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I am submitting herewith a thesis written by Brian Binkley entitled “Modeling and Analysis of Turbojet Compressor Inlet Temperature Measurement System Performance.” I have examined the final electronic copy of this thesis for form and content and recommend that it be accepted in partial fulfillment of the requirements for the degree of Master of Science, with a major in Mechanical Engineering.

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MODELING AND ANALYSIS OF TURBOJET COMPRESSOR INLET TEMPERATURE
MEASUREMENT SYSTEM PERFORMANCE

A THESIS
PRESENTED FOR THE
MASTER OF SCIENCE
DEGREE
The UNIVERSITY OF TENNESSEE, KNOXVILLE

BRIAN BINKLEY

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Abstract

Accurate measurement of turbine engine compressor inlet total temperature is paramount for controlling engine speed and pressure ratio. Various methods exist for measuring compressor inlet total temperature on turbojet engines with hydromechanical control. One method involves the use of an ejector-diffuser system (eductor) to pull air from the engine inlet in order to measure the incoming total temperature. Analysis of historical test data has revealed that the inlet temperature measurement can be biased at certain flight conditions causing engine mis-scheduling and off-nominal engine operation. This bias is characterized primarily by adverse heat transfer effects and secondly by poor flow quality in the eductor tubing. Alternate eductor system configurations have been proposed to mitigate temperature bias. A one-dimensional model of the eductor system was developed to facilitate the analysis of baseline and alternate eductor configurations. Computational Fluid Dynamics was used to aid model development and the model was calibrated to and validated against ground test data. The validated model is used to quantify the performance of several eductor configurations throughout the range of expected operating conditions and to quantify the amount of compressor inlet temperature measurement bias mitigation each configuration provides.

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Nomenclature

A	Area
AEDC	Arnold Engineering Development Center
Bi	Biot number
C	Coefficient, Specific heat
CFD	Computational fluid dynamics
D	Diameter
F	Factor, Linear momentum
Fo	Fourier number
h	Enthalpy
HLLC	Harten-Lax-van Leer-Contact
IGV	Inlet guide vane
J	Bessel function
k	Thermal conductivity
L	Length
LCM	Lumped capacitance method
M	Mach number
$\dot{m}$	Mass flow rate
N	Engine speed
Nu	Nusselt number
Pri	Primary
PS	Static Pressure
PT	Total Pressure
Q	Dynamic Pressure
q	Heat transfer rate
R	Gas Constant
RANS	Reynolds Averaged Navier Stokes Equations
r	Radius
Sec	Secondary
SST	Shear Stress Transport
SSOR	Successive symmetric over-relaxation
STD	Standard day
T2	Compressor inlet total temperature

TS	Static temperature
TT	Total temperature
u	Velocity
V	Volume
W	Engine airflow rate

Greek Symbols

γ	Ratio of specific heats
Δ	Difference
θ	Non-dimensional temperature
$\kappa\text{-}\omega$	Shear transport model
$\kappa\text{-}\epsilon$	Shear transport model
Λ	Ratio of mass flow function to linear momentum
ρ	Density

Subscripts

0	Initial
1	Eductor model primary stream entrance region
2	Eductor model secondary stream entrance region
3	Eductor model static pressure matching region
4	Eductor model exit region
13	Eductor model primary stream and mixing region
23	Eductor model secondary stream and mixing region
a	Eductor annulus region
b	Eductor bleed region, engine bay
c	Eductor snorkel/scoop exit region, calibration, characteristic
d	Eductor T2 interface tube exit region
dP	Pressure drop
e	Eductor exit region
i	Into heat transfer element, heat transfer element counter
j	Eductor jet region
m	Eductor mixing region, mean
n	Heat transfer element number
o	Eductor inlet region, out of heat transfer element
R	Referred (corrected)

w	Tube wall
	Superscripts
*	Critical, non-dimensional
N	New value in iterative procedure
O	Old value in iterative procedure

1 Introduction and Background Information

This report addresses work that was done to model and analyze the performance of a compressor inlet temperature measurement system called an eductor. Three eductor systems were analyzed to ensure adequate flow would be provided to the compressor inlet temperature sensors and to understand the impact of heat transfer on compressor inlet temperature measurement bias. The three eductor systems that were analyzed are:

- 1) baseline system which pulls air from the engine inlet and exhausts to engine inlet
- 2) snorkel system which receives ram air from outside the aircraft and exhausts to engine inlet
- 3) scoop system which receives ambient air from outside the aircraft, exhausts to engine bay and cools the engine bay with ram air.

1.1 Turbojet Engines with Hydromechanical Control

An important component of any aircraft propulsion system is the engine control. In general, there are two primary methods for controlling turbojet engines: electronic and hydromechanical controls. Early hydromechanical controls were used to control engine fuel flow but became more complex with the addition of more engine components such as variable inlet guide vanes and variable exhaust nozzles. Furthermore, modern turbojet engines have increased complexity driven by the need to improve engine performance while minimizing cost and require the use of more advanced electronic engine control systems. A key component of modern engine control (whether electronic or hydromechanical control) is the accurate measurement of compressor inlet air total temperature (T_2) which is used in controlling engine speed, compressor pressure ratio and nozzle area.

Modern turbojet propulsion systems are capable of rapid flight maneuvers subjecting the aircraft and engine components to large changes in temperature. Rapid changes in temperature can cause thermal boundary layer growth on external and internal surfaces exposed to airflow as well as thermal instabilities in the compressor inlet temperature measurement system. Temperature biases in the compressor inlet temperature measurement system can result in an incorrect inlet temperature measurement (T_2 bias) especially in hydromechanical controls where the response to changes in temperature cannot be accommodated as quickly as in an electronic controller. The purpose of this work is twofold:

- 1) To understand the flow characteristics and T2 bias problem (in particular the effects of heat transfer through the eductor tubes) of a hydromechanical compressor inlet temperature measurement system called an eductor.
- 2) To study alternate eductor configurations that have been proposed by engine and aircraft manufacturers for mitigating T2 bias to determine if each alternate configuration will provide adequate flow to the T2 sensing system.

In order to accomplish these goals, a model of the eductor flow and thermal physics was developed using modeling and simulation, ground and flight test data and concepts from compressible flow and heat transfer theory. The model was used to study the physics of the existing eductor system as well as predict the performance of the alternate eductor systems before they were tested on the ground or in flight. This thesis will describe the eductor system and alternate eductor systems, the effects of biased T2 measurement on engine performance, the application of the model and results thereof and provide a recommendation on the best eductor configuration.

1.2 Eductor System Description

The eductor is a system of small pipes, T2 sensors, an ejector and a mixing region that is used to entrain the engine inlet air and maintain positive airflow across the T2 sensors. The inlet of the baseline eductor system is located in the engine bay and is connected to the outer wall of the engine inlet duct (see Figure B-1a in the appendix for details). The aluminum eductor inlet duct is connected to the T2 measurement system using a flexible rubber hose. A 90° bend connects the flexible hose to the tube containing the engine T2 sensors - a temperature sensing bellows and a Resistance Temperature Detector (RTD). The T2 bellows is an oil filled device which is mechanically connected to cams in the main fuel controller. Heat is transferred between the eductor air stream and the T2 bellows as the air passes over the bellows. The expansion/contraction of the oil inside the bellows moves a piston and linkage connected to the cams controlling fuel and inlet guide vane schedules. The T2 RTD is an electronic thermocouple sensor a few inches downstream of the bellows and sends a signal to the exhaust gas temperature and nozzle position control. Downstream of the T2 bellows and RTD, the T2 duct bends 150 degrees prior to an ejector nozzle positioned inside the eductor tubing. Ejectors are pumping devices with no moving parts and can be used to move liquid or gas fluids with another liquid or gas (Taylor and Toline 1968). Ejector pumping is accomplished by accelerating a high pressure fluid stream (otherwise known as the primary stream) into the fluid that needs to be pumped (secondary stream) using a nozzle. As the high pressure air leaves the ejector nozzle it entrains the secondary air and lowers the static pressure resulting in a pumping mechanism (DalBello and Steffen 2002). For the purposes of this study, the primary stream is compressor

discharge bleed and the secondary stream is the engine inlet air being measured by the T2 sensors. The remaining length of the eductor acts as a constant area mixing region into which the ejector jet is directed. The ejector and eductor air streams mix in the mixing region and are exhausted back into the engine inlet.

The importance of the ejector in the eductor system can be demonstrated using the following example. Consider the case where the primary stream does not exist and the eductor system consists only of a secondary tube with the associated tube lengths, bends, and flow obstructions caused by the T2 sensors. The flow will only proceed in a positive direction if the exit static to inlet total pressure ratio is low enough to overcome the losses associated with tube lengths, bends and blockages. Now consider the case where the secondary tube inlet is configured such that the inlet and exit are at the same pressure datum. In this case, there will be no flow through the tube and the need for a motive force becomes apparent. The primary stream from the ejector provides this motive force and pumps the air through the secondary tube even when there is adverse pressure gradient between the inlet and exit of the secondary tube. For complete details on various ejector geometries and associated performance see (Taylor and Toline 1968).

The eductor can be considered as three separate flow paths (Figure B-1b): 1) the primary air stream which provides pumping 2) the secondary airstream which is the air stream being pumped and sampled by the engine T2 sensors (located upstream in relation to primary stream), and 3) the mixed stream. High pressure primary stream air accelerates through the converging ejector nozzle (region j) and is ejected into a mixing region which is attached to the secondary flow path (region a , annulus). The increase in velocity across the ejector nozzle (region j) lowers the static pressure in the mixing region. The constant area tube mixes the high velocity primary stream with the low velocity secondary stream providing entrainment for the secondary stream and a static pressure rise for the mixed stream (regions $a-e$).

The ejector-diffuser operates in both subsonic and supersonic modes. Depending on the pressure ratio across the ejector nozzle, the jet may be subsonic through the mixing region or be choked at the nozzle throat in which case it expands supersonically into the mixing region. A choked ejector nozzle will produce a supersonic jet in the mixing region typically having an oblique shock structure (the strength and location of the shocks are determined by the pressure rise through the mixing region) (Leipmann 2001). In both subsonic and supersonic primary flow, the entrainment of the secondary flow by the higher velocity primary flow and the reduced static pressure provide a mechanism for moving the secondary stream through the system.

1.3 Engine T2 Measurement Bias

Engine T2 measurement bias is defined as a difference between the actual air temperature entering the engine and the temperature of the air measured by the two T2 sensors. In the absence of instrumentation errors, the T2 sensors should measure a temperature approximately equal to the bulk average temperature of the air entering the engine face. However, it is possible for heat transfer to change the air temperature before it reaches the T2 sensors in the eductor. Additionally, it is possible for the T2 bellows to measure a different temperature than the T2 RTD since the RTD is a few inches downstream of the bellows. The effects of T2 measurement bias can be significant causing engine mis-scheduling resulting in lost or reduced engine performance.

1.3.1 Causes of T2 Bias

Three sources of T2 measurement bias will be considered:

- 1) inlet duct thermal boundary layer ingested into the T2 tube
- 2) T2 tube heat transfer
- 3) engine bay air re-ingestion into the engine inlet and T2 tube

Heat transfer from the engine inlet to the incoming free stream air during rapid changes in aircraft altitude and Mach number (transient maneuvering) biases the T2 measurement. Consider a flight maneuver in which the aircraft is flying straight and level and the engine inlet wall temperature is equal to the adiabatic wall temperature (i.e. the engine inlet is heat soaked). The aircraft begins a rapid dive followed by a rapid climb as shown by the altitude / Mach number plot in Figure B-2a and percent corrected engine speed (Equation 1-1) in Figure B-2b.

$$N_R = \frac{N}{\sqrt{T_T/T_{STD}}} \quad \text{Equation 1-1}$$

The aircraft dive causes a transient thermal lag which may be seen in Figure B-2c where the sensed compressor inlet total temperature, T2, is displayed as a function of time. The engine inlet wall temperature is predicted at the engine inlet using results from previous analysis of this particular system and the free stream total temperature is shown for comparison purposes. A thermal lag is observed during the climb where the engine inlet duct remains hot while the free stream total temperature rapidly changes causing the incoming T2 air to be biased from the engine inlet duct thermal boundary layer. The transient thermal lag of the engine inlet also contributes to engine inlet temperature distortion, but discussion of these effects is beyond the scope of this report. In general, all aircraft with significant lengths of engine inlet duct that are capable of rapid flight maneuvering will experience thermal boundary layer growth in

the engine inlet duct. The problem is significant with hydromechanically controlled systems in which the measured inlet air temperature is pulled from the inlet duct boundary layer and the control cannot respond quickly enough to the rapid changes in temperature.

The second contribution to T2 measurement bias is due to heat transfer through the T2 tube into the T2 air. This heat transfer is associated with a temperature difference between the air in the engine bay outside of the T2 tube and the air flowing through the T2 tube. This phenomenon is similar to the engine inlet duct heat transfer in that there is a thermal mass associated with the T2 tube which is exposed to engine bay temperatures and experience thermal lags during transient maneuvers.

