit plane, \( M_1 \). This was the assumption that was made for...
Pitot data processing. The error in this Mach number estimate, expressed in percentage as \(\frac{(M_1 - M_D)}{M_D} \times 100\%\), is plotted along the ordinate in Figure 3.16. The atmospheric approximation consistently under-predicts the Mach number at the nozzle exit. The error lessens as the nozzle pressure ratio approaches the design pressure ratio of 4.6.

For experiments presented in this work, core jets were typically operated between \(2.5 \leq NPR_C \leq 3.5\). The error in estimated Mach number are still fairly significant, 15-30%. But, the errors in Figure 3.16 represent a high limit to the expected errors. For actual Pitot pressure measurements, pressures are measured downstream of the core nozzle exit (see Figure 3.11). Oblique shocks within the nozzle, as well as in the jet flow downstream of the nozzle, provide a means for "static pressure recovery" that raise the static pressure of the jet closer to atmospheric pressure, therefore reducing the inaccuracy of assuming the jet static pressure is equal to ambient pressure.

![Figure 3.16: Example of error in assuming static pressure of over-expanded supersonic jet is equal to atmospheric pressure for Pitot probe data processing.](image)

3.8 Design and Planning of Experiments

The primary focus of this study is a comparison of the acoustics of military-style, rectangular dual-stream jets with the acoustics of single-stream jets. Towards this end, the effects of operating conditions on the acoustics of dual-stream jets are also investigated. In order to better understand changes to far-field noise, mean flow data in the form of Pitot pressure measurements and schlieren flow visualizations have also been acquired. Mean flow data are used to related changes in the jet flows to measured changes in the radiated noise. Tables 3.6 to 3.8 provide the matrix of jet operating conditions and types of measurements performed for each nozzle. "Acoustics" is abbreviated as 'Acs' in the tables. Tables 3.6 to 3.8 report the test matrices for the single-stream nozzles, dual-stream baseline, and
dual-stream with aft deck, respectively. Included in Table 3.6 are characteristic frequencies, \( f_C \), and Reynolds numbers, \( Re \), for the single-stream jets; and Tables 3.7 and 3.8 includes the Reynolds numbers for the core and bypass jets.

Acoustic measurements are typically performed in the major and minor axis directions to better understand the azimuthal variation of the far-field noise. To prevent test matrices from becoming prohibitively large, acoustic measurements in the minor axis plane are limited to the orientation in which the bypass jet exhausts in a plane bisecting the microphone and the core jet. Flow field measurements focus on the stream-wise surveys of the dual-stream jet exhaust but a select number of cross-stream surveys have been conducted. Cross-stream data acquired at the nozzle exit planes are used to estimate jet thrust and mass flow rates, while surveys conducted near the end of the potential cores of the jets are used to better understand the three-dimensional, downstream evolution of the jets.

Table 3.6: Test matrix for single-stream reference jets

<table>
<thead>
<tr>
<th>Nozzle ID &amp; Geometry</th>
<th>Operating Conditions</th>
<th>( f_c ) (kHz)</th>
<th>( Re ) (( \times 10^3 ))</th>
<th>Acs</th>
<th>Pitot</th>
<th>Flow Viz</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rectangular</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>( AR = 2 )</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>( MD = 1.65 )</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>( Deq = 2.25 ) cm</td>
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<td></td>
<td></td>
<td></td>
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<td></td>
</tr>
<tr>
<td></td>
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</tr>
<tr>
<td>SSEQ-Rect</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
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<td></td>
<td></td>
<td></td>
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</tr>
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<td></td>
</tr>
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</tr>
<tr>
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<tr>
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Table 3.7: Test matrix for baseline (BL), dual-stream rectangular (DSR) nozzles

<table>
<thead>
<tr>
<th>Nozzle ID &amp; Geometry</th>
<th>Operating Conditions</th>
<th>( Re_C ) ( Re_{BP} )</th>
<th>Acs</th>
<th>Pitot</th>
<th>Flow Viz</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>( NPR_C ) ( NPR_{BP} ) ( TTR_C )</td>
<td>( \times 10^3 )</td>
<td>( \times 10^3 )</td>
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<td>930</td>
<td>494</td>
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</tr>
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<td></td>
<td></td>
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<td>569</td>
<td>817</td>
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<td>2.88 2.34 2.68</td>
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<td>890</td>
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<td>2.88 2.34 2.68</td>
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<td>2.88 2.07 3.54</td>
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Table 3.8: Test matrix for dual-stream rectangular (DSR) nozzles with an aft deck (AD)

<table>
<thead>
<tr>
<th>Nozzle ID &amp; Geometry</th>
<th>Operating Conditions</th>
<th>$Re_C$</th>
<th>$Re_{BP}$</th>
<th>Acs</th>
<th>Flow Viz</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>NPR&lt;sub&gt;C&lt;/sub&gt;</td>
<td>NPR&lt;sub&gt;BP&lt;/sub&gt;</td>
<td>TTR&lt;sub&gt;C&lt;/sub&gt;</td>
<td>(×10&lt;sup&gt;3&lt;/sup&gt;)</td>
<td>(×10&lt;sup&gt;3&lt;/sup&gt;)</td>
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<tr>
<td><strong>DSR-FS-AD</strong></td>
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<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
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<td>2.26</td>
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<tr>
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<td></td>
<td>967</td>
<td>287</td>
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<td>459</td>
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<td></td>
<td></td>
<td></td>
<td>1.0</td>
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<tr>
<td><strong>DSR-Sym-AD</strong></td>
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<td></td>
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</tr>
<tr>
<td>#Bypass = 2</td>
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<td>1.20</td>
<td>2.10</td>
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<td>324</td>
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</table>
This chapter reports on a series of Pitot pressure measurements and flow visualizations of jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles that are shown in Figure 4.1. Specifically, steady, one-component axial Mach numbers and velocities, calculated from Pitot pressures, are presented along with schlieren visualization experiments. The primary intent of these measurements is to gain insight into the these complex jet flows, and, in Chapter 5, these data are used to link changes flow field features to changes in the far-field noise. Additionally, comparisons are made with single-stream jets. Centroid line Mach numbers and shear layer thicknesses are compared with single-stream, rectangular jet data acquired by Akatsuka et al. [4]. As well, the measured shock-cell spacing in the dual-stream jets is compared with a semi-empirical model developed for single-stream jets. Such comparisons give fundamental and qualitative insight into how supersonic, dual-stream jet flows differ from single-stream jets. The data reported in this chapter also begin to form an experimental database to validate future RANS CFD simulations.

Figure 4.1: Summary of baseline dual-stream, rectangular nozzles and coordinate system.
The Cartesian coordinate system, shown in Figure 4.1, is used for flow-field measurements. The origin of the coordinate system is placed at the geometric center of the nozzle exit plane. $X$ is oriented along the jet axis, in the downstream direction, and $Y$ is directed along the major axis of the nozzle. $Z$ is subsequently defined to be pointing in the minor axis direction, opposite the position of the fluid shield in the DSR-FS-BL nozzle.

4.1 Stream-Wise Survey of Jets

This section presents the results from stream-wise surveys of jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles along with corresponding flow visualization results. Pitot pressures measurements were acquired in the $XZ$ center plane of the jets. Table 4.1 lists the two sets jet operating conditions for the experiments. Measurements were acquired for both subsonic and supersonic bypass jet Mach numbers. The $NPR$'s are designed to produce, approximately, the same total thrust and mass flow rate, following the methods discussed in Section 2.5. But, because Pitot pressure measurements are limited to pure-air air jets, the thrusts and mass flow rates will not exactly match. Measurements are referred to by their Test Point (TP) number in the following sections.

Table 4.1: Dual-stream jet operating conditions for Pitot probe and flow visualization measurements

<table>
<thead>
<tr>
<th>Test Pt. (TP)</th>
<th>Estimated Values</th>
<th>Core (C)</th>
<th>Bypass (BP)</th>
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</thead>
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<td>$T_{net}$ (N)</td>
<td>$\dot{m}$ (kg/s)</td>
<td>NPR$_{C}$</td>
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<tr>
<td>2</td>
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<td>4</td>
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</tbody>
</table>

4.1.1 Comparison of Jet Mach Number Profiles

4.1.1.1 DSR-FS-BL Nozzle

Jet Mach number profiles and the corresponding mean flow visualization image for the DSR-FS-BL nozzle operating at TP2 are shown in Figure 4.2. The Mach number profiles in Figure 4.2b are normalized by the design Mach number of the core nozzle, $M_{D,C} = 1.65$, and the profiles have been staggered based on their non-dimensional axial position. All positions are non-dimensionalized by equivalent diameter of the dual-stream nozzle: $D_{Eq} = 2.76$ cm. Two core jet velocities are indicated in lower left corner of Figure 4.2b: 1)
the theoretical, fully-expanded, core jet velocity based on \( NPR_C, U_{J,C,Theory} \), and 2) the maximum jet velocity calculated from Pitot pressure measurements, \( U_{\text{max,meas}} \).

In Figure 4.2a, shock cells, forming a diamond-shaped pattern, are visualized in the jet out to 6\( D_{Eq} \) downstream of the nozzle exit. Interestingly, this mean flow visualization, does not show any distinct evidence of the bypass jet. Inspecting the Mach number profiles, shown in Figure 4.2a, the lower-velocity bypass jet is not evident in the Mach number profiles beyond \( X/D_{Eq} = 3 \). This suggests that the core and bypass jets become fully-mixed within a short distance downstream of the nozzle exit. But, for helium-air mixture core jets, the distance to achieve a fully-mixed state is hypothesized to extend farther downstream due to the greater mismatch in core and bypass jet velocities.

Because the jets may deflect in the \( Z \) direction, the position of the jet center is tracked by the centroid of momentum. For a given axial station, \( X = x_i \), the centroid of momentum, \( Z_{CL}(x_i) \) is defined as

\[
Z_{CL}(x_i) = \frac{\sum_{n=1}^{N} U(n)\rho(n)Z(n)}{\sum_{n=1}^{N} U(n)\rho(n)}
\]  

(4.1)

Figure 4.2: Flow visualization images (top) and Mach number profiles (bottom) of the DSR-FS-BL nozzle operating at TP2: \( M_{J,C} = 1.38, M_{J,BP} = 0.85 \).
where $N$ is the number of Pitot measurements gathered at $X = x_i$, $U$ is the mean velocity calculated from Equation (3.12), and $\rho$ is the density calculated from isentropic relations. Equation (4.1) amounts to a momentum-weighted average position. Identical results for the jet centroid position were obtained by integrating the velocity profiles at each axial position and defining $Z_{CL}(x_i)$ to be the position that divides the area under the velocity curve in half.

The solid black line, with open circles, in Figure 4.2b marks the jet centroid line. This line tracks the position of maximum calculated Mach number at each axial station. Flow visualizations and the slope of the jet centroid line confirm that the jet deflects downwards by approximately $3^\circ$. In spite of this deflection, downstream of $X/D_{Eq} = 5$, the axial Mach number profiles appear, qualitatively, to be symmetric about the jet centroid line.

The dotted lines in Figure 4.2b mark the half-velocity position of the jet, $Z_{0.5}$. At this cross-stream position, the local velocity is half of the centroid line value. This gives a visual estimate for the jet spread rate. In additional, the primary potential core is outlined with the unmarked, solid black lines. The core was defined to be the region, on either side of the centroid line, where the jet velocity is $\geq 90\%$ of the maximum measured velocity [103,181]. The potential core extends $4.5D_{Eq}$ downstream of the nozzle exit plane. Similar potential core lengths have been measured in single-stream rectangular jets by Veltin and McLaughlin [79], and in high-aspect ratio, dual-stream, rectangular jets by Murakami and Papamoschou [152] and Papamoschou et al. [103].

Jet Mach number profiles and the mean flow visualization image for the DSR-FS-BL nozzle operating at TP4 are shown in Figure 4.3. The operating conditions are $M_{J,C} = 1.33$, $M_{J,BP} = 1.17$, so the core jet is operating at over-expanded conditions and the bypass jet is operating at supersonic, under-expanded conditions. Most noticeable in Figure 4.3a, is the disrupted, irregular shock cell pattern in the jet. Weaker shock waves are seen to exist downstream to about $5.5D_{Eq}$ but the jet does not exhibit the 'shock diamond' pattern like that observed in Figure 4.2a.

Similar to the data shown in Figure 4.2, the data shown in Figure 4.3, also shown that the DSR-FS-BL TP4 jet deflects downward by approximately $3^\circ$. Compared with the TP2 condition, the primary potential core has slightly shortened by $0.5D_{Eq}$, and extends downstream to $X/D_{Eq} \approx 4$. Visually comparing the half-velocity positions (dotted black lines), it is seen that jets operating at the TP2 and TP4 conditions have similar outward spreading rates. Also in comparison to the TP2 jet, for $X/D_{Eq} \geq 3$, the lower-velocity bypass jet is no longer evident in the axial Mach number profiles. It appears then, that for
the DSR-FS-BL nozzle, the bypass jet has the effect of biasing the exhaust on a downward deflecting angle and then quickly becomes fully-mixed with the exhaust a short distance downstream. Although jet conditions can affect the shock cell structure of the jet, the qualitatively, the spreading rates of the jets appear similar.

Figure 4.3: Flow visualization images (top) and Mach number profiles (bottom) of the DSR-FS-BL nozzle operating at TP4: $M_{J,C} = 1.33, M_{J,BP} = 1.17$.

A hypothesis as to why the DSR-FS-BL jets is offered. Compressibility effects are known to reduce the growth rates of turbulent shear layers [182–184]. An increase in the convective Mach number of the large-scale turbulent structures has been found to have a stabilizing effect on shear layer growth [38, 182, 183]. At low convective Mach numbers, the most amplified instabilities are two-dimensional, or Kelvin-Helmholtz-like, structures [184–186]. A linear-stability analysis combined with supporting direct numerical simulation (DNS) results by Sandham and Reynolds [184] showed that as convective Mach number increases, the most-amplified disturbances are no longer the two-dimensional 'rollers' but rather obliquely-oriented instability waves that are less-efficient at mixing. Simultaneous hot-wires measurements Martens and McLaughlin [185] helped to confirm these findings.
The use of a secondary co-flow to enhance the mixing rates of supersonic exhaust jets has been explored in a number of other studies, including [103,123,152,181]. The "symmetric" model for convective Mach number [187] is

\[ M_{C,1} = M_{C,2} = \frac{U_1 - U_2}{c_1 + c_2} \]  

(4.2)

where \( U_1 \) and \( U_2 \) are the velocities of the fast and slow streams, respectively, and \( c_1 \) and \( c_2 \) are speeds of sounds in the respective streams. A supersonic, dual-stream jet has three shear layers: 1) between the faster jet, \( U_1 \), and ambient air, \( U_\infty \approx 0 \), 2) between the faster and slower jets, \( U_1 \) and \( U_2 \), respectively, and 3) between the slower jet, \( U_2 \), and ambient air, \( U_\infty \approx 0 \). From Equation (4.2), the convective Mach numbers in the three shears are:

\[ \frac{U_1 - U_2}{c_1 + c_2} < \frac{U_2}{c_2 + c_\infty} < \frac{U_1}{c_1 + c_\infty} \]

where the convective Mach number in the shear layer between the two jets is expected to be the lowest, and therefore have the highest turbulent mixing rates of the three shear layers. Therefore, it is conjectured that the enhanced mixing rate in the shear layer between the two streams, prior to the bypass jet becoming fully-mixed, imparts a bias on the direction of the core jet flow towards the direction of the bypass jet.

4.1.1.2 DSR-Sym-BL Nozzle

Jet Mach number profiles and flow visualization images for the DSR-Sym-BL nozzle operating at TP2 are shown in Figure 4.4. The operating conditions are \( M_{J,C} = 1.38, M_{J,BP} = 0.85 \), so the core jet is operating at over-expanded conditions while the bypass jet is subsonic. The presence of the two, thinner, lower-velocity bypass jets are not evident in the Mach number profiles shown in Figure 4.4b. The thinner bypass jets mix more rapidly with the core jet compared with the thicker bypass jet exhausting from the DSR-FS-BL nozzle. Papamoschou [140] similarly found that the thickness of the bypass jet is proportional to the distance required to fully-mixed with the core jet. But, more spatially resolved data, such particle image velocimetry (PIV) measurements, are required to resolve the thin bypass jets near the nozzle exit plane.

Shock waves, observed in Figure 4.4, are seen to exist in the jet to \( 5 < D_{Eq} < 5.5 \). The axial extent of the shock cells is less than that observed for the DSR-FS-BL nozzle operating with same jet conditions (see Figure 4.2a). The jet also shows a minimal deflection \(< 1^\circ\). Because the bypass jets exhaust symmetrically on either side of the core jet, the mixed
jet is not expected to have any deflection. The slight bias in jet angle is likely caused by two reasons: 1) minor imperfections in the nozzle due to the rapid-prototyping process manifesting as small flow non-uniformities in the jet and 2) slight misalignment of the Pitot probe traversing system with respect to the model.

Figure 4.4: Flow visualization images (top) and Mach number profiles (bottom) of the DSR-Sym-BL nozzle operating at TP2: \( M_{J,C} = 1.38, M_{J,BP} = 0.85 \).

The primary potential core, outlined by the unmarked, solid black lines in Figure 4.4b extends slightly more than \( 3D_{Eq} \) downstream of the nozzle exit. The potential core length is about \( 1D_{Eq} \) shorter than the potential core of the DSR-FS-BL TP2 jet. The shortened potential core of the TP2 jet exhausting from the DSR-Sym-BL nozzle marked by a higher jet spread rate as well. At \( 10D_{Eq} \) downstream, the half-velocity points (dotted black lines) are about \( 1.1D_{Eq} \) away from the jet centroid line. The half-velocity points of the TP2 jet exhausting from the DSR-FS-BL nozzle (see Figure 4.2b) are about \( 0.8D_{Eq} \) away from the jet centroid line. Comparing the 2 jets then, the jet exhausting from the DSR-Sym-BL nozzle spreads outward 40% farther from the jet centroid over \( 10D_{Eq} \) than the DSR-FS-BL TP2 jet.
Jet Mach number profiles and flow visualization images for the DSR-FS-BL nozzle operating at TP4 are shown in Figure 4.5. Jet operating conditions are $M_{JC} = 1.33$, $M_{JBP} = 1.17$. The jet has a similar shock cell structure and deflection angle as the DSR-Sym-BL TP2 jet in Figure 4.4. The primary potential core of the extends $4D_{Eq}$ to $5D_{Eq}$ downstream. Unlike the DSR-Sym-BL TP2 jet, the spread rate appear to be more similar to the DSR-FS-BL jets. Jet spread rate is discussed quantitatively in Section 4.1.2.

