

USM3D-ME Analyses Performed in Support of a Wind Tunnel Test of a Boundary-Layer Ingestion Configuration

Michael D. Bozeman*

NASA Langley Research Center, Hampton, VA, 23681

Boundary Layer Ingestion (BLI) has been proposed as a technology with the potential to decrease fuel burn. However, one major concern for BLI configurations is the potential degradation of the flow quality, both on the airframe and at the fan face, resulting from the tightly integrated propulsor. A wind tunnel test was performed in the National Transonic Facility (NTF) at the NASA Langley Research Center to investigate the flow quality ingested by a tail cone thruster configuration, similar to the Single Aisle Turboelectric Aircraft Concept with Aft Boundary Layer Ingestion (STARC-ABL). The wind tunnel model was a modified version of the Common Research Model (CRM) to include an aft-mounted, flow-through propulsor. The experimental data obtained from the wind tunnel test provide insight into the flow and enables an assessment of the accuracy of the USM3D-ME flow solver for predicting the flow at the fan face, which will be crucial for fan design purposes. Both grid refinement and turbulence model studies were performed for the Clean and Cruise mass flow plug (MFP) configurations at the condition corresponding to $Re_{MAC} = 5$ million, $M_\infty = 0.8$, and $\alpha = 2^\circ$. The selected grid refinement level and turbulence model were then used to perform simulations over the range of conditions considered in the NTF wind tunnel test. The condition sweep comparisons illustrate favorable agreement with the experimental data over the entire range of conditions and for all MFP configurations. The largest differences were observed for the Idle MFP configuration, with approximately 3% difference observed between USM3D-ME and the experimental data. Future work should investigate the impact of higher fidelity turbulence models and grid adaptation on the USM3D-ME predictions.

Nomenclature

Acronyms

AIP	=	Aerodynamic Interface Plane
BLI	=	Boundary Layer Ingestion
CFL	=	Courant-Friedrichs-Lewy condition
CRM	=	Common Research Model
CRM-TCT	=	Common Research Model with Tail Cone Thruster
HANIM	=	Hierarchical Adaptive Nonlinear Iteration Method
MFP	=	Mass Flow Plug
NTF	=	National Transonic Facility
PAI	=	Propulsion-Airframe Integration
QCR	=	Quadratic Constitutive Relation
R	=	Rotation correction
SA	=	Spalart-Allmaras
STARC-ABL	=	Single Aisle Turboelectric Aircraft Concept with Aft Boundary Layer Ingestion
TetrUSS	=	Tetrahedral Unstructured Software System

*Research Aerospace Engineer, NASA Langley Research Center, Configuration Aerodynamics Branch, AIAA Member.

Symbols

A	=	cross-sectional area at AIP, in ²
α	=	angle of attack, degrees
α_{flow}	=	local flow angle of attack, degrees
β_{flow}	=	local flow angle of sideslip, degrees
C_L	=	lift coefficient
C_D	=	drag coefficient
C_m	=	pitching moment coefficient
δ_i	=	distance from wall to node i, in.
γ	=	ratio of specific heats
g	=	gravitational constant, ft/s ²
h	=	distance from the wall, in.
$\dot{m}_{AIP}$	=	mass flow rate at the AIP, lbm/s
M_i	=	Mach number at probe location i
$M_{avg,AIP}$	=	average Mach number at the AIP
N	=	number of cells
P	=	static pressure, psi
P_∞	=	freestream static pressure, psi
P_t	=	total pressure, psi
$P_{t,i}$	=	total pressure at probe location i, psi
$P_{t,avg}$	=	radially-averaged total pressure, psi
$P_{t,\infty}$	=	freestream total pressure, psi
PR_{AIP}	=	average total pressure at the AIP normalized by freestream total pressure
R_{gas}	=	specific gas constant, lbf-ft/lbm-R
Re_{MAC}	=	Reynolds number based on mean aerodynamic chord
r_1, r_2	=	boundary layer growth parameters
T_t	=	total temperature, °F
y^+	=	dimensionless wall spacing

I. Introduction

BOUNDARY Layer Ingestion (BLI) has been proposed as a technology with the potential to decrease fuel burn. BLI configurations feature propulsion systems that are tightly integrated into the airframe such that the boundary layer, or a fraction of it, is ingested by the propulsor. To produce thrust, the propulsor must accelerate the flow to obtain an increase in velocity at the exit relative to the velocity of the ingested flow. The BLI concept is based on the observation that a BLI propulsor ingests lower momentum boundary layer flow providing a higher propulsive efficiency than a conventional underwing propulsor, which ingests freestream flow. However, one major concern for BLI configurations is the potential degradation of the flow quality, both on the airframe and at the fan face, resulting from the tightly integrated propulsor. This flow degradation needs to be addressed to take full advantage of the BLI technology. In the work of Carter et al. [1], a knowledge-based design tool known as CDISC [2] was employed to improve the flow quality through aerodynamic shaping while Owens et al. [3] demonstrated the use of active flow control for improving distortion at the fan face. There have been a variety of BLI applications investigated including distributed propulsion, Blended Wing Body (BWB), and tube-and-wing configurations [1, 3–11]. One example is the Single Aisle Turboelectric Aircraft Concept with Aft Boundary Layer Ingestion (STARC-ABL). The STARC-ABL is a turboelectric concept that employs a full-annular BLI propulsor powered by two underwing turbofans. An illustration of the STARC-ABL concept is provided in Fig. 1.

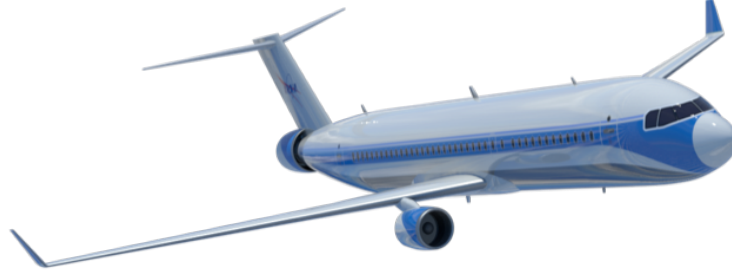


Figure 1. Single-aisle transport aircraft with aft-boundary layer ingestion (STARC-ABL) [12].

The STARC-ABL configuration utilizes a tail cone thruster to reduce the thrust requirements for the underwing turbofan engines. This configuration is considered a symmetric BLI application, which features more uniformly distributed distortion about the circumference relative to embedded, asymmetric BLI applications. Symmetric BLI applications typically feature an increase in wetted area relative to asymmetric applications since the entire nacelle is exposed. However, the improved flow quality reduces the challenges associated with fan design and is generally considered lower risk relative to asymmetric BLI. NASA has invested in the STARC-ABL concept with the goal of quantifying the potential benefits of this configuration. As a first step, a wind tunnel test was performed in the National Transonic Facility (NTF) at the NASA Langley Research Center to both characterize the flow in the inlet and to quantify the propulsion-airframe integration (PAI) effects as a function of flight condition for a configuration representative of the STARC-ABL concept. The wind tunnel configuration is a variant of the Common Research Model (CRM) [13] that includes a flow-through nacelle mounted on the aft portion of the fuselage. This configuration is designated as the Common Research Model with Tail Cone Thruster (CRM-TCT). This paper discusses CFD analyses performed in support of the wind tunnel test with comparisons to experimental data. The analyses performed for this work utilized the NASA Mixed-element USM3D (USM3D-ME) flow solver. An accompanying paper provides CFD results generated using the NASA LAVA flow solver, with comparisons to data collected in the NTF test [14]. Additionally, the NTF wind tunnel test of the CRM-TCT model is described in detail in Chan [15].

II. NTF Wind Tunnel Test

The experiment was performed in the NTF, which is a high-pressure, closed-circuit, cryogenic wind tunnel. The NTF can operate with both air and nitrogen as the medium. In nitrogen mode, the NTF offers the ability to obtain flight Reynolds numbers without requiring an increase in model size. Additionally, the test section features 12 slots and 14 reentry flaps on the floor and ceiling to prevent flow choking that can occur at near-sonic conditions. This allows for an extended Mach number range and/or larger wind tunnel models. The following subsections provide descriptions of the wind tunnel model, instrumentation, and test matrix. Note that the primary focus of this paper is the CFD performed in support of the wind tunnel test. As such, the experiments are only briefly described here. The reader is referred to Chan [15] for more details about the experiment and data obtained.

A. Model Geometry and Configurations

The CRM-TCT model utilized the CRM fuselage and wings, with modifications to the aft-portion of the fuselage to include a flow-through nacelle and T-tail. Additionally, the sting was attached to the underside of the fuselage, compared to the tail-mounted sting used for the CRM model, which required a 4-inch extension to the fuselage forward of the wings. An illustration of the CRM-TCT model is provided in Fig. 2.

In addition to the configuration shown in Fig. 2, a clean configuration without a tail cone thruster was considered to provide insight into the impact of the presence of the aft-mounted nacelle on the airframe. The wind tunnel test considered four, interchangeable mass flow plugs (MFPs). The primary configuration consists of the CRM-TCT with cruise mass flow plug, which was designed to provide a mass flow rate representative of what would be expected at a cruise condition. The other three mass flow plugs considered were designed to provide 90% and 110% of the cruise mass flow rate and an idle MFP. An illustration of the four mass flow plugs is provided in Fig. 3. Additionally, views of the flow exit area for each mass flow plug are provided in Fig. 4 and the flow exit areas are quantified in Table 1.

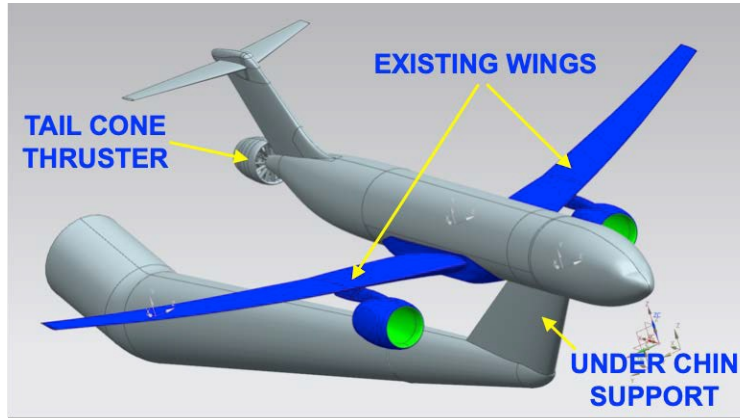


Figure 2. Common Research Model with Tail Cone Thruster (CRM-TCT) [16].

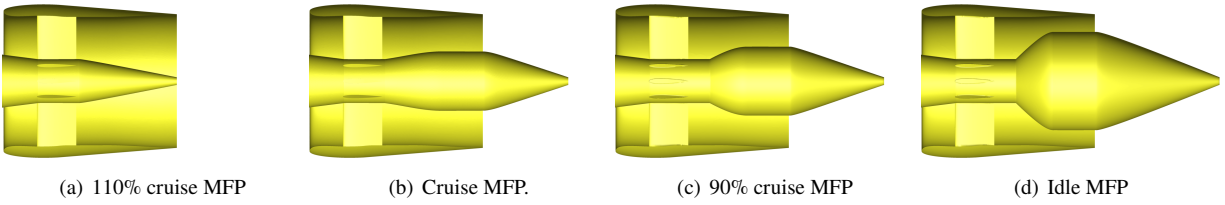


Figure 3. MFP geometries considered for CRM-TCT wind tunnel test.

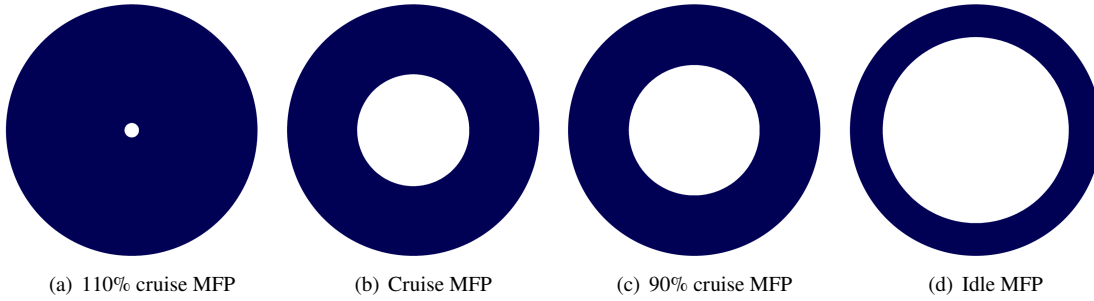


Figure 4. Illustration of flow exit areas for MFP geometries.

Table 1. Flow exit areas for MFP geometries.

MFP	Flow Area (in ²)
110% cruise	8.94
Cruise	7.20
90% cruise	6.56
Idle	4.06

B. Model Instrumentation

A variety of instrumentation was utilized for the CRM-TCT wind tunnel test. Due to existing components of the CRM model being used, the wings and fuselage already featured various static pressure taps. For the given test, static pressure taps were also included on the nacelle and mass flow plugs. Additionally, boundary layer rakes were employed to characterize the boundary layer on the aft fuselage, upstream of the tail cone thruster. Note that boundary layer measurements were obtained for both the cruise mass flow plug and clean configurations. The boundary layer rakes were only installed for select conditions and were removed before collecting inlet data to prevent contamination. Also, note that the forward and aft rakes were not installed simultaneously to ensure clean data measurements were obtained. An illustration of the eight boundary layer rakes is provided in Fig. 5.

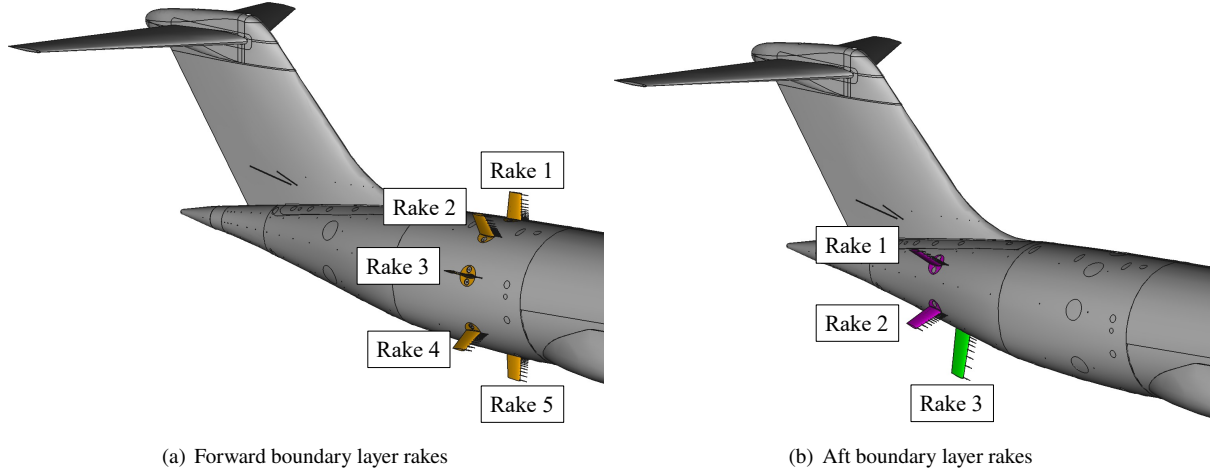


Figure 5. Illustration of forward and aft boundary layer rakes.

Finally, three nacelle rake architectures were employed to provide both flow angularity and total pressure data at the nacelle highlight. Note that the nacelle was designed to rotate to provide high-resolution data at the nacelle highlight. The primary rake architecture used to collect total pressure data featured eight struts, with six probes per strut, which is shown in Fig. 6a. Additionally, the 5-hole probe rake, shown in Fig. 6b, was used to obtain flow angularity measurements. The results described in this paper consider total pressure data obtained using this 48-probe rake and flow angularity data obtained using the 5-hole probe rake. The reader is referred to the accompanying paper for more information regarding the different nacelle rake architectures and other instrumentation details.