The third contribution to T2 measurement bias occurs when engine bay air is re-ingested into the engine inlet bleed holes shown in Figure B-1. The engine bay bleed holes are designed to provide cooling flow to the engine bay. There are multiple rows of bleed holes extending circumferentially through 90 degrees of the lower inlet duct as well as the upper inlet duct. At flight conditions and power settings where the engine bay pressure is higher than the engine inlet pressure, the flow through the bleed holes can flow from the bay into the inlet. This can be a significant source of T2 bias because the engine bay air is generally higher than the air in the engine inlet and T2 tube. Because engine bay bleed holes may be located just upstream of the T2 tube pickup (Figure B-1) hot engine bay air will enter the T2 tube if the engine bay air is re-ingested into the engine inlet. At certain flight conditions and engine power settings the engine bay may become pressurized such that the engine inlet pressure is lower than the engine bay pressure. The pressure difference is depicted in Figure B-2d where the red markers indicate areas where engine bay air is flowing in reverse and is re-ingested into the engine inlet via the engine bay cooling holes. Some of this re-ingested bay air enters the T2 duct since the bleed holes are located upstream of the T2 pickup in the engine inlet duct. This re-ingested bay air is usually higher temperature than free stream total temperature and can cause biased T2 duct air temperature as well as temperature distortion at the engine inlet.

1.3.2 Effects of Biased T2 Measurement

A biased T2 measurement can cause the control to mis-schedule the engine to an off-nominal operating point resulting in reduced compressor aerodynamic and aeromechanical stability margins. There are many factors that can diminish compressor stability margin such as engine deterioration, Reynolds number effects, horsepower extraction and throttle transients (Committee 1999). Consider the notional compressor performance map of Figure B-3. The nominal compressor steady state operating line is shown at the bottom of the figure and the compressor stability limit is shown at the top both as a function of pressure ratio and airflow. Adverse effects on compressor stability move the operating line up, the stability line down, or some combination of the two. The available stability margin is the difference in

stability limit and operating point after the destabilizing effects are accounted for. At high altitudes and high engine speeds there may not be much stability margin to begin with and a small change in operating point can cause compressor instability.

For example, consider the compressor pressure ratio as a function of corrected engine speed or corrected airflow as shown in Figure B-3. Corrected speed and corrected airflow represent the values that the engine would experience at standard day conditions and are given by Equation 1-1 and Equation 1-2, respectively.

$$W_R = W \frac{\sqrt{T_T/T_{STD}}}{P/P_{STD}} \quad \text{Equation 1-2}$$

At steady flight conditions, the compressor should operate on the designed operating line. However, a biased T2 measurement can cause mis-scheduling of variable inlet geometry, engine fuel flow and nozzle area resulting in migration of the compressor operating point away from the designed operating line. For a positive T2 bias (control measures higher temperature than is ingested into the engine) the control schedules a lower corrected speed than the engine should operate at and a negative T2 bias (control measures lower temperature than is ingested into the engine) will cause the control to schedule to a higher corrected speed than needed by the engine. Typically, the migration of the compressor operating point away from the nominal designed operating line results in a loss in stability margin.

1.4 Proposed Eductor Configuration Changes for Mitigating T2 Bias

Several configuration changes have been proposed to mitigate T2 bias and are briefly reviewed here. A summary of the proposed configurations is presented in Figure B-4 with the baseline eductor system shown in Figure B-4a. All configurations are restricted to using the baseline ejector due to cost, however, eductor inlet and exit geometries and locations may be modified.

1.4.1 Blocked Bleed Holes

The engine inlet bleed holes are located upstream of the T2 duct inlet to provide cooling flow to the engine bay. Because certain flight conditions can cause the direction of the bleed flow to reverse into the engine inlet, a group of engine inlet bleed holes immediately upstream of the T2 duct pickup can be blocked (Figure B-4b) to prevent ingestion of hot bay air into the T2 tube. This configuration cannot eliminate the heat transfer associated with engine inlet thermal lag and heat transfer. This configuration is the simplest and cheapest to implement since no major hardware modifications are needed. Blocking a

small portion of the bleed holes may have a negative effect on bay temperature. These effects were considered but analysis of these effects are beyond the scope of this thesis.

1.4.2 External Snorkel

An external snorkel can be located on the bottom of the aircraft (Figure B-4c) to collect air outside of the aircraft boundary layer. The snorkel is directly connected to the eductor by insulated tubes and the eductor exhausts to the engine inlet (unchanged from baseline). The snorkel and insulated tubes provide ram air to the eductor eliminating engine inlet duct heat transfer and engine bay air re-ingestion. The air enters the rectangular snorkel inlet, makes a 90 degree turn, and exits to the T2 interface tube connecting the snorkel to the eductor. The ram air entering the T2 duct will provide additional mass flow rate through the tube. Icing and aircraft thermal boundary layer ingestion are concerns for the snorkel, but will not be investigated in this report. Any flow obstruction upstream of the snorkel (landing gear, speed brakes, etc.) that may cause total pressure loss prior to the snorkel inlet is also a concern but will not be investigated in this report.

1.4.3 External Scoop

The external scoop configuration will also be located on the bottom of the aircraft (Figure B-4d). The T2 inlet is attached to the top of the scoop and the eductor exit (different from previous configurations) is modified to exhaust into the engine bay. The scoop configuration is designed such that ram air entering the scoop will provide additional cooling flow to the engine bay. The T2 interface tube is attached to the upper surface of the scoop and the total pressure at the T2 interface tube inlet will be close to the static pressure at the eductor exit (engine bay static pressure). The primary difference between the scoop and the snorkel is how the air is supplied to the T2 tube. The scoop provides a static type T2 pickup whereas the snorkel provides a ram type pickup (i.e. the snorkel is directly connected to the T2 eductor tube but the scoop provides flow to both the engine bay and the T2 eductor tube). The scoop has a larger inlet area than the snorkel in order to provide additional cooling flow to the engine bay as well as cool air to the T2 tube.

1.5 Objectives

To summarize, T2 measurement bias causes engine mis-scheduling and can be a significant problem for hydromechanically controlled engines. Alternate eductor configurations can prevent temperature biased air from entering the T2 measurement system. This thesis will discuss the modeling, simulation and analysis work that was performed to:

- 1) Develop an understanding of the flow in the baseline eductor system and the effects of T2 tube heat transfer on T2 bias.
- 2) Ensure that the alternate systems will provide sufficient flow to the eductor on the ground and in flight in order to minimize the risk associated with flight testing of the alternate systems.

2 Eductor System Modeling and Simulation

In order to understand the eductor system physics and to verify that the alternate configurations can provide sufficient flow to the T2 sensors throughout the expected range of operating conditions, a model was developed using compressible flow and heat transfer models in combination with CFD and test data. CFD was performed to determine the mass flow rates and Mach numbers through the eductor system prior to modeling since these quantities were unknown. Test data was used to validate the model. The result is a model that accurately describes the eductor flow and provides a cost effective means for predicting the performance of the eductor system (and alternate configurations) in a timely fashion.

2.1 Modeling Approach

The eductor can be thought of as a piecewise system such that it can be broken down into the following subsystems: the eductor inlet, the ejector (consisting of primary stream, secondary stream and mixed stream) and eductor exit. Models are developed for each of the subsystems and linked together in order to simulate the effects of the entire system working as a unit. This approach facilitates a rapid iterative design methodology and the analysis of multiple alternate designs. A schematic of this modeling approach is shown in Figure B-5 where the inlet subsystems that will be modeled are shown in the left box and the eductor subsystem is shown in the right box. Station nomenclature is identified by the letters, i.e. station *o* indicates conditions entering the eductor inlet system (ram air conditions for the snorkel and scoop). Performance indicators to be analyzed are the eductor inlet total pressure recovery (PT_c / PT_o), T2 circuit pressure ratio (PS_e / PT_d), T2 duct Mach number (M_d), and T2 duct mass flow rate (W_{sec}). All configurations have a common eductor model, i.e. the eductor inlet geometry may change or the eductor exit boundary condition may change, but the eductor model remains the same.

The total pressure losses through each of the eductor components are required in order to accurately model the flow through the eductor. Various methods were used to determine the magnitude of the total pressure losses and are listed in Table C-1 in the appendix. A summary of the model development is as follows:

- Three-dimensional CFD was performed first to determine pressure losses through complex inlet geometry, pressure losses due to blockages of the T2 bellows and RTD sensors and effects of the flow mixing downstream of the ejector as well as the mass flow rates of the primary and secondary streams.
- The one-dimensional model simplifying assumptions were made and the governing equations were developed.

- An iterative solution technique was developed to reduce the number of inputs required by the one-dimensional model (in particular the mass flow rates).
- The model was compared to CFD results and validated.
- Eductor ground test data was acquired and the model was calibrated.

Four variations of the eductor model were developed and the details of how each was developed are provided in the following sections:

- Baseline Eductor System Compressible Flow Model
- Snorkel Eductor System Compressible Flow Model
- Scoop Eductor System Compressible Flow Model
- Baseline Eductor System Heat Transfer Model

2.2 Baseline Eductor System Compressible Flow Model

The baseline eductor system compressible flow model consists of models of the eductor inlet, the eductor primary and secondary tube flows, the ejector and the eductor exit. The models are developed such that the exit conditions of one model are the entrance conditions of the next model (i.e. the exit of the eductor inlet is the entrance to the eductor secondary tube). A complete model of the baseline eductor system is attained by combining these models in sequence and solving the equations iteratively until the inlet and exit conditions of adjacent models match and the model is converged. Details on how these baseline models were developed are presented next.

2.2.1 Baseline Eductor Inlet Model

The baseline eductor inlet is located in the engine bay and is attached to the engine inlet such that temperature biased engine inlet air is pumped into the T2 tube (Figure B-1, Table C-1). The pressure drops across the baseline eductor inlet are modeled using results from historical pressure measurements. The results indicate that the total pressure entering the eductor system secondary stream is equal to the engine inlet static pressure (Figure B-19) throughout the engine throttle range. Therefore the baseline eductor secondary stream inlet boundary condition (*PTd* Figure B-5) is set to engine inlet static pressure and total temperature.

2.2.2 Baseline Eductor Primary and Secondary Tube Flow Models

CFD was used to support primary and secondary tube flow model development. Three-dimensional steady state CFD was performed to simulate the ejector operation and the flow upstream and downstream of the ejector. The results were used to determine the effects of total pressure losses due to

friction, pipe bends, sensor blockages in the primary and secondary flow paths upstream of the ejector and mixing effects downstream of the ejector. Boundary conditions for the eductor CFD simulations were implemented such that the primary total to exit static and secondary total to exit static pressure ratios covered the expected range of operation and are shown in Table C-2.

OVERFLOW (Buning, Nichols and Tramel 2009) was chosen as the CFD code to solve the three-dimensional time dependent Reynolds Averaged Navier Stokes (RANS) equations with a perfect gas assumption. A third order Harten-Lax-van Leer-Contact (HLLC) upwinding discretization scheme is used to model the space derivatives, and the time derivatives are implicitly discretized using second order differencing. For each time step, 10 levels of Newton sub-iterations were employed to time accurately advance the effect of the lagged boundary conditions to the current time level. The unfactored symmetric successive over-relaxation (SSOR) solver uses a relaxation method to resolve the resulting system of equations which is very stable in adverse pressure gradient flows. A two equation, Shear Stress Transport (SST) model was used to account for the turbulent viscosity. The wall boundary conditions are specified to be adiabatic and no slip. The inlet boundary conditions are total pressure and total temperature with velocity angles specified to enter the duct parallel to the inlet walls, and the exit boundary condition is static pressure. An automated grid decomposition algorithm was used with multiple processors to minimize the time to solution.

2.2.2.1 Eductor Secondary Stream Total Pressure Losses

The secondary stream total pressure losses (PT_a/PT_d Table C-1) associated with representative CFD results are shown in Figure B-6 and the total pressures shown are normalized to the incoming secondary stream total pressure (PT_d). The CFD results indicated that the ejector nozzle that provides the pumping to the eductor system can operate in both choked and unchoked conditions. CFD results are presented for both choked and unchoked ejector operation in Figure B-6.

First consider a choked ejector example with T2 tube pressures shown in Figure B-6a, the primary stream expands supersonically into the mixing region and the primary and secondary streams mix completely and exit uniformly. The flow loses minimal total pressure between the inlet and the T2 sensors - on the order of 2%. Blockage by the T2 sensors causes the majority of the total pressure loss in the T2 flow - approximately 11.5%. An additional 0.5% total pressure loss is encountered due to the flow turning through the 150° bend. For the choked ejector example the total pressure loss through the eductor T2 tube is approximately 14%.

Next, consider the unchoked ejector example of Figure B-6b where the primary stream accelerates through the ejector nozzle to 0.8 Mach number, the flow remains subsonic in the mixing region, the primary and secondary streams mix completely and the mixed flow exits uniformly. The lower

speed ejector flow results in lower speed secondary flow, and hence, lower total pressure losses. Like the choked ejector example, the majority of the total pressure loss is due to the blockage of the T2 sensors. For the lower speed flow due to unchoked ejector operation, the total pressure loss through the eductor T2 tube is only 2.6%.