![Flow visualization images (top) and Mach number profiles (bottom) of the DSR-Sym-BL nozzle operating at TP4: $M_{JC} = 1.33$, $M_{JBP} = 1.17$.](image)

Interestingly, although the jet Mach numbers are the same as the DSR-FS-BL TP4 jet shown in Figure 4.3, the DSR-FS-BL TP4 jet does not have the same disrupted, irregular shock cell pattern. It appears that the positioning of the bypass jet(s) relative to the core jet, within the nozzles, can have a noticeable effect on the shock cell structure of the jet. This could be exploited for propulsion applications. The dual-stream nozzle could be configured in a way such that the position of the bypass jet weakens the shock waves, and hence lessen thrust losses due to imperfect expansion of the jet exhaust.
4.1.2 Jet Spread Rate

To more quantitatively assess the spreading rates of the dual-stream jets, the vorticity thickness is used to quantify the width of the shear layer on either side of the jet centroid line. This measure of shear layer thickness has been used for measurements of supersonic jets including Doty and McLaughlin [170], Veltin [3], Powers [1], and Akatsuka et al. [4]. The vorticity thickness is defined as

$$\delta_\omega(x_i) = \frac{U(x_i)_{\text{max}}}{|dU/dZ|}$$  \hspace{1cm} (4.3)

where $U(x_i)_{\text{max}}$ is the maximum calculated jet velocity at each axial position. The velocity derivative, $dU/dZ$, is approximated on either side of the centroid line using the central-difference method based "gradient" function in MATLAB. The vorticity thickness is calculated on either side of the jet centroid line, where the positions in the $+Z$ direction are considered above the line, and positions in the $-Z$ direction are considered below the line.

![Figure 4.6: Vorticity thickness of dual-stream jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles. Dotted lines run through the points above the jet centroid line. Solid lines are single-stream vorticity thickness data for an AR 2, $M_J = 1.36$, rectangular jet measured by Akatsuka et al. [4].](image)

The vorticity thicknesses, for both dual-stream nozzles operating at Test Points 2 and 4, are shown in Figure 4.6. Dotted lines run through the vorticity thicknesses calculated from velocity data above the centroid lines. Along with the dual-stream data, vorticity thickness data of a single $M_J = 1.36$ jet issuing from a rectangular, $AR= 2$, $M_D = 1.65$ nozzle, measured by Akatsuka et al. [4], is plotted as the unmarked solid lines. All jets have comparable vorticity thicknesses out to $X/D_{Eq} \approx 6$. Downstream of this point, the DSR-Sym-BL TP2 jet has a greater shear layer thickness, on either side of the centroid line, compared with the other jets. This agrees with the previous, qualitative, observation that the DSR-Sym-BL TP2 jet spreads more rapidly than the other jets. The other dual-stream
jets have similar shear layer growth rates as the single-stream rectangular jets out to \(X/D_{Eq} = 10\). The linear growth of the jet shear layers matches the expected trend for a turbulent shear flow \([181,188]\).

The general trends seen in Figure 4.6 are expected to hold for helium-air jets with some caveats. Vorticity thickness measurements of subsonic and supersonic (single-stream) helium-air jets by Doty and McLaughlin \([170]\) compared well with similar measurements of pure-air jets for the first jet diameters downstream. Farther downstream the spreading rate of helium-air jets was slightly decreased compared with the pure-air jets. A similar phenomenon was observed in measurements of heated jets reported by Seiner et al. \([189]\). Increasing the jet velocity, either through heating the jet or adding helium increases the convective Mach number of instability waves in the jet shear layer. An increase in the convective Mach numbers, \(M_C\), of turbulent structures has been found to have a stabilizing effect on shear layer growth \([38,182,183]\). So the growth of dual-stream jets with helium-air mixture core jets is not expected to be as rapid as the pure air jets.

### 4.1.3 Centroid Line Measurements

Jet centroid line Mach numbers are now presented. The centroid line Mach numbers for the DSR-FS-BL and DSR-Sym-BL nozzles operating at Test Point 2 are plotted in Figure 4.7a and the Mach numbers for Test Point 4 are shown in Figure 4.7b. Also included in both figures are the single-stream, centerline velocities of an \(AR = 2, M_J = 1.36\), rectangular jet measured by Akatsuka et al. \([4]\). Single-stream jet data are the same in both figures. The extent of the supersonic cores of the dual-streams are marked in the figures by the dashed vertical lines.

DSR-FS-BL TP2 jet and the single-stream jet have similar potential core lengths and Mach number decay rates (slopes) beyond the end of the potential core. The sonic core of the DSR-Sym-BL TP2 jet, which has the highest spread rate, is about 1.75 \(D_{Eq}\) shorter than the sonic cores of the DSR-FS-BL TP2 and single-stream jets. Although the spread rate of the DSR-Sym-BL TP2 jet is greatest, its sonic core length is no shorter than the same nozzle operating at Test Point 4, shown in Figure 4.7b.

From the Test Point 4 data in Figure 4.7b, it is seen that the single-stream jet has a longer sonic core length compared with the dual-stream nozzles. The sonic core of the single-stream jet extends downstream to \(X/D_{Eq} = 7.5\), or about 1.5-1.75 \(D_{Eq}\) farther downstream than the sonic cores for the DSR-Sym-BL and DSR-FS-BL jets, respectively.
Downstream of their respective potential cores, the Mach numbers of the single and dual-stream jets decay at virtually the same rate.

![Graph showing Mach numbers vs. X/D for Test Points 2 and 4](image)

(a) Test Point 2: $M_{J,C} = 1.38, M_{J,BP} = 0.85$

(b) Test Point 4: $M_{J,C} = 1.33, M_{J,BP} = 1.17$

Figure 4.7: Comparison of centroid line Mach numbers for jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles. Also included are the centerline single-stream centerline velocities for an AR 2, $M_J = 1.36$, rectangular jet measured by Akatsuka et al. [4].

Both the single and dual-stream jets have comparable shock-cell spacings. The larger Mach number fluctuations of the Test Point 2 jets are indicative of stronger shock waves in the supersonic core compared with the Test Point 4 jets. The disrupted shock cell pattern of the DSR-FS-BL TP4 jet observed in Figure 4.3a translates to minimal Mach number fluctuations within the supersonic core of the jet.

The are five to six distinct shock cells within the potential cores of all jets before the centroid line Mach numbers begin to linearly decay with downstream position. The shock cell spacing, $X_S$, is marked in Figures 4.7a and 4.7b. The shocks are spaced approximately $0.625 D_{Eq}$ for all jets, with the DSR-FS-BL TP2 jet being an exception due to the irregular
shock structure. Because the core jet geometry is unchanged between the two nozzles, and because the core jet Mach number varies little between Test Points 2 and 4, the core jet conditions are thought to govern the shock cell structure of the dual-stream jet exhaust. The shock cell pattern of rectangular jets is dependent on the height (minor axis dimension) of the nozzle [79]. The non-dimensional shock cell spacing is noted in Figures 4.7a and 4.7b. The reference lengths for the spacing are the dual stream nozzle exit height, \( H_E = 1.73 \) cm, the core nozzle height, \( H_C = 0.89 \) cm.

The estimated shock cell spacings are now compared with a model for the shock cell spacing in single-stream, rectangular jet. Using a 2-dimensional assumption for the jet growth, where the jet width (major axis dimension) is taken to be invariant, and equating the mass flow rate at the nozzle exit plane to a position downstream where the static pressure has isentropically equated to atmospheric pressure, Veltin and McLaughlin [79] provided an expression for the fully-expanded rectangular jet height, \( H_{C,J} \):

\[
\frac{H_{C,J}}{H_C} = \frac{1 + \left(\frac{\gamma - 1}{2}\right) M_{J,C}^2}{1 + \left(\frac{\gamma - 1}{2}\right) M_{D,C}^2} \left( \frac{M_{D,C}}{M_{J,C}} \right)^{\frac{\gamma + 1}{\gamma - 1}} \tag{4.4}
\]

Tam [190] proposed a vortex-sheet model to estimate the shock cell spacing and screech frequency of single-stream rectangular and asymmetric (elliptic) supersonic jets. Morris et al. [191] used the boundary element method to extend the work of Tam [190] to jets exhausting from nozzles with arbitrary shapes. The success these models is predicated on the assumptions used to approximate the fully-expanded jet geometry. For single-stream rectangular jets, Tam [190] made use of the the 2-dimensional approximation for jet growth given by Equation (4.4). The models of Tam [190] and Morris et al. [191] have favorable agreement with each other and with Powell’s [192] experimental data. Tam’s [190] expression for the shock cell spacing of a single-stream, rectangular jet is

\[
X_S = \frac{2 \left( \frac{H_{C,J}}{H_C} \right) \sqrt{M_{J,C}^2 - 1}}{1 + \left( \frac{1}{AR} \right)^2 \left( \frac{H_{C,J}}{H_C} \right)^2} \tag{4.5}
\]

where \( H_{C,J}/H_C \) is calculated using Equation (4.4) and \( H_C \) is the height of the core nozzle, which is 1.15 cm for the nozzles depicted in Figure 4.1. The core nozzles have a design Mach number of \( M_{D,C} = 1.65 \) and an aspect ratio \( (AR = W/H_C) \) of 3.0.

Table 4.2 compares the predicted shock spacings calculated using Equation (4.5) with the shock cell lengths determined from the Pitot pressure measurements shown in Figure 4.7.
Predictions for the dual-stream jets were made using the dimensions and operating conditions of the core nozzle in the dual-stream model. The predictions based on a vortex-sheet model of a single-stream jet show reasonable agreement with the measured shock cell spacings of the single and dual-stream rectangular jets. Flow-field measurements with improved spatial resolution (e.g. PIV) over a wider-range of dual-stream jet operating conditions would be better suited for assessing how accurately single-stream model predicts the shock spacing of dual-stream jets, although the initial comparisons are encouraging.

Table 4.2: Comparison of measured single and dual-stream jet shock cell spacing with single-stream jet model.

<table>
<thead>
<tr>
<th>$M_J$</th>
<th>$X_S/H_C$ (meas)</th>
<th>$X_S/H_C$ (Tam [190])</th>
<th>Error (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.33</td>
<td>1.50</td>
<td>1.41</td>
<td>5.9</td>
</tr>
<tr>
<td>1.36</td>
<td>1.50</td>
<td>1.44</td>
<td>4.2</td>
</tr>
<tr>
<td>1.38</td>
<td>1.50</td>
<td>1.56</td>
<td>4.4</td>
</tr>
</tbody>
</table>

4.1.4 Self-Similar Velocity Profiles

The Mach number profiles for jets exhausting from both dual-stream nozzles are now compared directly with one another. The Mach number profiles for the DSR-FS-BL and DSR-Sym-BL nozzles operating at Test Point 2 are shown in Figure 4.8a, and similarly for Test Point 4 in Figure 4.8b. To make the comparisons the Z coordinate was zeroed to the position of the centroid line to account for jet deflections. Measurements near $X/D_{Eq} = 5$ and 6 were performed at different positions for the DSR-FS-BL and DSR-Sym-BL jet surveys, so the offsets should not necessarily be interpreted as velocity differences.

The Mach number profiles, in general, show good agreement with each other, in terms of peak velocity and shape. There are expected differences in velocity profile shape for $X/D_{Eq} < 3$ because the DSR-FS-BL jet is not fully-mixed. For Test Point 2, shown in Figure 4.8a, the Mach number profiles have similar peak values downstream to $X/D_{Eq} \approx 5$. Farther downstream, the DSR-FS-BL TP2 jet has higher peak Mach numbers compared with the DSR-Sym-BL TP2 jet. Initially, the DSR-FS-BL TP2 jet spreads more rapidly than the DSR-Sym-BL TP2 jet, as evidenced by the wider Mach number profiles out to $X/D_{Eq} = 4$. However, this trend not continued downstream. As seen by the greater vorticity thickness downstream of $X/D_{Eq} \geq 7$ in Figure 4.6, the DSR-Sym-BL TP2 grows at a faster rate.
The aligned Mach number profiles of the jets operating at Test Point 4 show comparable peak Mach numbers and profile shape over the axial extent of the measurements. Qualitatively, the Mach number profiles have a similar shape as their Test Point 2 profiles. Their similarity is explored further by examining their normalized velocity profiles. To normalize the velocity profiles, the jet velocities measured at each axial station, $X = x_i$, are normalized by the maximum value, $U(x_i)_{\text{max}}$, at that position. The non-dimensional cross-stream coordinate, $\eta$, is defined in Equation (4.6).

$$\eta = \frac{Z - Z_{0.5}}{\delta_\omega} \quad (4.6)$$

$Z_{0.5}$ is the cross-stream position where $U(x_i) = 0.5U(x_i)_{\text{max}}$ and $\delta_\omega$ is the vorticity thickness previously defined in Equation (4.3). Similar normalization methods have been shown
to collapse the velocity profiles of moderate Reynolds number jets [27], elliptic jets [193], military-style jets [1,3], and rectangular jets [3].

The normalized velocity profiles for the DSR-FS-BL and DSR-Sym-BL nozzles operating at Test Point 2 are shown in Figure 4.9. The left column shows the normalized profiles for measurements below the jet centroid lines and the right column shows the normalized profiles for measurements above the centroid line. Data are colored based on axial position ranging from blue at the nozzle exit plane and transitioning to red at $X/D_{Eq} = 10$. By and large, the data collapse to a common curve at most axial positions on both sides of the jet thus providing confidence in the data quality. The collapse of the data supports the methodology for calculating jet velocity using Equation (3.12). If the estimates of jet velocity were inaccurate, the data would not collapse to the normalized profiles.

![Normalized velocity profiles for DSR-FS-BL (top) and DSR-Sym-BL (bottom) nozzles operating at Test Point 2: $M_{J,C} = 1.38, M_{J,BP} = 0.85$.](image)

In Figure 4.9, there noticeable scatter in the data towards the low-speed side of the shear layers, $U/U(x_i)_{max} < 0.4$. At each measurement position, 1000 sample points were collected to minimize random errors associated with transducer sensitivity (see Section 3.7.3.1 for further discussion). The loss of similarity below $U/U(x_i)_{max} = 0.4$ is
likely due the additional bypass stream near the nozzle exit and flow entrainment farther downstream.

Attention is drawn towards the below-centroid line normalized profiles for the DSR-FS-BL nozzle shown Figure 4.9a. The fluid shield bypass jet exhausts below the jet centroid line. Near the nozzle exit plane, the normalized profiles exhibit a failure to collapse with the downstream profiles due to the velocity deficit caused by the lower-speed bypass jet. Above the jet centroid line, Figure 4.10b, there is favorable agreement of the normalized profiles at all axial positions. Normalized velocity profiles for the DSR-Sym-BL TP2 jet are shown in the bottom row of Figure 4.9. The bypass jets exhaust on either side of the jet centroid line. But the lower-speed bypass jets cause minimal deviations in the shape of the normalized profiles.

The normalized velocity profiles for the DSR-FS-BL and DSR-Sym-BL nozzles operating at Test Point 4 are shown in Figure 4.10. Profiles for the DSR-FS-BL jets are shown in the top row and DSR-Sym-BL jet profiles are shown in the bottom row. Similar to the TP2 jet profiles shown in Figure 4.9, there is a good collapse of the data for $X/D_{Eq} \geq 3$. Nearer
the nozzle exits some discrepancies are observed in the normalized profiles. Figure 4.10a shows the profiles for the DSR-FS-BL TP4 jet below the jet centroid line. The scatter in the normalized profiles for $X/D_{Eq} \leq 3$ is due to the velocity deficit of the bypass jet. Above the jet centroid line the DSR-FS-BL TP4 jet profiles at all axial positions collapse well.

The normalized profiles for the DSR-Sym-BL TP4 jet are shown in the bottom row of Figure 4.10. Unlike the TP2 condition, the increased Mach number of the bypass jet now results in the data acquired at $X/D_{Eq} \leq 1.5$ to deviate from the normalized profile shape. In general, once the bypass jet fully mixes with the core jet within the first few jet diameters, the dual-stream jet velocity profiles collapse to a reasonably-consistent self-similar shape.

### 4.2 Cross-Stream Survey of Jets

A series of cross-stream Pitot probe measurements were acquired to gain better insight into the three-dimensional nature of the dual-stream jets. Whereas the stream-wise measurements previously presented give insight into the downstream evolution of the jets in the minor axis ($Z$-axis) direction, the cross-stream measurements allow for better understanding of how the jets grow in the major axis ($Y$-axis) direction. This has important implications for assessing the quality of the flow exhausting from the small-scale nozzles, estimating the accuracy of jet performance estimates, and relating flow field characteristics to changes in the far-field noise. To this end, a series of cross-stream Pitot pressure measurements were acquired in the $+YZ$ half-plane, assuming lateral symmetry of the jet flow, at axial positions of $X/D_{Eq} = 0$ and 4. A diagram of the cross-stream survey, in front of the DSR-FS-BL nozzle, is shown in Figure 4.11. Nozzle exit plane estimates are used to assess the flow quality and accuracy of jet operating condition estimates. The downstream measurements acquired at $X/D_{Eq} = 4$ were designed to be at, or near the end of the potential cores of the jets.
4.2.1 Nozzle Exit Plane Survey

Cross-stream Mach number contours measured at \( X/D_{Eq} = 0 \) are shown in Figure 4.12. Data are shown for the DSR-FS-BL nozzle operating at Test Points 2 and 4 in Figures 4.12a and 4.12b, respectively. Both are higher-speed core jet and lower-speed bypass jet are distinctly resolved in the contours. The black dots indicate the position of Pitot probe measurements. The Mach number contours of both the core and bypass jets each appear relatively uniform. This gives confidence that the flow exiting both the core and bypass nozzles is uniform and devoid of any significant flow abnormalities. There are however some discrepancies. Mach numbers near the 'edges' of the contours, specifically near \((Z/D_{Eq}, Y/D_{Eq}) = (0.1, 0.55)\), are lower than the anticipated values. The lateral extent of the jets in the \( Y \)-direction falls short of the expected value of \( Y/D_{Eq} = 0.63 \). Both effects are at least partially attributed to interpolation error due the coarse spacing of the measurement locations. As an example, the lateral extent of the jet was measured to extend only out to \( Y/D_{Eq} = 0.55 \) but the next closest measurement positions are at \( Y/D_{Eq} = 0.70 \), which is beyond the expected edge of the jet. As well, measured Mach numbers of the core jet are lower than the expected values reported in the captions of each figure. This error is likely due to 2 main reason. The first reason is the imperfect assumption that the static pressure is equal to atmospheric pressure. This knowingly underestimates the Mach numbers for over-expanded jets, as discussed in Section 3.7.3.2. The second reason, closely tied to the first, is that, inevitably, there are oblique shock waves and expansion fans within the secondary diverging section of the dual-stream nozzles which lead to variations in the flow Mach number.