C. Test Matrix

The design condition for the CRM-TCT model was designated as $Re_{MAC} = 10 \times 10^6$, $Mach = 0.8$, and $\alpha = 2^\circ$. For the NTF test, the conditions were expanded about the design condition to enable assessments of the impacts of Reynolds number, Mach number, and angle of attack. The test matrix, outlined in Table 2, covered a range of conditions including Mach numbers from 0.75 to 0.85, Reynolds numbers based on the mean aerodynamic chord from 5 to 15 million, and angles of attack from -3 to 4 degrees. Note that not all conditions were run for each configuration.

Table 2. CRM-TCT NTF test matrix.

Re_{MAC} (million)	T_t (°F)	$Mach$	α (deg)
5	120	0.75, 0.80, 0.85	-3° to 3°
10	-50	0.75, 0.80, 0.85	0.5° to 3°
15	-120	0.75, 0.80, 0.85	0.5° to 3°

As shown in Table 2, negative angles of attack were only considered for a Reynolds number of 5×10^6 . Issues were encountered during testing with the model balance fouling due to excessive loads on the horizontal stabilizer at

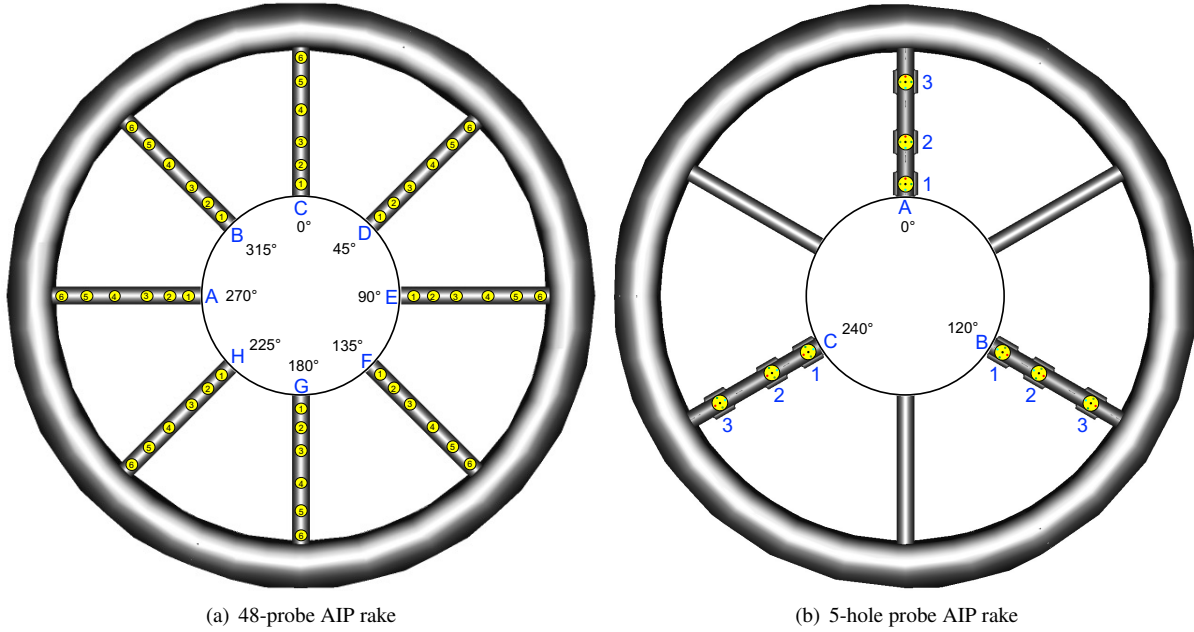


Figure 6. Illustration of 48-probe and 5-hole probe rake architectures.

negative angles of attack. Negative angles of attack were only considered for the lowest dynamic pressure as a result. Additionally, the horizontal stabilizer was removed for these angles.

III. Computational Methods

A. Grid Generation

Computational grids were generated using the HeldenMesh™ v4.14 grid generation software. HeldenMesh™ is an unstructured grid generator that is part of a software suite developed by the Helden Aerospace Corporation. It utilizes multithreading for the rapid generation of both tetrahedral and mixed-element grids. HeldenMesh™ allows the user to specify the desired resolution of the surface grid through a series of inputs including base spacing, minimum spacing, edge spacing, and maximum surface deviation. This process is fairly automated depending on the complexity of the geometry as well as the user preferences. HeldenMesh™ uses cell stretching to resolve surface curvature with anisotropic triangles. The user can control the degree of anisotropy through the maximum aspect ratio definition. HeldenMesh™ utilizes tetrahedral or prismatic elements to resolve the boundary layer based on a user-specified growth rate, number of layers, and initial wall spacing. Finally, the remaining volume is filled with tetrahedral elements, with pyramidal elements used as necessary to provide transitions from prismatic to tetrahedral elements.

For this work, a series of mixed-element grids was generated for the CRM-TCT configuration with the cruise mass flow plug. Note that the wind tunnel sting was included in the computational model. The grids were generated such that the coarsest grid level has a y^+ of 1.0 for the most limiting flow condition. Once the coarse grid was generated, the medium and fine grids were obtained by varying the refinement factor in HeldenMesh™ to provide approximately a factor of three increase in the cell count between successive grid levels. The y^+ was linearly scaled by the refinement factor to simultaneously increase the refinement in the boundary layer. In HeldenMesh™, the boundary layer cell growth is defined by Eq. 1. For this work, the growth rate parameters, r_1 and r_2 , were held constant. The resulting grid sizes are provided in Table 3. Additionally, illustrations of the aft fuselage and symmetry plane for the coarse, medium, and fine grids are provided in Figs. 7 and 8.

$$\delta_{i+1} = \delta_i [1 + r_1 [r_2 + 1]^i]^i \quad (1)$$

Table 3. Grid parameters and sizes for CRM-TCT with cruise MFP.

	Refinement Factor	y^+	r_1	r_2	Cells (million)
Coarse	1.00	1.00	0.125	0.015	35.3
Medium	0.44	0.44	0.125	0.015	94.1
Fine	0.20	0.20	0.125	0.015	281.7

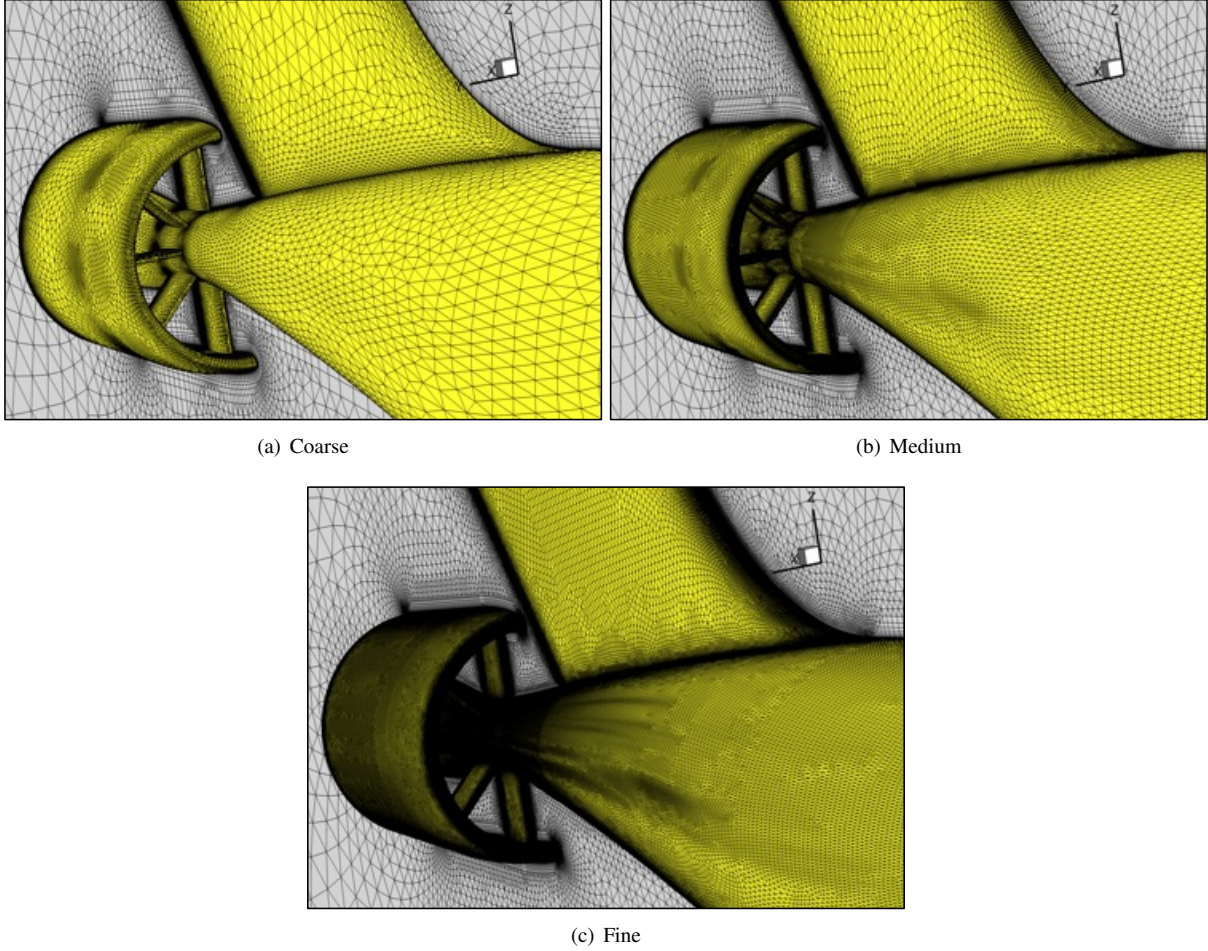
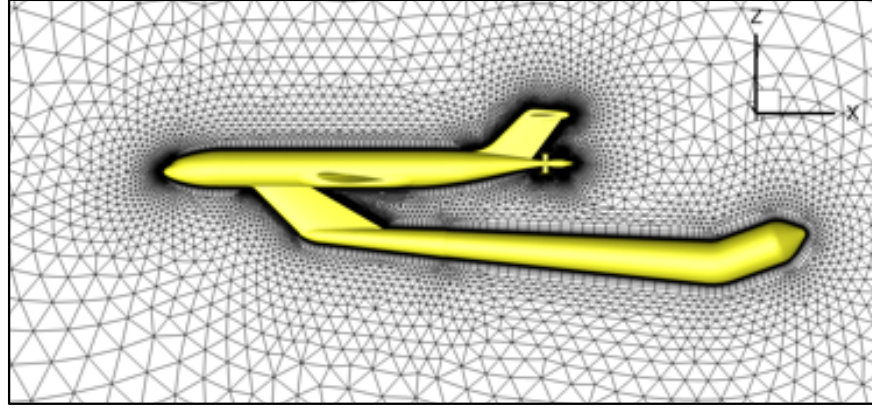
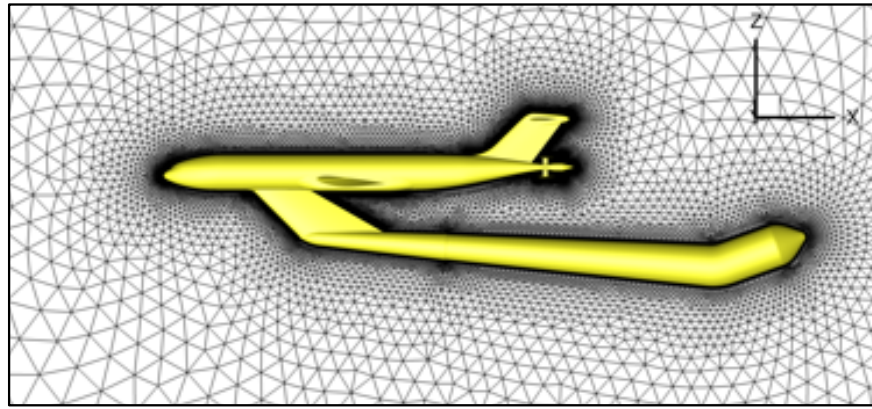


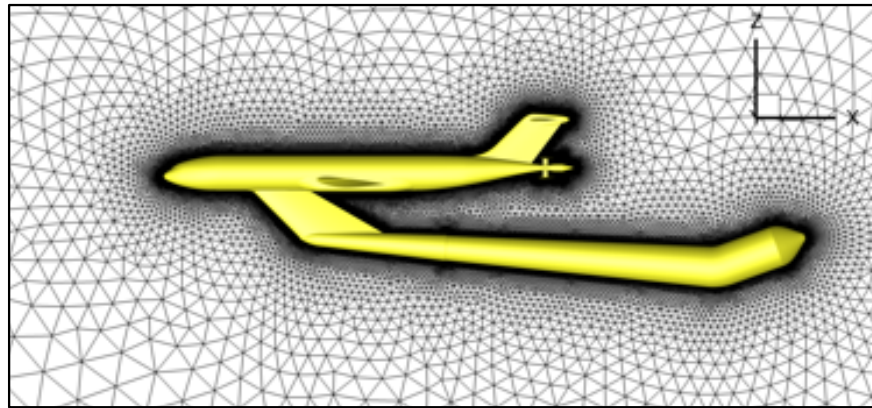
Figure 7. Illustration of aft fuselage for coarse, medium, and fine grid levels.



(a) Coarse



(b) Medium



(c) Fine

Figure 8. Illustration of symmetry plane for coarse, medium, and fine grid levels.

B. USM3D-ME Flow Solver

The mixed-element USM3D (USM3D-ME) flow solver was used for this work, which is an extension of the legacy USM3D flow solver. USM3D is an unstructured-grid, cell-centered, Navier-Stokes solver developed at the NASA Langley Research Center as part of the Tetrahedral Unstructured Software System (TetrUSS) [17, 18]. Compared to the legacy version of the code, USM3D-ME offers compatibility with mixed-element grids featuring tetrahedral, prismatic, pyramidal, and hexahedral elements [19] along with other enhancements to improve robustness and time to

solution. One significant enhancement included with USM3D-ME is the Hierarchical Adaptive Nonlinear Iteration Method (HANIM), which has been shown to be beneficial for some problems through improved robustness, accelerated convergence, and automation [20]. The automation offered by HANIM includes a Courant-Friedrichs-Lewy (CFL) number adaptation capability, which automatically adjusts the CFL number based on the current state of the solution. This can potentially increase the convergence rate and does not require CFL input from the user. For this work, the inviscid terms were computed using Roe's flux difference splitting [21] without a flux limiter. The method for turbulence closure was the one-equation Spalart-Allmaras model with negative provisions (SA-neg) [22], rotation correction (R) [23, 24], and Quadratic Constitutive Relation (QCR-2000) [25]. This model is referred to as the SA-neg-R-QCR model for the remainder of the paper. A turbulence model study was additionally performed, which considered the SA model with negative provisions both without corrections (SA-neg) and with only the rotation correction (SA-neg-R). Finally, HANIM was used for all simulations presented in this paper.

IV. Results

USM3D-ME was used to perform simulations over the range of conditions shown in Table 2. The results were used to both assess the accuracy of USM3D-ME and to evaluate best practices for predicting the flow in the nacelle for the tailcone thruster. The following subsections describe the results of the analyses performed for the CRM-TCT configuration, which include both grid refinement and turbulence model studies for both the clean and cruise MFP configurations, and condition sweeps for the four MFP configurations described in Section II.A. Note that the CRM-TCT wind tunnel test was primarily focused on the PAI effects for a tailcone thruster configuration. However, force and moment data was collected during the test. For the present work, force and moment comparisons were performed over the range of conditions considered in the NTF test. However, due to size limitations, the force and moment comparisons are only provided for the grid refinement and turbulence model studies discussed in the next subsection.

A. Grid Refinement Study

The grid refinement study was performed for both the clean and cruise MFP configurations. Comparisons to experimental data as a function of grid refinement are provided for the test condition corresponding to $Re_{MAC} = 5 \times 10^6$, $M_\infty = 0.8$, and $\alpha = 2^\circ$. Note that initial comparisons illustrated a shift in angle of attack between the experimental data and the CFD solutions. Figure 9a shows a comparison of the lift coefficient (C_L) versus angle of attack (α) for the clean configuration on the medium grid level. The experimental data provided in Fig. 9 has been corrected as described in Chan [15]. From the comparison, it appears that USM3D-ME is overpredicting the lift coefficient relative to the experimental data. However, the drag polar provided in Fig. 9b shows good agreement between USM3D-ME and the experimental data. Based on this observation, all solutions described in this section were lift-matched to the experimental data point by iteratively modifying the angle of attack until the target lift coefficient was achieved.