The secondary stream CFD total pressure loss results were curve fit as a function of the CFD boundary conditions (Table C-2) for use in the model. In general, the total pressure loss associated with airflow through a pipe is a function of the Mach number. The secondary stream Mach number is a function of the pressure difference between the secondary inlet total (region *d* Figure B-5) and mixing region static (region *a* Figure B-5) pressures. The mixing region static pressure is controlled by the ejector and, hence, is a function of the pressure difference between the primary inlet total and eductor exit static pressure. Therefore, the secondary stream total pressure loss is curve fit as a function of both primary stream total pressure and secondary stream total pressure to exit static pressure ratios (Equation 2-1). The total pressure losses are updated incrementally as the eductor model iterates to convergence. The curve fit is shown in Figure B-7 where the markers represent the CFD runs and the surface is colored by percent of total pressure (PT_o) and is constrained to pass through the CFD points.

$$\Delta PT_{secondary} = f\left(\frac{PT_j}{PS_e}, \frac{PT_o}{PS_e}\right) \quad \text{Equation 2-1}$$

2.2.2.2 Eductor Primary Stream Total Pressure Losses

Next consider the primary stream total pressure loss (PT_j/PT_b – Table C-1). The primary stream tube (from compressor bleed to ejector nozzle - Figure B-1) has an inner diameter of 0.305 inches. The ejector tube inlet boundary condition was the compressor exit static pressure. The CFD predicted a total pressure loss across the primary tube (from inflow to just prior to ejector exit) to be constant over the expected operating range such that the primary stream total pressure is approximately 90% of the compressor exit static pressure.

An alternate method for determining the primary stream total pressure loss from empirical data was used to verify the CFD results. The pressure drop coefficient can be determined by estimation of the friction effects along the full length of tube, the inlet loss associated with sharp corners and developing flow, and the losses due to bends in the tube. The pressure drop coefficient (K) is found in the Darcy Equation (Equation 2-2) for pressure loss (Crane Valves 1999).

$$\Delta P = K \frac{1}{2} \rho u^2 \quad \text{Equation 2-2}$$

Generally, the Darcy equation is used only for incompressible flow. However, this equation may be used for compressible flow provided the following restrictions are observed as specified in (Crane Valves 1999).

- 1) *If the calculated pressure drop ($P_1 - P_2$) is less than about 10% of the inlet pressure P_1 , reasonable accuracy will be obtained if the specific volume used in the formula is based upon either the upstream or downstream conditions, whichever are known.*
- 2) *If the calculated pressure drop ($P_1 - P_2$) is greater than about 10%, but less than about 40% of inlet pressure P_1 , the Darcy equation may be used with reasonable accuracy by using a specific volume based upon the average of upstream and downstream conditions...*

Using the definition of Mach number to write u in terms of tube Mach number and static temperature and the ideal gas equation to write ρ in terms of static pressure and temperature, Equation 2-2 can be rewritten as Equation 2-3 to determine the primary stream total pressure loss from inflow to just prior to ejector exit ($PS_{comp} - PT_{pri}$). The static temperatures are determined from total temperatures and isentropic relations. The pressure drop coefficient (K) was determined to be 3.94 (details on how this value was calculated are provided in Appendix D).

$$\Delta P = PS_{comp} - PT_{pri} = 3.94(0.5) \left(\frac{PS_{comp}}{RTS_d} \right) (M_d \sqrt{\gamma RTS_d}) \quad \text{Equation 2-3}$$

The empirical and CFD methods for determining primary stream total pressure loss are compared in Figure B-8. The primary stream total pressure (PT_j) is shown as a function of primary stream inlet boundary condition (PS_b) both of which are non-dimensionalized by eductor exit static pressure (PS_e). The red data represent the ejector total pressure drop using results from CFD and the blue data represent the ejector total pressure drop using the derived pressure loss coefficient and a tube Mach number of 0.2. The derived pressure loss coefficient method results in about 1-1.5% increase in total pressure drop. The comparisons indicate that both the CFD and empirical methods result in approximately the same total pressure at the ejector and the pressure drop is 10% of the inlet pressure (which substantiates the use of the Darcy equation).

2.2.3 Baseline Ejector Model

An existing one-dimensional ejector model developed at the Arnold Engineering Development Center (AEDC) based on the work reported in (Taylor and Toline 1968) was reviewed prior to developing a new ejector model. However, this model required that the mass flow rate of both secondary and primary streams be known. Eductor flow rate data was not available until several months after the modeling

efforts were initiated. Therefore, a more general ejector model was needed which could iteratively determine the mass flow rate of both streams. The one-dimensional model of the ejector was developed and validated using the existing AEDC ejector model. The one-dimensional ejector model was then calibrated for eductor specific flow phenomena (using CFD early in the model development and test data late in the model development) to facilitate the analysis of the eductor configurations and produce estimates of key performance indicators.

2.2.3.1 Ejector Model Simplifying Assumptions

The following simplifying assumptions are made in the development of the ejector model:

- The flow is steady, i.e. no mass accumulates inside the control volume.
- The working fluid (air) behaves as an ideal gas at the operating conditions of interest.
- The working fluid (air) is calorically perfect with constant specific heats.
- Conservation laws hold, i.e., mass, linear momentum and energy are conserved.
- Secondary stream remains subsonic throughout the eductor.
- Primary stream can be subsonic or supersonic.
- The mixing process is isentropic.
- Any expansion or contraction processes (such as shocks) occur prior to mixing.
- Area changes are small enough to permit quasi-one-dimensional flow assumptions.

2.2.3.2 Ejector Model Governing Equations

The one-dimensional equations governing the flow of air through the eductor system are conservation of mass, conservation of linear momentum, and conservation of energy. Additional relations such as the ideal gas law and isentropic relations between pressure, temperature, density, and Mach number are used in the model where appropriate modeling assumptions permit. Details on the derivations of the mathematical eductor model equations are presented in Appendix A.

2.2.3.2.1 Conservation of Mass – Continuity

The one-dimensional continuity equation states that the mass must remain constant for steady, quasi-one-dimensional flows. This can be stated alternatively as the mass flow entering the system must equal the mass flow exiting the system (Leipmann 2001, pp. 40).

$$\rho u A = \text{const} \quad \text{Equation 2-4}$$

2.2.3.2.2 Conservation of Linear Momentum

$$(\rho_2 u_2^2 A_2 - \rho_1 u_1^2 A_1) = (P_1 A_1 - P_2 A_2) + \int_1^2 P dA \quad \text{Equation 2-5}$$

Consider the compressible flow of air through a one-dimensional duct in which the area increases as a function of position and the flow proceeds from station 1 to station 2 as shown in Figure B-10. When viscous and body terms are neglected, the steady state linear momentum equation reduces to a balance of inertial forces with pressure forces. The left hand side of the linear momentum equation describes the change of inertial forces from inlet to outlet and the right hand side is the sum of the pressure forces where the $P_1 A_1$ is the pressure force at the inlet and the $P_2 A_2$ is the pressure force at the exit. The integral represents the force due to the pressure along the side of the body and is approximated in the one-dimensional model by breaking the fluid body into many smaller fluid bodies and summing the delta pressures and delta areas for each of the smaller pieces. (Leipmann 2001, pp. 47-49)

In order to account for viscous effects in the linear momentum equation, the model of the eductor was calibrated to CFD results over the range of expected eductor operating conditions. The results of the CFD simulations provided a means for determining the effects of viscous flows inside the eductor, in particular where mixing occurs. The model was calibrated by adjusting the inviscid linear momentum equations iteratively until the model predicted mass flow rates and total pressure drops matched CFD results. This calibration factor is applied to ensure that the one-dimensional model accurately simulates the three-dimensional viscous flow and accounts for non-isentropic processes. The momentum calibration factor was curve fit as a function of eductor primary stream boundary conditions as a linearly decreasing function of the ratio of primary stream total pressure to eductor exit static pressure and is shown in Figure B-13. The calibration factor (F_{cal}) is applied to the linear momentum equation as shown in Equation 2-6 where F_j is the linear momentum in the primary stream prior to mixing, F_a is the linear momentum in the secondary stream prior to mixing and F_e is the linear momentum of the mixed, exit stream. As the primary stream pressure ratio increases (resulting in higher velocity flow) the calibration factor decreases resulting in increased total pressure losses.

$$F_e = F_{cal}(F_j + F_a) \quad \text{Equation 2-6}$$

2.2.3.2.3 Conservation of Energy

$$h + \frac{1}{2}u^2 = \text{const} \quad \text{Equation 2-7}$$

When the flow is assumed to be adiabatic and have no shaft work, the energy equation reduces to the statement that the total enthalpy is constant. Equation 2-7 remains true even if a shock wave is present in the eductor, since the total enthalpy remains constant across a shock wave. (Leipmann 2001, pp. 41-43)

2.2.3.2.4 Isentropic Flow Relations

Given the definition of Mach number and assuming air behaves as a perfect gas, the energy equation can be re-written as the ratio of total to static temperature as a function of Mach number (M) and ratio of specific heats (γ) (Equation 2-8).

$$\frac{T_0}{T} = 1 + \frac{\gamma - 1}{2} M^2 \quad \text{Equation 2-8}$$

Isentropic flow relations give the connection between Mach number and other variables for an ideal gas with constant specific heats. (Moran and Shapiro 2004, pp. 257)

$$\frac{T_0}{T} = \left(\frac{P_0}{P}\right)^{(\gamma-1)/\gamma} = \left(\frac{\rho_0}{\rho}\right)^{\gamma-1} \quad \text{Equation 2-9}$$

2.2.3.3 *Ejector Model Solution Scheme*

2.2.3.3.1 Boundary Conditions

The model requires five boundary conditions – total pressure (PT_j) and temperature (TT_j) in the primary stream just upstream of the mixing, total pressure (PT_a) and temperature (TT_a) in the secondary stream just upstream of mixing, and exit static pressure (PSe). Two geometric inputs are also required by the model- ejector nozzle (primary stream) diameter and mixing region diameter (which is equal to the diameter of the secondary stream (T2) tube).

2.2.3.3.2 1D Static Pressure Constraint

One physical constraint is applied to the eductor model for both subsonic and supersonic ejector operation. Consider the choked ejector case as shown in Figure B-11a. A notional slip line is shown depicting the boundary between the two streams of different velocity and density. Generally the entrainment of the secondary stream into the primary stream is not one-dimensional. The interaction of the two streams tends to decrease the velocity of the high speed stream (thereby increasing static pressure) and increase the velocity of the low speed stream (decreasing static pressure) until they are mixed (uniform static pressure). The one-dimensional approximation leverages the physical requirement that the

mixed stream must have a uniform static pressure and constrains the static pressure in both streams to be equal in the mixing region. Mixing correlations can be applied thereafter to determine the remaining flow properties (see Appendix A for details). This “matching” of static pressure occurs downstream of the ejector nozzle where the primary flow has expanded (increased flow area) and the secondary stream flow area has decreased resulting in increased velocity and decreased pressure.

The constraint is applied similarly for subsonic ejector operation. As shown in Figure B-11b, the static pressure matching occurs at the ejector nozzle exit where the flow area is known since the primary flow does not expand.

2.2.3.3.3 Iterative Solution Algorithm

If only the boundary conditions are explicitly known and the mass flow rates and Mach numbers are unknown, an iterative solution approach must be used. The solution steps are outlined in Appendix D and follow the flow chart shown in Figure B-12.

2.2.3.4 Ejector Mixing Region Total Pressure Loss

Downstream of the ejector nozzle, the primary and secondary streams mix inside a constant area mixing region. Consider the total pressure loss in the mixing region (P_{Te}/P_{Tm} – Table C-1). Total pressures in the mixing region are shown in Figure B-9 where the pressures have been normalized by primary stream inlet total pressure. For the choked ejector example (Figure B-9a) the loss in total pressure in the mixing region is approximately 62% which can be attributed to oblique shocks caused by the supersonic primary stream expanding and decelerating into the mixing region. For the unchoked ejector example (Figure B-9b) there are no shocks in the mixing region and the total pressure loss is an order of magnitude less - approximately 6.6%.

2.2.4 Baseline Eductor Exit Model

The results of (Vick 1962) show that for air outlets discharging cross wise into transonic flows the local static pressure of the discharging air must be higher than the static pressure in the cross stream. Therefore, the local static pressure at the eductor exit (region e Figure B-5) must be higher than the static pressure of the cross stream (engine airflow) into which the eductor flow discharges. The exit boundary condition of the eductor model is determined using the engine inlet static pressure and curve fits of the results of (Vick 1962). For engine inlet Mach numbers representative of power settings above engine idle, the resulting exit pressure boundary condition of the eductor system (P_{Se} Figure B-5) is approximately 3% higher than engine inlet static pressure.