Mach number contours measured at \( X/D_{Eq} = 0 \) for the DSR-Sym-BL TP2 and TP4 jets are shown in Figures 4.12c and 4.12d, respectively. The Mach number contours of the core jets are relatively uniform across the span of the jet. The thin bypass jet is not well-resolved due to the coarse spacing of the exit plane measurements. Its presence is
somewhat evident in the lower Mach numbers measured above and below the core jet, most notably in Figure 4.12d. Future work would benefit from nozzle exit plane surveys with improved spatial resolution.

Figure 4.12: Cross-stream Mach number contours measured at $X/D_{Eq} = 0$. Top: DSR-FS-BL nozzle, Bottom: DSR-Sym-BL nozzle. Black dots indicate position of Pitot probe measurements.

Using the nozzle exit plane measurements, an attempt is now made to assess the accuracy of the estimated thrust and mass flow rate values reported in Table 4.1. Starting with
mass flow rate, the 1-dimensional mass flow rate equation can be written as

\[ \dot{m} = \rho U A = P \sqrt{\frac{\gamma}{RT_0}} \left( \frac{T_0}{T} \right)^{\frac{1}{2}} MA \]  

with the Mach number, \( M \), is calculated from Pitot pressure measurements. The static pressure, \( P \) is assumed to be equal to ambient pressure, \( P_\infty \), as discussed in Section 3.6.1. The total temperature is taken to be ambient temperature, \( T_\infty \), for unheated, pure-air jets. The temperature ratio, \( T/T_0 \), can be calculated from isentropic relations. Making these substitutions into Equation (4.7), and converting Equation (4.7) to a double integral over the measurement area, gives the following approximation for the jet mass flow rate at the nozzle exit plane

\[ \dot{m} = 2P_\infty \sqrt{\frac{\gamma}{RT_\infty}} \int_{Y_{\min}}^{Y_{\max}} \int_{Z_{\min}}^{Z_{\max}} \left( M \left[ 1 + \left( \frac{\gamma - 1}{2} \right) M^2 \right]^{\frac{1}{2}} \right) dZdY \]  

where the factor of 2 in Equation (4.8) assumes symmetric flow about the Z-axis and accounts for the fact that Pitot measurements were acquired over only half of the nozzle area.

A similar approach is taken to estimate jet thrust from the Pitot pressure measurements. The 1-dimensional thrust equation can be expressed as

\[ T = \dot{m}U + (P - P_\infty) A = \gamma PM^2 A + (P - P_\infty) A \]  

where, again, the static pressure is assumed to be equal to ambient pressure and constant. Under this assumption, the \( (P - P_\infty) A \) term on the right hand side of Equation (4.9) is zero. Making these substitutions, and converting Equation (4.9) to a double integral over the measurement area, gives the following approximation for the jet thrust at the nozzle exit plane

\[ T = 2P_\infty \gamma \int_{Y_{\min}}^{Y_{\max}} \int_{Z_{\min}}^{Z_{\max}} (M^2) dZdY \]  

where the factor of 2 in Equation (4.10) arises because Pitot measurements were only acquired over half of the nozzle area. The integrals in Equations (4.8) and (4.10) were numerically evaluated using the trapezoid integration function, "trapz" in MATLAB. Nozzle exit plane measurements were integrated over all of the measurement points shown in Figure 4.12. Measurements outside of the jet flow do not contribute to the thrust and mass flow rate estimates because the integrands evaluate to zero.
Table 4.3 lists the mass flow rate and thrust estimates calculated from Equations (4.8) and (4.10) using the Mach number data presented in Figure 4.12. Alongside the experimentally-estimated values are the theoretical estimates calculated from Equations (2.3) and (2.8). The theoretical thrust estimates include an 8% loss due to oblique shocks with in the nozzle (see Section 2.5.2). There is very favorable agreement between the mass flow rate values calculated from the experimental data and the estimated values. The agreement is encouraging, in that it shows that pressure losses in the dual-stream nozzle are not significant. However, it would be worthwhile in future measurements to compare theoretical estimates with more spatially-resolved pressure measurements acquired at the nozzle exit plane.

Table 4.3: Comparison of estimated jet mass flow rate and thrust levels from Pitot pressure measurements and theoretical values.

<table>
<thead>
<tr>
<th>Nozzle ID</th>
<th>Test Point (TP)</th>
<th>Mass Flow Rate (kg/s)</th>
<th>Thrust (N)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Integral Theory</td>
<td>Integral Theory</td>
</tr>
<tr>
<td>DSR-FS-BL</td>
<td>2</td>
<td>0.26</td>
<td>0.26</td>
</tr>
<tr>
<td></td>
<td>4</td>
<td>0.27</td>
<td>0.27</td>
</tr>
<tr>
<td>DSR-Sym-BL</td>
<td>2</td>
<td>0.27</td>
<td>0.26</td>
</tr>
<tr>
<td></td>
<td>4</td>
<td>0.27</td>
<td>0.27</td>
</tr>
</tbody>
</table>

The experimentally estimated thrust values agree to within 6% of the theoretical estimates. These results validate the methods, discussed in Section 2.5, used to calculate net thrust. In conjunction with more spatially-resolved pressure measurements acquired at the nozzle exit plane, experimentally-determined estimates of thrust would be improved by instrumenting the nozzles with pressure taps to measure the static pressure distribution on the nozzle walls. Further validation of the calculate thrust values could be obtained by comparing experimental estimates with estimates from RANS simulations.

### 4.2.2 Downstream Survey

Cross-stream Mach number contours measured at $X/D_{eq} = 4$ are shown in Figure 4.13. By this axial position, the stream-wise measurements indicate that the bypass jet has fully merged with the core jet. As well, from the stream-wise data presented in Section 4.1, this position is expected to be near or beyond the end of the primary potential core of the jets. Mach number data for the DSR-FS-BL nozzle are shown in the top row of Figure 4.13 while Mach number contours for the DSR-Sym-BL nozzle are presented in the bottom row.
Qualitatively comparing the shapes of the jets exhausting from both nozzles, the DSR-FS-BL jets appear more rectangular at $X/D_{Eq} = 4$ whereas the DSR-Sym-BL jets appear more oblong or elliptic. The differences are likely due to the position of the bypass jet(s). With the bypass jets exhausting on either side of the core jet in the DSR-Sym-BL nozzle, they may 'squeeze' the core jet outward in the $Y$-direction giving the mixed jet its more oblong shape. Comparing the jets operating at different Test Points, the TP4 jets (right column in Figure 4.13) spread out farther and thus have a wider shear layers in the $Y$-direction compared with their Test Point 2 counterparts.

In all contours shown, the higher velocity core flow extends noticeably farther in the $Y$-direction compared with the $Z$-direction. This shows that, at least downstream to $X/D_{Eq} = 4$, the dual-stream jets do not undergo axis-switching. Pitot probe measurements of single-stream rectangular jets reported by Akatsuka et al. [4] showed evidence of axis-switching within the the first six equivalent diameters downstream of the nozzle exit. Heeb et al. [73] note that the occurrence of axis switching in low-aspect ratio jets is not consistent. While Veltin and McLaughlin [79] observed this phenomenon for an AR=1.75 nozzle, it was not observed by Gutmark et al. [194] for an AR= 3 nozzle. Both, Gutmark et al. [194] and Zaman [195] note that this phenomenon is dependent on boundary layer conditions within the nozzle. Gutmark et al. [194] also notes that the axis switching phenomenon is characterized by an increase in the jet spread rate. Although the DSR-Sym-BL TP2 jet shown in Figure 4.13c does not show evidence of axis-switching, the vorticity thickness of this jet, shown in Figure 4.6, increases at a greater rate than the other jets for $X/D_{Eq} \geq 6$. It is therefore hypothesized that the DSR-Sym-BL TP2 jet begins to undergo axis-switching at position between $6 < X/D_{Eq} < 7$. Future cross-stream measurements of these jets at farther downstream positions are warranted to confirm this hypothesis.

Papamoschou et al. [103] considered an area-based measure of dual-stream rectangular jet growth rates. Traditional measures of round jet shear layer thickness include vorticity thickness, Equation (4.3), and "half-velocity" thickness, $U = 0.5U_{max}$, but such measures may not always be appropriate for rectangular jets which are (obviously) not round, and may grow at different rates along their major and minor axes. Instead, Papamoschou et al. [103] proposed an area-based thickness, defined as $\delta_A = \sqrt{4A_{0.5}/\pi}$, where $A_{0.5}$ is the cross-sectional area of the jet bounded by $U = 0.5U_{max}$. They extended this concept to consider $A(U)$, that is, the cross-sectional area of the jet enclosed by isocontours of $U$. Theoretically, the area $A(U)$ ranges from 0 at the jet centerline where the velocity is maximum to and approaches $\infty$ as $U \to 0$ outside of the jet [196].
Figure 4.13: Cross-stream Mach number contours measured at $X/DEq = 4$. Top: DSR-FS-BL nozzle, Bottom: DSR-Sym-BL nozzle. Black dots indicate position of Pitot probe measurements.

This concept of velocity-bounded area is applied to the dual-stream jet data in Figure 4.13. The variation in velocity-bounded areas (non-dimensionalized by the nozzle exit area $A_E$)
as functions of velocity and Mach number are shown in Figure 4.14. Areas were calculated from the $Y, Z$ coordinates of the velocity isocountours in MATLAB. Areas are only plotted for isocontours that remained completely within the measurement box. No corrections were made to account for the fact that measurements were only acquired in the half-plane of the jet.

The relationship between axial velocity and the area enclosed by the isocontours of velocity are shown in Figure 4.14a. The velocity axis (abscissa) is logarithmic and velocity is normalized by the maximum value calculate from the Mach number contours in Figure 4.13. The trends agree with those observed by Papamoschou et al. [103]. As the velocity approaches the maximum value, the area bounded by that velocity tends toward zero. Moreover, at lower velocities, the relationship between area and $\ln(U/U_{\text{max}})$ is linear. This agrees with the Papamoschou et al.’s [103] hypothesis that the bounding velocity decreases exponentially with increasing area.

Interestingly, the TP2 2 and TP2 4 jets align themselves onto distinct curves with different slopes. The areas bounded by velocity of the TP4 jets (blue curves in Figure 4.14a) increase with decreasing velocity at a faster rate than the Test Point 2 jets (red curves). A
greater change in the bounded area with velocity implies a smaller velocity gradient in the shear layer. This effect is explored further in Figure 4.14b where the velocity-bounded area is plotted against Mach number normalized by the design Mach number of the core nozzle, $M_{D,C} = 1.65$. For each Test Point, the curves for the DSR-FS-BL and DSR-Sym-BL jets collapse on top of each other. It is seen that the TP44 jets have a lesser velocity gradient through their shear layers compared with the TP2 jets.
Acoustic Measurements of Rectangular, Dual-Stream Jets

The chapter reports on the results from a series of far-field acoustic measurements of low-$BPR$, dual-stream jets. The study first aims to gain a fundamental insight into how the individual jet operating conditions, thrust, bypass ratio, and specific thrust. The second goal to better understand how nozzle design and configuration affect the far-field noise. The final goal of this chapter is to show that dual-stream, rectangular jets can offer an acoustic benefit over equivalent single-stream round and rectangular jets. A majority of the focus is placed on heat-simulated jets as the data are more relevant to practical applications. Results from flow-field measurements and flow visualizations, discussed in Chapter 4, will be used to assist in the interpretation of acoustic data.

The first half of the chapter examines the effects of individual jet operating conditions, including core jet $NPR$, (simulated) core jet $TTR$, core jet velocity, and bypass jet $NPR$ on the noise radiated from jets exhausting from a dual-stream nozzle with a single fluid shield. The second half of the chapter focuses on acoustic measurements of dual-stream jets operating at approximately constant estimated thrust and constant mass flow rate conditions. The effects of bypass ratio and dual-stream nozzle configuration on far-field noise are explored. Bypass ratio effects are considered for both the single fluid shield (DSR-FS-BL) nozzle and nozzle with two symmetric fluid shields (DSR-Sym-BL), both shown in Figure 5.1. Comparisons are made with single-stream jets, operating at equivalent conditions, to show that dual-stream, rectangular jets can offer a noise reduction benefit over their single-stream counterparts. Lastly, results are then reported for dual-stream nozzles with the addition of an aft deck to generically simulate nozzles integrated with an aircraft fuselage.
Figure 5.1: Summary of baseline dual-stream, rectangular nozzles and coordinate system.

Figure 5.1 provides a summary of the baseline (BL) nozzles and spherical coordinate system. Complete details of the nozzles are provided in Section 2.3.1. As they are shown, the major axis direction of the nozzles is into the plane of the page. The $\phi = 0^\circ$ direction is aligned with the $Y$-axis, and directed along the major axis of the nozzles. Acoustic data is presented for far-field measurements acquired in the $\phi = 0^\circ$ and $\phi = 270^\circ$ azimuthal directions. Diagrams of the nozzle orientation relative to a far-field microphone, or both azimuthal orientations, are shown in Figure 5.2.

(a) $\phi = 0^\circ$ azimuthal direction.

(b) $\phi = 270^\circ$ azimuthal direction

Figure 5.2: Dual-stream nozzle orientations relative to a far-field microphone for acoustic measurements in the $\phi = 0^\circ$ and $\phi = 270^\circ$ azimuthal directions. Diagrams show a head-on view of DSR-FS-BL nozzle.
5.1 Effects of Jet Operating Conditions on Far-Field Noise

Before comparing the effects of thrust and bypass ratio on dual-stream rectangular jet noise, in which both core and bypass jet operating conditions simultaneously change, it is pertinent to develop an understanding of how the individual jet operating conditions independently affect the radiated noise. The three jet operating conditions that are able to be independently controlled in the Penn State facility are core jet NPR, core jet (He-air simulated) total temperature ratio, and bypass jet NPR. This section discusses how variations to each of these parameters affects the far-field noise. To this end, a series of experiments were conducted with the DSR-FS-BL nozzle in which each quantity was independently changed while the other two were held constant.

5.1.1 Effect of Core Jet Nozzle Pressure Ratio

The changes to the noise radiated from dual-stream rectangular jets due to changes in the core jet nozzle pressure ratio are now discussed. The far-field noise was measured in the \( \phi = 0^\circ \) azimuthal direction only. In order to isolate the effects of core jet pressure ratio, the core jet was operated unheated, \( TTR_C = 1.0 \), and the bypass jet was held at a fixed pressure ratio of \( NPR_{BP} = 2.5 \). \( NPR_C \) was varied from 2.5 to 3.5. The jet operating conditions, along with the net thrust and mass flow rates, are listed in Table 5.1. Note that both net thrust and net mass flow rate increase as \( NPR_C \) increases. Also listed in the table are the BPR, velocity ratio, thrust ratio \( (U_{J,BP}/U_{J,C}) \), and characteristic frequency, \( f_C = U_{J,C}/D_{J,C} \), of the core jet.

Table 5.1: DSR-FS-BL jet operating conditions for \( NPR_C \) variation

<table>
<thead>
<tr>
<th>Operating Conditions</th>
<th>( T_{net} ) (N)</th>
<th>( m_{net} ) (kg/s)</th>
<th>BPR</th>
<th>( U_{J,BP}/U_{J,C} )</th>
<th>( T_{BP}/T_C )</th>
<th>( f_C ) (kHz)</th>
</tr>
</thead>
<tbody>
<tr>
<td>( NPR_C )</td>
<td>( NPR_{BP} )</td>
<td>( TTR_C )</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.50</td>
<td>2.50</td>
<td>1.00</td>
<td>83</td>
<td>0.25</td>
<td>0.42</td>
<td>1.00</td>
</tr>
<tr>
<td>3.00</td>
<td>2.50</td>
<td>1.00</td>
<td>102</td>
<td>0.29</td>
<td>0.35</td>
<td>0.92</td>
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<tr>
<td>3.50</td>
<td>2.50</td>
<td>1.00</td>
<td>121</td>
<td>0.32</td>
<td>0.30</td>
<td>0.87</td>
</tr>
</tbody>
</table>

An acoustic summary of the core nozzle pressure effects on the far-field noise is presented in Figure 5.3. Nozzle operating conditions are listed in Figures 5.3a and 5.3d. Spectra are shown for three polar angles: the downstream direction, \( \theta = 45^\circ \), to the side of the nozzle,
\( \theta = 90^\circ \), and in the upstream direction, \( \theta = 120^\circ \). Spectra were non-dimensionalized by the characteristic frequency based on core jet operating conditions. The difference in the non-dimensionalizing factor, \( 10 \log_{10}(f_c) \), between the \( NPR_C = 2.5 \) and \( NPR_C = 3.5 \) cases is 0.36 dB.

Figure 5.3: Acoustic summary of the effects of core jet nozzle pressure ratio on the far-field noise, in the \( \phi = 0^\circ \) azimuthal direction. Core jet NPR varied while core jet \( TTR \) and bypass jet \( NPR \) were held constant.

At \( \theta = 45^\circ \), there is an increase in the spectra amplitude across all frequencies. The peak frequency remains near \( St = 0.2 \), regardless of core jet Mach number (\( M_{J,C} \)). This trend is consistent with acoustic measurements of single-stream round jets [114]. At the sideline and upstream polar angles, an increase in the radiated noise across most frequencies is observed. The \( BBSAN \) peak shifts to lower frequencies as \( M_{J,C} \) increases. At higher core jet Mach numbers, a screech tone is present in the spectra at all polar angles. Dual-stream jet directivities are plotted in Figure 5.3d. Overall sound pressure levels were calculated from spectra "as-is" including any screech tones that might be present. Most notably
the peak noise levels increase by as much as 7 dB OASPL with increasing $M_{J,C}$. For the $NPR_C = 2.5$ and 3.0 cases, which are the most over-expanded conditions considered, the relative strength of the BBSAN over the fine-scale noise, causes the overall sound pressure levels to increase for $\theta \geq 80^\circ$.

### 5.1.2 Effect of Core Jet Total Temperature Ratio

This section explores the effect of core jet total temperature ratio, $TTR_C$, on the noise radiated from dual-stream rectangular jets. To isolate core total temperature ratio effects, the core nozzle pressure ratio was held constant at $NPR_C = 2.72$, and the bypass jet nozzle pressure ratio was held constant at $NPR_{BP} = 3.83$. The core jet simulated total temperature ratio was varied from 1.0 to 3.0. The jet operating conditions are listed in Table 5.2, along with the net thrust and mass flow rates, $BPR$, velocity ratio, thrust ratio, and core jet $f_C$. Spectra were made non-dimensional using the characteristic frequency of the core jet. The difference between non-dimensionalizing factor, $10 \log_{10}(f_c)$, for $TTR_C = 1.00$ and $TTR_C = 3.00$, is 3 dB. Between $1.60 \leq TTR_C \leq 3.00$, the change in net thrust is less than 4%.