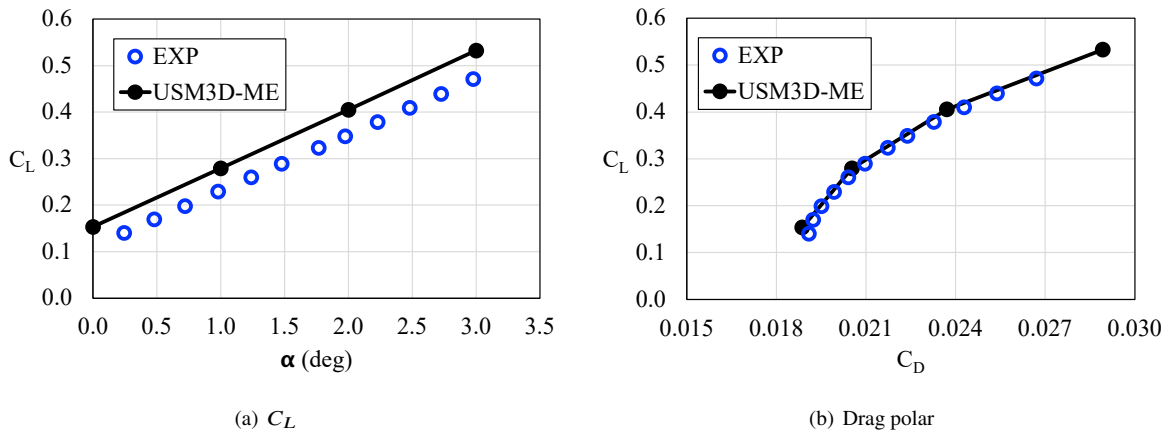


Figure 9. Lift curve and drag polar for clean configuration at $Re_{MAC} = 5 \times 10^6$ and $M_\infty = 0.8$, medium grid.

1. Clean Configuration

The comparisons provided for the clean configuration include integrated force and moment coefficients, static pressure distributions on the aft fuselage, and boundary layer profiles at two axial locations. The force and moment coefficients are provided in Fig. 10 as a function of N , where N is the number of cells in the grid. Figure 10a illustrates successful lift-coefficient matching for all grid levels. The corresponding drag coefficient results provided in Fig. 10b show that the USM3D-ME predictions are moving away from the experimental data with increasing grid refinement. However, the drag coefficient difference between the finest and coarsest grid level was observed to be six counts for a roughly 9x increase in grid size. The pitching moment coefficient results in Fig. 10c show that USM3D-ME underpredicts the pitching moment relative to the experiment, but the results are slowly trending towards the experiment with increasing grid refinement.

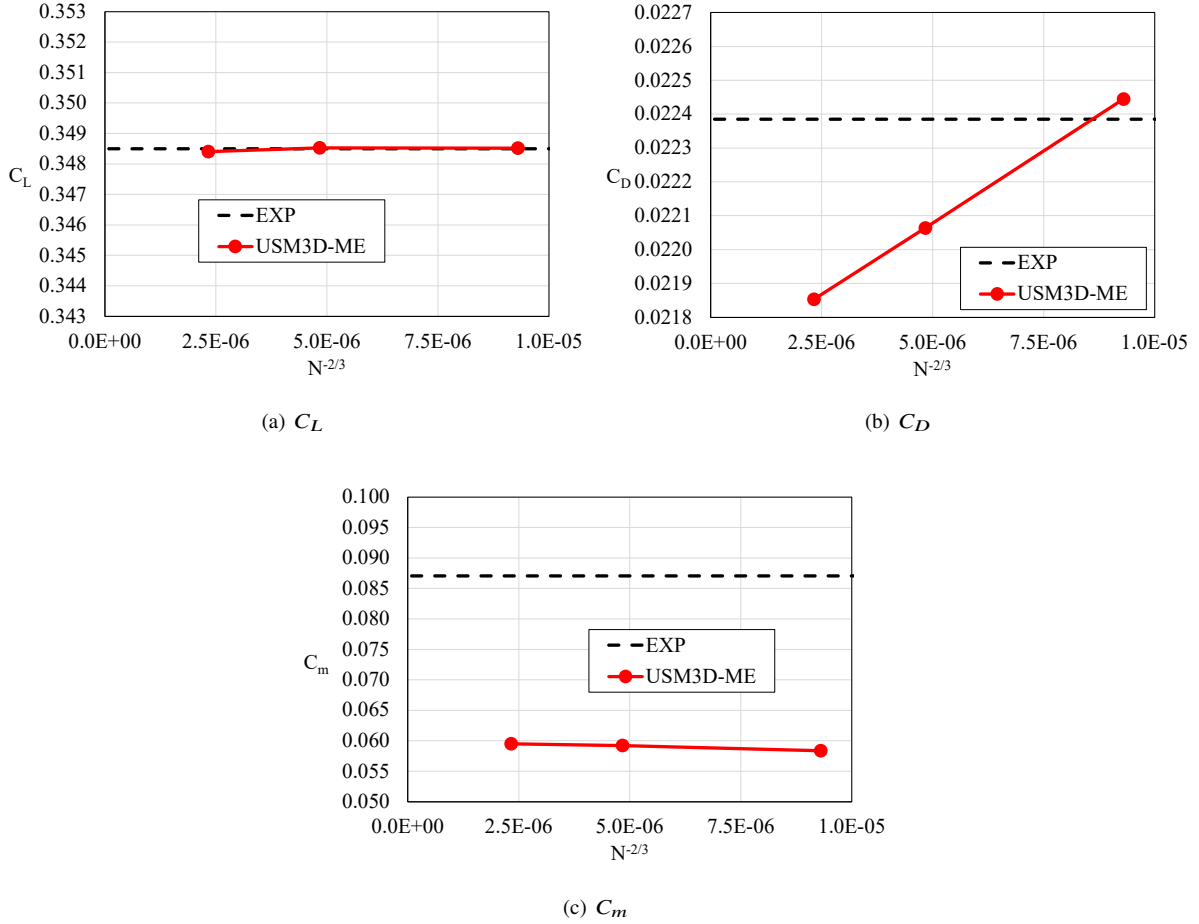


Figure 10. Force and moment coefficients vs. grid size, clean, $Re_{MAC} = 5 \times 10^6$, $M_\infty = 0.8$, and $C_L = 0.36$.

Next, static pressures comparisons are provided for the aft fuselage surface taps illustrated in Fig. 11. The results provided in Fig. 12 show favorable agreement with the experimental data, with the USM3D-ME predictions trending towards the experimental data with increasing grid refinement. The medium and fine grid results are observed to be nearly identical. Based on these results, grid convergence was achieved for this metric.

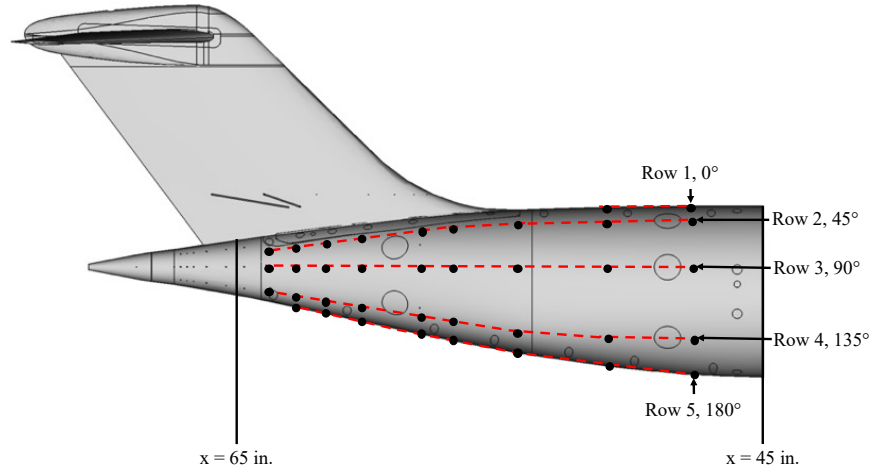
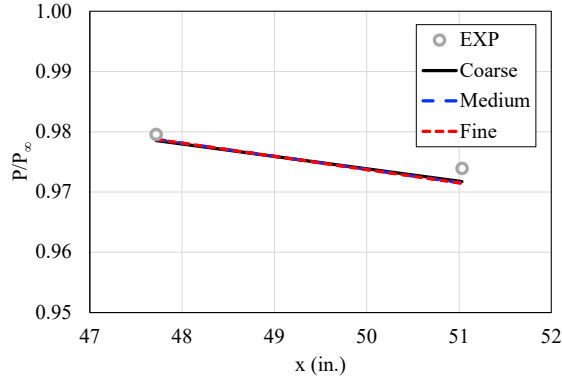
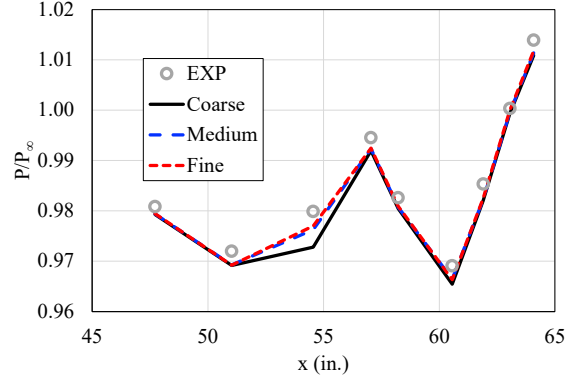


Figure 11. Illustration of static pressure ports on aft body.

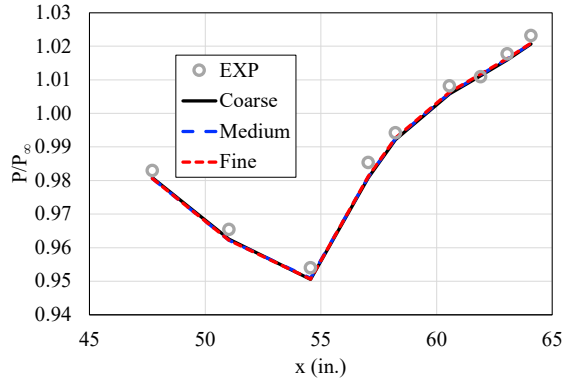
Finally, the boundary layer profile comparisons are provided in Figs. 13 and 14. Figure 13 shows the five boundary layer profiles designated as forward. The results show good agreement with experiment for rakes 1-3. Rake 4 shows the influence of the wing wake, which impacts this boundary layer rake. The USM3D-ME predictions improve, relative to experiment, as the grid size increases. However, the medium grid shows the best agreement nearest the wall. For rake 5, the boundary layer profile is heavily influenced by the wake of the sting strut, which is located directly upstream. The results show that USM3D-ME overpredicts the total pressure relative to the experiment. Studies were performed to better resolve the sting strut wake in hopes to better predict the total pressure losses at this boundary rake location, with no improvements observed. The aft rake comparisons provided in Fig. 14 show that the USM3D-ME predictions are in agreement with experiment for rakes 1 and 2. Similar to forward rake 5, the aft rake 3 comparisons highlight challenges with correctly predicting the sting strut wake influence on the boundary layer profiles. The results are observed to improve with increasing grid refinement, with small difference observed between the medium and fine grid levels.



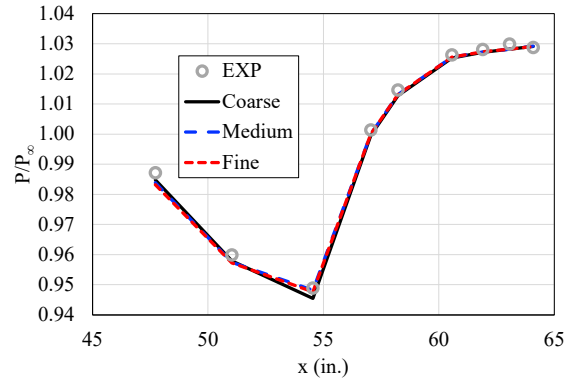
(a) Row 1, 0° (top)



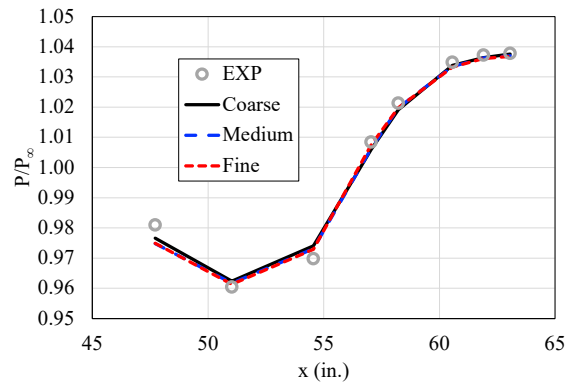
(b) Row 2, 45°



(c) Row 3, 90°



(d) Row 4, 135°



(e) Row 5, 180° (bottom)

Figure 12. Static pressure on surface of aft-fuselage, clean, $Re_{MAC} = 5 \times 10^6$, $M_\infty = 0.8$, and $C_L = 0.36$.

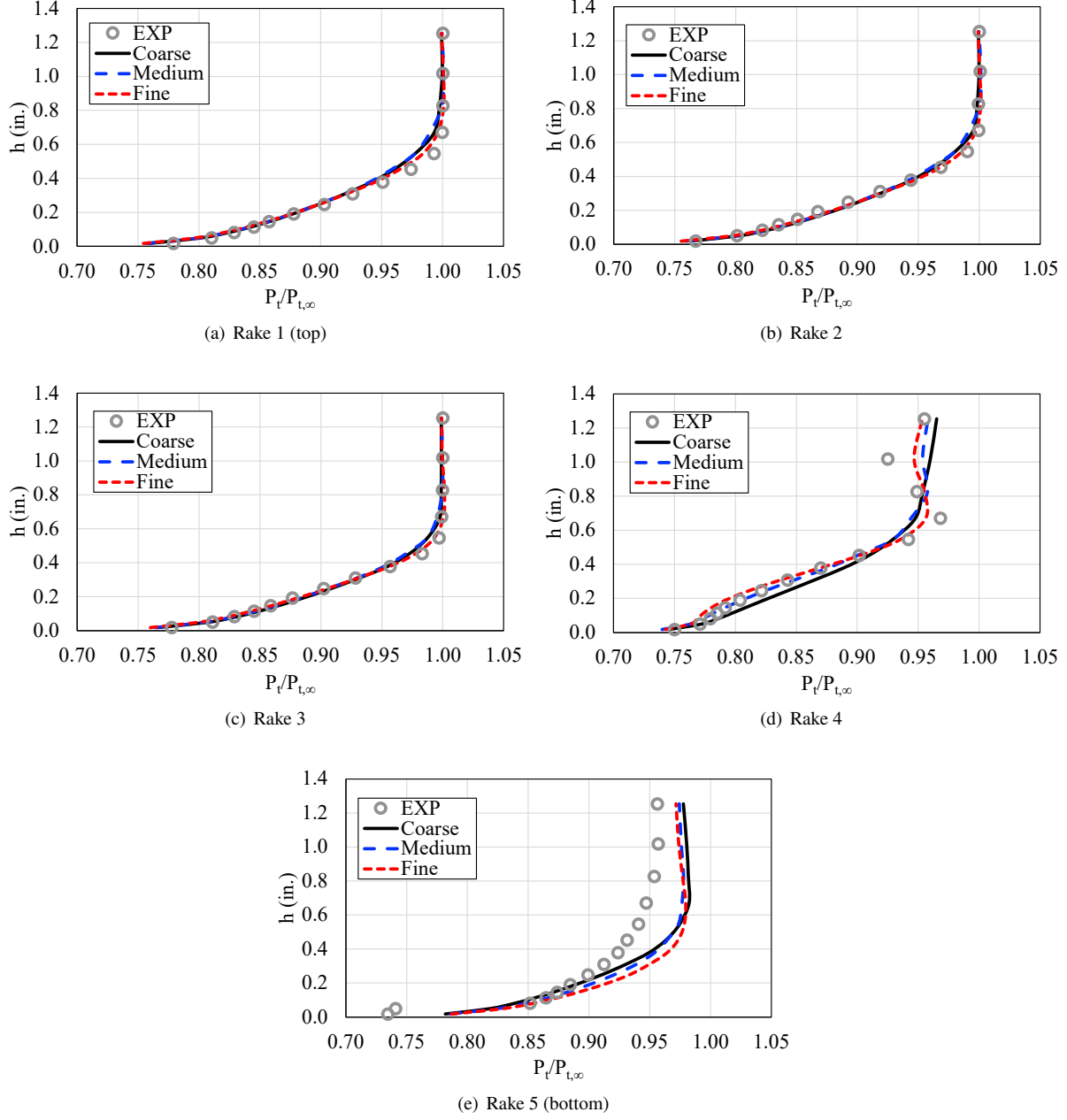


Figure 13. Boundary layer profile comparisons for forward rakes, clean, $Re_{MAC} = 5 \times 10^6$, $M_\infty = 0.8$, and $C_L = 0.36$.