2.2.5 Validation of the Baseline Ejector Model

Consider an example scenario in which the ejector boundary conditions are specified for steady state operation and the type of ejector inlet configuration is arbitrary. The predictions shown in Figure B-14 demonstrate the ability of the ejector to pump secondary air from/to the same pressure datum. Primary stream pressure and Mach number predictions are shown in red and secondary stream predictions are shown in blue. The total and static pressures have been normalized to primary stream inlet total pressure. The trade of static pressure for increased velocity can be seen in the primary stream between regions o and j and the ratio of inlet to exit secondary pressure (o to e) is close to unity. The primary stream is choked at region j (ejector nozzle) and expands supersonically into the mixing region. Region m represents an isentropic process just downstream of the ejector where the static pressures of the two streams are constrained to be equal. The primary stream becomes subsonic between stations j and m with a loss in total pressure and decrease in velocity. The primary and secondary streams are mixed at region m before exiting the ejector at station e .

The following example demonstrates agreement of the one-dimensional calibrated model viscous flow predictions with the theoretical one-dimensional viscous flow represented by the Fanno line. The Fanno line approximates one-dimensional flow with friction and non-uniformities (such as shock waves) (Leipmann 2001) and provides insight to the effects of entropy change (Thompson 1972). A constant mass flow and constant area Fanno line for the primary stream is displayed in Figure B-15 where the ordinate is the static to total temperature ratio (non-dimensional enthalpy) and the abscissa is the entropy change. Primary stream inlet conditions are shown at station b and the primary stream is subsonic until it reaches the ejector nozzle (j) at which point it becomes choked. As shown on the Fanno line, the choked point is the point of maximum entropy in the primary flow. The flow expands supersonically into the constant area mixing region following the Fanno line from the point labeled j to the point labeled $j-m$ (supersonic conditions between station j and station m). The primary flow then becomes subsonic again following the dotted line from point j to point m . For the one-dimensional control volume analysis, the process from supersonic to subsonic (whether this process is a normal shock or, more likely, a series of oblique shocks) is of no concern since the control volume can be chosen to include the shocks and the viscous effects inside the shocks can be ignored because the total enthalpy is conserved (Moran and Shapiro 2004). The final point e represents the change in primary conditions to mixed conditions and the entropy change between m and e is due to the mixing of the primary and secondary streams and wall shear stress through the remaining length of the mixing region.

The model predictions of T2 tube mass flow rate are compared to T2 tube mass flow rates derived from experimental ground test data in Figure B-16. The measurements used to derive the experimental T2

tube mass flow consisted of a total temperature measurement, total pressure measurement and static pressure measurement in the T2 tube upstream of the T2 sensors. These measurements are used with the isentropic relations from Equation 2-8 and Equation 2-9 to derive the experimental T2 tube Mach number at the instrumented tube location. The T2 tube mass flow rate is then determined from the flow area, flow speed (Mach number) and density (derived from static temperature, static pressure and the ideal gas law) as shown in Equation 2-10.

$$\dot{m} = \rho AV = P_s AM \sqrt{\frac{\gamma}{RT_s}} \quad \text{Equation 2-10}$$

Comparisons of model predictions to test data are shown in Figure B-16. The model predictions represent CFD calibrated predictions and it is clear that a second calibration is needed to match test data. The model was calibrated to test data by applying a calibration factor to the total pressure losses that were derived from CFD results as a function of percent corrected engine speed (since the eductor performance is primarily a function of engine speed) to match the mass flow rate observed in ground test data. The calibration is applied as shown in Equation 2-11 where the CFD ratio is adjusted until the T2 tube mass flow rate equals the measured values throughout the engine speed range. The calibration factor is shown as a function of percent corrected speed in Figure B-17. The calibration factor is highly non-linear due to the non-linearity of the model predictions and increases as a function of percent corrected speed with a maximum correction of approximately 2%. The eductor model nonlinearities (and the required non-linear calibration) are attributed to the transonic ejector flow in the 60-80% speed range. Additional CFD simulations at eductor operating conditions near the 60-80% speed range would further characterize the transonic ejector flow and would likely improve the eductor model predictions in the absence of ground test data calibration.

$$PT_a = F_c \left(\frac{PT_a}{PT_d} \right)_{CFD} PT_d \quad \text{Equation 2-11}$$

Results of the model calibration to test data are shown in Figure B-18. Here it may be seen that the model shows good agreement with the mean flow rate derived from test data. Also, it is demonstrated that the T2 tube mass flow is nearly a linear function of percent corrected engine speed. This calibration applies to all configurations since the factor is applied to the engine side of the eductor system (which remains unchanged for all alternate configurations and is discussed in the following sections).

2.3 Snorkel Eductor System Compressible Flow Model

The snorkel eductor system is the first alternate configuration that was modeled and consists of math models of the snorkel inlet, the T2 interface tube connecting the snorkel to the eductor secondary tube, the eductor primary and secondary tube flows, the ejector and the eductor exit. A complete model of the snorkel eductor system is attained by combining these models in sequence and solving the equations iteratively until the inlet and exit conditions of adjacent models match and the model is converged. Details on how these snorkel eductor models were developed are presented next.

2.3.1 Snorkel Eductor Inlet Model

The snorkel inlet model consists of two parts. The first part is the snorkel which protrudes from the bottom surface of the aircraft. The second part is the flexible tube connecting the snorkel flow directly to the secondary tube of the eductor. Details on how models for these two parts were developed are described next.

CFD simulations were performed for the five flight conditions shown in Table C-3 to characterize the total pressure recovery across the snorkel inlet (Table C-1). The five conditions were chosen to simulate the range of operating conditions that could be flown with the snorkel configuration. The CFD model included the snorkel geometry attached to a flat plate simulating the bottom of the aircraft and upstream conditions specified as uniform free stream total pressure and temperature. Since the snorkel is designed to receive air outside of the aircraft boundary layer, no boundary layer was modeled in the CFD. For each flight condition the snorkel tube exit back pressure was varied such that the total pressure recovery can be determined as a function of the Mach number at the snorkel exit as shown in Figure B-20 where the markers indicate conditions where the CFD was performed and the solid lines are curve fits of the CFD results.

The T2 interface tube connects the external inlet (scoop or snorkel) to the eductor (Table C-1). The tube has an inner diameter of approximately 1 inch and has one 90 degree bend. The total pressure drop across the T2 interface tube can be curve fit from CFD results as a function of the Mach number at the tube exit for use in the one-dimensional model. The CFD results and curve fit are shown in Figure B-21 where the total pressure entering the T2 interface tube is given by PT_c and the total pressure leaving the T2 interface tube (and entering the eductor) is given by PT_d and the pressure drop is shown as a function of Mach number at the tube exit.

The snorkel eductor secondary stream inlet boundary condition is determined using the curve fits of Figure B-20 and Figure B-21 (i.e. the combined total pressure loss associated with the snorkel and T2 interface tube). By specifying the free stream conditions entering the snorkel (altitude and Mach number)

and the tube Mach number, the total pressure loss can be determined for the snorkel and the T2 interface tube. Combining the two total pressure losses provides the input to the eductor secondary tube model.

2.3.2 Snorkel Eductor Primary and Secondary Tube Flow Models

The primary and secondary eductor tubes did not change between configurations. Therefore the primary and secondary tube flow models are the same as described for the baseline configuration in section 2.2.2.

2.3.3 Snorkel Ejector Model

The ejector also did not change between configurations. Therefore the ejector model for the snorkel configuration is the same as described for the baseline model in section 2.2.3.

2.3.4 Snorkel Eductor Exit Model

The snorkel was designed to deliver free stream air to the eductor system with the highest possible mass flow through the eductor in order to mitigate the effects of T2 tube heat transfer and thermal lag. The exit of the eductor with snorkel inlet configuration is located in the engine inlet (Figure B-4c) as was the case with the baseline configuration. This allows for a high secondary stream pressure ratio since the free stream total pressure is always higher than the engine inlet pressure. Since the exit remained unchanged from the baseline configuration, the eductor exit boundary conditions are the same as those described for the baseline model in section 2.2.4.

2.4 Scoop Eductor System Compressible Flow Model

The scoop eductor system was the second alternate configuration that was modeled and consists of math models of the scoop inlet, the T2 interface tube connecting the scoop to the eductor secondary tube, the eductor primary and secondary tube flows, the ejector and the eductor exit. A complete model of the scoop eductor system is attained by combining these models in sequence and solving the equations iteratively until the inlet and exit conditions of adjacent models match and the model is converged. Details on how these scoop eductor models were developed are presented next.

2.4.1 Scoop Eductor Inlet Model

The scoop inlet model also consists of two parts. The first part is the scoop which protrudes from the bottom surface of the aircraft. Clean flow enters the scoop and is directed one of two places. Most of the incoming flow is ejected into the engine bay for cooling but a small portion of the flow enters the T2 eductor tube via the connection on the top surface of the scoop (Figure B-4). The second part is the flexible tube connecting the scoop flow to the secondary tube of the eductor. Because the scoop is

connected to the engine bay, there is a possibility for hot engine bay air to reverse flow through the scoop and into the T2 measurement system when the engine bay is pressurized during ground operation. Counter measures were implemented to prevent such a scenario the details of which are beyond the scope of this report. Details on how models for these two parts were developed are described next.

Similar to the snorkel configuration, CFD simulations were performed for the five flight conditions shown in Table C-2 to characterize the T2 tube total pressure using the scoop inlet (Table C-1). The CFD results were used to iterate the scoop design for optimum T2 tube flow rate and bay cooling flow. The final scoop configuration is designed such that the total pressure entering the eductor secondary stream is approximately equal to the eductor exit static pressure. CFD results for the scoop are shown in Figure B-22 where the ratio of the total pressure entering the T2 interface tube (PT_c) to the free stream total pressure (PT_o) is shown as a function of the Mach number at the T2 interface tube inlet for each of the five flight conditions. For each of the flight conditions, the scoop and T2 interface tube exit boundary conditions were varied in order to determine the total pressure losses as a function of Mach number. At the 0.7 Mach number CFD runs a shock formed in the scoop below the T2 tube pickup resulting in a slightly different trend in the data shown in Figure B-22.

Since the same T2 interface tube is used for both the snorkel and scoop configurations, the T2 interface tube results from the snorkel analysis (section 2.3.1) are also used for the scoop analysis.

2.4.2 Scoop Eductor Primary and Secondary Tube Flow Models

The primary and secondary eductor tubes did not change between configurations. Therefore the primary and secondary tube flow models are the same as described for the baseline configuration in section 2.2.2.

2.4.3 Scoop Ejector Model

The ejector also did not change between configurations. Therefore the ejector model for the snorkel configuration is the same as described for the baseline model in section 2.2.3.

2.4.4 Scoop Eductor Exit Model

The scoop was designed to mimic the pumping capabilities of the baseline eductor system by providing flow to the T2 tube that is near engine bay pressure and exhausting the eductor to the engine bay. Hence the eductor exit was modified to exhaust to the engine bay for the scoop eductor system only. Therefore, the eductor exit boundary condition for the scoop configuration is engine bay static pressure.

2.5 Baseline T2 Tube Heat Transfer Modeling

The baseline T2 tube heat transfer was modeled in order to determine the impact of T2 tube heat transfer on T2 bias. A summary of T2 bias mitigation approaches and their respective T2 bias components are summarized in Table C-4. Determination of T2 bias due to engine bay air re-ingestion and engine inlet thermal boundary layer is beyond the scope of this thesis. However, T2 tube heat transfer is readily estimated once the T2 tube mass flow rate is determined using the eductor compressible flow model. By predicting the T2 bias due to T2 tube heat transfer, the other two causes of T2 bias can be isolated. For example, T2 bias due to engine inlet thermal boundary layer effects is negligible when the aircraft is on the ground or in steady level flight such that the engine inlet duct temperature is in equilibrium with the ambient temperature. By eliminating one of the causes of T2 bias, the third cause of T2 bias can be inferred using the known T2 bias and the predicted T2 bias due to T2 tube heat transfer.

2.5.1 Heat Transfer Relations

In order to determine the baseline T2 tube heat transfer, the T2 tube will be modeled beginning at the T2 tube pickup screen and ending just before the T2 sensors in the eductor (Figure B-23). Several assumptions will be made for developing the T2 tube heat transfer model:

- The air entering the T2 tube pickup screen (Figure B-1) has a temperature equal to free stream total temperature. This essentially removes the effects of engine bay air re-ingestion and engine inlet duct heat transfer from the incoming air and provides an isolated prediction of T2 tube heat transfer effects.
- The T2 tube geometry may be discretized such that each element of the tube is solved sequentially and the resulting outlet temperature of the element is the inlet temperature to the next element.
- Initial temperature of the tube wall is the average of the engine bay air and incoming T2 tube air temperature at each element.
- The lumped capacity method (LCM) can be used when the element Biot number is less than 0.1.
- For development purposes, the engine bay temperature is greater than the tube wall temperature and the tube wall temperature is greater than the air temperature inside the tube.
- Material properties (density, specific heat and thermal conductivity) are assumed to be constant over the length of each element but are allowed to vary from element to element.
- The tube wall temperature is assumed to be constant over the length of each element, but not the entire length of the T2 tube.

- Heat conduction along the length of the tube is negligible in comparison to the heat transferred radially through the wall.
- The engine bay air flow over the T2 tube can be modeled as a tube in crossflow.
- An average forward engine bay temperature can be used to describe the T2 tube external temperature boundary condition.
- There are no source terms due to heat generation within the tube wall.