<table>
<thead>
<tr>
<th>Operating Conditions</th>
<th>$T_{net}$ (N)</th>
<th>$\dot{m}_{net}$ (kg/s)</th>
<th>$BPR$</th>
<th>$U_{J,BP}/U_{J,C}$</th>
<th>$T_{BP}/T_{C}$</th>
<th>$f_C$ (kHz)</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.72 3.83 1.00</td>
<td>111</td>
<td>0.30</td>
<td>0.59</td>
<td>1.13</td>
<td>0.67</td>
<td>18.8</td>
</tr>
<tr>
<td>2.72 3.83 1.60</td>
<td>115</td>
<td>0.27</td>
<td>0.71</td>
<td>0.89</td>
<td>0.64</td>
<td>23.8</td>
</tr>
<tr>
<td>2.72 3.83 2.20</td>
<td>117</td>
<td>0.26</td>
<td>0.80</td>
<td>0.76</td>
<td>0.62</td>
<td>28.0</td>
</tr>
<tr>
<td>2.72 3.83 3.00</td>
<td>119</td>
<td>0.24</td>
<td>0.91</td>
<td>0.65</td>
<td>0.60</td>
<td>32.7</td>
</tr>
</tbody>
</table>

Far-field acoustic data are first presented for measurements in the $\phi = 0^\circ$ azimuthal direction. An acoustic summary of the effect of core jet total temperature ratio far-field noise is shown in Figure 5.4. Representative spectra at three polar angles are shown in the top row. Spectra for the $TTR_C = 1$ condition are only shown for the $\theta = 90^\circ$ and $120^\circ$ polar angles in Figures 5.4b and 5.4c.

From the OASPL’s plotted in Figure 5.4d, there is a substantial increase, over 6 dB OASPL, in the peak noise radiation direction as $TTR_C$ is increased. The most significant increase occurs between $TTR_C = 1$ and 1.6 as the (estimated) turbulence convection velocity becomes supersonic with respect to ambient conditions. At higher $TTR_C$’s, the
peak noise direction shifts upstream, and is consistent with an increase in convective Mach number. The effects of $TTR_C$ on the radiated noise become increasingly small at upstream polar angles beyond $\theta = 100^\circ$. At upstream angles, $BBSAN$ peak, shown in Figures 5.4b and 5.4c, varies little with $TTR_C$. This is most likely due to the core jet Mach number, $M_{J,C} = U_{J,C}/a_{J,C}$, being constant between tests. Similarly for single-stream jets, Kuo et al. [49] observed a saturation of the $BBSAN$ with increasing $TTR$.

![Graphs showing SPL vs St for different values of TTR and NPR]

Figure 5.4: Acoustic summary of the effects of simulated core jet total temperature ratio on the far-field, in the $\phi = 0^\circ$ azimuthal direction. Core jet $TTR$ varied while $NPR_C$ and $NPR_{BP}$ were held constant.

Figure 5.5 presents an acoustic summary of the effects of core jet total temperature ratio on the far-field noise in the $\phi = 270^\circ$ azimuthal direction. Between $1.6 \leq TTR_C \leq 2.2$ the radiated noise increases across all polar polar angles. Between $2.2 \leq TTR_C \leq 3.0$ there is less increase in the OASPL. There is also an upstream shift in peak emission direction with increasing core jet total temperature ratio. The $BBSAN$ peak, shown in
the spectra plotted in Figures 5.5b and 5.5c, does not vary with $TTR_C$ for the two highest temperature ratios considered.

Figure 5.5: Acoustic summary of the effects of simulated core jet total temperature ratio on the far-field, in the $\phi = 270^\circ$ azimuthal direction. Core jet $TTR$ varied while $NPR_C$ and $NPR_{BP}$ were held constant.

5.1.3 Variation in OASPL with Core Jet Velocity

Upon exploring the both the effects of core nozzle pressure ratio and core (simulated) total temperature ratio, it is now worthwhile to consider the variation in OASPL with core jet velocity, $U_{J,C}$. Experiments were conducted with DSR-FS-BL nozzle and this investigation limits its consideration to changes in OASPL in the $\phi = 0^\circ$ azimuthal direction only. The specific jet operating conditions used to study the effects of $U_{J,C}$ are listed in table 5.2. Net thrust and mass flow rates are given, as well as $U_{J,C}$, $\log_{10}(U_{J,C})$, and velocity ratio.
The right-most column lists the symbol (Sym) used for plotting the data in Figures 5.6 and 5.7.

The variations in OASPL at $\theta = 90^\circ$ and at $\theta = 45^\circ$ are shown in Figures 5.6 and 5.7, respectively. Core jet velocity was increased by either increasing the core nozzle pressure ratio or by increasing the simulated core total temperature ratio. The open symbols are used to represent core jet velocity increased through increases in $NPR_C$ while the core $TTR$ was kept at a constant 1.0 (pure air jets). Closed, square symbols denote cases where velocity was increased by increasing the simulated temperature ratio while $NPR_C$ was held constant at 2.72. The $\times$ symbols mark conditions where the core jet conditions were kept constant at $NPR_C = TTR_C = 3.0$, while $NPR_{BP}$ was increased from 2.0 to 3.5. The fully-expanded core jet velocity, $(U_{J,C})$ was calculated from isentropic relations. The jet velocity was non-dimensionalized by the approximate speed of sound in dry air, $c_{\infty} = 340$ m/s. Non-dimensional logarithm of velocity, $\log(U_{J,C}/c_{\infty})$, is plotted along the abscissa in Figures 5.6 and 5.7.

The variation in OASPL with velocity follow similar trends at both polar angles although the dominant noise sources are different. Noise from large-scale structures is the dominant source in the $\theta = 45^\circ$ direction while fine-scale and shock-associated noise are the dominant sources in the $\theta = 90^\circ$ direction. In both polar angles the data cluster themselves into groups with two distinct curves. The unheated jet overall sound pressure levels increase more rapidly with core jet velocity than do the heat-simulated jet overall sound pressure levels. Whether or not the unheated jet overall sound pressure levels follow the heat-simulated jet sound pressure levels over the same velocity range is uncertain. The current analysis would benefit from further measurements. It may be worthwhile to note that a "slope-switching" trend was observed by Viswanathan [18] for heated, single-stream round jet data (Figures 32 and 33 in [18]). The author remarks that increasing the jet temperature ratio decreases the slopes (velocity exponents) of the curves.

The large spread in the data for the lowest core jet velocity of 325 m/s ($\log(U_{J,C}/c_{\infty}) = -0.020$), suggests that the bypass jet can make a significant contribution to the far-field noise. In the $\theta = 90^\circ$ direction (Figure 5.6), at $\log(U_{J,C}/c_{\infty}) = -0.020$, core jet conditions remain unchanged, but the radiated noise increases by nearly 9 db OASPL as $NPR_{BP}$ increases from 2.0 to 3.5. In this polar direction, noise contributions from the large-scale structures are expected to be minimal. This would suggest that the relatively thin bypass jet can be a substantial source of fine-scale mixing noise. In the $\theta = 45^\circ$ direction, Figure 5.7, where large-scale mixing noise is the dominant source, the far-field noise increases by 6 db OASPL with $NPR_{BP}$ for $\log(U_{J,C}/c_{\infty}) = -0.020$. For the lowest core
Table 5.3: DSR-FS-BL jet operating conditions for $U_{J,C}$ variation

<table>
<thead>
<tr>
<th>Operating Conditions</th>
<th>$T_{net}$ (N)</th>
<th>$\dot{m}_{net}$ (kg/s)</th>
<th>$U_{J,C}$ (m/s)</th>
<th>$\log U_{J,C}/c_{\infty}$</th>
<th>$U_{J,BP}/U_{J,C}$</th>
<th>Sym</th>
</tr>
</thead>
<tbody>
<tr>
<td>NPR$_C$ 2.00 2.00 1.00</td>
<td>57 0.20</td>
<td>325 -0.020</td>
<td>1.00</td>
<td>▽</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.00 2.50 1.00</td>
<td>64 0.22</td>
<td>325 -0.020</td>
<td>1.13</td>
<td>○</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.00 3.00 1.00</td>
<td>72 0.23</td>
<td>325 -0.020</td>
<td>1.22</td>
<td>△</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.00 3.50 1.00</td>
<td>79 0.25</td>
<td>325 -0.020</td>
<td>1.29</td>
<td>◊</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.50 2.00 1.00</td>
<td>76 0.24</td>
<td>368 0.035</td>
<td>0.88</td>
<td>▽</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.50 2.50 1.00</td>
<td>84 0.25</td>
<td>368 0.035</td>
<td>1.00</td>
<td>○</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.50 3.00 1.00</td>
<td>91 0.27</td>
<td>368 0.035</td>
<td>1.08</td>
<td>△</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.50 3.50 1.00</td>
<td>98 0.28</td>
<td>368 0.035</td>
<td>1.14</td>
<td>◊</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 3.80 1.00</td>
<td>121 0.32</td>
<td>398 0.069</td>
<td>1.08</td>
<td>■</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 2.00 1.00</td>
<td>95 0.27</td>
<td>398 0.069</td>
<td>0.82</td>
<td>▽</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 2.50 1.00</td>
<td>102 0.29</td>
<td>398 0.069</td>
<td>0.92</td>
<td>○</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 3.00 1.00</td>
<td>110 0.30</td>
<td>398 0.069</td>
<td>1.00</td>
<td>△</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 3.50 1.00</td>
<td>117 0.32</td>
<td>398 0.069</td>
<td>1.06</td>
<td>◊</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.50 2.00 1.00</td>
<td>113 0.31</td>
<td>421 0.093</td>
<td>0.77</td>
<td>▽</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.50 2.50 1.00</td>
<td>121 0.32</td>
<td>421 0.093</td>
<td>0.87</td>
<td>○</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.50 3.00 1.00</td>
<td>128 0.34</td>
<td>421 0.093</td>
<td>0.95</td>
<td>△</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.50 3.50 1.00</td>
<td>135 0.35</td>
<td>421 0.093</td>
<td>1.00</td>
<td>◊</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 3.80 1.57</td>
<td>125 0.29</td>
<td>499 0.167</td>
<td>0.86</td>
<td>■</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 3.80 2.18</td>
<td>117 0.27</td>
<td>588 0.238</td>
<td>0.73</td>
<td>■</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 3.80 3.00</td>
<td>130 0.25</td>
<td>690 0.307</td>
<td>0.63</td>
<td>■</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 2.00 3.00</td>
<td>104 0.20</td>
<td>690 0.307</td>
<td>0.47</td>
<td>×</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 3.00 3.00</td>
<td>118 0.23</td>
<td>690 0.307</td>
<td>0.58</td>
<td>×</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 3.50 3.00</td>
<td>126 0.24</td>
<td>690 0.307</td>
<td>0.61</td>
<td>×</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

jet velocities, although the bypass jet is relatively thin in the $\phi = 0^\circ$ azimuthal direction, the bypass jet can still make substantial contributions to the noise radiated to downstream polar angles.

At higher core jet velocities, the core jet becomes an increasingly dominant acoustic source and the variation in the noise levels with NPR$_{BP}$ diminishes. For the lowest core jet velocity, as NPR$_{BP}$ is increased from 2.0 to 3.5, the OASPL’s increase by 9 dB in
Figure 5.6: Variation in OASPL with core jet velocity. OASPL measured at $\theta = 90^\circ$ polar angle in the $\phi = 0^\circ$ azimuthal direction.

Figure 5.7: Variation in OASPL with core jet velocity. OASPL measured at $\theta = 45^\circ$ polar angle in the $\phi = 0^\circ$ azimuthal direction.

at $90^\circ$ and by 6 dB in at $45^\circ$. However, for the highest core jet velocity, in both polar angle directions, the spread in noise levels decreases to 1 dB OASPL. The spectra for $\log(U_{J,C}/c_\infty) = -0.020$, $NPR_{BP} = 2.0$ and 3.5, are shown in Figure 5.8a. The increase in $NPR_{BP}$ is associated with nearly uniform 5 dB increase in the radiated noise across all frequencies. Similar trends are observed for $\log(U_{J,C}/c_\infty) = 0.093$, shown in Figure 5.8b, except the core jet is now a more-dominant noise source and increasing $NPR_{BP}$ has a lesser effect on increasing noise levels. The following section aims to further elucidate the effects of the bypass nozzle pressure ratio on the radiated noise.
5.1.4 Effect of Bypass Jet Nozzle Pressure Ratio

This section works to further understand of the effects of the bypass jet pressure ratio, \( NPR_{BP} \), on the radiated noise from dual-stream jets. As in the previous sections, experiments were conducted using DSR-FS-BL nozzle. To isolate the effects of the bypass jet, the core jet was held at a fixed supersonic, heat-simulated operating condition of \( NPR_C = TTR_C = 3.0 \) while \( NPR_{BP} \) was varied from 2.0 to 4.0. Jet operating conditions are listed in Table 5.4. Spectra were made non-dimensional using the characteristic frequency of the core jet, which is constant for all cases, and listed in the right-most column of Table 5.4.

### Table 5.4: Jet operating conditions for BPR variation

<table>
<thead>
<tr>
<th>Operating Conditions</th>
<th>( T_{net} ) (N)</th>
<th>( \bar{m}_{net} ) (kg/s)</th>
<th>( BPR )</th>
<th>( U_{J,BP}/U_{J,C} )</th>
<th>( T_{BP}/T_C )</th>
<th>( f_C ) (kHz)</th>
</tr>
</thead>
<tbody>
<tr>
<td>( NPR_C ) ( NPR_{BP} ) ( TTR_C )</td>
<td>104</td>
<td>0.20</td>
<td>0.43</td>
<td>0.47</td>
<td>0.21</td>
<td>33.6</td>
</tr>
<tr>
<td>3.00</td>
<td>3.00</td>
<td>2.00</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00</td>
<td>3.00</td>
<td>3.00</td>
<td>118</td>
<td>0.23</td>
<td>0.65</td>
<td>0.58</td>
</tr>
<tr>
<td>3.00</td>
<td>3.00</td>
<td>3.50</td>
<td>126</td>
<td>0.24</td>
<td>0.76</td>
<td>0.61</td>
</tr>
<tr>
<td>3.00</td>
<td>3.00</td>
<td>4.00</td>
<td>133</td>
<td>0.26</td>
<td>0.87</td>
<td>0.64</td>
</tr>
</tbody>
</table>

An acoustic summary of the bypass jet effects on far-field noise in the \( \phi = 0^\circ \) azimuthal direction is presented in Figure 5.9. Representative far-field spectra are shown for three polar angles \( (\theta = 45^\circ, 90^\circ, \text{and} 120^\circ) \) in the top row and the directivity is shown in bottom row.

Figure 5.8: Variation in far-field spectra at \( \theta = 45^\circ \) with \( NPR_{BP} \) for core jet velocities of (a) 325 m/s and (b) 421 m/s.

(a) \( \log(U_{J,C}/a_\infty) = -0.020 \)

(b) \( \log(U_{J,C}/a_\infty) = 0.093 \)
Figure 5.9: Acoustic summary of the effects of bypass jet nozzle pressure ratio on the far-field noise from dual-stream rectangular jets. Bypass jet NPR varied while core jet NPR and TTR were held constant. Far field noise measured in the $\phi = 0^\circ$ azimuthal direction.

Most notably, there is little variation in the directivity as $NPR_{BP}$ is increased from 2.0 to 4.0. Overall sound pressure levels are within 1 dB across all polar angles. The data in Figure 5.9d at $45^\circ$ and $90^\circ$ correspond to the highest core jet velocity data plotted in Figures 5.6 and 5.7.

In examining the spectra for the $\theta = 45^\circ$ direction in Figure 5.9a, the highest $NPR_{BP}$ cases reduce higher frequency noise by up to 2 dB. In the sideline and upstream directions, the two highest $NPR_{BP}$ conditions exhibit greater $BBSAN$ levels compared with the lowest bypass pressure. In addition, a weak screech tone also appears in the spectra for the higher $NPR_{BP}$ conditions. The increase in $BBSAN$ and the emergence of a screech tone are likely due to the bypass jet becoming increasingly under-expanded.
Far-field acoustic data are now presented for measurements in the $\phi = 270^\circ$ azimuthal direction. An acoustic summary of the bypass stream effects on far-field noise is shown in Figure 5.10. Representative far-field spectra are shown for three polar angles ($\theta = 45^\circ$, $90^\circ$, and $120^\circ$) in the top row.

Figure 5.10: Acoustic summary of the effects of bypass nozzle pressure ratio on the far-field noise from dual-stream rectangular jets. Bypass jet $NPR$ varied while core jet $NPR$ and $TTR$ were held constant. Noise measured in the $\phi = 270^\circ$ azimuthal direction.

At the $\theta = 45^\circ$ polar angle, increasing $NPR_{BP}$ from 2.0 to 3.0 reduces the radiated noise at frequencies above the mixing noise peak. This results in a 1.5 dB OASPL reduction in the peak noise direction and demonstrates that the bypass jet can act as an acoustic fluid shield [92]. In Figure 5.10d, it can be seen that the $NPR_{BP} = 4.0$ case is marked by a significant change in the directivity. This is conjectured to be a result of the jet deflecting in the $\phi = 270^\circ$ direction towards the microphone array causing the increased noise levels at the highest polar angles. But, bypass pressure ratios as high as 4.0 are not expected to be practical for full-scale aircraft engines. At the $\theta = 90^\circ$ and $120^\circ$ polar
angles, shown in Figures 5.10b and 5.10c, respectively, the bypass jet has little effect on
the overall sound pressure levels. However, as bypass nozzle pressure ratio is increased, a
broadening and shifting of the BBSAN peak is observed. The screech tone previously
shown in the spectra in Figure 5.9 is much stronger in the $\phi = 270^\circ$ direction.

5.2 Dual-Stream Jet Noise Variation with Velocity Ratio and Thrust

The influence of velocity ratio, $U_{J,BP}/U_{J,C}$, and $T$ on the noise radiated from jets exhausting
from the DSR-FS-BL nozzle are now examined. Core pressure and temperature ratios, and
bypass pressure ratio were simultaneously varied between tests. Jet operating conditions,
estimated thrust values, and velocity ratios are listed in Table 5.5. Operating conditions are
listed by increasing estimated thrust. Thrust was estimated in Newtons, N, following the
method described in Section 2.5. Jet velocities, $U_J$, were estimated based on fully-expanded
conditions using isentropic relations. The range of test conditions spans a 13% increase in
estimated thrust and nearly a halving of velocity ratio. Far-field acoustic measurements
were performed in both the $\phi = 0^\circ$ and $270^\circ$ azimuthal directions. The changes in OASPL
with velocity ratio and thrust are only examined at the $45^\circ$ polar angle. Measurements
indicated this to be the peak or near the peak noise direction.