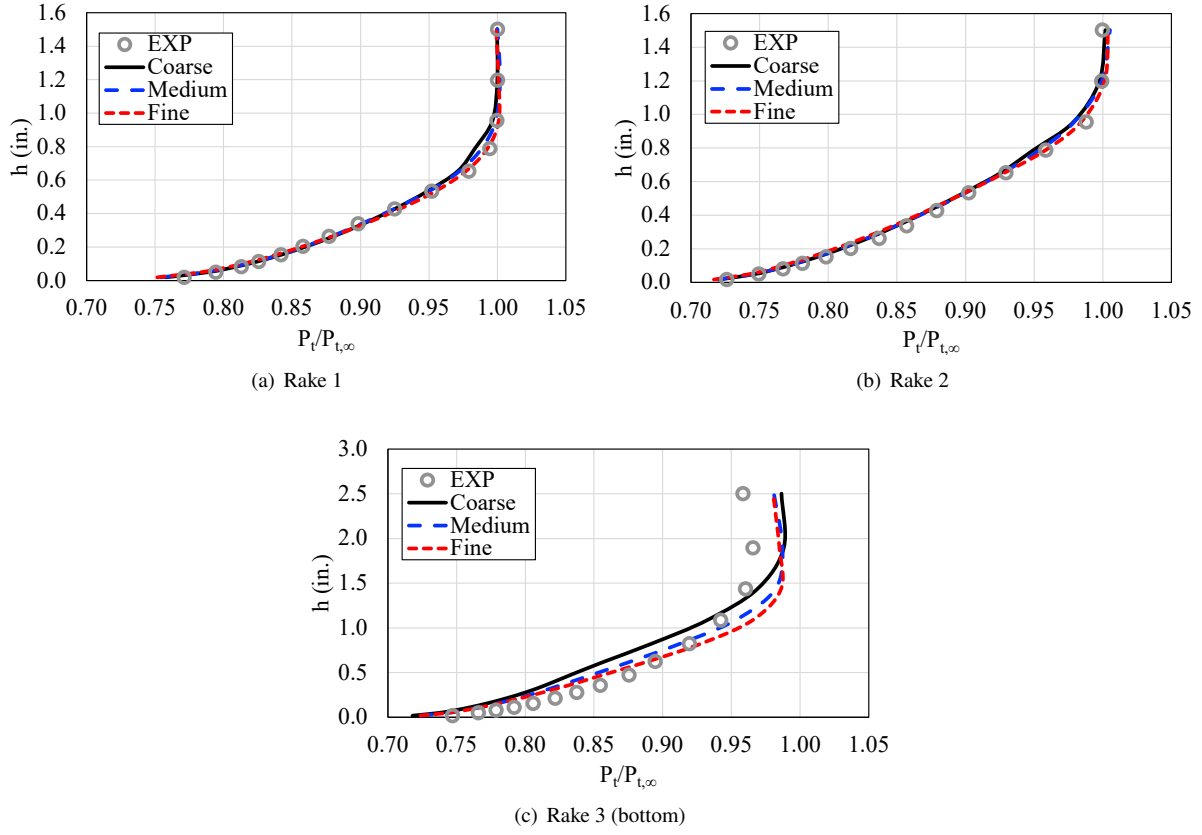


Figure 14. Boundary layer profile comparisons for aft rakes, clean, $Re_{MAC} = 5 \times 10^6$, $M_\infty = 0.8$, and $C_L = 0.36$.

2. Cruise MFP Configuration

The corresponding comparisons for the cruise MFP configuration are provided below. Figure 15 shows the force and moment coefficients as a function of grid refinement level. The lift coefficient results provided in Fig. 15a illustrate successful lift-coefficient matching for all grid levels. The drag coefficient results shown in Fig. 15b illustrate a similar trend to that observed in Fig. 10b for the clean configuration, with a drag coefficient difference of roughly seven counts for a 9x increase in grid size, and the predictions are observed to move away from the experiment with increasing grid size. However, better agreement with experiment is observed for the cruise MFP configuration with drag coefficient predictions within 1 count of the experimental value for both the medium and fine grid levels. Once again, the results show the tendency of USM3D-ME to underpredict the pitching moment coefficient, relative to experiment.

The next comparisons are provided in Fig. 16, which show the results of the integrated quantities at the AIP including pressure recovery, PR_{AIP} , average Mach number, $M_{avg,AIP}$, and mass flow rate. The experimentally obtained values are also provided for reference. The experimental data at the AIP was collected by rotating the 48-probe rake architecture in 2.5° increments. The pressure recovery value was calculated as the arithmetic average of the total pressure values measured in the experiment. Additionally, Eqs. 2, 3, and 4 were used to calculate the Mach number at each probe (M_i), average Mach number ($M_{avg,AIP}$) and mass flow rate ($\dot{m}_{AIP}$) at the AIP based on the measured pressure data. Note that the NTF ran in cryogenic mode for the Re_{MAC} values of 10 and 15 million. As a result, γ and R_{gas} varied with flow condition. Currently, USM3D-ME is only able to perform simulations using air. To enable consistent comparisons, the total pressure data, either measured in experiment or extracted from CFD, was used to calculate the quantities of interest using the standard values of γ and R_{gas} for air, which were defined to be 1.4 and 53.35 ft-lbf/R, respectively.

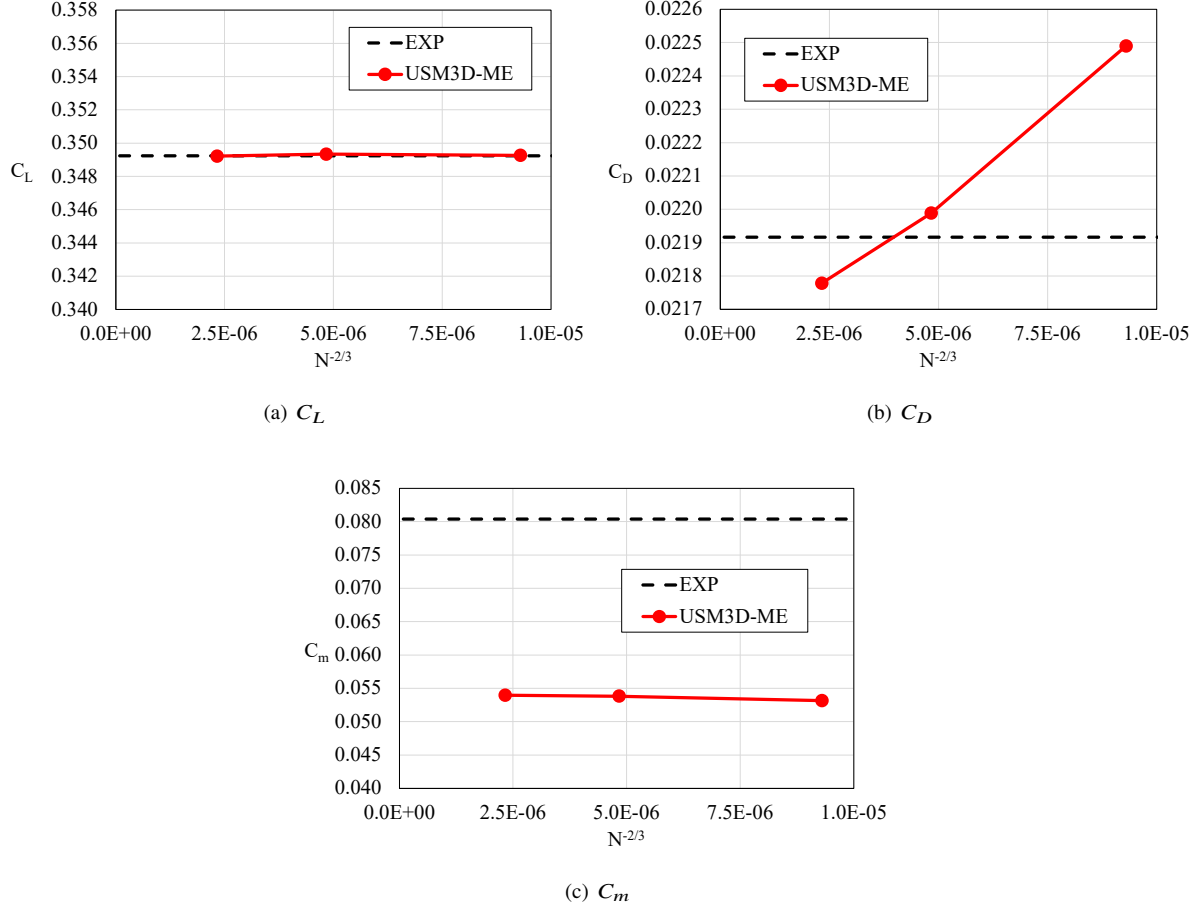


Figure 15. Force and moment coefficients vs. grid size, cruise MFP, $Re_{MAC} = 5 \times 10^6$, $M_\infty = 0.8$, and $C_L = 0.36$.

$$M_i = \sqrt{\frac{2}{\gamma - 1} \left[\left(\frac{P_{t,i}}{P_\infty} \right)^{\frac{\gamma-1}{\gamma}} - 1 \right]} \quad (2)$$

$$M_{avg,AIP} = \frac{1}{n} \sum_{i=1}^n M_i \quad (3)$$

$$\dot{m}_{AIP} = A \sqrt{\frac{\gamma g}{R_{gas} T_{t,\infty}}} \frac{1}{n} \sum_{i=1}^n M_i P_{t,i} \left(1 + \frac{\gamma-1}{2} M_i^2 \right)^{-\frac{\gamma+1}{2(\gamma-1)}} \quad (4)$$

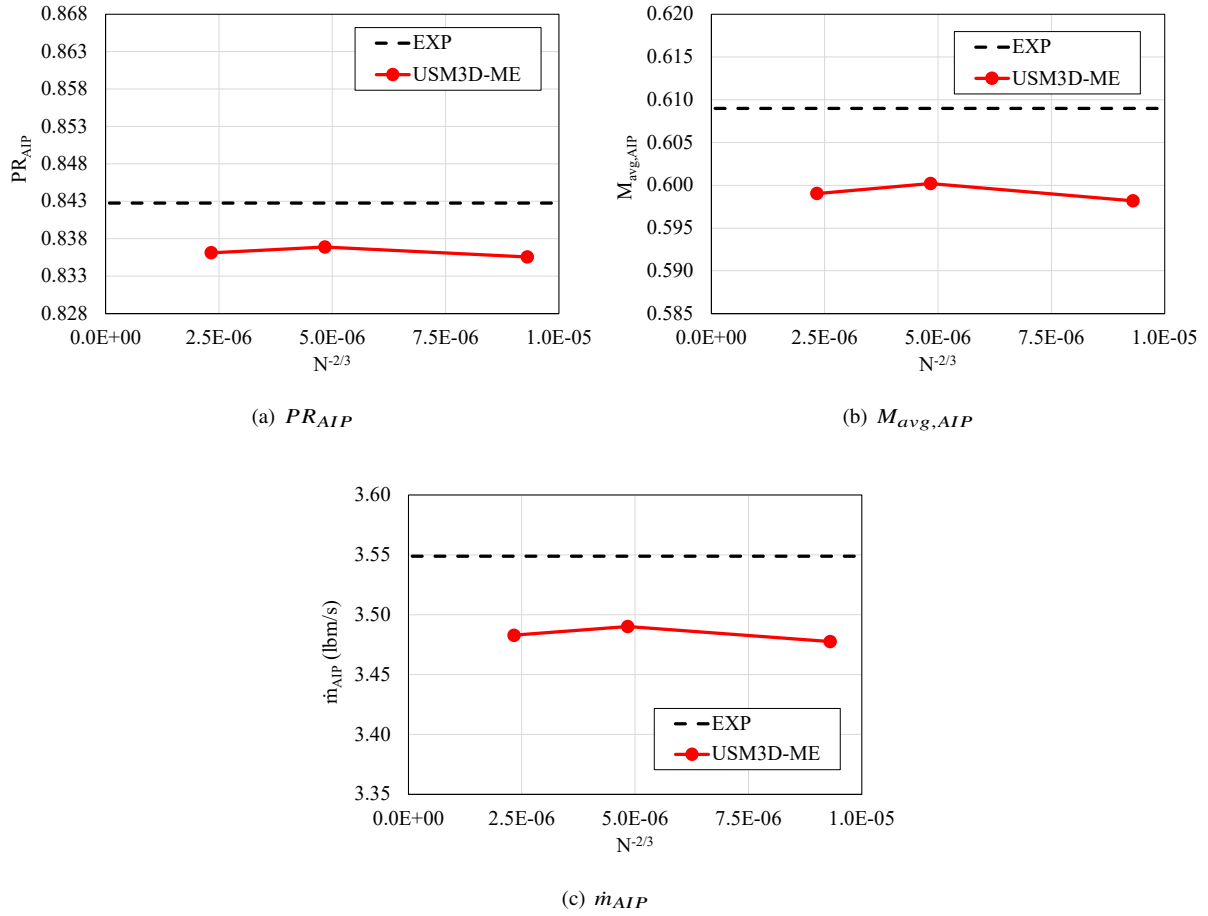


Figure 16. Integrated AIP quantities vs. grid size, cruise MFP, $Re_{MAC} = 5 \times 10^6$, $M_\infty = 0.8$, and $C_L = 0.36$.

Note that the span of the y-axes in Fig. 16 was chosen to be roughly 5% of the experimental value for the three metrics shown. The results show that USM3D-ME is within 2% of the experimental data for all grid levels, with only minor differences observed between the medium and fine grids. Note that USM3D-ME underpredicts all three quantities relative to the experiment, which makes the predictions conservative in this case. Also, the integrated results at the AIP are nonmonotonic, with a change in slope occurring at the medium grid level. Generally, the goal is to see asymptotic convergence to the experimental value. However, the difference between the medium and fine grid predictions is small and there is no evidence that the results would significantly change with further increases in grid size.

Next, the total pressure distributions at the AIP are compared for the 48-probe AIP rake architecture. Figure 17 provides qualitative comparisons of the flow at the AIP. Note that the pressure contours for the USM3D-ME predictions are provided for the 48-probe rake values, which are extracted at the same locations corresponding to the experimental data. The total pressure contour comparisons illustrate agreement between the USM3D-ME predictions and the experimental data for all grid levels. The primary impact of grid refinement is observed in the two vortices at the bottom of the AIP. As the grid is refined, the vortices are observed to be better resolved. However, the USM3D-ME predictions show larger total pressure values in the two vortices relative to the experiment. Figures 18a-18f show the total pressure distributions for each of the six rings included with the 48-probe AIP rake for the coarse, medium, and fine grid levels. Additionally, Fig. 18g shows the radially-averaged total pressure, which is the arithmetic average of all six probes at the given circumferential location. USM3D-ME accurately predicts the location of the vortices between 135 and 225 degrees. However, USM3D-ME shows a tendency to underpredict the total pressure relative to the experiment. The exception is observed at roughly 165 and 190 degrees, where USM3D-ME overpredicts the total pressure peaks. The result is a lower predicted pressure recovery relative to experiment, as observed in Fig. 16. Note that the results show improved agreement with experiment for increasing grid refinement in some regions and worse agreement in others. In

fact, considering the radially-averaged results, the coarse grid agrees better with the experiment than the finer grid levels. However, the integrated results in Fig. 16 show that both the medium and fine grids agree better with experiment than the coarse grid. This is due to the overshoot in the total pressure peaks shown for the finer grid levels. The overpredicted total pressure peaks leads to an increase in the average total pressure, which, in this case, improves the accuracy of the integrated results.