Consider the flow of air through adjacent tube elements shown in Figure B-23. The boundary conditions on the n th element are the bay air temperature (Tb), the temperature of the air entering the element ($Ti_n=To_{n-1}$), the air temperature leaving the element ($To_n=Ti_{n+1}$). In general, the exit temperature and the tube wall temperature are unknown and the temperature entering the first element and the bay temperature are known. Since the mean air temperature in each element is unknown, the air properties are iteratively refined until the model converges. The temperature exiting the tube (or entering the eductor) is the desired quantity and will provide an indication of the temperature rise due to T2 tube heat transfer. T2 tube heat transfer can be considered in three parts:

- 1) Convection heat transfer between the engine bay air and the T2 tube wall
- 2) Conduction heat transfer through the T2 tube wall
- 3) Convection heat transfer between the T2 tube wall and the T2 tube air stream

The T2 tube external convection (between bay air and tube wall) as well as the internal convection (between tube wall and tube air stream) causes a change in the transient tube wall temperature, therefore these heat transfer processes are coupled and cannot be solved independently. Details on the heat transfer solution process for each tube element are discussed next.

2.5.1.1 Heat Transfer Convection Coefficients

The heat transfer coefficient can be determined using the definition of Nusselt number (Equation 2-12). Empirical correlations such as the Gnielinski relationship are readily available for determining the Nusselt number as a function of Reynolds number and Prandtl number for a variety of flow geometries (Incropera, et al. 2007, pp. 515). The Reynolds number of the internal T2 tube flow is determined using values from the eductor compressible flow model solution. The internal heat transfer coefficient is determined in this fashion for each of the tube elements and varies along the length of the tube as shown in Figure B-24. Similarly, the external T2 tube heat transfer coefficient is determined from Hilpert's empirical correlation (Incropera, et al. 2007, pp. 426) and is a function of Reynolds number and Prandtl

number in the engine bay. The engine bay air velocity is estimated based on engine bay pressure differentials and is used to determine the engine bay Reynolds number.

$$Nu = \frac{hD}{k} \quad \text{Equation 2-12}$$

2.5.1.2 Method for Determining the Transient Wall Temperature

The Biot number is used to determine if the lumped capacitance simplification is applicable. The LCM is generally not an acceptable method to use when the Biot number is greater than 0.1. The Biot number is given by Equation 2-13 where L_c is the characteristic length, h is the convection coefficient and k is the material thermal conductivity. The characteristic length can be described as the ratio of the volume of the material to the surface area of the material (for circular tubes the characteristic length typically works out to be close to the wall thickness).

$$Bi = \frac{hL_c}{k} \quad \text{Equation 2-13}$$

In general, the Biot number for the aluminum tube is much less than 0.1 since the thermal conductivity of the aluminum is large in comparison to the heat transfer coefficient. However, the flexible hose material has low thermal conductivity resulting in a Biot number much larger than 0.1. The tube Biot number is shown in Figure B-24 where it can be seen that the aluminum portion of the tube satisfies the Biot number criterion and flexible hose does not.

If the Biot number criterion is satisfied then the conduction through the T2 tube wall is negligible. The LCM assumes that each tube element can be represented by a lump of material representing the volume of the tube element (Figure B-25). Assuming no work is performed, an energy balance on the lump of material reveals that the net rate of convection heat transfer must equal the rate of thermal energy storage in the material (Equation 2-14).

$$\dot{q}_b - \dot{q}_d = \dot{q}_{stored} \quad \text{Equation 2-14}$$

The convective heat transfer is given by Newton's Law of Cooling (Equation 2-15) and correlations for determining the convective coefficient (h) are readily available in the literature (Incropera, et al. 2007, pp. 426, 515).

$$\dot{q} = hA(\Delta T) \quad \text{Equation 2-15}$$

The energy stored in the material can be estimated via the LCM using Equation 2-16 where the density, specific heat, and volume are properties of the material. When applied to the discretized tube elements, the density, specific heat and volume are not required to be constant from element to element.

This provides a method for accommodating changes in material (such as aluminum tube to rubber hose) and changes in geometry.

$$\dot{q}_{stored} = \rho CV \frac{dT}{dt} \quad \text{Equation 2-16}$$

Substituting the heat transfer expressions into the energy balance equation (and assuming heat transferred into the volume is positive) results in Equation 2-17.

$$h_b A_b (T_b - T_w) - h_d A_d (T_w - T_d) = \rho CV \frac{dT_w}{dt} \quad \text{Equation 2-17}$$

Re-arranging Equation 2-17 yields a first order differential equation with constant coefficients (Equation 2-18).

$$\frac{dT_w}{dt} + C_1 T_w = C_2 \quad \text{Equation 2-18}$$

Where:

$$C_1 = \frac{(h_b A_b + h_d A_d)}{\rho CV} \quad \text{Equation 2-19}$$

And:

$$C_2 = \frac{(h_b A_b T_b + h_d A_d T_d)}{\rho CV} \quad \text{Equation 2-20}$$

Solutions to first order ordinary differential equations of this form are readily available (Ince 1956) and take the form of an exponential function. The resulting solution (Equation 2-21) can be used to determine the transient wall temperature accounting for both internal and external convection heat transfer. The solution requires the internal and external temperature boundary conditions as well as the initial condition for wall temperature (T_0). The initial condition is selected for each element such that the wall temperature is initially the average of the external temperature and the temperature entering the element.

$$T_w = \frac{C_2}{C_1} + \left[T_0 - \frac{C_2}{C_1} \right] \exp(-C_1 t) \quad \text{Equation 2-21}$$

If the Biot number criterion is not satisfied, then the conduction through the tube wall cannot be neglected and an alternate approach must be formulated to determine the transient wall temperature. In general, the heat conduction through the tube wall is a two-dimensional process, however, if the tube is modeled as an infinite cylinder (cylinders having a length to radius ratio greater than 10) then the heat conduction in the length direction is negligible when compared to the conduction in the radial direction (Figure B-26).

The heat equation for radial conduction through a tube wall is given by Equation 2-22. The derivatives in the circumferential and length directions are negligible compared to the radial derivative for infinite cylinders and have been omitted from Equation 2-22 and it is assumed that there is no heat generation in the tube wall (Incropera, et al. 2007).

$$\frac{1}{r} \frac{\partial}{\partial r} \left(kr \frac{\partial T}{\partial r} \right) = \rho C_p \frac{\partial T}{\partial t} \quad \text{Equation 2-22}$$

Assuming a uniform initial temperature and convective boundary conditions, the non-dimensionalized exact solution is given by Equation 2-23 (Incropera, et al. 2007, pp. 276-277) where J_0 and J_1 are Bessel functions of the first kind.

$$\theta^* = \frac{(T_w - T_b)}{(T_0 - T_b)} = \sum_{n=1}^{\infty} C_n \exp(-Z_n^2 Fo) J_0(Z_n r^*) \quad \text{Equation 2-23}$$

where

$$Fo = \alpha t / r_o^2$$

$$r^* = r / r_o$$

$$C_n = \frac{2}{Z_n} \frac{J_1(Z_n)}{J_0^2(Z_n) + J_1^2(Z_n)}$$

$$Z_n \frac{J_1(Z_n)}{J_0(Z_n)} = Bi$$

Additional simplifications can be made to the infinite series in Equation 2-23. If the Fourier number is greater than 0.2 the infinite series can be approximated by the first term (i.e. $n=1$). However, if the Fourier number condition is not met, the infinite series can be approximated by a few terms if the inclusion of additional terms does not affect the resulting temperatures. For example, an approximation to the infinite series using five terms is displayed in Table C-5 (Schneider 1957). The fourth and fifth temperature values are close enough to justify the use of only five terms for practical engineering applications. Equation 2-23 may be used to solve for the transient internal tube wall temperature (T_w) without assuming negligible conduction through the T2 tube wall.

2.5.1.3 Fluid Temperature Rise Across Tube Element

Once the time lagged wall temperature is known, the increase in temperature across the tube element can be determined. Consider the model on the left side of Figure B-25 and perform an energy balance on the fluid (as opposed to the wall material). Assuming that no work is done to the fluid and no heat is generated within the fluid, the energy balance results in Equation 2-24 where q is the heat

transferred between the wall and fluid, and the right hand side is the change in enthalpy across the fluid element and represent energy averaged values (or bulk temperature values). The tube wall to fluid heat transfer term is determined using Newton's law of cooling and results in Equation 2-25. Equating the heat transfer terms yields Equation 2-26 from which the temperature of the fluid leaving the tube element ($T_{m,o}$) may be determined.

$$q = \dot{m}C_p(T_{m,o} - T_{m,i}) \quad \text{Equation 2-24}$$

$$q = h_d A_d (T_w - T_{d,avg}) \quad \text{Equation 2-25}$$

$$\dot{m}C_p(T_{m,o} - T_{m,i}) = h_d A_d \left(T_w - \frac{T_{m,o} - T_{m,i}}{2} \right) \quad \text{Equation 2-26}$$

The resulting equations (Equation 2-21 or Equation 2-23) can be used to solve for the wall temperature (T_w). The wall temperature is then substituted into Equation 2-26 and the temperature of the air leaving the tube element can be determined ($T_{m,o}$). These equations can be solved directly; however, without *a priori* knowledge of the average temperature in the tube element, the air properties cannot be accurately determined. The air temperature is initially specified as the average of the internal and external temperatures. The heat transfer equations are solved and the air properties are re-calculated as a function of the new average fluid temperature. The process is repeated until the temperature of the fluid leaving the tube element converges.

2.5.2 Validation of Heat Transfer Model

Since the T2 tube is discretized in order to solve the heat transfer equations, a study was performed to determine grid sensitivity. The grid study results shown in Figure B-27 indicate that the heat transfer model reaches convergence for grid densities higher than a factor of 8 times the original grid density. The upper plot of Figure B-27 contains the 2-norm of the error in temperature at each iteration defined by Equation 2-27 where T_i^N is the new temperature of the air leaving the i^{th} element at a particular iteration and T_i^O is the old temperature at the i^{th} element at the same iteration. Since the temperature difference can be negative, the absolute valued temperature difference is used in order to satisfy the requirements for a vector norm (Quarteroni, Sacco and Saleri 2007, pp. 19). The model converges linearly to machine precision in 3 iterations. As the number of grid points is increased, the model converges more rapidly. For a grid density of 8x, the error is reduced by five orders of magnitude in 2 iterations.

$$\|T\|_2 = \left(\sum_{i=1}^n |T_i^N - T_i^O|^2 \right)^{1/2} \quad \text{Equation 2-27}$$

The air temperature at 40% of the T2 tube length is shown in the lower portion of Figure B-27 for conditions representative of ground operation at idle engine speed and 70 degrees Fahrenheit ambient temperature. For all elements excluding the first element, the initial guessed temperature is equal to the converged temperature of the previous element. As the grid density is increased, the temperature rise through the tube becomes more accurate and hence the guess of the initial temperature is improved. For grid densities of 8x and higher, the predicted temperature at 40% tube length converges to 75.5 degrees Fahrenheit which indicates that the temperature of the air flowing through the tube has increased by 5.5 degrees Fahrenheit. While the temperature predictions for grid densities higher than 8x are marginally more accurate than those attained using higher grid densities, the time required to simulate an entire flight maneuver is significantly greater. Therefore, a grid density of 8x was chosen for the heat transfer analysis in order to balance the need for accurate predictions and the need for timely results.

Two bare-wire thermocouple measurements are available from historical ground test data that can be used to quantify the amount of temperature rise through the T2 tube due to heat transfer from the engine bay. The thermocouples were positioned as shown in Figure B-28 such that one thermocouple ($TT_{d,1}$) measures the air temperature entering the T2 tube and the second thermocouple ($TT_{d,2}$) measures the air temperature at the junction between the T2 tube and the eductor. The difference in the two thermocouples provides an indication of the temperature rise due to heat transfer between the T2 air and the engine bay. Comparisons of the model predicted and measured T2 tube heat transfer for the ground test are shown in Figure B-29. The engine power settings are noted by the vertical lines and the ordinate is the difference between the temperature of the air entering the T2 tube and leaving the T2 tube as the air proceeds into the eductor (Equation 2-28).

$$\Delta T = TT_{d,2} - TT_{d,1} \quad \text{Equation 2-28}$$

For engine speeds less than 80% the model predictions and the measured data agree well. The temperature rise between speeds of 50 to 60% as well as the temperature rise between 60 and 80% speed are captured by the model predictions. Above 80% speed the model predictions are approximately 4 degrees higher than the measured data as indicated by the red “Predicted” curve in Figure B-29. The difference between model prediction and experimental data is likely due to engine bay flow phenomena not accounted for in the heat transfer model. Above 80% engine speed, the engine nozzle provides increased pumping for the engine bay air. The difference in model predictions and experimental data is likely due to uncertainty in the engine bay temperature used as the external T2 tube temperature boundary condition. For example, by changing the T2 tube external boundary condition from an average of the forward engine bay temperatures to a value between the ambient and engine bay temperatures (only above 80% speed), the agreement between model predictions and test data is much better between 80%

and 90% engine speed. The difference between these two model predictions with different external tube temperature boundary conditions illustrates the sensitivity of the T2 tube temperature and T2 sensed temperature to engine bay air temperature. Configurations such as the scoop that bring cooling air into the engine bay would certainly reduce the amount of heat transfer through the T2 tube effectively lowering T2 bias. Unfortunately, detailed forward engine bay air temperature gradients are unknown and only bulk average air temperatures are known. Therefore all model predictions will use an average of the forward bay temperatures as the T2 tube external temperature boundary condition with the understanding that this assumption may introduce some uncertainty in the results.