<table>
<thead>
<tr>
<th>Operating Conditions</th>
<th>$T_{net}$ (N)</th>
<th>$m_{net}$ (kg/s)</th>
<th>$BPR$</th>
<th>$U_{J,BP}/U_{J,C}$</th>
<th>$T_{BP}/T_{C}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>NPR$<em>C$  NPR$</em>{BP}$ TTR$_C$</td>
<td>115 0.28</td>
<td>0.71 0.90</td>
<td>0.64</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.72 3.85 1.57</td>
<td>116 0.28</td>
<td>0.53 0.83</td>
<td>0.44</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 3.21 1.51</td>
<td>117 0.26</td>
<td>0.80 0.77</td>
<td>0.62</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.72 3.83 2.18</td>
<td>118 0.26</td>
<td>0.59 0.72</td>
<td>0.43</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 3.20 2.02</td>
<td>119 0.24</td>
<td>0.91 0.65</td>
<td>0.60</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.72 3.81 3.00</td>
<td>120 0.24</td>
<td>0.65 0.63</td>
<td>0.41</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.00 3.17 2.66</td>
<td>123 0.26</td>
<td>0.43 0.64</td>
<td>0.28</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.34 2.60 2.00</td>
<td>125 0.24</td>
<td>0.44 0.55</td>
<td>0.24</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.39 2.45 2.50</td>
<td>126 0.22</td>
<td>0.42 0.47</td>
<td>0.20</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Figure 5.11 presents the variation in far-field noise with velocity ratio. The $0^\circ$ azimuthal
direction is shown in the left panel while the $270^\circ$ azimuthal direction is shown in the right
A first-order least-squares fit for the data is also shown in each panel. The data in both azimuthal directions follow a general trend of decreasing OASPL with increasing velocity ratio. The rate of decrease is greater in the 270° azimuthal direction. In this direction, the bypass jet exhausts in a plane bisecting the far-field microphones and the core jet.

Figure 5.11: OASPL at $\theta = 45^\circ$ variation with estimated velocity ratio for jets exhausting from the DSR-FS-BL nozzle.

Figure 5.12 presents the variation in far-field noise with estimated thrust. A first-order, least-squares fit for the data is also shown in each panel. The left panel presents changes in the 0° azimuthal direction. The least-square fit is only applied to the first six data points on the left. There is, initially, an increase in measured noise with thrust. However, at higher thrust levels the points deviate from the curve and plateau to a nearly constant value of 125 dB. Whether this deviation marks a true noise saturation, or is a product of the imperfect thrust estimation, or some combination of both is unclear. This work would benefit from RANS CFD simulations that could be used to provide more accurate estimates of dual-stream jet thrust. The right panel of Figure 5.12 shows the change in OASPL in the 270° azimuthal direction. A least-square fit is applied to all of the data points. There is a monotonic increase in measured noise levels with increasing thrust.
5.3 Effects of Bypass Ratio on Dual-Stream Jet Noise

The remainder of this chapter is now focused on comparing the acoustics of dual-stream jets on a constant net thrust and net mass flow rate basis. The goal of this section to gain insight into how both $BPR$ and nozzle configuration affect the noise radiated from dual-stream rectangular jets.

5.3.1 Experimental Test Plan

The testing approach adopted in this section attempts to follow that of Tanna [93] and Tanna and Morris [94]. Acoustic comparisons are made on a constant thrust and constant mass flow rate (quantities estimated), and constant exit area basis. To this end, core pressure ratio, core (simulated) temperature ratio, and bypass pressure ratio are simultaneously varied. The methods for estimating thrust and mass flow rate are described in detail in Section 2.5. Previous dual-stream jet studies, including [18,93,94,101–104] only considered nozzle configurations in which each jet exhausted to atmosphere, making the estimation of thrust using quasi-1D flow assumption more accurate and straightforward. The individual jets were not embedded upstream in a secondary diverging section as they are in this work.
Experiments were conducted using two dual-stream nozzle configurations: the DSR-FS-BL and DSR-Sym-BL nozzles. Far-field noise was measured in the $\phi = 0^\circ$ and $270^\circ$ azimuthal directions. Results are presented from a series of acoustic measurements of the jets operating at bypass ratios of 0.2, 0.3, 0.4, and 0.5. Narrowband spectra and directivity are presented for jets exhausting from each nozzle and comparisons are made between the two nozzles. Representative spectra are presented for six polar angles: four downstream angles between $30^\circ$ and $60^\circ$, to the side of the jet, $\theta = 90^\circ$, and an upstream angle of $\theta = 120^\circ$.

Dual-stream rectangular jet operating conditions chosen for this section are tabulated in Table 5.6. The first four Test Points have been chosen for a target net thrust, $T_{net}$, of 101N and mass flow rate of 0.21kg/s. The estimated dual-stream jet thrust levels, reported in Table 5.6, are within 2% of the target value. The bypass ratio was varied from 0.20 to 0.50. Test Points 5 and 6 have similar thrust levels as Test Points 1 – 4, but have approximately 12.5% greater specific thrust values. The objective of these test conditions is to verify the findings of Test Points 1 – 4, but at elevated $TT\!R_C$’s, that are more applicable to tactical aircraft operation. For brevity, Test Points 1 – 4, are referred to as "baseline specific thrust" conditions, with an average specific thrust of and Test Points 5 and 6 are referred to as "elevated specific thrust" conditions.

Thrust and bypass ratios were chosen based off of relevant values reported in the literature. The thrust per unit exit area of the jets is approximately $17\, \text{N/cm}^2$, based on a nominal thrust of 101N. Compare this with the F135-PW-100 low-$\text{BPR}$ turbofan engine, installed on the F-35 family of aircraft. Based on the values reported in Table 1.2, the F135-PW-100 engine similarly generates $17.8\, \text{N/cm}^2$. Bypass jet pressure ratios were chosen to be within a similar range as the fluid shield pressure ratios reported by Martens and Haber [92]. Moreover, a bypass ratio of 0.50 is the maximum that was considered by Papamoschou et al. [101,103]. The specific thrust, $F_S$, levels fall between those reported Tables 1.1 and 1.2, for high- and low-$\text{BPR}$ turbofan engines, respectively.

The complete test program for this section is given in Table 5.7. The program indicates the azimuthal direction and test points for far-field acoustic measurement of jets exhausting from each nozzle. Nozzles are referred to by their ID in each figure. In addition, a small image of the nozzle included in most figures. In Section 5.3.2, data are first presented for Test Points 1 – 4. Following this, Section 5.3.3 presents the results of experiments at the elevated specific thrust conditions: Test Points 5 and 6.
Table 5.6: Dual-stream jet operating conditions for BPR variation

<table>
<thead>
<tr>
<th>Test Pt. (TP)</th>
<th>Operating Conditions</th>
<th>$T_{net}$ (N)</th>
<th>$\dot{m}_{net}$ (kg/s)</th>
<th>$F_S$ ($N/(kg/s)$)</th>
<th>BPR</th>
<th>$U_{J, BP}/U_{J, C}$</th>
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<tbody>
<tr>
<td>1</td>
<td>3.25 1.20 2.10</td>
<td>99</td>
<td>0.21</td>
<td>477</td>
<td>0.20</td>
<td>0.50</td>
</tr>
<tr>
<td>2</td>
<td>3.11 1.61 2.26</td>
<td>100</td>
<td>0.21</td>
<td>491</td>
<td>0.30</td>
<td>0.45</td>
</tr>
<tr>
<td>3</td>
<td>2.98 1.99 2.46</td>
<td>101</td>
<td>0.21</td>
<td>488</td>
<td>0.40</td>
<td>0.52</td>
</tr>
<tr>
<td>4</td>
<td>2.88 2.34 2.68</td>
<td>103</td>
<td>0.21</td>
<td>494</td>
<td>0.50</td>
<td>0.56</td>
</tr>
<tr>
<td>5</td>
<td>3.09 1.43 2.95</td>
<td>99</td>
<td>0.19</td>
<td>534</td>
<td>0.30</td>
<td>0.35</td>
</tr>
<tr>
<td>6</td>
<td>2.88 2.07 3.54</td>
<td>101</td>
<td>0.19</td>
<td>545</td>
<td>0.50</td>
<td>0.45</td>
</tr>
</tbody>
</table>

Table 5.7: Test program for baseline dual-stream rectangular nozzle acoustic measurements

<table>
<thead>
<tr>
<th>Nozzle ID</th>
<th>Azimuthal Direction ($\phi$)</th>
<th>Test Points (TP)</th>
</tr>
</thead>
<tbody>
<tr>
<td>DSR-FS-BL</td>
<td>0 270</td>
<td>✓ ✓ ✓ ✓ ✓ ✓</td>
</tr>
<tr>
<td>DSR-Sym-BL</td>
<td>0 270</td>
<td>✓ ✓ ✓ ✓ ✓ ✓</td>
</tr>
</tbody>
</table>

5.3.2 Acoustic Measurements at Baseline Specific Thrust Conditions

The effects of bypass ratio on the noise radiated from jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles is studied for Test Points 1 – 4. Jet BPR was varied from 0.3 to 0.5 for the DSR-FS-BL nozzle and from 0.2 to 0.5 for the DSR-Sym-BL nozzle. Jet conditions have been chosen to approximately produce a target net thrust of 101 N with a corresponding mass flow rate of 0.21 kg/s. These correspond to a nominal specific thrust of 488 N/(kg/s). Data are first presented from far-field acoustic measurements in the $\phi = 0^\circ$ azimuthal direction followed by measurements acquired in the $\phi = 270^\circ$ direction.

Figure 5.13 displays the far-field spectra measured in the $\phi = 0^\circ$ azimuthal direction for jets exhausting from the DSR-FS-BL (left panel) and DSR-Sym-BL nozzles (right panel). Increasing bypass ratio has almost no effect on the mixing noise of the DSR-FS-BL jets. Alternatively, mixing noise of the DSR-Sym-BL jets is more sensitive to changes in bypass ratio. Increasing bypass ratio results in reductions of up to 4 dB in the peak mixing noise. Above $St = 1.0$, reductions as high as 6 dB are observed as the bypass ratio
is increased from 0.2 to 0.5. It is interesting that such significant reductions are observed in the azimuthal direction bisecting the two bypass jets. Powers et al. [197] observed a similar phenomenon in round jets with fluid insert noise reduction devices. But the noise reductions associated with the DSR-Sym-BL jets must be considered carefully.

Although reductions are observed as jet bypass ratio is increased, the largest reductions are observed as the jet bypass ratio is increased from 0.2 to 0.3. The DSR-Sym-BL TP1 has the highest mixing noise levels of all spectra shown in Figure 5.13. The spectra measured in the 45° polar direction for the dual-stream nozzles operating at Test Points 2 and 4 ($BPR = 0.3$ and 0.5, respectively) are compared against each other in Figure 5.14. For both Test Points, the DSR-FS-BL and DSR-Sym-BL jets have similar peak mixing noise levels. In the peak noise direction, the DSR-Sym-BL jets have lower high frequency noise levels compared with the DSR-FS-BL jets.

![Figure 5.13: Far-field spectra measured in the $\phi = 0^\circ$ azimuthal direction for jets exhausting from the DSR-FS-BL (left) and DSR-Sym-BL (right) nozzles.](image)

In the upstream direction, the $BBSAN$ is sensitive to changes in the bypass ratio, particularly for the DSR-FS-BL jets shown in Figure 5.13a. As the $BPR$ is increased
from 0.3 to 0.5, there is a nearly complete elimination of the \textit{BBSAN} at the sideline and upstream angles. For the DSR-Sym-BL jets, shown in Figure 5.13b, the \textit{BBSAN} peak is reduced by up to 4 dB and shifts to higher frequencies as \textit{BPR} is increased.

These results are initially unexpected as both the core and bypass jets are operating off of their design conditions. Flow field measurements give insight into the \textit{BBSAN} reductions. Flow visualization of the DSR-FS-BL TP4 jet shown in Figure 4.3a shows a weakened, irregular shock cell pattern in the jet compared with the more regularly-spaced, diamond-shaped shock cells of the DSR-FS-BL TP2 jet shown in Figure 4.2a. This is corroborated by the centroid line Mach number measurements in Figure 4.7. The DSR-FS-BL TP4 jet has minimal Mach number fluctuations along the jet centroid line. The disruption of the shock cell system in the DSR-FS-BL TP4 jet leads to the significant reductions in the \textit{BBSAN}. A similar effect is seen for the DSR-Sym-BL jets. Attention is once again drawn to the centroid line Mach number measurements in Figure 4.7. The Mach number fluctuations of the DSR-Sym-BL TP4 jet are less than the TP2 jet. The weakened shock cell system is associated with lower \textit{BBSAN} levels.

Figure 5.14: Far-field spectra measured in the $\phi = 0^\circ$, $\theta = 45^\circ$ direction for jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles.

The directivities of jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles measured in the $\phi = 0^\circ$ azimuthal direction are shown in Figure 5.15. The higher mixing noise levels of the DSR-Sym-BL TP1 jet translate to peak noise levels being approximately 2 dB OASPL higher than the other jets. But as \textit{BPR} is increased, the peak noise levels of jets exhausting from the DSR-Sym-BL nozzle decrease by as much as 3.5 dB OASPL between Test Points 1 and 4. The minor changes in the peak noise are consistent with the
minor effects of $NPR_{BP}$ on far-field noise noted in the discussion of Figure 5.9. Operating at Test Point 2, the DSR-FS-BL and DSR-Sym-BL jets, in Figures 5.15a and 5.15b, respectively, have comparable peak noise levels. As the bypass ratio is increased to 0.5, the DSR-Sym-BL jet offers a slight acoustic advantage in the peak noise over the DSR-FS-BL nozzle. The DSR-Sym-BL TP4 jet is about 1 dB OASPL quieter than the DSR-FS-BL TP4 jet. It is possible that jet bypass ratios greater than 0.5 would produce more significant peak noise reductions. However, bypass ratios greater than 0.5 are not expected to be practical for full-scale tactical aircraft engines.

![Figure 5.15: Directivity of jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles measured in the $\phi = 0^\circ$ azimuthal direction.](image)

From the overall sound pressure levels presented in Figure 5.15, increases in jet bypass ratio also result in reductions to the noise radiated towards polar angles greater than 90°. This is particularly true for jets exhausting from the DSR-FS-BL nozzle, shown in Figure 5.15a. The $\theta = 120^\circ$ spectra for the DSR-FS-BL jets, shown in Figure 5.13, show sizable reductions in the $BBSAN$ peak, up to 10 dB near $St = 0.7$, with increasing $BPR$. This translates into noise reductions of 3-4 dB OASPL, for the DSR-FS-BL TP4 jet, shown in Figure 5.15a, at the highest polar angles. For the DSR-Sym-BL jets, shown in Figure 5.15b, there is a consistent decrease in the upstream OASPL’s with increasing $BPR$. The DSR-Sym-BL TP4 jet ($BPR= 0.50$) is 2 dB OASPL quieter than the DSR-Sym-BL TP2 ($BPR= 0.20$) for $\theta \geq 80^\circ$.

Figure 5.16 shows the far-field spectra measured in the $\phi = 270^\circ$ azimuthal direction for jets exhausting from the DSR-FS-BL (left panel) and DSR-Sym-BL (right panel) nozzles.
There is little change in the large-scale mixing noise of jets exhausting from either nozzle. In the $\phi = 270^\circ$ azimuthal direction, the microphones measured the noise radiated from the jet in the same plane as the vorticity thickness measurements plotted in Figure 4.6. Vorticity thickness measurements show similar growth rates for both nozzles operating at Test Points 2 and 4 downstream to $X/D_{Eq} \approx 6$ which is beyond the estimated ends of the jets’ primary potential core. Previous studies have shown the largest scale noise sources to be located near the end of the potential core [176,198]. The similar jet growth rates and shear layer thicknesses out to the end of the potential cores leads to the similar large-scale mixing noise levels.

![Figure 5.16](image)

**Figure 5.16**: Far-field spectra measured in the $\phi = 270^\circ$ azimuthal direction for jets exhausting from the DSR-FS-BL (left) and DSR-Sym-BL (right) nozzles.

Similar to the measurements acquired in the $\phi = 0^\circ$ direction, increasing $BPR$ leads to reductions in the $BBSAN$ peak at upstream angles. An almost complete elimination of the $BBSAN$ is observed for the DSR-FS-BL jets as the bypass ratio is increased from 0.3 to 0.5. These results are not unexpected as previous studies have shown the broadband shock associated noise to be omni-directional [48,50,51].
The directivities of jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles measured in the $\phi = 270^\circ$ azimuthal direction are shown in Figure 5.17. Peak noise levels are, in general, 2 dB OASPL higher in this azimuthal direction compared with the $\phi = 0^\circ$ azimuthal direction shown in Figure 5.15. The only exception being the DSR-Sym-BL TP2 jet, which has comparable peak noise levels in both azimuthal directions. Jets exhausting from either nozzle show little change in peak noise levels or peak noise direction with changes in bypass ratio. Again, the elimination of the $BBSAN$ peak of the DSR-FS-BL TP4 jet leads to upstream noise reductions of 3-4 dB OASPL compared with lower $BPR$ jets exhausting from the same nozzle.

Figure 5.17: Directivity of jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles measured in the $\phi = 270^\circ$ azimuthal direction.