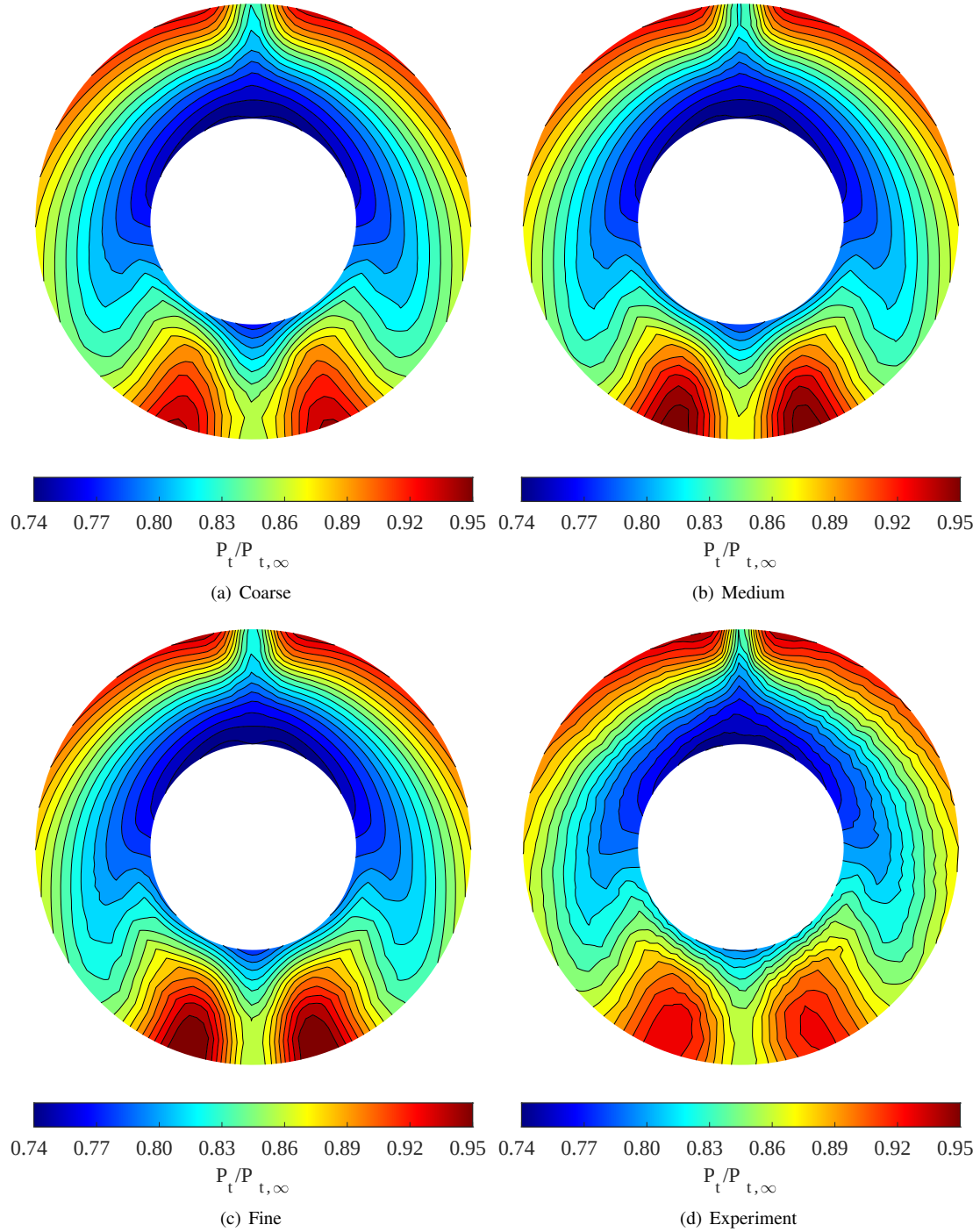


Figure 17. Total pressure contour plots as a function of grid size, cruise MFP, $Re_{MAC} = 5 \times 10^6$, $M_\infty = 0.8$, and $C_L = 0.36$.

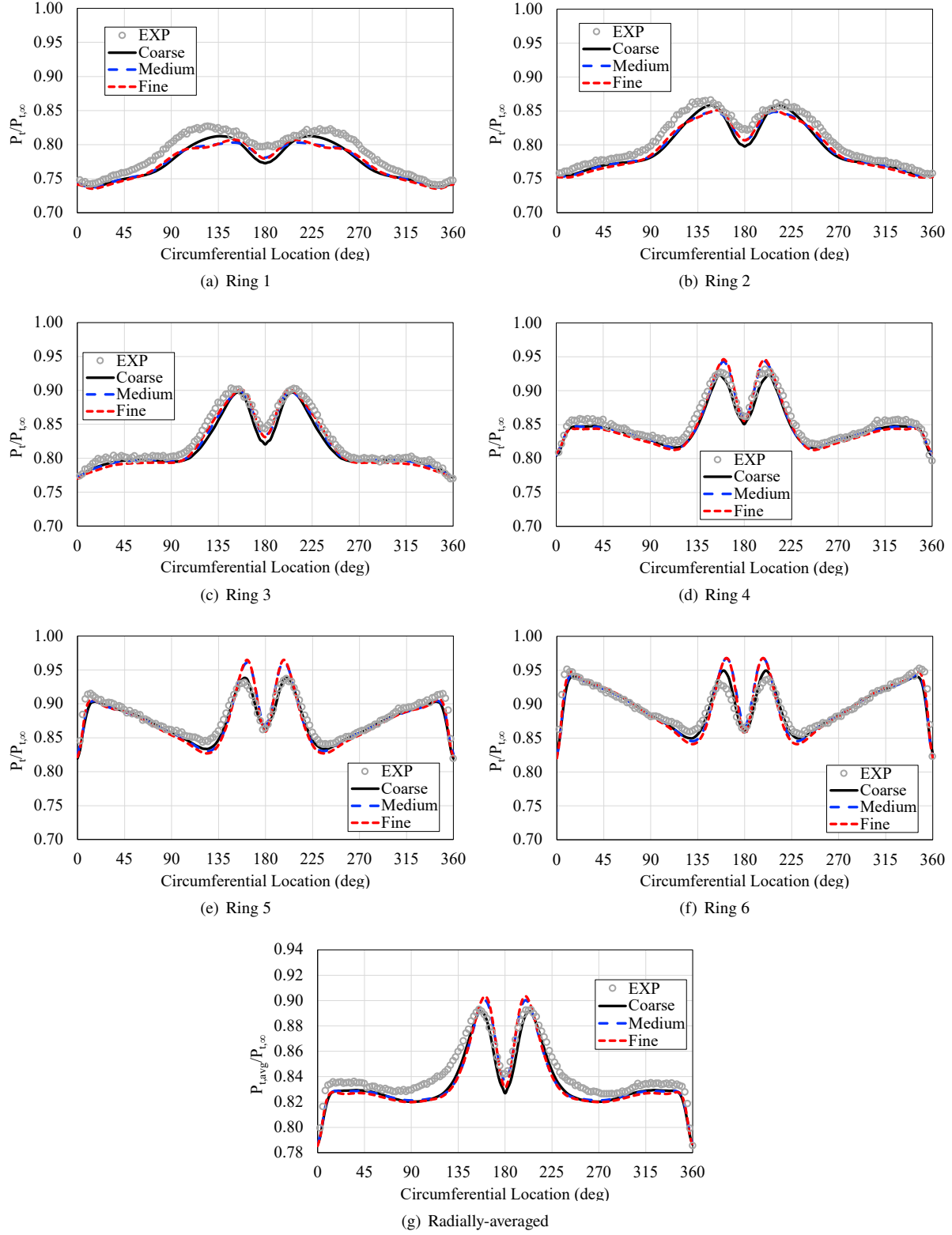


Figure 18. Total pressure distributions as a function of grid size, cruise MFP, $Re_{MAC} = 5 \times 10^6$, $M_\infty = 0.8$, and $C_L = 0.36$.

Finally, flow angularity comparisons are provided in Figs. 19 and 20. Flow angularity measurements were obtained using the 5-hole probe AIP architecture discussed in Section II.A. Figures 19a-c show α_{flow} distributions for rings 1-3 and Fig. 19d shows the radially-averaged values. Similarly, Figs. 20a-c show β_{flow} distributions and Fig. 20d shows the radially-averaged values. The results illustrate qualitative agreement between USM3D-ME and the experimental data. Also, grid refinement only has a minor impact, which is predominantly observed between 135 and 225 degrees where the two vortices are located. Note that some asymmetry is observed in the experimental data, which is not observed in USM3D-ME. Recall that the CFD simulations were performed using a half-span grid and the results here are obtained by mirroring about the plane of symmetry. Due to this, the results are forced to be symmetric in USM3D-ME. However, the geometry and flight condition is symmetric about the centerline and any asymmetries observed here are likely due to variations in the instrumentation. Each ring consists of three 5-hole probes, which are rotated by 120° to provide 360° of data. Figures 19 and 20 show discontinuities in the experimental data at 60° , 180° , and 300° illustrating that there was some variation in the flow angles measured by each of the three probes. Overall, the flow angularity comparisons show favorable agreement between USM3D-ME and the experimental data.

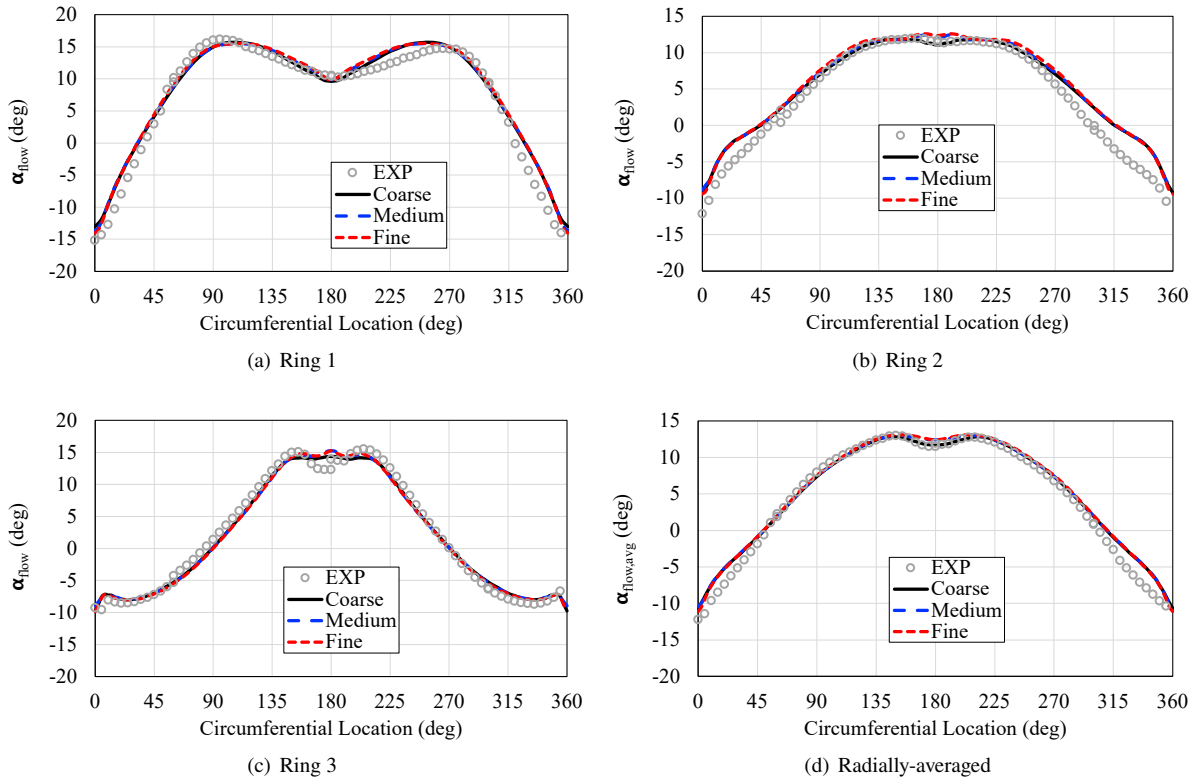


Figure 19. α_{flow} as a function of grid size, cruise MFP, $Re_{MAC} = 5 \times 10^6$, $M_\infty = 0.8$, and $C_L = 0.36$.

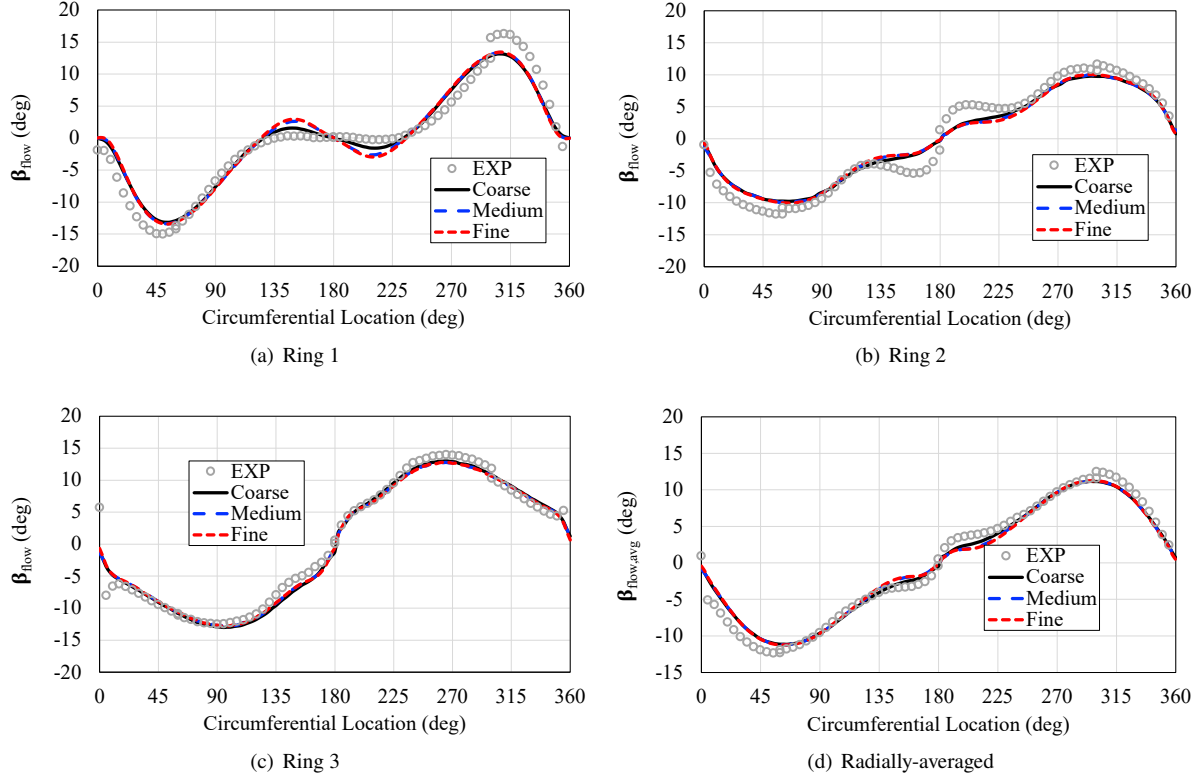


Figure 20. β_{flow} as a function of grid size, cruise MFP, $Re_{MAC} = 5 \times 10^6$, $M_\infty = 0.8$, and $C_L = 0.36$.

3. Grid Refinement Study Summary

Based on the results shown for the clean and cruise MFP configurations, grid convergence was not achieved. This is most apparent when analyzing the integrated force and moment coefficients, and the integrated quantities at the AIP. The goal was to identify the sensitivity to grid refinement and make an educated decision regarding the grid size, which will be used to perform condition sweeps for further comparison to the NTF wind tunnel test data. For this work, the medium grid level was chosen as the best compromise of accuracy and computational cost. All remaining results were produced with the medium grid level.