3 Eductor Performance Analysis

This section describes the analysis that was performed to determine if the alternate eductor configurations provide sufficient flow to the T2 sensing system and to determine the effects of T2 tube heat transfer on T2 bias. The analysis is performed for two aircraft maneuvers; a sea level static (SLS) throttle transient (Ground Throttle Transient) and a transient dive/climb maneuver at altitude (Flight Transient Maneuver). A performance comparison will be reported in section 3.1.1 for the SLS case and section 3.2.4 for the flight transient case.

3.1 Ground Throttle Transient

Consider a sea level static throttle transient such that the engine speed is incrementally increased in a stair step fashion such as is displayed in Figure B-30. As the engine speed increases, the engine inlet pressure decreases (baseline and snorkel eductor exit pressure) and the compressor exit pressure increases (primary stream inlet pressure). The increased compressor exit pressure and the decreased eductor exit pressure results in increased eductor pumping thereby increasing the T2 tube mass flow rate.

3.1.1 Baseline Model Results (Ground Throttle Transient)

Results of modeling the baseline configuration for the sea level static throttle transient are displayed in Figure B-31 through Figure B-35 where the model predictions are indicated by the markers and the lines are curve fits of the results.

Pressure losses across each component of the baseline eductor system are shown in Figure B-31 where the losses are shown as component exit to inlet total pressure ratios. The total pressure loss through the baseline eductor T2 tube inlet (PT_d/PT_o) is negligible for all engine power settings indicating that the flow velocity is low in this portion of the system. The total pressure loss associated with the tube bends and blockage of the T2 sensors is shown as PT_a/PT_d and is approximately 1% at low engine power settings and as much as 9% at high engine power settings. The total pressure loss in the primary stream is a constant 10.5% at all engine power settings (PT_j/PT_b). The loss in total pressure between the ejector and mixing region is shown as PT_m/PT_j and is approximately 20% at low power and 60% at high power (due to shock wave as the primary stream expands into the mixing region). Finally, the total pressure loss through the mixing region (PT_e/PT_m) varies between 6% and 57% at low and high power, respectively and is due to the mixing of the primary with the secondary stream.

The resulting mass flow rate through the T2 tube as engine speed is increased is shown in Figure B-32. The T2 mass flow rate is nearly a linear function through the engine speed range at sea level operation. As the engine speed increases, the ejector transitions from subsonic to supersonic operation and

the mass flow rate is increased. The primary stream Mach number after isentropic expansion into the mixing region is shown in Figure B-33 where it may be seen that the flow is subsonic at the lowest engine power setting and supersonic for all other power settings.

There are two factors that contribute to T2 bias during SLS operation: 1) engine bay air re-ingestion and 2) conduction heat transfer through T2 duct walls. Predictions of T2 bias for the baseline configuration are shown in Figure B-34. The measured engine bay temperature (in the vicinity of the T2 tube) is shown and represents the external temperature boundary condition for the T2 tube heat transfer model. The predicted T2 tube wall temperature at the location of the T2 sensor is also shown and lags the engine bay temperature significantly. The predicted temperature at the T2 sensor includes only the effects of transient conduction from the engine bay through the T2 duct walls and into the incoming secondary stream. The contribution of T2 tube heat transfer to the temperature bias is on the order of 9-14 degrees Fahrenheit. The remainder of the temperature bias (up to 35 degrees) must be due to engine bay air re-ingestion. In fact, the measured pressure differential across the bleed holes indicates that the flow through the bleed holes is reversed (from the engine bay into the engine inlet) as indicated in Figure B-35, such that the bleed air is ineffective for the purpose of cooling the engine bay. Blocking the engine inlet bleed holes (Figure B-4b) in this case would eliminate the bay air re-ingestion, but would not affect the T2 tube heat transfer. A blocked bleed holes configuration could be combined with a configuration that mitigates T2 tube heat transfer for significant improvements in T2 bias.

3.1.1 Snorkel Model Results (Ground Throttle Transient)

Model predictions of total pressure losses for the snorkel/eductor configuration are shown in Figure B-36 for the ground run throttle transient. The snorkel/eductor benefits from the higher pressure external air connected directly to the T2 tube. While this may contribute to large increases in T2 tube mass flow in flight, the increase is not as significant at low Mach numbers on the ground. The total pressure loss across the snorkel is small as indicated by the PT_c/PT_o curve with a maximum total pressure loss of 1% at high power. The total pressure loss across the T2 interface tube is shown as (PT_d/PT_c) and is also small (maximum of approximately 4% at high power). The total pressure loss due to T2 tube bends and sensor blockage (PT_a/PT_d) is between 1% at low power and 20% at high power. The total pressure loss of the primary stream is equal to that of the baseline eductor system since the primary stream and eductor exit are unchanged from the baseline configuration, therefore, the primary stream total pressure loss (PT_j/PT_b) is not shown on the plot. The pressure losses through the mixing region are shown as PT_m/PT_j and PT_e/PT_m and are slightly increased over the baseline results due to the increased flow velocity. The primary stream loss due to expansion into the mixing region (PT_m/PT_j) varies between 20% at low power and 64% at high power and the mixing region total pressure loss varies between 5% at low

power and 51% at high power. The T2 tube physical mass flow rate is shown in Figure B-37 and increases with increasing engine power setting as expected. When compared to the baseline eductor system T2 tube mass flow rate (also shown on Figure B-37) it is clear that the snorkel performance will always provide adequate flow through the T2 sensing system on the ground.

3.1.1 Scoop Model Results (Ground Throttle Transient)

The total pressure losses through the combined scoop/eductor system for the throttle transient are shown in Figure B-38. Similar to the snorkel configuration, the total pressure loss from the scoop inlet to T2 tube inlet (PT_c/PT_o) is a maximum of 3% at high power. The total pressure loss across the T2 interface tube (PT_d/PT_c) has a maximum loss of 2% at high power. As in the baseline results, the majority of the total pressure loss in the T2 tube is due to bends and sensor blockages as indicated by the PT_a/PT_d curve and is less than 10% at high power.

The ejector operation is also similar to previous configurations such that the ejector operates supersonically through most of the engine power setting range as demonstrated in Figure B-39 where the ejector nozzle Mach number is shown for the throttle transient. Although the back pressure boundary condition was changed to the engine bay, the scoop provides positive flow operation as shown by the mass flow rate through the T2 tube in Figure B-40. Here the magnitude of the mass flow rate is closer to that of the baseline configuration indicating that the T2 tube inlet total pressure to eductor exit pressure ratio is similar to the baseline.

3.1.1 Performance Comparisons (Ground Throttle Transient)

Comparisons of pressure ratios across the eductor T2 flow path (PT_d/PSe) for three configurations during the SLS throttle transient are shown in Figure B-41. This pressure ratio provides an indication of the overall effect each eductor inlet/exit configuration has on the eductor operation and is the most important performance parameter used in this analysis. The PT_d includes the effects of total pressure loss across the various eductor inlets and the PSe includes the effects of the different eductor exit boundary conditions. Since the ejector configuration does not change (and hence the primary stream flow) for the three configurations, the T2 circuit pressure ratio becomes the important parameter for performance comparison.

Note the difference in the shapes of the curves in Figure B-41. The baseline eductor T2 circuit pressure ratio tends to decrease as the engine power setting is increased indicating that the eductor exit pressure has increased. As the engine power setting is increased, the engine inlet airflow and the Mach number in the engine inlet increases. As the Mach number in the engine inlet increases, it becomes more difficult to pump eductor air into the engine inlet effectively raising the eductor exit pressure. The snorkel

configuration experiences the same phenomenon but the scoop does not since the eductor exits to the engine bay for the scoop configuration.

The baseline T2 circuit pressure ratio is the lowest of the three configurations indicating the baseline inlet has the lowest total pressure recovery. The scoop operates such that the pressure entering the eductor is equal to the pressure leaving the eductor. The snorkel provides the best total pressure recovery due to more streamlined flow path and is expected to provide the highest rate of mass flow through the T2 circuit. For all three configurations, the predicted pressure ratios across the eductor shown in Figure B-41 are higher than the minimum required pressure ratio to sustain positive eductor flow.

Comparisons of T2 tube airflow predictions are presented in Figure B-42. As expected, the snorkel configuration provides the highest mass flow rate due to the added benefit of higher T2 circuit pressure ratio. The scoop configuration also provides a higher T2 mass flow rate than the baseline configuration making the scoop a desirable configuration due to the additional benefits of increased cooling flow into the engine bay.

3.2 Flight Transient Maneuver

Consider a maneuver such that the aircraft begins in steady level flight and is followed by a dive to a lower altitude and climb back to the original altitude such as depicted in Figure B-2. Maneuvers such as this introduce an additional transient phenomenon in the T2 bias problem. The rapid changes associated with diving and climbing introduce thermal lag in the engine inlet duct as well as the T2 tube. Thermal lag in the engine inlet duct heats up the engine inlet boundary layer (which is ingested into the T2 tube) and thermal lag in the T2 tube causes the T2 tube wall temperature to heat/cool the air flowing through the tube causing T2 bias.

The eductor operation is similar in flight as on the ground. As engine power setting changes, the ejector pumps more air through the system. For the alternate configurations, the secondary flow is augmented by changes in the eductor inlet pressure as the aircraft changes speed and altitude.

3.2.1 Baseline Model Results (Flight Transient Maneuver)

Model results for the baseline configuration are presented for the flight transient maneuver in Figure B-43 through Figure B-45.

Total pressure losses through the baseline eductor system are shown in Figure B-43. Since the engine power setting does not drop to idle for this maneuver (Figure B-2b) the ejector is choked for the entire maneuver and the total pressure losses are nearly constant. Total pressure losses throughout the baseline T2 tube inlet (PT_d/PT_o) are negligible and the total pressure losses through the T2 tube (PT_a/PT_d) are approximately 9% and are in agreement with the results from the ground throttle transient

at high power. The total pressure lost as the primary stream expands into the mixing region (PT_m/PT_j) is approximately 55-60%. The total pressure lost through the mixing region (PT_e/PT_m) is not constant since the eductor exit pressure (engine inlet pressure) is a function of engine power setting and altitude and is approximately 60% for the maneuver.

The T2 tube physical mass flow rate is shown in Figure B-44 for the flight transient. The physical mass flow increases as the altitude decreases and decreases as the aircraft climbs back to altitude primarily due to density variations with altitude and small changes in engine power setting (Figure B-2b).

The effects of T2 tube heat transfer on the T2 measurement are shown in Figure B-45. The maximum predicted temperature bias due to heat transfer through the T2 tube is approximately 15 degrees Fahrenheit and occurs due to the thermal lag of the tube at the end of the climb maneuver. For reference, the measured engine bay temperature is also shown indicating that the heat transfer direction is into the T2 air stream. Also shown is the predicted T2 tube wall temperature at the location of the T2 sensor which lags the measured engine bay temperature. The remainder of the temperature bias as shown by the difference between the measured and predicted T2 values indicates that there must be additional heat transfer effects. The additional heat transfer effects can be attributed to engine inlet thermal lag and engine bay air re-ingestion. Engine bay air re-ingestion can only contribute when the pressure across the engine bay bleed holes is positive as shown in Figure B-2d and only occurs near the end of the flight maneuver.

These results indicate that the primary contribution of T2 measurement bias is due to the thermal lag of the T2 tube and the engine inlet duct during aircraft transient operation. Engine bay air re-ingestion is the primary contributing factor to T2 measurement bias when the T2 tube and engine inlet are in a heat soaked state and is a secondary contribution to T2 measurement bias in flight during aircraft transient maneuvers where engine inlet thermal lag is prevalent. The contribution of T2 tube heat transfer to T2 measurement bias is significant for this case, contributing as much as 38% of the total temperature bias.

3.2.2 Snorkel Model Results (Flight Transient Maneuver)

Predictions of total pressure losses for the snorkel/eductor configuration in a dive and climb flight transient are shown in Figure B-46. As with the baseline configuration, the total pressure losses are nearly constant. The snorkel recovery is high (PT_c/PT_o) with only a 1% loss in total pressure. The T2 interface Tube also contributes minimal total pressure loss (PT_d/PT_c) of approximately 3-4%. Total pressure loss across the T2 tube bends and sensors is approximately 17% consistent with the ground run results at high power. The predicted T2 tube physical mass flow rate is shown in Figure B-47 and the magnitude is higher than the baseline configuration. As the aircraft dives to a lower altitude, the increasing Mach

number causes an increase in total pressure at the snorkel inlet effectively raising the mass flow rate through the snorkel.