5.3.3 Acoustic Measurements at Elevated Specific Thrust

It is now worthwhile to consider whether the observed changes in dual-stream jet noise with bypass ratio and nozzle configuration are consistent for the same bypass ratios, but different $NPR_C$-$TTR_C$-$NPR_{BP}$ pairs. The objective is to begin to verify the results of Section 5.3.2 by operating the dual-stream jets at the same bypass ratios, but at different operating conditions. To this end, a limited series of experiments have been conducted using the DSR-FS-BL and DSR-Sym-BL nozzles, operating at elevated specific thrust levels, compared with Test Points 1 – 4. The jets were operated at Test Points 5 and 6, listed in Table 5.6. Testing conditions correspond to a 12.5% increase in specific thrust over Test Points 1 – 4. The increases in specific thrust, namely core jet $TTR$, brings these
jet conditions closer to that of real tactical aircraft. Acoustic measurements were limited to the $\phi = 0^\circ$ azimuthal direction because the measured changes with $BPR$ were greatest in this direction (see Section 5.3.2).

cFigure 5.18. Spectra for the DSR-FS-BL jets are shown in the left panel. There is little change in the DSR-FS-BL jet mixing noise as the bypass ratio is increased from 0.3 (TP5) to 0.5 (TP6). At a polar angle of 120°, there is almost a complete elimination of the $BBSAN$ peak, as the bypass ratio is increased up to 0.5. These results are consistent with those shown for the baseline specific thrust conditions in Figure 5.13. Spectra for the DSR-Sym-BL jets are shown in the right panel of Figure 5.18. Similar to the results presented in Figure 5.13, the increase in $BPR$ produces a decrease in the downstream mixing noise levels. As well, as $BPR$ is increased to 0.5, the $BBSAN$ peak, towards upstream polar angles of 120°, is shifted to higher frequencies, but is not eliminated, as is shown for the DSR-FS-BL jet. This reinforces the conclusion that the nozzle configuration, in terms of bypass jet positioning and bypass jet sizing, can affect which jet noise components are most susceptible to changes in $BPR$.

Directivities of the DSR-FS-BL and DSR-Sym-BL jets, operating at elevated specific thrust levels, are shown in Figure 5.19. OASPL’s for the DSR-FS-BL jet are shown in fig. 5.19a. The peak noise levels are not appreciably affected as the $BPR$ is increased from 0.3 to 0.5. This is consistent with the results obtained for the baseline specific thrust levels, shown in Figure 5.15a. The elimination of the $BBSAN$ peak results in a 2.5-3 dB OASPL reduction in the far-field noise, at upstream polar angles greater than 100°. The directivities for the DSR-Sym-BL jets operating at elevated specific thrust conditions are plotted in Figure 5.19b. Peak noise levels, near $\theta = 50^\circ$, reduce by 3 dB OASPL as $BPR$ is increased from 0.3 to 0.5. This reduction is greater than that observed for the DSR-Sym-BL jets operating at baseline thrust conditions, shown in Figure 5.15b. These results suggest that the bypass flow in the DSR-Sym-BL jets may be more effective at reducing noise at higher (simulated) core jet total temperatures.
Figure 5.18: Far-field spectra, measured in the in the $\phi = 0^\circ$ azimuthal direction, for dual-stream jets operated at an elevated specific thrust of 540 N/(kg/s).

Figure 5.19: Overall sound pressure levels, measured in the in the $\phi = 0^\circ$ azimuthal direction, for dual-stream jets operated at an elevated specific thrust of 540 N/(kg/s).
5.4 Comparisons of Dual-Stream Jets with Single-Stream Equivalent Jets

The goal of this section seeks to answer the questions: do dual-stream rectangular jets offer an acoustic benefit over equivalent single-stream jets? And if so, under what operating conditions and in what azimuthal and/or polar angle directions? To answer these questions, dual-stream jet acoustic data are compared with that of round and rectangular single-stream jets operating at thrust- and mass flow rate-equivalent conditions. Comparisons are made for dual-stream jet bypass ratios of less than 1.0. Comparisons are first presented for both the DSR-FS-BL and DSR-Sym-BL jets operating at low BPR’s of 0.50 and less. Following this, acoustic measurements of DSR-FS-BL jets operating at elevated BPR’s, between 0.65 and 1.0, are discussed.

5.4.0.1 Low Bypass Ratio Comparisons

The DSR-FS-BL and DSR-Sym-BL jets operating at Test Points 2 and 4 are now compared with the SSEQ-Rect and SSEQ-Rnd jets. The operating conditions for both the single- and dual-stream jets are listed in Table 5.8. From the results of Section 5.3, it was shown that the changes in dual-stream jet noise, due to changes in BPR, are greatest in the $\phi = 0^\circ$ azimuthal direction. Because of this, comparisons are made with single-stream jets in this azimuthal orientation.

Table 5.8: Jet operating conditions for comparing the far-field acoustics of SSEQ jets with low bypass ratio dual-stream jets

<table>
<thead>
<tr>
<th>Type</th>
<th>Test Pt. (TP)</th>
<th>Operating Conditions $\begin{array}{ccc} NPR_C &amp; NPR_{BP} &amp; TTR_C \end{array}$</th>
<th>$T_{net}$ (N)</th>
<th>$\dot{m}_{net}$ (kg/s)</th>
<th>$F_S$ (N/(kg/s))</th>
<th>$BPR$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Dual</td>
<td>2</td>
<td>3.11 1.61 2.26</td>
<td>100</td>
<td>0.21</td>
<td>491</td>
<td>0.30</td>
</tr>
<tr>
<td></td>
<td>4</td>
<td>2.88 2.34 2.68</td>
<td>103</td>
<td>0.21</td>
<td>494</td>
<td>0.50</td>
</tr>
<tr>
<td>SSEQ</td>
<td></td>
<td>2.60 – 1.86</td>
<td>101</td>
<td>0.21</td>
<td>491</td>
<td>–</td>
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</table>

Comparisons of the far-field spectra are shown in Figure 5.20. The DSR-FS-BL jets are compared with SSEQ jets in Figure 5.20a, and similarly for the DSR-Sym-BL jets in Figure 5.20b. Focusing first on Figure 5.20a, at $\theta = 33^\circ$ and $45^\circ$, the DSR-FS-BL and SSEQ jets have similar peak mixing noise levels, to within 2 dB, at $St = 0.15$. At polar angles less than $50^\circ$, the SSEQ-Rnd jet has a broader mixing noise peak compared with
the SSEQ-Rect jet. But at 45°, near the peak noise emission angle, the DSR-FS-BL and DSR-Sym-BL jets have similar noise levels, within 2 dB, across all frequencies. At a polar angle of 33°, for $St \geq 0.30$, all dual-stream jet conditions have spectra levels approximately 5 dB lower than the equivalent round jet. But, at polar angles upstream of 45°, all dual-stream jets have greater noise levels for frequencies above $St = 0.30$. At upstream polar angles, the dual-stream jets have greater $BBSAN$ and high-frequency ($St > 1$) levels compared with the single-stream jets. It is possible that the relatively thin bypass jets are responsible for the addition high-frequency noise content.

![Figure 5.20](image)

(a) DSR-FS-BL nozzle, $\phi = 0^\circ$

(b) DSR-Sym-BL nozzle, $\phi = 0^\circ$

Figure 5.20: Far-field spectra, measured in the $\phi = 0^\circ$ azimuthal direction, of dual-stream jets operating at $BPR's \leq 0.5$ compared with SSEQ jets operating at equivalent mass flow rate and thrust conditions.

The OASPL's of the dual-stream and SSEQ jets are shown in Figure 5.21. The peak noise levels of both the DSR-FS-BL TP2 and DSR-Sym-BL-TP2 jets are up to 4 dB OASPL louder than the SSEQ-Rect jet. As well, the elevated temperatures of the dual-stream core jet, for the TP2 conditions, results in the peak noise levels being shifted upstream to higher polar angles, compared with SSEQ-Rect jet. For both the DSR-FS-BL and
DSR-Sym-BL jets operating at TP2, the peak noise levels are within 1 dB OASPL of the SSEQ-Rnd jet. But, the directivity peak of the DSR-Sym-BL TP4 jet is shifted upstream, which results in elevated noise levels in $50^\circ \leq \theta \leq 70^\circ$ polar arc compared with the SSEQ-Rnd jet. For upstream polar angles greater than $90^\circ$, the greater $BBSAN$ and high-frequency noise levels of the DSR-FS-BL and DSR-Sym-BL TP4 jets, results in the dual-stream jets being up 5 dB OASPL louder than both single-stream jets. But, as shown in Figure 5.21b, the elimination of the $BBSAN$ peak for the DSR-FS-BL TP4 jet condition, seen in Figure 5.20a, reduces the upstream noise of the DSR-FS-BL TP4 jet down to levels comparable to the SSEQ-Rnd jet.

5.4.0.2 Elevated Bypass Ratio Comparisons

It is hypothesized that greater dual-stream jet bypass ratios are required for the dual-stream jets to demonstrate a noise reduction benefit compared with SSEQ jets. To test this hypothesis, a limited number of experiments have been conducted with DSR-FS-BL jet operating at elevated bypass ratios of 0.65 and 0.91. It was thought that the greater bypass ratios, in tandem with the thicker fluid shield, compared to the thin bypass jets of the DSR-Sym-BL nozzle, offers the best chance at reducing the radiated noise to levels less than those of equivalent single jets. Operating conditions for the dual-stream and SSEQ jets are listed in Table 5.9. The dual- and single-stream jets are thrust-matched to within 3.5%.
Table 5.9: Jet operating conditions for comparing the far-field acoustics of SSEQ jets with elevated bypass ratio DSR-FS-BL jets

<table>
<thead>
<tr>
<th>Type</th>
<th>Test Pt. (TP)</th>
<th>Operating Conditions</th>
<th>$T_{net}$ (N)</th>
<th>$m_{net}$ (kg/s)</th>
<th>$F_S$ ($N/(kg/s)$)</th>
<th>BPR</th>
</tr>
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<tbody>
<tr>
<td>Dual</td>
<td>7</td>
<td>3.00 3.14 2.66</td>
<td>120</td>
<td>0.24</td>
<td>500</td>
<td>0.65</td>
</tr>
<tr>
<td></td>
<td>8</td>
<td>2.72 3.83 3.00</td>
<td>119</td>
<td>0.24</td>
<td>496</td>
<td>0.91</td>
</tr>
<tr>
<td>SSEQ</td>
<td></td>
<td>2.96 – 1.81</td>
<td>123</td>
<td>0.24</td>
<td>520</td>
<td>–</td>
</tr>
</tbody>
</table>

Comparisons of the far-field spectra are shown in Figure 5.22. Far-field acoustic data were obtained in both the $\phi = 0^\circ$ and $270^\circ$ azimuthal directions. Figure 5.22a presents the results of measurements acquired in the $\phi = 0^\circ$ azimuthal direction for the DSR-FS-BL and SSEQ-Rect jet. Similar to the results for $BPR$’s less than 0.50, shown in Figures 5.13a and 5.18a, the far-field noise levels vary little as $BPR$ is increased from 0.65 to 0.91. However, at polar angles of 50° and 60°, for frequencies above $St = 0.5$, the spectra levels decrease by 2 dB, as bypass ratio is increased. Unlike the low-$BPR$ conditions, increasing $BPR$ further, up to 0.91, has little effect on the $BBSAN$ peak. In the downstream arc, up to $\theta = 90^\circ$, the DSR-FS-BL TP7 jet spectra overlap the SSEQ-Rect spectra, particularly for $St > 0.20$. A similar trend is also observed for the DSR-FS-BL TP8 condition.

Attention is now turned the results of acoustic measurements obtained in the $\phi = 270^\circ$ azimuthal direction, shown in Figure 5.22b. Now, dual-stream jet data are compared with both the SSEQ-Rect and SSEQ-Rnd jets. At the two lowest polar angles shown, $\theta = 33^\circ$ and 45°, above $St \approx 0.15$, the DSR-FS-BL TP7 and TP8 jets demonstrate significant noise reduction benefit over the SSEQ jets, with the dual-stream jet spectra level being up to 6-7 dB lower than the single jet spectra at the highest frequencies. Increasing the $BPR$ from 0.65 (TP7) to 0.91 (TP8), further reduces the dual-stream jet noise levels in the arc of maximum noise emission: $45^\circ \leq \theta \leq 60^\circ$.

Overall sound pressure levels are shown for the $\phi = 0^\circ$ and $270^\circ$ azimuthal directions in Figures 5.23a and 5.23b, respectively. Focusing first on the $\phi = 0^\circ$ azimuthal direction, in Figure 5.23a, the peak noise levels of the DSR-FS-BL TP7 and TP8 jets are within $\pm 1$ dB OASPL of the SSEQ-Rect jet, and is within experimental uncertainty. But, between polar angles of 50° and 80°, DSR-FS-BL TP7 jet is 2 dB OASPL lower than both the SSEQ-Rect and DSR-FS-BL TP8 jets. This is the result of the increased high-frequency content of DSR-FS-BL TP7 spectra, shown in Figure 5.22a.
OASPL comparisons for measurements acquired in the \( \phi = 270^\circ \) azimuthal directions are presented in Figure 5.23b. The increased bypass jet pressure, and hence increased \( BPR \), has a distinct shielding effect on the far-field for the DSR-FS-BL jets. The peak noise levels of the DSR-FS-BL TP7 jet are nearly 2-2.5 dB OASPL quieter than the peak noise levels of the SSEQ-Rect and SSEQ-Rnd jets. But, similar to the results shown for the lower \( BPR \) conditions in Figure 5.21b, the peak noise direction of the DSR-FS-BL TP7 jet is shifted upstream compared with that of the single-stream jets. This leads to the DSR-FS-BL TP7 jet being up to 2 dB OASPL louder than the SSEQ jets between 50° and 80°. However, increasing bypass ratio can have a significant noise reduction benefit. In the direction of maximum noise emission, for a \( BPR \) of 0.91, the DSR-FS-BL TP8 jet is 4 dB OASPL quieter than both the SSEQ-Rect and SSEQ-Rnd jets. The presence of a fluid shield in dual-stream jets has demonstrated the potential to have a noise reduction benefit. However, the bypass pressures and \( BPR \)'s required to achieve this benefit may not be feasible for full-scale aircraft operation.

![Figure 5.22: Far-field spectra of DSR-FS-BL jets operating at \( BPR \)'s of 0.65 and 0.91 compared with SSEQ jets operating at equivalent mass flow rate and thrust conditions.](image-url)
Figure 5.23: Overall sound pressure levels of DSR-FS-BL jets operating at $BPR$’s of 0.65 and 0.91 compared with SSEQ jets operating at equivalent mass flow rate and thrust conditions.

5.5 Effects of Aft Deck on Far-Field Noise

Integration of the nozzle with the aircraft fuselage can potentially shield the noise and other aircraft observables from being detected on the ground [164,165]. Blended nozzle-body designs with rectangular-shaped nozzle exhausts are featured in existing tactical aircraft such as the Northrop-Grumman B-2 Spirit bomber, Northrop-Grumman X-47B unmanned demonstrator, and Lockheed Martin F-22 Raptor fighter. The effects of an aft deck on the noise radiated from rectangular, dual-stream exhaust jets have received little attention, even though they are likely to be incorporated with the multi-stream rectangular exhaust architectures that are receiving more serious consideration for future tactical aircraft.

The emphasis of this section is placed on developing a better understanding of the effects of an aft deck on the far-field noise radiated from jets exhausting from the aft-deck variants of the DSR-Sym-BL (aft deck variant: DSR-Sym-AD) and DSR-FS-BL (aft deck variant: DSR-FS-AD) nozzles. Data are presented for measurements performed in the $\phi = 0^\circ$ and $270^\circ$ azimuthal directions. Complete details on the design of the aft deck nozzles are discussed in Section 2.3.2, but the geometries for the DSR-FS-AD and DSR-Sym-AD nozzles are summarized in Figures 5.24a and 5.24c, respectively. The aft deck nozzles oriented at $\phi = 0^\circ$ and $270^\circ$, relative to a microphone, are shown in Figures 5.24b
and 5.24d, respectively. In the \( \phi = 270^\circ \) azimuthal orientation, the aft deck protrudes \( 1D_{eq} \) downstream in a plane bisecting the jet flows and the microphone.

Acoustic comparisons are made on a thrust and mass flow rate equivalent basis. Aft deck nozzles are compared with their baseline nozzle counterparts. A summary of the aft deck nozzle test program is presented in Table 5.11. A limited number of conditions were tested to keep the experimental matrix reasonably small. Far-field acoustic data will be presented for the dual-stream jets operating at Test Points 1, 2, and 4 with corresponding bypass ratios of 0.20, 0.30, and 0.50, respectively. Jet total pressures and simulated total temperatures for each test point are given in Table 5.6. It was assumed that the aft deck has a negligible impact on thrust, although on real aircraft, weight, drag, and deck loading are likely to impact aircraft performance.

Figure 5.24: Summery of aft deck nozzle geometries and nozzle orientations relative to a far-field microphone for acoustic measurements in the \( \phi = 0^\circ \) and \( \phi = 270^\circ \) azimuthal directions.
Table 5.10: Dual-stream jet operating conditions to study aft deck effects

<table>
<thead>
<tr>
<th>Test Pt. (TP)</th>
<th>Operating Conditions</th>
<th>$T_{net}$ (N)</th>
<th>$m_{net}$ (kg/s)</th>
<th>$F_S$ ($N/(kg/s)$)</th>
<th>BPR</th>
<th>$U_{J,BP}/U_{J,C}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>3.25 1.20 2.10</td>
<td>99</td>
<td>0.21</td>
<td>477</td>
<td>0.20</td>
<td>0.50</td>
</tr>
<tr>
<td>2</td>
<td>3.11 1.61 2.26</td>
<td>100</td>
<td>0.21</td>
<td>491</td>
<td>0.30</td>
<td>0.45</td>
</tr>
<tr>
<td>4</td>
<td>2.88 2.34 2.68</td>
<td>103</td>
<td>0.21</td>
<td>494</td>
<td>0.50</td>
<td>0.56</td>
</tr>
</tbody>
</table>

Table 5.11: Dual-stream aft deck nozzle test program

<table>
<thead>
<tr>
<th>Nozzle Class</th>
<th>Nozzle ID</th>
<th>Azimuthal Direction ($\phi$)</th>
<th>Test Points (TP)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Baseline Nozzles</td>
<td>DSF-FS-BL</td>
<td>0 270</td>
<td>✓ ✓ ✓</td>
</tr>
<tr>
<td>DSF-Sym-BL</td>
<td>0 270</td>
<td>✓ ✓ ✓</td>
<td></td>
</tr>
<tr>
<td>Aft Deck Nozzles</td>
<td>DSF-FS-AD</td>
<td>0 270</td>
<td>✓ ✓ ✓</td>
</tr>
<tr>
<td>DSF-Sym-AD</td>
<td>0 270</td>
<td>✓ ✓ ✓</td>
<td></td>
</tr>
</tbody>
</table>

5.5.0.1 Aft Deck Effects: DSR-FS-BL Nozzle

Far-field spectra of jets exhausting from the DSR-FS-AD and DSR-FS-BL nozzles operating at Test Point 2, $BPR = 0.30$ are shown in Figure 5.25. The top row displays the far-field spectra measured in the $\phi = 0^\circ$ direction while the bottom row shows the spectra measured in the $\phi = 270^\circ$ direction. The presence of the aft deck has little-to-no influence on either the shape or amplitude of the spectra. This result is particularly surprising in the $270^\circ$ azimuthal direction in which one might expect the aft deck to shield the high-frequency noise that is generated near the nozzle exit.