B. Turbulence Model Study

In addition to a grid refinement study, a turbulence model study was performed to evaluate the sensitivity of the USM3D-ME predictions to the turbulence model options. Previously, results were shown for the SA-neg-R-QCR turbulence model. For this study, the SA-neg-R and SA-neg variations were also considered. This study was performed using the medium grid level and for both the clean and cruise MFP configurations at the condition corresponding to $Re_{MAC} = 5$ million, $M_\infty = 0.8$, and $C_L = 0.36$. Select results are provided in the following subsections.

1. Clean Configuration

For the clean configuration, the largest differences were observed in the boundary layer profiles. Specifically, forward rakes 4 and 5 and aft rake 3 were observed to be the most challenging to predict accurately. The results for these rakes are shown in Fig. 21 for the SA-neg-R-QCR, SA-neg-R, and SA-neg models. The results show minimal influence for the two bottom rakes (forward rake 5 in Fig. 21b, and aft rake 3 in Fig. 21c). Figure 21a shows the most significant variation with turbulence model, with the SA-neg-R-QCR providing the closest match to the experimental data.

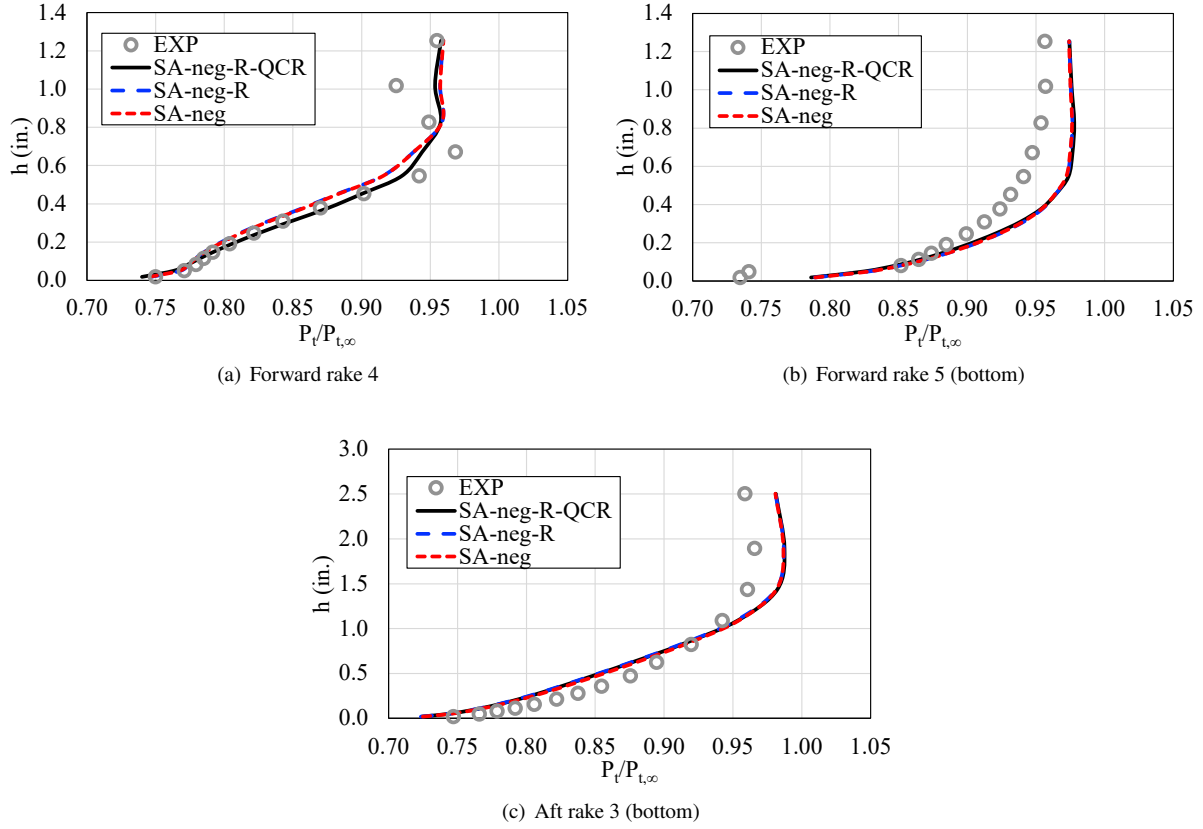


Figure 21. Boundary layer profile comparisons as a function of turbulence model, clean, medium grid, $Re_{MAC} = 5 \times 10^6$, Mach = 0.8, and $C_L = 0.36$.

2. Cruise MFP

For the cruise MFP configuration, integrated quantities at the AIP are provided in Fig. 22, total pressure contour plots are provided in Fig. 23, and distributed values at the AIP are provided in Fig. 24. Note that the integrated quantities are provided as a function of both grid refinement and turbulence model, while the total pressure contour plots and distributed values are provided for all three turbulence model variations, but only for the medium grid level. The results provided in Fig. 22 show that both the SA-neg and SA-neg-R models more closely match the experimental data relative to the baseline, SA-neg-R-QCR model. This observation is true for all grid refinements. The total pressure contour plots show that the two vortices are the most significantly impacted by turbulence model option. Qualitatively, the SA-neg-R-QCR model shows the closest agreement to the experimental data. This is confirmed in the distributed results provided in Fig. 24, which shows that the total pressure peak overshoot is more significant for both the SA-neg-R and SA-neg models. Additionally, the SA-neg-R and SA-neg models both underpredict the total pressure at 180 degrees relative to both the experiment and the SA-neg-R-QCR prediction. However, the SA-neg-R and SA-neg model show better agreement with experiment from 0 to 135 degrees and 225 to 360 degrees where the SA-neg-R-QCR model is observed to overpredict the total pressure losses at the AIP. The result is that the integrated values provided in Fig. 22 show that the SA-neg and SA-neg-R model predictions more closely match the experiment than the SA-neg-R-QCR model. Finally, the results provided in Fig. 24b-c illustrate a lack of sensitivity to turbulence model for the flow angularity measurements.

Based on the results shown thus far, one could conclude that the excessive losses at the AIP predicted by the SA-neg-R-QCR make it the less desirable option since it is shown to provide less favorable agreement with the experimentally obtained integrated quantities at the AIP. However, this is only for a single condition. Further analyses were performed to evaluate the impact of turbulence model on the predicted trends. The results of an alpha sweep performed for the cruise MFP configuration at the condition corresponding to $Re_{MAC} = 5$ million and $M_\infty = 0.8$

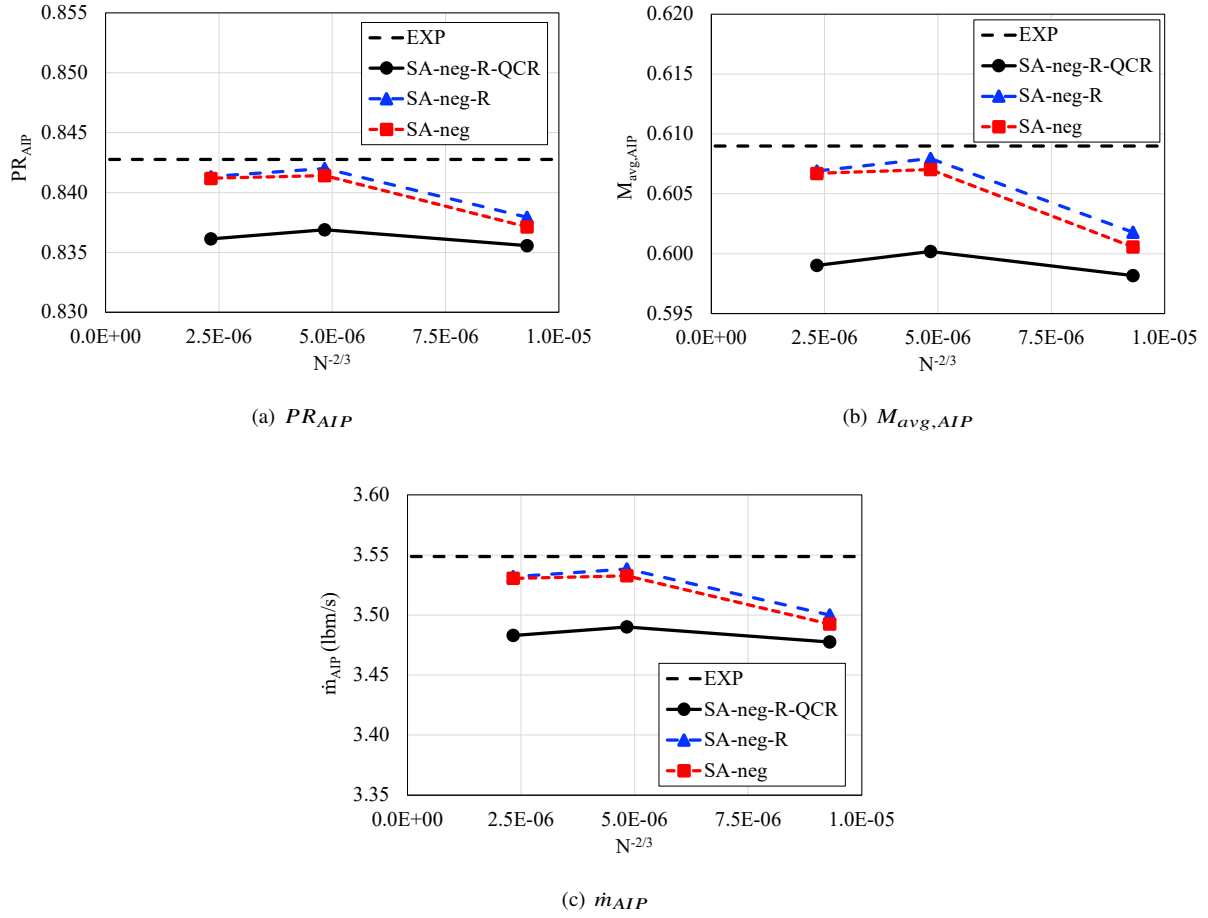


Figure 22. Integrated AIP quantities as a function of turbulence model, cruise MFP, medium grid, $Re_{MAC} = 5 \times 10^6$, Mach = 0.8, and $C_L = 0.36$.

are provided in Fig. 25 for the three turbulence model variations. Note that the decision was made to plot the AIP quantities against the lift coefficient based on the previous discussion that illustrated an angle of attack shift between the experimental data and the CFD predictions. The results show that while the absolute values predicted by the SA-neg and SA-neg-R models better match the experiment, the predicted trends do not agree with experiment. However, the SA-neg-R-QCR predictions are observed to accurately capture the experimentally observed trends.

3. Summary of Turbulence Model Study

The results provided in this subsection illustrate the importance of considering more than a single data point to evaluate the impact of the turbulence model when the desire is to perform simulations over a range of conditions. Note that force and moment coefficient predictions were not provided in this paper for the turbulence model study for space considerations. However, the force and moment predictions also illustrated the most consistent agreement with the experimental data using the SA-neg-R-QCR model. Based on the results provided, the SA-neg-R-QCR model was selected for the NTF condition sweeps that will be discussed in the next subsection.

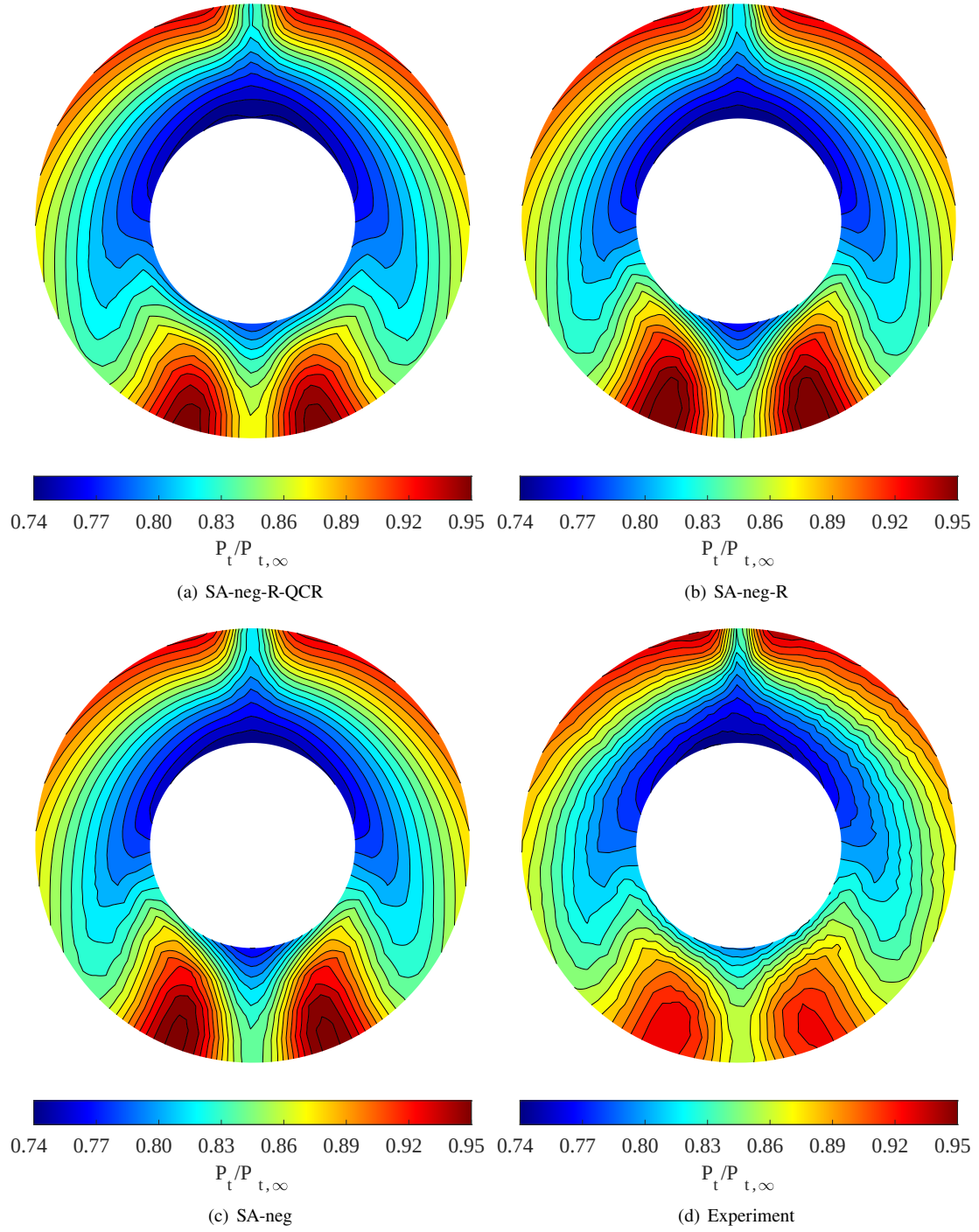


Figure 23. Total pressure contour plots as a function of turbulence model, cruise MFP, medium grid, $Re_{MAC} = 5 \times 10^6$, Mach = 0.8, and $C_L = 0.36$.