3.2.3 Scoop Model Results (Flight Transient Maneuver)

The total pressure losses through the scoop/eductor system for the aircraft transient maneuver are shown in Figure B-48. The scoop performance is largely a function of the aircraft Mach number. As the Mach number increases, the flow rate through the scoop also increases and pressurizes the engine bay. Since the eductor is exhausted to the engine bay for the scoop configuration, the eductor performance will be a function of not only the engine power setting, but also of the aircraft Mach number. The total pressure loss between the scoop inlet and T2 tube inlet (PT_c/PT_o) is 11% at the beginning of the maneuver and increases to 37% at the bottom of the dive where the aircraft Mach number is highest. The T2 interface tube (PT_d/PT_c) total pressure losses are similar to other configurations and are less than 5% through the maneuver. The total pressure loss due to T2 tube bends and sensor blockage varies between 9 and 20% as the eductor exit static pressure varies. As the engine bay becomes pressurized at the higher Mach numbers at the bottom of the dive, the eductor exit static pressure increases thereby lowering the eductor exit velocity and total pressure causing an increase in the mixing region pressure loss (PT_e/PT_m) to a maximum of 69%. The cumulative total pressure loss in the T2 tube is perhaps a more useful performance indicator for the scoop than other configurations due to the complexity of the scoop system. The cumulative total pressure loss in the T2 tube is shown as PT_a/PT_o and indicates that the cumulative total pressure loss is near 30% at the beginning of the maneuver, increases to 44% through the dive and returns to approximately 25% near the end of the maneuver.

The T2 tube physical mass flow shown in Figure B-49 shows a nearly constant T2 tube mass flow rate through the maneuver. Since the eductor exit pressure varies with the aircraft Mach number, the T2 tube mass flow rate tends to remain constant not showing the peak at the bottom of the dive like the baseline or snorkel configurations.

3.2.4 Performance Comparisons (Flight Transient Maneuver)

Comparisons of T2 circuit pressure ratios across the eductor (PT_d/PSe) during the aircraft transient for the three configurations are shown in Figure B-50. As is the case with the sea level static throttle transient, the snorkel configuration has the highest T2 circuit pressure ratio.

Note the trend in the scoop predictions during the dive and climb maneuver. The T2 circuit pressure ratio drops approximately 20% due to the increased T2 tube total pressure losses and reduced mass flow rate as the aircraft Mach number increases in the dive. While the snorkel and baseline T2 circuit pressure ratios remain nearly constant during the flight maneuver, the scoop configuration eductor

performance decreases slightly at high Mach numbers. In spite of the drop in eductor pressure ratio for the scoop configuration, the pressure ratio remains well above the minimum required pressure ratio throughout the maneuver surpassing even the snorkel at some conditions.

The T2 tube physical mass flow rate comparisons are shown in Figure B-51. Although the scoop configuration eductor suffers from slight performance decrease at the high Mach numbers during the dive, the scoop mass flow rate tends to be higher than baseline at lower Mach numbers. The results are comparable to the sea level static throttle transient and the highest T2 mass flow is provided by the snorkel configuration.

4 Conclusions

In summary, the two important goals of this work are to determine the significance of heat transfer through the T2 tube with respect to T2 bias and to ensure that the alternate eductor configurations will provide adequate flow to the T2 sensing system (eductor).

The four eductor configurations that were studied included the baseline eductor, baseline eductor with blocked engine inlet bleed holes, eductor with scoop inlet and engine bay exit, and eductor with snorkel inlet and baseline exit. An engineering compressible flow model was developed to predict the performance of these alternate configurations and a heat transfer model was developed to predict the effects of T2 tube heat transfer on T2 measurement bias.

The heat transfer model predictions indicated that the T2 temperature bias due to heat transfer between the engine bay and T2 tube is significant and is comparable in magnitude to the T2 temperature bias due to engine inlet thermal boundary layer and engine bay air ingestion into the eductor system. It was shown that the T2 tube heat transfer can be as much as half of the total T2 bias. The predictions also indicated that the eductor tubes are very sensitive to engine bay temperature and that cooling the engine bay would result in a significant reduction in T2 tube heat transfer. The heat transfer model was also used to separate the effects of T2 tube heat transfer from engine bay air re-ingestion indicating that blocking the bleed holes in front of the baseline T2 tube pickup would effectively mitigate part of the T2 bias.

The compressible flow model predictions indicated that the snorkel provides the highest T2 tube mass flow rate. Although beyond the scope of this report, the potential for loss in total pressure at the snorkel inlet due to icing or upstream flow obstructions is not negligible and would need to be considered in further detail. With a sufficient icing mitigation approach and snorkel placement away from upstream flow obstructions, the snorkel is a viable approach for mitigating T2 bias.

It was shown that the scoop can provide similar levels of T2 tube mass flow when compared to the baseline configuration on the ground. It was also shown that the scoop T2 tube mass flow could be less than the baseline configuration T2 tube mass flow at certain flight conditions. However, the scoop can provide additional cooling to the engine bay potentially reducing the effects of T2 tube heat transfer. Icing is a concern on the leading edge of the scoop, however is not anticipated to be an issue with the T2 tube due to the shape and location of the T2 tube pickup. The effects of aircraft thermal boundary layer ingestion into the scoop are also beyond the scope of this report, but would be similar to engine inlet thermal boundary layer effects on the baseline configuration T2 bias.

It was shown that for all three configurations, the eductor pressure ratio (PT_d/PSe) is sufficiently higher than the minimum required for positive flow. This gives an indication that the ejector can provide enough suction to pump air through the T2 tube even in the presence of an adverse pressure gradient.

Based on the results of this analysis, the snorkel is the best alternate configuration since the snorkel mitigates engine inlet and engine bay air ingestion into the T2 tube while maintaining the highest T2 tube flow rate and eductor pressure ratio margin. With proper icing mitigation and proper placement on the aircraft to avoid upstream flow obstructions, the snorkel is recommended as the most viable solution for mitigating T2 bias.

Bibliography

- Barber, T. J., and O. L. Anderson. "An Analytical Study of a Supersonic Mixer-Ejector Exhaust System." *29th Aerospace Sciences Meeting*. Reno, NV: AIAA, 1991.
- Buning, P. G., R. H. Nichols, and R. W. Tramel. "Addition of Improved Shock-Capturing Schemes to OVERFLOW 2.1." *AIAA 19th Computational Fluid Dynamics Conference*. San Antonio: American Institute of Aeronautics and Astronautics, 2009.
- Committee, SAE S-16. *Inlet Total-Pressure-Distortion Considerations for Gas-Turbine Engines*. Aerospace Information Report, ARP1419, Society of Automotive Engineers, 1999.
- Crane Valves. *Flow of Fluids Through Valves, Fittings and Pipe*. Technical Paper No. 410M, The Woodlands, TX: Crane Co., 1999.
- DalBello, T., and C. J. Steffen. "Parametric Study of a Mixer/Ejector Nozzle with Mixing Enhancement Devices." *40th Aerospace Sciences Meeting*. Reno, NV: AIAA, 2002.
- Drummond, C. K. "Preliminary Dynamic Tests of a Flight-Type Ejector." *28th Joint Propulsion Conference*. Nashville, TN: AIAA/ASME/SAE, 1992.
- Hale, J. W. *Comparison of Diffuser-Ejector Performance with Five Different Driving Fluids*. Technical Report 63-207, Arnold Air Force Base, TN: USAF Arnold Engineering Development Center, 1963.
- Ince, E. L. *Ordinary Differential Equations*. Mineola, New York: Dover, 1956.
- Incropera, F. P., D. P. Dewitt, T. L. Bergman, and A. S. Lavine. *Fundamentals of Heat and Mass Transfer*. 6th ed. Hoboken, New Jersey: John Wiley and Sons, 2007.
- Kuhlberg, J., and W. Zimmerman. "Flight Testing of an All Electronic Propulsion Controls System." *16th Joint Propulsion Conference*. Hartford, CT: AIAA/ASME/SAE, 1980.
- Laskaridis, P., A. Gullia, and K. W. Ramsden. "A Novel Method for Characterizing Indoor Gas Turbine Test Facilities - Prediction and Control of Engine-Cell Performance." *25th Aerodynamic Measurement Technology and Ground Testing Conference*. San Francisco, CA: AIAA, 2006.
- Leipmann, H.W. *Compressible-Fluid Dynamics*. Mineola, New York: Dover, 2001.
- Moran, M. J., and H. N. Shapiro. *Fundamentals of Engineering Thermodynamics*. 5th edition. Hoboken, New Jersey: John Wiley and Sons, 2004.

- Nagaraja, K. S., and D. L. Hammond. "One Dimensional Compressible Ejector Flows." *9th Propulsion Conference*. Las Vegas, NV: AIAA/SAE, 1973.
- Quarteroni, Alfio, Riccardo Sacco, and Fausto Saleri. *Numerical Mathematics*. Berlin: Springer, 2007.
- Schneider, P. J. *Conduction Heat Transfer*. Reading, Massachusetts: Addison-Wesley, 1957.
- Singh, , P., S. N. Singh, and V. Seshadri. "Experimental Investigations on Non-Circular Ejector Air Diffusers." *39th Fluid Dynamics Conference*. San Antonio, TX: AIAA, 2009.
- Staff, Ames Research. *Equations, Tables, and Charts for Compressible Flow*. Technical Report 1135, Moffet Field, CA: NACA, 1953.
- Taylor, T., and F. R. Toline. *Summary of Exhaust Gas Ejector-Diffuser Research*. TR-68-84, Arnold AFB: USAF Arnold Engineering Development Center, 1968.
- Thompson, P. A. *Compressible-Fluid Dynamics*. 1st ed. New York, New York: McGraw-Hill, 1972.
- Vermeulen, P. J., V. Ramesh, G. C. Meng, D. N. Miller, and N. Domel. *Ejector Pumping Enhancement Through Pulsing Primary Flow*. AFOSR Grant Final Report, Wright-Patterson Air Force Base, OH: USAF Air Force Office of Scientific Research, 2005.
- Vick, A. R. *An Investigation to Determine the Discharge and Thrust Characteristics of Auxiliary-Air Outlets for a Stream Mach Number of 3.25*. Technical Note TN-D-1478, Hampton, Virginia: NASA, 1962.

Appendices

Appendix A: Derivation of Eductor Model Equations

The following values are assumed to be known prior to eductor model execution (the reader is encouraged to reference Figure B-52 for subscript nomenclature):

- 1) Primary Stream
 - a. Ejector nozzle cross sectional area (A_1)
 - b. Gas constant and ratio of specific heats for primary stream fluid (R_1, γ_1)
 - c. Total pressure (PT_1)
 - d. Total temperature (TT_1)
- 2) Secondary Stream
 - a. Gas constant and ratio of specific heats for secondary stream fluid (R_2, γ_2)
 - b. Total pressure (PT_2)
 - c. Total temperature (TT_2)
- 3) Mixed Stream
 - a. Mixing region cross sectional flow area (A_2)
 - b. Eductor exit static pressure (PS_{exit})
- 4) Calibration Factors
 - a. Viscous effects
 - b. Test data

It is assumed that the primary and secondary stream total pressures include losses from the eductor inlet and the application of these losses is handled prior to model execution. The eductor model includes solutions for subsonic and supersonic ejector nozzle flows. The ejector nozzle is determined to be choked by comparing the critical nozzle pressure (Equation A-1) to the eductor exit static pressure.

$$P^{1*} = PT_1 \left(1 + \frac{\gamma - 1}{2} \right)^{\frac{\gamma}{1-\gamma}} \quad \text{Equation A-1}$$

If P^{1*} is greater than PS_{exit} then the primary stream is choked and the supersonic solution is executed. The primary and secondary stream specific heats at constant pressure are calculated using Equation A-2 which assumes that both streams are an ideal gas.

$$Cp_1 = Cp_2 = \frac{\gamma R}{\gamma - 1} \quad \text{Equation A-2}$$

Supersonic Solution

The mixing region static pressure (PS_3 in Figure B-52) is the coupling mechanism for the primary and secondary streams and is generally an unknown quantity. The model execution begins by guessing the mixing region static pressure.