The OASPL’s of jets exhausting from both the aft deck and baseline nozzles are shown in Figure 5.26. The left panel presents the directivity in the $\phi = 0^\circ$ azimuthal direction and the directivity in the $\phi = 270^\circ$ azimuthal direction is shown in the right panel. As might be expected from the far-field spectra, the aft deck has minimal influence on the directivity or peak noise levels in either azimuthal direction.

The far-field spectra of jets exhausting from the DSR-FS-AD and DSR-FS-BL nozzles operating at Test Point 4, $BPR = 0.50$ are shown in Figure 5.27. The top row displays...
the far-field spectra measured in the $\phi = 0^\circ$ direction while the bottom row shows the spectra measured in the $\phi = 270^\circ$ direction. In the $\phi = 0^\circ$, $\theta = 45^\circ$ direction, the addition of an aft deck results in up to a 3 dB reduction in the mixing noise at frequencies above $St = 0.2$. At the sideline and upstream polar angles, jets exhausting from the aft deck nozzle, have slightly lower noise levels, on the order of 1 dB, at frequencies above $St = 1$. However, in the $\phi = 270^\circ$, $\theta = 45^\circ$ direction, the aft deck spectrum shows increased high frequency noise levels compared with the baseline nozzle. These increased levels may be the result of scattering or additional trailing edge noise associated with the aft deck. At the upstream polar angles, there is again a reduction in the high frequency noise for jets exhausting from the aft-deck nozzle. The reduction in high-frequency noise, known to be generated near the nozzle exit, is likely due to aft-deck shielding.
Overall sound pressure levels of jets exhausting from both the aft deck and baseline nozzles operating at Test Point 4, $BPR = 0.50$ are shown in Figure 5.28. The left panel presents the directivity in the $\phi = 0^\circ$ azimuthal direction and the directivity in the $\phi = 270^\circ$ azimuthal direction is shown in the right panel. Notably, in the $\phi = 0^\circ$ direction, the aft deck nozzle demonstrates an approximately 2 dB OASPL reduction in the peak noise levels compared with the baseline nozzle at the same operating conditions. In the $\phi = 270^\circ$ direction, at the downstream-most polar angles, the aft deck jet noise levels are higher compared with the baseline nozzle. This is due to the additional high frequency noise similar to that shown in Figure 5.27d. In the $\phi = 270^\circ$ azimuthal direction, the aft deck nozzle demonstrates noise reductions of 1-1.5 dB OASPL at the highest polar angles.
Figure 5.27: Far-field spectra for jets exhausting from the DSR-FS-AD and DSR-FS-BL nozzles. Jets operating at Test Point 4, \( BPR = 0.5 \). Top row: \( \phi = 0^\circ \) azimuthal direction. Bottom row: \( \phi = 270^\circ \) azimuthal direction.
Figure 5.28: Directivity of jets exhausting from the DSR-FS-AD and DSR-FS-BL nozzles. Jets operating at Test Point 4, $BPR = 0.5$. 

(a) $\phi = 0^\circ$ azimuthal direction

(b) $\phi = 270^\circ$ azimuthal direction
5.5.0.2 Aft Deck Effects: DSR-Sym-BL Nozzle

This subsection focuses on the DSR-Sym-BL nozzle and its aft deck variant: the DSR-Sym-AD nozzle. Far-field spectra of jets exhausting from both nozzles operating at Test Point 1, $BPR = 0.20$ are shown in Figure 5.29. The top row displays the far-field spectra measured in the $\phi = 0^\circ$ direction while the bottom row shows the spectra measured in the $\phi = 270^\circ$ direction.

The presence of the aft deck has little influence on the $BBSAN$ between the $90^\circ$ and $120^\circ$ polar angles. The addition of an aft deck also results in a reduction or elimination of the screech tone. The turbulent mixing noise peak in the $\theta = 45^\circ$ direction is not significantly affected by the aft deck. While there are some reductions in the higher-frequency noise

Figure 5.29: Far-field spectra for jets exhausting from the DSR-Sym-AD and DSR-Sym-BL nozzles. Jets operating at Test Point 1, $BPR = 0.02$. Top row: $\phi = 0^\circ$ azimuthal direction. Bottom row: $\phi = 270^\circ$ azimuthal direction.
due to the aft deck in the $\phi = 270^\circ$ direction, at $\theta = 45^\circ$, the directivities will show that there are not necessarily noise reductions across the peak directivity polar angle arc.

The directivities of jets exhausting from the DSR-Sym-AD and DSR-Sym-BL nozzle operating at Test Point 1 are shown in Figure 5.30. In the $\phi = 0^\circ$ azimuthal direction, shown in Figure 5.30a, the aft deck has no effect on the peak noise levels, although there is some reduction at polar angles farther downstream. In the $\phi = 270^\circ$ azimuthal direction, shown in Figure 5.30b, the directivity peak is shifted upstream compared with the baseline nozzle. The upstream shift in directivity causes the aft deck jet noise levels to increase by up to 2dB OASPL, compared with the baseline jet. There is little to no difference between the aft deck and baseline jets at polar angles greater than $80^\circ$.

![Figure 5.30: Directivity of jets exhausting from the DSR-Sym-AD and DSR-Sym-BL nozzles. Jets operating at Test Point 2, BPR = 30%.](image)

Far-field spectra of jets exhausting from the DSR-Sym-AD and DSR-Sym-BL nozzles operating at Test Point 4, $BPR = 50\%$ are shown in Figure 5.31. In both azimuthal directions, at downstream polar angles, aft deck spectra have higher mixing noise peaks compared with the baseline jets. At higher polar angles there is little difference between the baseline and aft deck jet spectra.

The directivities of jets exhausting from the DSR-Sym-AD and DSR-Sym-BL nozzle operating at Test Point 4 are shown in Figure 5.32. In the $\phi = 0^\circ$ azimuthal direction (Figure 5.32a), the aft deck jet has a broader directivity peak that results in increased noise levels between $24^\circ \leq \theta < 42^\circ$. The aft deck jet directivity in the $\phi = 270^\circ$ direction (Figure 5.32b) shows an upstream shift in the directivity peak. This trend was also
observed for the $BPR=0.20$ condition shown in Figure 5.30b. For a $BPR$ of 0.50, the peak aft deck jet noise levels are 1.5-2 dB OASPL higher than the baseline jet levels at the same polar angles. At the upstream polar angles, the aft deck jets have similar noise levels as the baseline jets.

Figure 5.31: Far-field spectra for jets exhausting from the DSR-Sym-AD and DSR-Sym-BL nozzles. Jets operating at Test Point 4, $BPR = 0.50\%$. Top row: $\phi = 0^\circ$ azimuthal direction. Bottom row: $\phi = 270^\circ$ azimuthal direction.
Figure 5.32: Directivity of jets exhausting from the DSR-Sym-AD and DSR-Sym-BL nozzles. Jets operating at Test Point 4, $BPR = 0.5$. 

(a) $\phi = 0^\circ$ azimuthal direction 

(b) $\phi = 270^\circ$ azimuthal direction
6 Summary and Conclusions

6.1 Summary of Goals and Objectives

The primary goal of this dissertation was to gain insight into the aeroacoustic characteristics of low-$BPR$, rectangular, dual-stream supersonic jets. Specific focus was placed on better understanding the effects of dual-stream nozzle configuration and comparing the noise of dual-stream jets with single-stream jets. It was hypothesized that supersonic dual-stream jets would provide a noise reduction benefit compared with supersonic, single-stream jets operating at (estimated) thrust- and mass flow rate-matched conditions. This study included an examination of the mean flow field through flow visualization and Pitot pressure measurements, in tandem with far-field acoustic measurements of single- and dual-stream jets. Flow field measurements provided physical insight into changes to flow field features responsible for measured changes in the far-field noise. The goals of this dissertation were achieved through the completion of the research objectives concisely summarized below:

1. Design and fabricate a series of variable geometry, rectangular, dual-stream nozzles for aeroacoustic experiments.

2. Design and install upgrades to the Penn State High-Speed Jet Aeroacoustics facility for experimental flow field and acoustic measurements of dual-stream jets.

3. Complete a series of flow visualizations and steady Pitot pressure measurements of supersonic dual-stream jets. Use flow field data to quantify the accuracy of jet thrust and mass flow rate estimates and assist in the physical interpretation of acoustic measurements.
4. Perform a series of fundamental acoustic experiments of jets issuing from a dual-stream nozzle with a single fluid shield to gain insight into the effects of jet operating conditions on dual-stream jet noise.

5. Compare the measured noise of heat-simulated, supersonic jets exhausting from two different geometry dual-stream nozzles that were operated at constant (estimated) net thrust and constant (estimated) net mass flow rate conditions to assess the effects of bypass ratio and nozzle configuration on the radiated noise.

6. Measure noise changes due to the addition of an aft deck installed downstream of the dual-stream nozzle exit planes to simulate installed nozzle effects.

7. Compare the far-field noise of dual-stream jets with single-stream jets operating at equivalent thrust and mass flow rate conditions to determine whether dual-stream jets offer a noise reduction benefit.

The first two objectives were met and described in the upgrades to the High-Speed Jet Aeroacoustics facility and design of the dual-stream nozzles in Sections 2.2 and 2.3, respectively. The third objective was completed using a high-speed schlieren system and a single Pitot probe installed on computer-controlled traversing stages. The fourth objective was completed through a series of acoustic experiments aimed at isolating the effects of core jet pressure ratio, core jet temperature ratio, core jet velocity and bypass jet pressure ratio. The effects of changes to bypass-to-core jet velocity ratio, and net thrust, on the far-field noise are also considered. The fifth objective was completed using two different geometry dual-stream rectangular nozzles: one with a single fluid shield (DSR-FS-BL nozzle), and one with two, symmetric fluid shields (DSR-Sym-BL nozzle). Jet operating conditions are varied to increase $BPR$ from 0.2 to 0.5, while maintaining both (estimated) thrust and mass flow rate constant. The sixth objective was met by testing a series of dual-stream nozzles with an aft deck extension. The final objective was met through comparisons with two single-stream equivalent (SSEQ) nozzles: a rectangular nozzle and a round nozzle, that were operated at mass flow rate and thrust-matched conditions.
6.2 Summary of Primary Results

6.2.1 Mean Flow Measurements

Stream-wise Pitot pressure measurements were acquired for pure-air jets exhausting from the DSR-FS-BL and DSR-Sym-BL nozzles. Jets were operated at the same pressure ratios as acoustic experiments. Axial Mach numbers were estimated from Pitot pressure data. Within the first $3/D_{Eq}$ downstream of the DSR-FS-BL nozzle exit, the bypass jet was found to have fully-mixed with the core jet. Axial Mach number profiles for the DSR-Sym-BL jets did not show a velocity deficit indicative of the lower-speed bypass jet. DSR-Sym-BL jets were observed to deflect downwards, towards the bypass jet, by approximately $3^\circ$. Schlieren flow visualizations are used to reinforced this observation. It is conjectured that enhanced mixing in the shear layer between the core and bypass jets, prior to the bypass jet becoming fully-mixed, imparts a bias on the direction of the core jet flow.

Within $3/D_{Eq}$ downstream of the nozzle exit, the normalized axial velocity profiles for the DSR-FS-BL and DSR-Sym-BL jets, collapse to a self-similar curve. Closer to the nozzle exit plane, bypass stream of the DSR-FS-BL jet causes the shape of the normalized velocity profile to deviate from the similarity curve.

Shear layer vorticity thicknesses are compared with single-stream rectangular jet data. By and large, both the single- and dual-stream jet shear layer thicknesses overlap over the first $10/D_{Eq}$ downstream of the nozzle exit. The vorticity thickness for the DSR-Sym-BL jet operating at the lowest BPR condition deviates to higher values between $6 \leq X/D_{Eq} \leq 7$.

Centroid line velocity profiles are compared with single-stream rectangular jet data. In general, the dual-stream jets have shorter potential core lengths compared with the single-stream jet. But, downstream of the potential cores, the centroid line velocities of the single- and dual-stream jets decay at similar rates. As well, the dual-stream jet shock cell spacing, estimated from centroid line velocities, compares to within 6% of theoretical models developed for single-stream jets.

Pitot pressure data acquired at the exit planes of the dual-stream nozzles are used to inspect the exit flow uniformity as well as assess the accuracy of thrust and mass flow rate estimates. It was found that the experimentally estimated thrust values agree to within 6% of the theoretical estimates, while the experimentally estimated mass flow rates agree to within 1% of the theoretical estimates.
6.2.2 Acoustic Measurements of Dual-Stream Jets with a Single Fluid Shield

A series of acoustic experiments aimed at isolating the effects of core jet pressure ratio, core jet temperature ratio, core jet velocity and bypass jet pressure ratio have been conducted using the DSR-FS-BL nozzle. To isolate these effects, one operating condition, either $NPR_C$, $TTR_C$, or $NPR_{BP}$ was varied, while the other two were maintained at constant values. The effects of changes to bypass-to-core jet velocity ratio, and net thrust, on the far-field noise were also studied.

Increasing either the core jet pressure ratio or core jet total temperature ratio resulted in an increase in the peak noise levels. As well, the direction of maximum noise emission shifted to higher polar angles. Both trends are consistent with increasing core jet velocity. As well, the $BBSAN$ was found to saturate as core jet total temperature ratio was increased above 1.6. This is consistent with results observed for single-stream jets.

For constant, subsonic core jet velocities, relative to the ambient speed of sound, increasing the velocity of the bypass jet was found to increase the noise radiated to polar angles of $45^\circ$ and $90^\circ$. This shows that for relatively low-speed core jets, the relatively thin bypass jet can contribute to the large-scale mixing noise as well as the fine-scale mixing noise. Inspection of the spectra in the $45^\circ$ direction confirm that the noise increase is broadband, across all frequencies, including the mixing noise peak, and not just due to the addition of high-frequency noise.

In the $\phi = 0^\circ$ azimuthal direction, increasing the bypass jet pressure ratio has negligible influence on the noise radiation to any polar angle. For this study, the core jet was operated at a heat-simulated, supersonic jet Mach number of 1.36. But in the $\phi = 270^\circ$ azimuthal direction, increasing the bypass pressure ratio reduced the peak noise levels. For the highest bypass pressure considered, there was a substantial change to the jet directivity. This is thought to be due the mixed jet deflecting towards or away from the far-field microphones.

The effects of bypass-to-core jets velocity ratio were investigated in both azimuthal directions. In the $\phi = 270^\circ$ azimuthal direction, the bypass jets exhaust in a plane bisecting the far-field microphones and the heat-simulated core jet. The data show that the noise radiated to a polar angle of $\theta = 45^\circ$ decreases with increasing velocity ratio. The noise levels decrease most rapidly in the $\phi = 270^\circ$ azimuthal direction. This trend shows that the bypass jet can be an effective fluid shield for reducing the radiated noise.
The effects of thrust on radiated noise were also considered. In the $\phi = 270^\circ$ azimuthal direction, the noise radiated to a polar angle of $\theta = 45^\circ$ monotonically increases with thrust. In the $\phi = 0^\circ$ azimuthal direction the trend is not as consistent: there is an initial increase in the noise with thrust, but then the noise levels plateau. The exact reasons for this trend are not yet clear and require further investigation.

### 6.2.3 Effects of Bypass Ratio and Nozzle Configuration on Far-Field Acoustics

The effects of bypass ratio and nozzle configuration were studied though a series of acoustic measurements featuring the DSR-FS-BL and DSR-Sym-BL nozzles. Jet operating conditions were chosen to hold both total mass flow rate and net thrust constant while $BPR$ was increased from 0.20 to 0.50.

It was found that nozzle configuration can affect which components of noise, either turbulent mixing noise or $BBSAN$, are most susceptible to changes in jet operating conditions. It was found that in either azimuthal direction, the turbulent mixing noise levels of jets exhausting from the DSR-FS-BL nozzle are largely unaffected by increased in jet $BPR$. The jet spread rate and shear layer thickness do not vary with bypass ratio. As a results of this, the peak overall sound pressure levels, which are generated by large-scale turbulent mixing, remain constant for changes in $BPR$.

However, as the DSR-FS-BL jet $BPR$ is increased, an elimination of the $BBSAN$ peak at upstream angles is observed. Centroid line Pitot pressure measurements and schlieren flow visualizations confirmed that this is due to a weakening of the shock cell structure of the jet. The elimination of the $BBSAN$ results in the DSR-FS-BL jet, with a $BPR$ of 0.50, being 3-4 dB OASPL quieter than the $BPR = 0.30$ condition. These results were unexpected, because for the $BPR = 0.50$ conditions, the core jet is supersonic and over-expanded, while the bypass jet is supersonic and over-expanded, and therefore, both jets were expected to support shock waves.

In the $\phi = 0^\circ$ azimuthal direction, the peak noise levels of the DSR-Sym-BL jets decrease by 3.5 dB OASPL as the bypass ratio is increased from 0.20 to 0.50. At upstream polar angles, weakening of the $BBSAN$ peak results in upstream noise reductions of 2 dB OASPL as $BPR$ is increased. Interestingly, the shock-noise reductions are not as great as those measured for the DSR-FS-BL jets, although the core and bypass jets were operated at the same pressures and their nozzles have the same design Mach numbers. This suggests
that dual-stream nozzle configuration, or the placement of the bypass jet(s) relative to the core jet, affects the shock noise levels.

Although the peak noise levels of the DSR-Sym-BL jets decrease with increasing \( BPR \), for a bypass ratio of 0.50, both the DSR-FS-BL and DSR-Sym-BL jets have equivalent peak noise levels. Whether or not the peak noise levels of the DSR-Sym-BL jets continue to decrease as \( BPR \) is increase further is worthy of future investigation.

Dual-stream nozzles with aft deck extensions were designed to be generic representations of installed nozzles to simulate the effects nozzle integration with the aircraft fuselage. To summarize the effects of an aft deck on jets exhausting from the DSR-FS-AD nozzle: at lower bypass ratios, the aft deck has little influence on the radiated noise in either the \( \phi = 0^\circ \) or \( \phi = 270^\circ \) azimuthal directions. When the bypass ratio is increased to 0.50, there is an approximately 2 dB OASPL reduction in the peak mixing noise in the \( \phi = 0^\circ \) direction. In the \( \phi = 270^\circ \) direction, at \( BPR = 0.50 \), the aft deck can reduce upstream noise levels by up to 1.5 dB OASPL. For the DSR-Sym-AD nozzle, in the \( \phi = 270^\circ \) azimuthal direction, there is an upstream shift in the peak noise polar angle compared with the baseline nozzle. This results in noise increases at polar angles between 60\(^\circ\) and 80\(^\circ\).