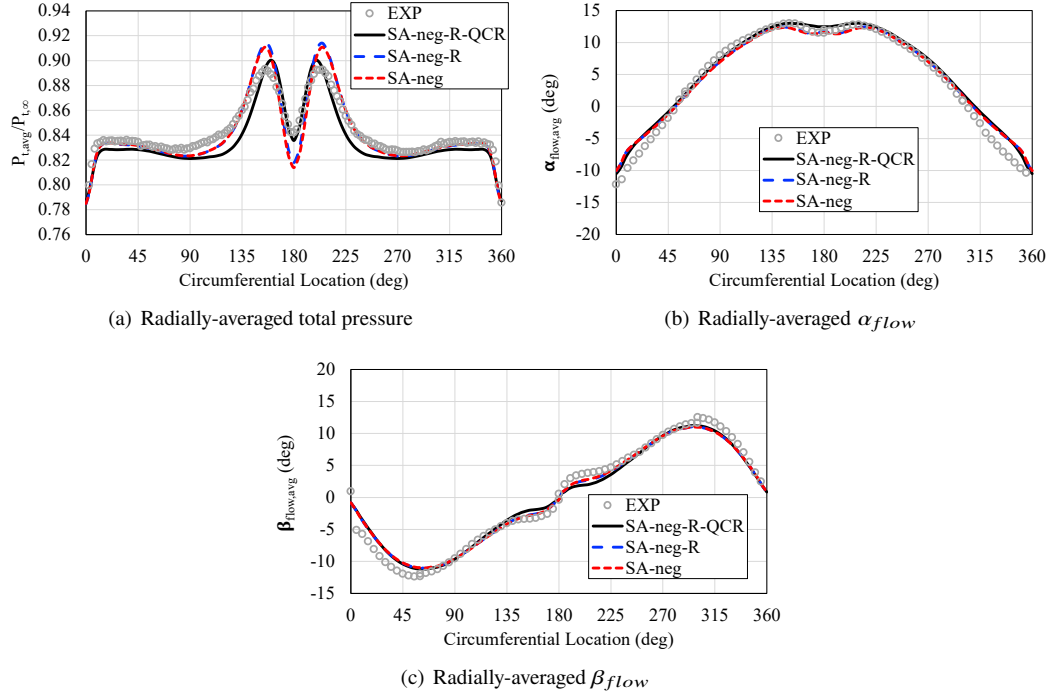


Figure 24. Radially-averaged, distributed quantities at the AIP as a function of turbulence model, cruise MFP, medium grid, $Re_{MAC} = 5 \times 10^6$, Mach = 0.8, and $C_L = 0.36$.

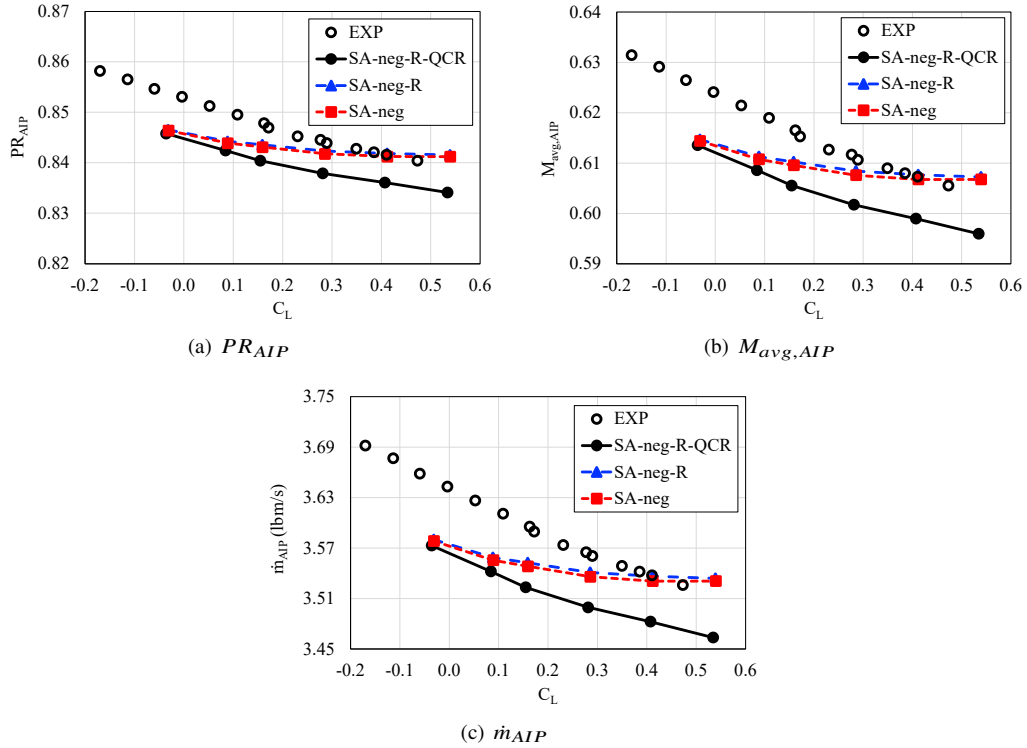


Figure 25. Integrated AIP quantities as a function of turbulence model and C_L , cruise MFP, $Re_{MAC} = 5 \times 10^6$ and $M_\infty = 0.8$.

C. NTF Condition Sweeps

USM3D-ME simulations were performed for the CRM-TCT model variations over the range of conditions considered in the NTF wind tunnel test (Table 2). Note that the angle-of-attack sweeps were performed for $\alpha = 0^\circ$ to 3° in 1° increments. All simulations discussed in this subsection utilized the medium grid level and the SA-neg-R-QCR turbulence model. Force and moment data were obtained in the wind tunnel test and were used to compare to values predicted by USM3D-ME. However, those comparisons are not provided here. Due to size limitations, only integrated quantities at the AIP are provided for the condition sweeps discussed in the following subsections. Note that, once again, the α sweep results are provided as a function of lift coefficient due to the observed α shift.

1. Cruise MFP

The primary configuration for the wind tunnel test was the cruise MFP configuration. As such, this configuration was tested for the broadest range of conditions including Reynolds numbers of 5, 10, and 15 million, Mach numbers of 0.75, 0.8, and 0.85, and angles of attack ranging from 0.5 to 3 degrees. Also, an extended range of α was considered for $Re_{MAC} = 5$ million, but with the horizontal stabilizer removed to prevent over-ranging the force balance. This extended sweep included angles of attack ranging from -3° to 2° without the horizontal tail to supplement the α range of 0.5° to 3° tested with the horizontal tail included. For this condition, USM3D-ME predictions were additionally performed for $\alpha = -2^\circ$ and -1° with the horizontal stabilizer removed to be consistent with the experiment. The results for the cruise MFP configuration are provided in Fig. 26, 27, and 28 for $Re_{MAC} = 5, 10$, and 15 million.

The pressure recovery results in Fig. 26a show that USM3D-ME accurately predicts the trends observed in the experiment over the entire range of Mach numbers and angles of attack. This observation is true for all three quantities provided in Fig. 26. Note that the y-axis increments in all plots were chosen to be roughly 2% of the mean value measured in the experiment. The results show that USM3D-ME tends to underpredict the pressure recovery at the AIP. However, the results are within 1% of the experimental data for all conditions. The corresponding average Mach number values at the AIP are provided in Fig. 26b and the mass flow rate at the AIP is provided in Fig. 26c. The $M_{avg,AIP}$ comparisons are similar in quality to the pressure recovery, with accurately predicted trends and values within 1% of the experimental data. For the mass flow rate, Fig. 26c, the error is slightly larger, with an average difference of roughly 2% relative to the experimental data. This is due to compounding differences from the pressure recovery and average Mach number, which are both used to calculate the mass flow rate. Also note that the mass flow rate comparisons illustrate the potential of a Mach shift, with the $M_\infty = 0.8$ predictions comparing well with the $M_\infty = 0.75$ measurements. The same is true for the $M_\infty = 0.85$ predictions and the $M_\infty = 0.8$ measurements. However, looking at Fig. 26b, this is not the case. For this Reynolds number, the variation in mass flow rate at the AIP with Mach number is on the same order as the difference between the experimental data and USM3D-ME predictions.

Similarly, the results shown in Fig. 27 and 28 illustrate favorable agreement between the USM3D-ME predicted trends and the experiment for $Re_{MAC} = 10$ and 15 million. The comparisons show that USM3D-ME is within roughly 1% or less of the experimental data for all metrics shown and both Reynolds numbers in Figs. 27 and 28. Comparing the results from Figs. 26, 27, and 28, the differences between USM3D-ME and the experimental data are shown to decrease with increasing Reynolds number. This observation could point towards inadequacies in our ability to predict viscous effects since momentum effects become more dominant with increasing Reynolds number. For future work, it would be insightful to evaluate higher fidelity turbulence model options to further investigate these trends.

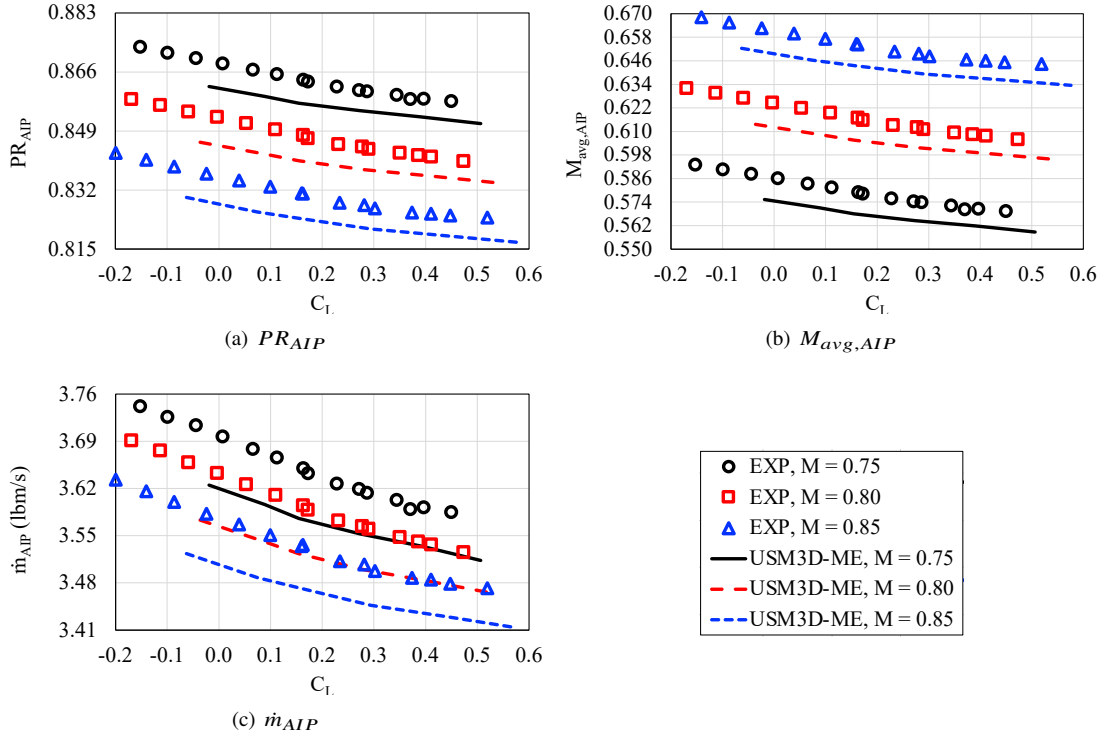


Figure 26. Integrated AIP quantities as a function of C_L and M_∞ , cruise MFP, $Re_{MAC} = 5 \times 10^6$.

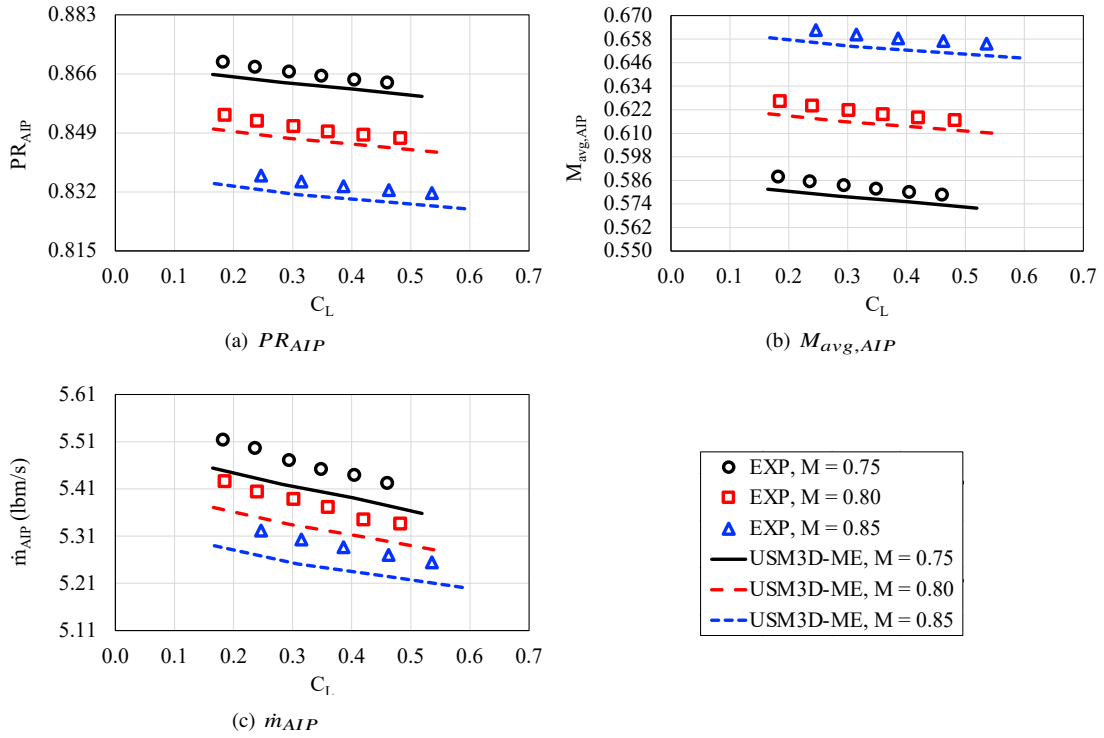


Figure 27. Integrated AIP quantities as a function of C_L and M_∞ , cruise MFP, $Re_{MAC} = 10 \times 10^6$.

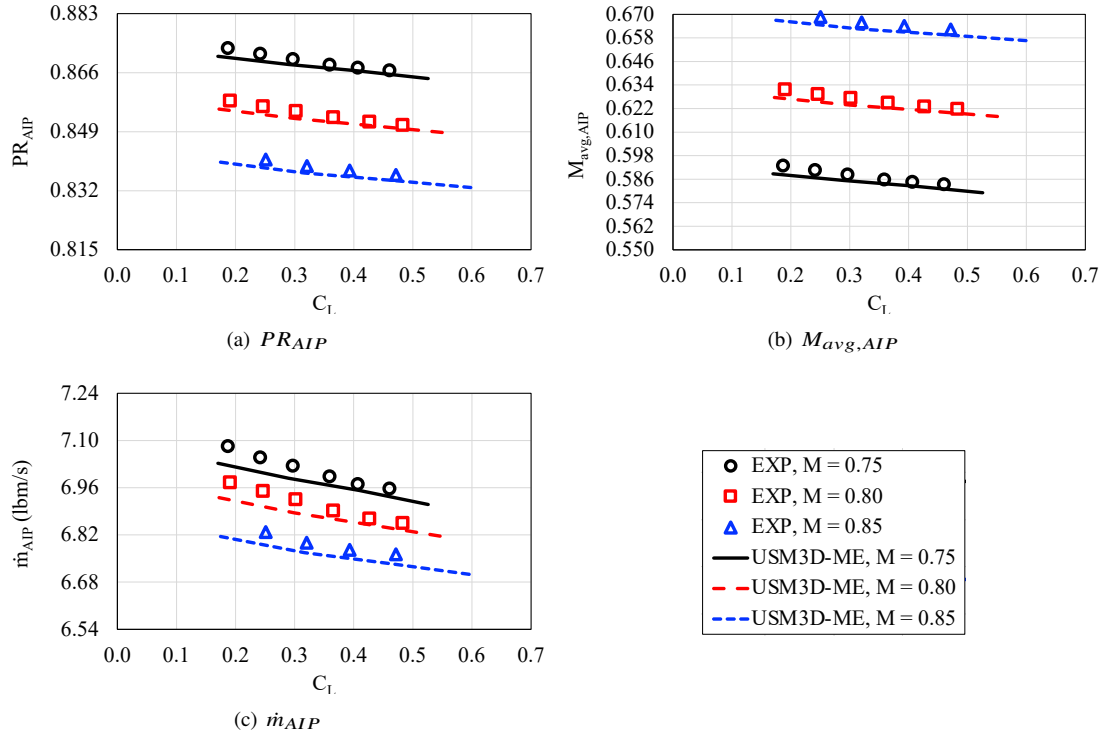


Figure 28. Integrated AIP quantities as a function of C_L and M_∞ , cruise MFP, $Re_{MAC} = 15 \times 10^6$.

2. 90% Cruise MFP

The results for the 90% cruise MFP configuration are provided in Fig. 29 for $Re_{MAC} = 5$ million and $M_\infty = 0.75, 0.8$, and 0.85 . The comparisons show favorable agreement, qualitatively similar to that shown in Fig. 26 for the cruise MFP configuration at these conditions. For this configuration, USM3D-ME shows a tendency to underpredict the pressure recovery with an average difference of less than 1%. This is propagated to the Mach number and mass flow rate values, with the largest difference observed for the mass flow rate. The mass flow rate shows an average difference of roughly 1.5%, with a maximum difference of 2% observed for the highest combination of Mach number and angle of attack.

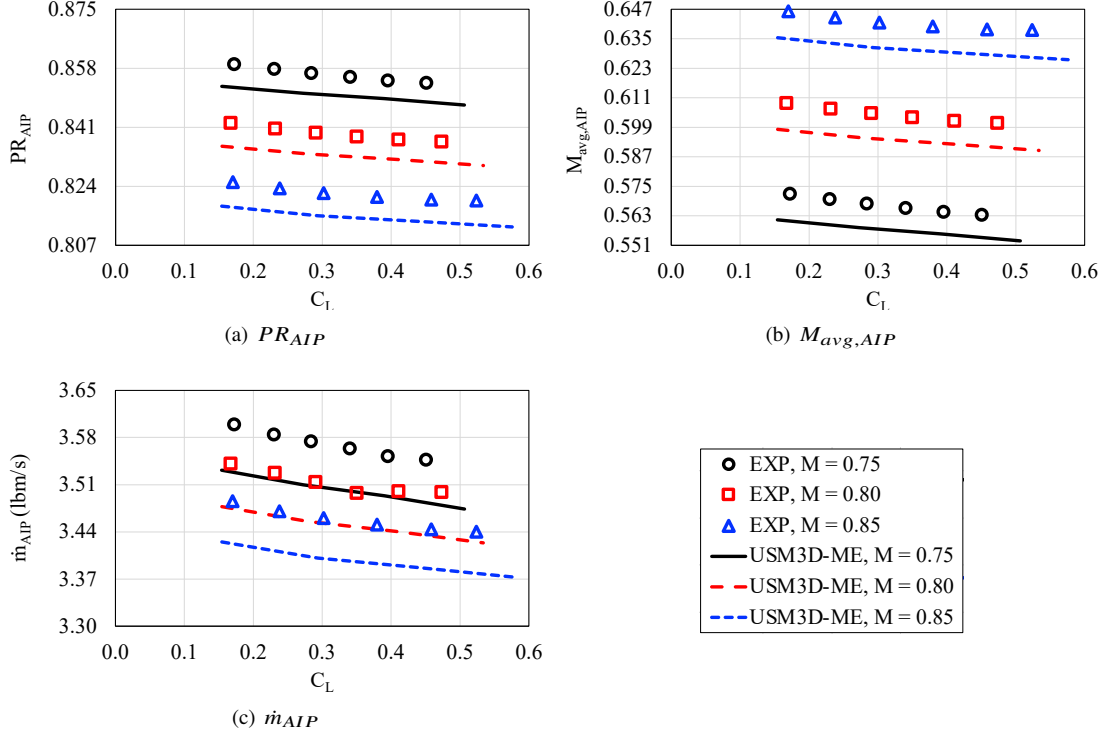


Figure 29. Integrated AIP quantities as a function of C_L and M_∞ , 90% cruise MFP, $Re_{MAC} = 5 \times 10^6$.

3. 110% Cruise MFP

Next, the results for the 110% cruise MFP configuration are provided in Fig. 30 for $Re_{MAC} = 5$ million and $M_\infty = 0.75, 0.8$, and 0.85 . Compared to the cruise MFP and 90% cruise MFP configurations, the results provided in Fig. 30 illustrate improved agreement with the experimental data. USM3D-ME still underpredicts the quantities at the AIP, but the results are closer than observed for the other configurations discussed thus far. For this configuration, the maximum difference for both the pressure recovery and Mach number at the AIP is less than 0.5%. The resulting mass flow rate predictions are similarly within 0.5% of the experimental data.

4. Idle MFP

Finally, comparisons are provided in Fig. 31 for the idle MFP configuration for $Re_{MAC} = 5$ million and $M_\infty = 0.75, 0.80$, and 0.85 . The USM3D-ME predicted trends deviate from the experimental data for the lowest angles of attack. This configuration showed the largest differences between USM3D-ME and the experimental data, with differences of up to 3% observed for both $M_{avg,AIP}$ and mass flow rate at the AIP at the lowest angle of attack. However, improved agreement is observed between USM3D-ME and the experimental data as the angle of attack is increased. The USM3D-ME predictions are observed to be within 1% of the experimental data for the highest angle of attack. In an effort to improve the comparisons for the idle MFP configuration, variations in local grid refinement and turbulence model options were explored with no improvement observed.

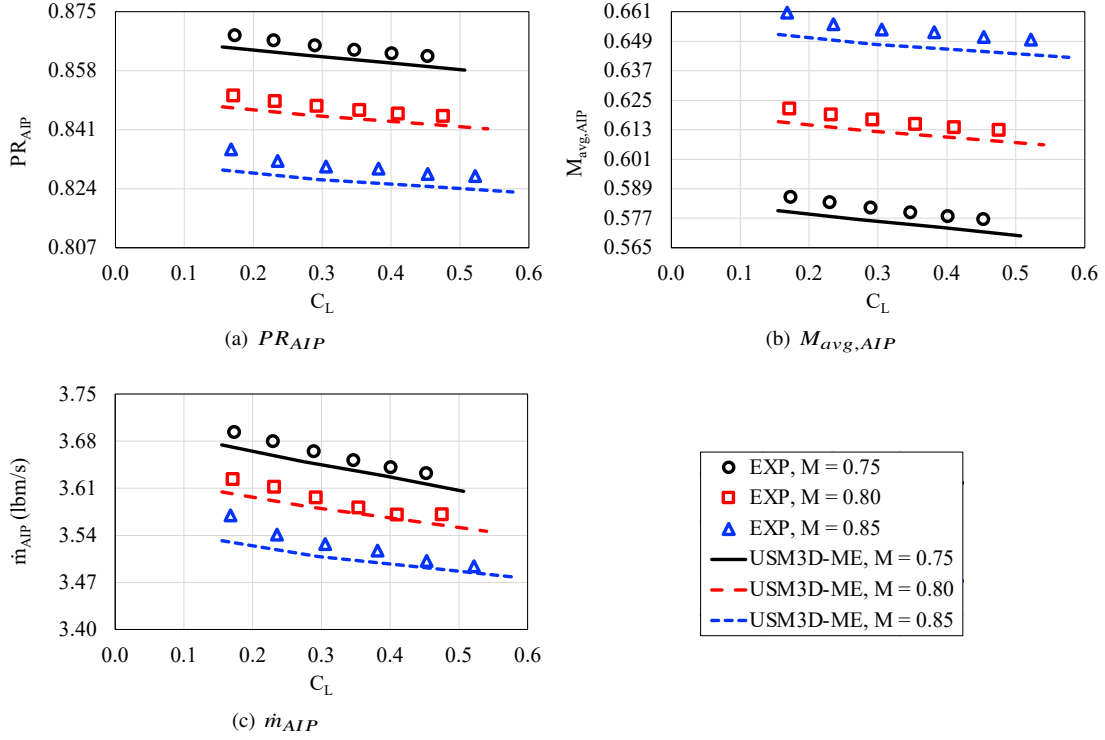


Figure 30. Integrated AIP quantities as a function of C_L and M_∞ , 110% cruise MFP, $Re_{MAC} = 5 \times 10^6$.

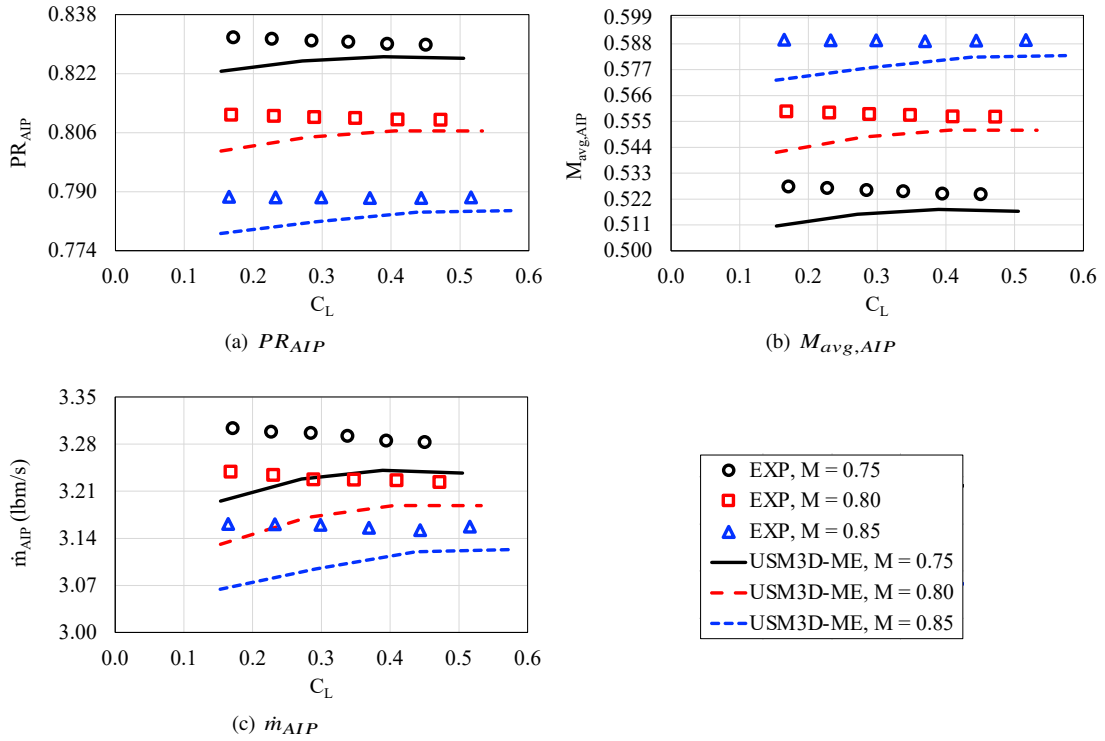


Figure 31. Integrated AIP quantities as a function of C_L and M_∞ , idle MFP, $Re_{MAC} = 5 \times 10^6$.

5. Summary of Condition Sweeps

The results in this subsection illustrate favorable agreement between the USM3D-ME predicted trends and the experimental data. For the cruise MFP configuration, the agreement between the USM3D-ME predictions and experimental data is observed to improve with increasing Reynolds number. Additionally, results were provided for the 90% cruise, 110% cruise, and idle MFP configurations for $Re_{MAC} = 5$ million and $M_\infty = 0.75, 0.8, \text{ and } 0.85$. The 90% cruise MFP comparisons illustrated similar agreement with experiment to that observed for the cruise MFP at the same conditions. For the 110% cruise MFP configuration, the comparisons illustrated the best agreement between USM3D-ME and the experimental data relative to the other MFP configurations at the same Reynolds number. The comparisons for the idle MFP configuration, on the other hand, illustrated the largest differences between the USM3D-ME predictions and experimental data. The differences were observed to decrease with increasing angle of attack, with differences of less than 1% at the highest angle of attack. Future work should investigate these trends further to assess the mechanism(s) leading to improved agreement between USM3D-ME and the experiment with increasing Reynolds number and those resulting in the largest differences being observed for the idle MFP configuration. Overall, the comparisons provided in this subsection illustrate favorable agreement between USM3D-ME and experiment for the CRM-TCT model tested in the NTF.

V. Concluding Remarks

BLI propulsion technology offers the potential to reduce fuel burn due to the tightly integrated propulsor and lower momentum flow at the fan face. Recently, a BLI configuration was experimentally tested in the NTF wind tunnel at the NASA Langley Research Center. The wind tunnel configuration was a modified version of the CRM with a flow-through nacelle mounted on the aft-fuselage, representative of a tailcone thruster configuration. The goal was to assess PAI effects and to investigate the flow quality ingested at the inlet. Additionally, the data obtained provides an experimental dataset to enable an evaluation of the accuracy of CFD for predicting the flow in the inlet. This paper focused on an evaluation of the accuracy of the USM3D-ME flow solver for predicting the inlet flow for the CRM-TCT model through comparisons to the experimental data obtained in the NTF wind tunnel test.

The primary configuration for the wind tunnel test was the tailcone thruster configuration, with four interchangeable mass flow plugs to allow for mass flow rate modulation. The four mass flow plug variations included cruise, 90% cruise, 110% cruise, and idle. Additionally, a clean configuration was included to provide a baseline without a tailcone thruster. The wind tunnel model was instrumented with static pressure ports on the wing, aft fuselage, nacelle, and vertical tail. Boundary layer rakes were included for select conditions. For the tailcone thruster configuration, multiple AIP architectures were employed to collect total pressure data and flow angularity data at the AIP. Note that the AIP rakes were able to rotate to obtain high-resolution data and provide better insight into the flow in the inlet.

The USM3D-ME analyses that were performed included grid refinement and turbulence model studies for both the clean and cruise MFP configurations at the test condition corresponding to $Re_{MAC} = 5$ million, $M_\infty = 0.8$, and $C_L = 0.36$. Initial comparisons to the experimental data illustrated an angle of attack shift between the experiment and CFD. To account for this, the grid refinement and turbulence model studies were performed by iteratively incrementing the angle of attack to match the predicted lift-coefficient to the experimentally measured value. Three grid levels were considered for the grid refinement study. The USM3D-ME predictions agree favorably with the experimental data for both configurations. Note that grid convergence was not achieved for either configuration. However, only small differences were observed between the two finest grid levels. For the clean configuration, the most significant differences were observed for the boundary layer profile comparisons. The boundary layer rakes located on the bottom of the fuselage (180°) are impacted by the wake generated by the sting strut. This feature was not accurately predicted by USM3D-ME. However, the boundary layer profiles at all other locations were found to be in agreement with the experiment. For the cruise MFP configuration, the total pressure distributions at the AIP predicted by USM3D-ME agree favorably with the experimental data. USM3D-ME showed a tendency to underpredict the total pressure at the AIP, which propagates to the Mach number and mass flow rate calculations. The turbulence model study showed that the SA-neg and SA-neg-R models agree better with experiment than the SA-neg-R-QCR model for the quantities at the AIP. However, further analyses showed that the trends with angle of attack are incorrectly predicted by the SA-neg and SA-neg-R models. From this work, the SA-neg-R-QCR model was selected as the best option along with the medium grid, which was determined to be the best tradeoff between computational cost and accuracy.

The NTF condition sweeps show favorable agreement with experiment for all configurations. For the cruise MFP configuration, the comparisons show better agreement with increasing Reynolds number. For the case of $Re_{MAC} = 5$ million, average differences of roughly 1% were observed between USM3D-ME and the experiment for the pressure

recovery and Mach number at the AIP, with average differences of roughly 2% for the mass flow rate. The comparisons for $Re_{MAC} = 15$ million showed slightly better agreement with differences of 0.5% for the pressure recovery and Mach number, and 1% for the mass flow rate. For the 90% cruise MFP, the results illustrate similar level of agreement as observed for the cruise MFP configuration at the $Re_{MAC} = 5$ million condition, with better agreement observed for the 110% cruise MFP configuration. The largest differences were observed for the idle MFP configuration, with differences as large as 3% between USM3D-ME and the experimental data.

Overall, USM3D-ME predictions agree favorably with the experimental data, increasing confidence in the ability to accurately predict the flow ingested by the nacelle of a tailcone thruster configuration. Future work should investigate higher fidelity turbulence models to assess the impact, if any, on the predicted trends with variation in MFP and Reynolds number. It would also be interesting to see if higher fidelity turbulence models improve agreement with experiment for the boundary layer profiles on the bottom of the fuselage, which were shown to be impacted by the wake of the sting strut. Finally, grid adaptation for both the surface and volume portions of the grid could be insightful and help isolate differences caused by discretization error and those caused by modeling error.

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