### 6.2.4 Comparison of Acoustic Measurements of Dual-Stream Jets with Single-Stream Jets

The far-field noise of dual-stream jets has been compared with with single-stream jets operating at equivalent thrust and mass flow rate conditions to determine whether dual-stream jets offer a noise reduction benefit. A rectangular SSEQ nozzle was designed based off the dual-stream nozzle design, and operated at equivalent thrust and mass flow rate conditions as the dual-stream jets. As well, a SSEQ round nozzle, which has been extensively tested at Penn State over the past six years, was operated at equivalent thrust and mass flow rates, on a per unit area basis, as the dual-stream jets. The designs and operation of the SSEQ nozzles are described in Sections 2.4 and 2.5.5, respectively. Acoustic comparisons of single- and dual-stream jets were made for both low-\( BPR \) (\(< 0.5\)) and elevated-\( BPR \) (\(0.5 < BPR < 1.0\)) conditions. Complete experimental results are reported in Section 5.4.

Jets with \( BPR \)'s of 0.30 and 0.50, exhausting from both the DSR-FS-BL and DSR-Sym-BL nozzles, were compared with the SSEQ-Rect and SSEQ-Rnd jets. Comparisons were
made for acoustic measurements obtained in the $\phi = 0^\circ$ azimuthal direction. Operating at a bypass ratio of 0.30, both the DSR-FS-BL and DSR-Sym-BL jets are up to 4 dB OASPL louder than the SSEQ-Rect jet. But, the peak dual-stream jet noise levels at these conditions are within 1 dB OASPL of the SSEQ-Rnd jet. As the $BPR$ is increased to 0.50, the peak noise levels of the DSR-Sym-BL jet are about 2 dB OASPL quieter than the SSEQ-Rnd jet. But, for a bypass ratio of 0.50, The dual-streams jet are 2-3 dB OASPL louder than the SSEQ-Rect jet. At upstream polar angles, the dual-stream jet spectra have higher-amplitude, high-frequency noise content, as well as greater levels of broadband shock-associated noise, compared with SSEQ jets. It the thought that the relatively thin bypass jets are responsible for the addition high-frequency noise content. This added noise results in the dual-stream jets being 4-5 dB OASPL louder than the equivalent single jets.

Far-field acoustic measurements of DSR-FS-BL jets operating at elevated bypass ratios of 0.65 and 0.91 were then compared with the SSEQ jets. Acoustic data for both the dual- and single-stream jets were acquired in the $\phi = 0^\circ$ and $270^\circ$ azimuthal directions. In the $\phi = 0^\circ$ azimuthal direction, the peak noise levels of the SSEQ-Rect and dual-stream jets were within 1 dB OASPL of each other. But, at upstream angles, the higher-frequency content of the dual-stream jets resulted in 2-2.5 dB OASPL increase over the SSEQ-Rect jet. However, in the $\phi = 270^\circ$ azimuthal direction, the DSR-FS-BL jets demonstrated a noise reduction benefit compared with the SSEQ-Rect and SSEQ-Rnd jets. The peak noise levels of the DSR-FS-BL jet, exhausting with a $BPR$ of 0.65, are 2-2.5 dB OASPL quieter than the equivalent single-stream rectangular and round jets. Increasing the $BPR$ to 0.91 results in peak noise reductions of 4 dB OASPL compared with the SSEQ-Rect and SSEQ-Rnd jets. The presence of a fluid-shield can indeed result in dual-stream jet noise reductions compared with single-stream jets. But, for the range of operating conditions considered, the bypass pressures and $BPR$’s required to achieve this benefit may not be feasible for full-scale aircraft operation. Additional testing is suggested in the following section to explore this further.

6.3 Recommendations for Future Work

A series of additional experimental and computational studies are recommended in order to gain more thorough insight into the noise radiated from supersonic, dual-stream jets as well as improve understanding of the underlying physical mechanisms within the jet flow responsible for noise generation. Future work should attempt to better understand how nozzle design parameters, e.g., bypass jet shape and sizing, and jet operating conditions
affect radiated noise levels. An approach should be taken to focus on jet operation with practical considerations for realistic engine operating conditions, with a goal on progressing towards larger-scale testing at industry facilities. An outline for the approach to work towards this goal is described by the studies recommended below.

In comparing the far-field acoustics of dual-stream jets with single-stream equivalent jets, it is critical that the single-stream jet operating conditions be chosen appropriately, and accurately. It has been argued that appropriate equivalent single-stream jet conditions are those that match the net thrust and the net mass flow rate of the dual-stream jets. To this end, care must be taken to accurately determine the thrust and mass flow rate levels of the dual-stream jets. In this work, these values were estimated from a one-dimensional treatment of each jet, in combination with an estimate for losses due to oblique shock waves. Because the dual-stream nozzles are rapid-prototyped, nozzles can be fabricated, or machined, to be outfitted with static pressure taps along the sidewalls and upper and lower surfaces of the core and bypass jets. Measuring the axial and cross-stream static pressure distributions within the nozzle, particularly at, or as near as possible, to the nozzle exit plane could be used to validate, or improve dual-stream thrust estimates. Such measurements could be performed in tandem with more spatially-resolved Pitot pressure or PIV measured of the jet at the nozzle exit plane.

In working towards improved methods of determining dual-stream thrust and mass flow estimates, it is recommended that a series of RANS CFD simulations also be conducted. The existing Pitot pressure data reported in this dissertation, along with potential static pressures measurements within the nozzle, would serve at the experimental database to verify the accuracy of the simulations. Once verified, simulations for unheated, and heated (or helium-air mixed species) simulations should be performed to determine the thrust and mass flow rates for various dual-stream jet operating conditions. A parametric numerical study could be performed over a practical range of dual-stream jet operating conditions to inform a computational database of thrust and mass flow rate values. Once developed, a reduced-order model, such as that described by Crowell and Myers [199], could be developed in order to interpolate the performance metrics, e.g., thrust, mass flow, of dual-stream jet conditions not explicitly simulated. In this way, the reduced-order model could be used to predict sets of mass flow rate and thrust-match dual-stream jet conditions for experimental aeroacoustic measurements. A verification of the predicted thrust levels could be obtained through the fabrication of a bench-top thrust stand, such as those described by Valdez and Tinney [111] and Nardozzo [200].
Additional experiments should work towards simulating the aircraft exhaust at aircraft carrier take-off conditions. With the operation of the afterburner at take-off, jet exhaust temperatures can be in excess of 2000 K, or $TTR \geq 7.0$, [90, 201, 202]. It is conjectured that the increased velocity mismatch between an unheated bypass jet, and a highly-heated core jet, will affect the aeroacoustic performance of dual-stream jets. Due to greater difference in jet velocities, it is anticipated that the bypass jet will persist for farther distance downstream before fully mixing with the core jet. In addition, the greater density ratio between the two jets further alters the acoustic impedance at the shear layer between the two jets. Taken together, these effects are theorized to increase the noise reduction benefit of the fluid shield. A tentative set of dual-stream jet operating conditions aimed at testing this theory, at elevated (simulated) core jet total temperature ratios, is proposed in Table 6.1. The equivalent single jet conditions are: $NPR_{SSEQ} = 2.85$, $TTR_{SSEQ} = 2.52$.

Included in this test matrix are bypass ratios of 0.60 and 0.70. It would be worthwhile to consider testing the DSR-Sym-BL nozzle at a bypass ratios above 0.50 to investigate whether the trend of decreasing peak noise levels with increasing $BPR$ is consistent. Alternatively, one could also consider inverted velocity ratios as a means of dual-stream jet noise reduction. Previous studies on high-$BPR$ dual-stream jets [93] have demonstrated noise noise reduction benefits. In principle, an inverted velocity ratio could be achieved in a full-scale, multi-stream engine exhaust by installing an afterburner in the tertiary bypass stream nozzle.

Increased temperature ratio experiments could be coupled with additional CFD simulations. Simulations could be performed at temperature ratios above the limits of helium-air mixture experiments. It would be worthwhile for simulations to investigate the effects of the cooler bypass jet on the infrared signature of the aircraft exhaust. The infrared signature of the

<table>
<thead>
<tr>
<th>Operating Conditions</th>
<th>$T_{net}$ (N)</th>
<th>$\dot{m}_{net}$ (kg/s)</th>
<th>$BPR$</th>
<th>$U_{JB}/U_{JC}$</th>
<th>$T_{BP}/T_{C}$</th>
</tr>
</thead>
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<tr>
<td>NPR$_C$</td>
<td>NPR$_{BP}$</td>
<td>TTR$_C$</td>
<td></td>
<td></td>
<td></td>
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<tr>
<td>3.92</td>
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<td>1.20</td>
<td>120</td>
<td>0.20</td>
<td>0.21</td>
</tr>
<tr>
<td>3.76</td>
<td>3.86</td>
<td>1.56</td>
<td>120</td>
<td>0.20</td>
<td>0.30</td>
</tr>
<tr>
<td>3.63</td>
<td>4.18</td>
<td>1.91</td>
<td>120</td>
<td>0.20</td>
<td>0.40</td>
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<tr>
<td>3.50</td>
<td>4.53</td>
<td>2.23</td>
<td>120</td>
<td>0.20</td>
<td>0.50</td>
</tr>
<tr>
<td>3.38</td>
<td>4.91</td>
<td>2.52</td>
<td>120</td>
<td>0.20</td>
<td>0.60</td>
</tr>
<tr>
<td>3.29</td>
<td>5.26</td>
<td>2.75</td>
<td>120</td>
<td>0.20</td>
<td>0.70</td>
</tr>
</tbody>
</table>
hot exhaust is one means of detecting tactical aircraft. The addition of a cooler, tertiary fan flow in fighter aircraft exhaust may reduce the spectral infrared radiation and improve aircraft stealthiness [150].

Lastly, further studies on dual-stream nozzle design and configuration should be conducted. Acoustic measurements have shown that the positioning of the bypass streams relative to the core jet can affect which noise components are reduced (or increased) with changes in jet operating conditions. Furthermore, comparisons with single-stream jets showed the potential for noise reduction benefits when the fluid shield exhausted in a plane between the microphone and core jet. In the aircraft carrier environment, shipboard personnel will be the side of the aircraft during take-off, and under it during fly-overs. Therefore, it is worthy to consider fabricated a dual-stream jet model with a 'U-shaped' bypass jet that exhausts on three sides of the core jet. To this end, a model with this geometry is currently being designed for aeroacoustic testing at Penn State.

Hopefully this study has inspired continued research into the low-$BPR$, dual-stream, supersonic, rectangular jets. The recommendations for future small-scale testing and computational simulations, outlined above, lay out the additional framework for improved understanding of the aeroacoustic and performance (i.e. thrust) characteristics of the complex jet flows. It is hoped that through, continued, rigorous research, industry-scale experiments will be justified, and, in the future, implementation on future generations of aircraft.
Appendix A
Photographs of Dual-Stream Jet Facility Upgrades

This appendix presents additional photographs of the dual-stream jet plenum assembly and installation in the PSU High-Speed Anechoic Jet facility. Photographs of the flow conditioning components are shown in Appendix A.1 and photographs of the assembly of the dual-stream plenum are shown in Appendix A.2.

A.1 Photographs of Dual-Stream Plenum Flow Conditioning

This appendix section shows photographs of the core and bypass air flow conditioning within the dual-stream plenum. A dimensioned drawing and photograph of the core flow condition are provided in Figure A.1. The core flow conditioning consists of a perforated cone upstream of aluminum honeycomb. Both flow conditioning components are bolted to a polycrylic sleeve that is bolted within the core flow plenum, which is shown in Figure A.3. A photograph, looking upstream, of the partially-assembled dual-stream plenum is shown in Figure A.2. The bypass jet honeycomb flow straightening is marked in the photograph.
(a) Schematic of core jet flow conditioning in dual-stream jet plenum.

(b) Photograph of core jet flow conditioning in polycrylic sleeve.

Figure A.1: Photograph and schematic drawing of the core flow conditioning sleeve.

Figure A.2: Photograph of the dual-stream plenum, looking upstream, with focus on honeycomb flow straightening in bypass flow.

A.2 Photographs of the Dual-Stream Plenum Assembly and Installation
Figure A.3: Core flow plenum.

Figure A.4: Assembled, outer, aft end of dual-stream jet plenum

Figure A.5: Photograph of the bypass flow Pitot tube installed in dual-stream plenum.
Figure A.6: Photograph of the partially assembled plenum. Secondary core plenum supports are labeled.
Figure A.7: Photograph of the fully-assembled and installed dual-stream jet plenum.
Appendix B
Photographs and CAD Drawings of Dual-Stream Nozzle Components

B.1 Photographs of Dual-Stream Nozzle Components

This section shows additional photographs of the components of the dual-stream jet models. The major model components, their fabrication material, and their function are summarized in Table 2.2. Additional details of the model design are discussed in Section 2.3. Annotated photographs of model Stage I, that transition the cross-sectional shape of the air flow from circular to rectangular, are shown in Figures B.1 and B.2. Photographs of Stage III, that forms the core flow converging-diverging section and first half of the bypass converging section, are shown in fig. B.3. The second half of the bypass flow converging section is formed by the dual-stream nozzle, Stage IV, and is shown, attached to Stage III, in Figure B.4. Photographs of the fully-assembled dual-stream model are shown in Figures B.5 and B.6.
Figure B.1: Head-on view, looking upstream, of dual-stream model Stage I, the circular-to-rectangular cross-section transition. Part was rapid-prototyped using the PolyJet HD method with RGD720 as the material.

(a) Head-on view, looking downstream, of dual-stream model Stage I with focus on the Pitot probe in the core flow. (b) Close-up photographs of Pitot tube interface with Stage I on the outside of the model.

Figure B.2: Photographs of the core flow Pitot tube installed in Stage I.
(a) Side view of Stage III with side wall removed and without nozzle.

(b) Head-on view of Stage III without nozzle.

Figure B.3: Annotated photographs of dual-stream model Stage III.

Figure B.4: Side view of Stage III with DSR-FS-BL nozzle attached. Approximate contour of bypass jet flow path is outlined by the dotted, blue curves.
Figure B.5: Photograph of the fully-assembled dual-stream jet model looking along the major axis of the nozzle.
Figure B.6: Photograph of the fully-assembled dual-stream jet model looking along the minor axis of the nozzle.
B.2 CAD Drawings of Dual-Stream Nozzle Components

This section shows the dimensioned, engineering drawings of the CAD models of all metal components of the dual-stream jet model shown in Figures 2.7 and 2.9 and listed in Table 2.2. These are the engineering drawings that were used for part fabrication. Specifically, drawings are shown for 1) model Stage 0, in Figures B.7 to B.9, 2) the mating flange plates between Stage I and II, and Stage II and III in Figures B.10 and B.11, respectively, and 3) model Stage III in Figures B.12 to B.16.
Drill and Tap to 1/4-20 10 screw holes evenly spaced 36
7.125 bolt circle
5.125 1/8in O-ring groove
6.625 To straight thread onto size 6 pipe
2.75 8 per inch
4.25
8.00
.75
1.50
6.25
SECTION A-A
SCALE 1 : 3

Figure B.7: Dimensioned drawing of the aluminum outer component of dual-stream model Stage 0. The outer component threads on to the outer, 15.24 cm NPT plenum pipe. Stage 0 is the adapter that attached models to the dual-stream plenum.
Figure B.8: Dimensioned drawing of the aluminum inner component of dual-stream model Stage 0. The inner component threads on to the inner, 10.16 cm NPT plenum pipe. Stage 0 is the adapter that attached models to the dual-stream plenum.
Figure B.9: Dimensioned drawing of the aluminum attachment plate for dual-stream model in place. The plate bolts on to the outer Stage 0 component and holds the dual-stream model in place.

Note: Material thickness can be up to 0.25in depending on what is on hand.
Figure B.10: Dimensioned drawing of the aluminum flange plate between dual-stream model Stages I and II. Stage I is the circular-to-rectangular cross-section transition and Stage II is the contraction to final width.

Stage 1-2 Flange Plate
SMH 12 July 2017
Aluminum
QTY: 2
Figure B.11: Dimensioned drawing of the aluminum flange plate between dual-stream model Stages II and III. Stage II is the contraction to final width and Stage III is the core converging-diverging section as well as bypass converging section.
Figure B.12: Dimensioned drawing of the stainless steel Stage 3A. Two Stage 3A parts are used to form the upper and lower surfaces of the core nozzle converging-diverging section.
Figure B.13: Dimensioned drawing of the stainless steel Stage 3B. Stage 3B forms the outer converging surface of the bypass jet.
Figure B.14: Dimensioned drawing of the aluminum Stage 3C. Two Stage 3C parts form left and right straight sidewalls dual-stream model Stage III.
Figure B.15: Hole dimensioning for the aluminum Stage 3C.
Figure B.16: Dimensioned drawing of the aluminum nozzle attachment plate. Two attachment plates are used to attach the interchangeable dual-stream nozzles to model Stage III.
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URL http://dx.doi.org/10.1016/j.jsv.2010.11.025 67, 142


Vita

Scott Hromisin

Scott M. Hromisin was born in Kingston, Pennsylvania on January 28, 1990 to Ralph and Carol Hromisin. He lived in Larksville, Pennsylvania and graduated from Wyoming Valley West High School in 2008. As a senior in high school, Scott was elected president of both the Student Council and the National Honors Society. He graduated from the Pennsylvania State University in 2013 with a Bachelor of Science degree in Mechanical Engineering. During this time, Scott also completed three, semester-long co-op rotations with NASA’s Marshall Space Flight Center and worked as an undergraduate honors student in the Penn State Applied Research Lab’s Garfield Thomas Water Tunnel. Scott chose to continue his graduate school studies at PSU, working with Dr. Dennis K. McLaughlin. In December 2013, he completed his Masters of Science in Mechanical Engineering, with a focus on laser-Doppler velocimetry measurements of supersonic, impinging jets.

Scott began his doctoral studies in Aerospace Engineering at PSU in 2014, under the co-advisement of Dr. Dennis K. McLaughlin and Dr. Philip J. Morris. He received funding for his doctoral education through the Department of Defense SMART scholarship and worked as an engineering intern with the Applied Aerodynamics and Store Separation Branch at NAVAIR, in Patuxent River, Maryland. The focus of Scott’s doctoral research, both at Penn State and as a NAVAIR intern, was on the design, development, and testing of a dual-stream, supersonic jet models in Penn State’s High-Speed Jet Aeroacoustics facility. After gradation, Scott will work as a civilian employee at NAVAIR. His future research interests include: aeroacoustic performance of multi-stream jets, simulation and reduced-order modeling of supersonic and hypersonic flows, high-speed measurements and diagnostics, and unsteady aerodynamics.