Since the ejector nozzle is choked the ejector nozzle Mach number is set to unity ($M_1 = 1.0$). Using the ejector nozzle Mach number and the primary stream total pressure, the primary stream static pressure may be calculated using the isentropic relation of Equation A-3.

$$PS_1 = PT_1 \left(1 + \frac{\gamma-1}{2} M_1^2\right)^{\frac{\gamma}{1-\gamma}} \quad \text{Equation A-3}$$

The primary stream inlet static temperature is determined in a similar fashion using Mach number and total temperature with the isentropic relationship of Equation A-4.

$$TS_1 = TT_1 / \left(1 + \frac{\gamma-1}{2} M_1^2\right) \quad \text{Equation A-4}$$

The primary stream mass flow rate is determined using the definition of mass flow rate (product of density, area and velocity) re-written for a compressible fluid (Equation A-5).

$$\dot{m}_1 = PS_1 A_1 M_1 \sqrt{\frac{\gamma}{R_1 TS_1}} \quad \text{Equation A-5}$$

The linear momentum at the primary stream inlet is calculated assuming negligible viscous effects (Equation A-6) and is calibrated for viscous effects in a later step.

$$F_1 = PS_1 A_1 (1 + \gamma M_1^2) \quad \text{Equation A-6}$$

The mixing region static pressure is constrained to be equal in both the primary and secondary streams by setting the primary and secondary stream mixing region static pressures equal to guessed mixing region static pressure (Equation A-7).

$$PS_{13} = PS_{23} = PS_3 \quad \text{Equation A-7}$$

Conservation of mass is applied on primary stream between primary stream inlet and mixing region (Equation A-8).

$$\dot{m}_{13} = \dot{m}_1 \quad \text{Equation A-8}$$

Total pressure and temperature are conserved between inlet stations and mixing region as shown in Equation A-9.

$$PT_{13}=PT_1$$

$$PT_{23}=PT_2$$

Equation A-9

$$TT_{13}=TT_1$$

$$TT_{23}=TT_2$$

The primary stream Mach number in the mixing region (prior to shock wave and mixing with secondary stream) is calculated using the isentropic relation of Equation A-10.

$$M_{13} = \sqrt{\frac{2}{\gamma - 1} \left[\left(\frac{PT_{13}}{PS_{13}} \right)^{\frac{\gamma-1}{\gamma}} - 1 \right]}$$

Equation A-10

The primary stream static temperature in the mixing region (prior to shock wave and mixing) can then be determined using the result of Equation A-10 and the isentropic relationship of Equation A-11.

$$TS_{13} = \frac{TT_{13}}{\left(1 + \frac{\gamma-1}{2} M_{13}^2\right)}$$

Equation A-11

As the primary stream expands into the mixing region its cross sectional flow area changes and can be calculated using the definition of mass flow rate (Equation A-12) rearranged to solve for area at the mixing region.

$$A_{13} = \frac{\dot{m}_{13}}{PS_{13}M_{13}} \sqrt{\frac{R_1 TS_{13}}{\gamma}}$$

Equation A-12

The secondary stream cross sectional area (Equation A-13) in the mixing region is then easily determined from mixing region geometry and expanded primary stream cross section.

$$A_{23}=A_d - A_{13}$$

Equation A-13

Similar to the primary stream, the secondary stream Mach number in the mixing region is calculated using guessed mixing region static pressure and known total pressure using the isentropic relationship of Equation A-14.

$$M_{23} = \sqrt{\frac{2}{\gamma - 1} \left[\left(\frac{PT_{23}}{PS_{23}} \right)^{\frac{\gamma-1}{\gamma}} - 1 \right]} \quad \text{Equation A-14}$$

The secondary stream static temperature in the mixing region (prior to mixing) can then be determined using the result of Equation A-14 and the isentropic relationship of Equation A-15.

$$TS_{23} = TT_{23} / \left(1 + \frac{\gamma-1}{2} M_{23}^2 \right) \quad \text{Equation A-15}$$

Similar to the primary stream, the secondary stream mass flow rate is determined using secondary stream cross sectional area, Mach number, and static temperature and pressure in the mixing region (Equation A-16).

$$\dot{m}_{23} = PS_{23} A_{23} M_{23} \sqrt{\frac{\gamma}{R_2 TS_{23}}} \quad \text{Equation A-16}$$

For both the primary and secondary streams, the linear momentum is calculated at the mixing region assuming negligible viscous effects using Equation A-17 and Equation A-18, respectively.

$$F_{13} = PS_{13} A_{13} (1 + \gamma M_{13}^2) \quad \text{Equation A-17}$$

$$F_{23} = PS_{23} A_{23} (1 + \gamma M_{23}^2) \quad \text{Equation A-18}$$

Mach numbers (and total to static pressure and temperature ratios) can be calculated at any other upstream location in each of the air streams using the isentropic relationship between area ratio and Mach number (Equation A-19).

$$\frac{A}{A^*} = \frac{1}{M} \left[\left(\frac{2}{\gamma - 1} \right) \left(1 + \frac{\gamma - 1}{2} M^2 \right) \right]^{(\gamma+1)/2(\gamma-1)} \quad \text{Equation A-19}$$

At this point, both the primary and secondary streams have been fully defined up to the mixing region and the mixing solution can be used to complete the model calculations.

Subsonic Solution

Similar to the supersonic solution, the first step in the subsonic solution is to guess the mixing region static pressure (PS_3 in Figure B-52). Since the primary flow is subsonic, the primary stream cross sectional area is maximum at the exit of the ejector nozzle (secondary stream flow area is minimum). The

ejector nozzle cross sectional area is known and the primary stream flow properties can be calculated directly. This allows the coupling of the primary and secondary streams to occur at the ejector nozzle exit. Therefore, the primary and secondary stream mixing region static pressures are set to the guessed mixing region static pressure, the total pressures and temperatures are known inputs and the cross sectional flow areas are also known inputs as shown in Equation A-20.

$$PS_{I3}=PS_3$$

$$PS_{23}=PS_3$$

$$PT_{I3}=PT_I \quad \text{Equation A-20}$$

$$PT_{23}=PT_2$$

$$TT_{I3}=TT_I$$

$$TT_{23}=TT_2$$

$$A_{I3}=A_I$$

$$A_{23}=A_d - A_{I3}$$

The primary stream Mach number in the mixing region prior to mixing is calculated from total pressure and assumed mixing region static pressure using the isentropic relationship of Equation A-21.

$$M_{13} = \sqrt{\frac{2}{\gamma - 1} \left[\left(\frac{PT_{13}}{PS_{13}} \right)^{\frac{\gamma-1}{\gamma}} - 1 \right]} \quad \text{Equation A-21}$$

The primary stream static temperature is calculated from Mach number and total temperature using the isentropic relationship of Equation A-22.

$$TS_{13} = TT_{13} / \left(1 + \frac{\gamma-1}{2} M_{13}^2 \right) \quad \text{Equation A-22}$$

The primary stream mass flow rate is calculated from the definition of mass flow rate re-written for a compressible fluid (Equation A-23).

$$\dot{m}_{13} = PS_{13} A_{13} M_{13} \sqrt{\frac{\gamma}{R_1 TS_{13}}} \quad \text{Equation A-23}$$

The secondary stream Mach number in the mixing region prior to mixing is calculated from total pressure and assumed mixing region static pressure using the isentropic relationship of Equation A-24.

$$M_{23} = \sqrt{\frac{2}{\gamma - 1} \left[\left(\frac{PT_{23}}{PS_{23}} \right)^{\frac{\gamma-1}{\gamma}} - 1 \right]} \quad \text{Equation A-24}$$

Similar to the primary stream calculation, the secondary stream static temperature and mass flow rate are calculated from the isentropic relation of Equation A-25 and the definition of mass flow rate of Equation A-26, respectively.

$$TS_{23} = TT_{23} / \left(1 + \frac{\gamma-1}{2} M_{23}^2 \right) \quad \text{Equation A-25}$$

$$\dot{m}_{23} = PS_{23} A_{23} M_{23} \sqrt{\frac{\gamma}{R_2 TS_{23}}} \quad \text{Equation A-26}$$

The linear momentum of the primary and secondary streams is calculated from Equation A-17 and Equation A-18 as was done in the supersonic solution. As with the supersonic solution, the upstream conditions can be calculated using the area ratio / Mach number isentropic relationship of Equation A-19. At this point both the primary and secondary streams have been completely defined for the subsonic solution and the mixing solution can be used to complete the process.

Mixing Solution

By conservation of linear momentum, the mixed stream momentum is the sum of the primary and secondary stream momentums (Equation A-27).

$$F_3 = F_{13} + F_{23} \quad \text{Equation A-27}$$

By conservation of mass, the mixed stream mass flow rate is the sum of the primary and secondary stream flow rates assuming no leaks in the system (Equation A-28).

$$\dot{m}_4 = \dot{m}_1 + \dot{m}_2 \quad \text{Equation A-28}$$

The mixed stream gas constant and specific heat are calculated using mass averaged mixing correlations of Equation A-29 and Equation A-30, respectively.

$$R_4 = \frac{\dot{m}_1 R_1 + \dot{m}_2 R_2}{\dot{m}_4} \quad \text{Equation A-29}$$

$$Cp_4 = \frac{\dot{m}_1 Cp_1 + \dot{m}_2 Cp_2}{\dot{m}_4} \quad \text{Equation A-30}$$

Conservation of energy is applied to calculate the mixed stream total temperature assuming that there is no heat transfer or work done during the mixing process such that the total enthalpy is conserved (Equation A-31).

$$TT_4 = \frac{\dot{m}_1 Cp_1 TT_1 + \dot{m}_2 Cp_2 TT_2}{\dot{m}_4 Cp_4} \quad \text{Equation A-31}$$

Since the momentum terms were calculated assuming viscous effects were negligible, the mixed stream momentum is calibrated for viscous effects using Equation A-32 where the mix loss factor was determined from CFD results.

$$F_4 = \text{mix loss factor} * F_3 \quad \text{Equation A-32}$$

The mixed stream Mach number can be isolated from the governing equations by taking the ratio of the mass flow rate equation with the linear momentum equation (Equation A-33). By taking the ratio of these two equations, the unknown mixed stream static pressure is eliminated and the mixed stream Mach number can be determined. Re-arranging Equation A-33 and substituting the isentropic relation for Ts_4 results in Equation A-34.

$$\frac{\dot{m}_4}{F_4} = \frac{M_4 \sqrt{\frac{\gamma}{R_4 T S_4}}}{(1 + \gamma M_4^2)} \quad \text{Equation A-33}$$

$$\frac{R_4 TT_4}{\gamma} \left(\frac{\dot{m}_4}{F_4} \right)^2 = \frac{M_4^2 \left(1 + \frac{\gamma-1}{2} M_4^2 \right)}{(1 + \gamma M_4^2)^2} \quad \text{Equation A-34}$$

By letting the RHS of Equation A-34 be defined as shown in Equation A-35, Equation A-34 can be rearranged and expanded into a quadratic equation in Mach number squared (Equation A-36) which has four solutions. Since the mixed flow cannot be supersonic, the real subsonic solution is chosen (Equation A-37).

$$\Lambda = \frac{R_4 TT_4}{\gamma} \left(\frac{\dot{m}_4}{F_4} \right)^2 \quad \text{Equation A-35}$$

$$\left(\Lambda\gamma^2 - \frac{\gamma-1}{2}\right)M_2^4 + (2\Lambda\gamma - 1)M_2^2 + \Lambda = 0 \quad \text{Equation A-36}$$

$$M_4^2 = \frac{(1 - 2\Lambda\gamma) \pm \sqrt{1 - 2\Lambda(\gamma - 1)}}{(1 - \gamma + 2\Lambda\gamma^2)} \quad \text{Equation A-37}$$

Once the mixed stream Mach number is determined, the mixed stream static pressure is calculated using the mixed stream momentum (Equation A-38).

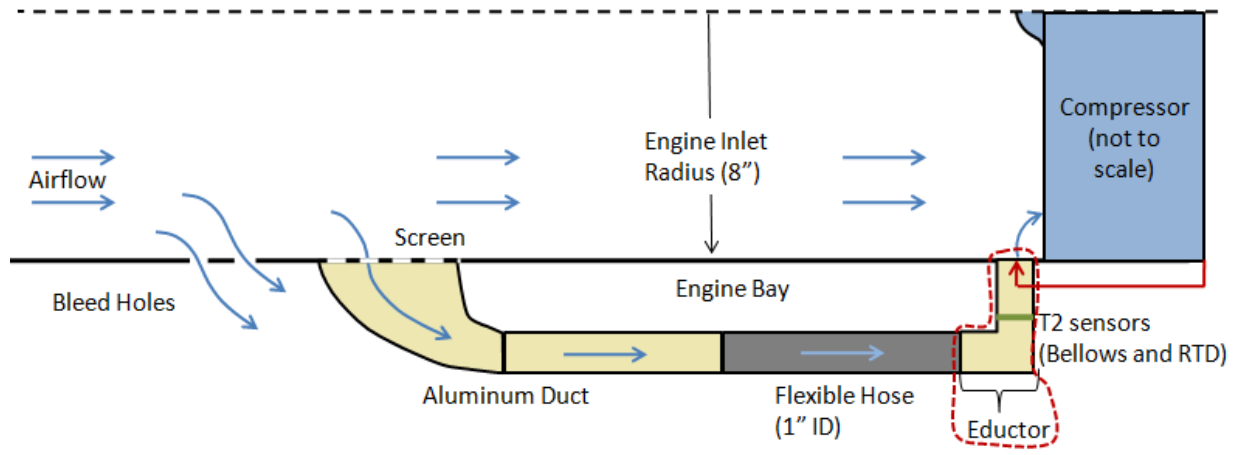
$$PS_4 = \frac{F_4}{A_d(1 + \gamma M_4^2)} \quad \text{Equation A-38}$$

Finally, the mixed stream total pressure (Equation A-39) is calculated using the Mach number from Equation A-37 and the static pressure from Equation A-38.

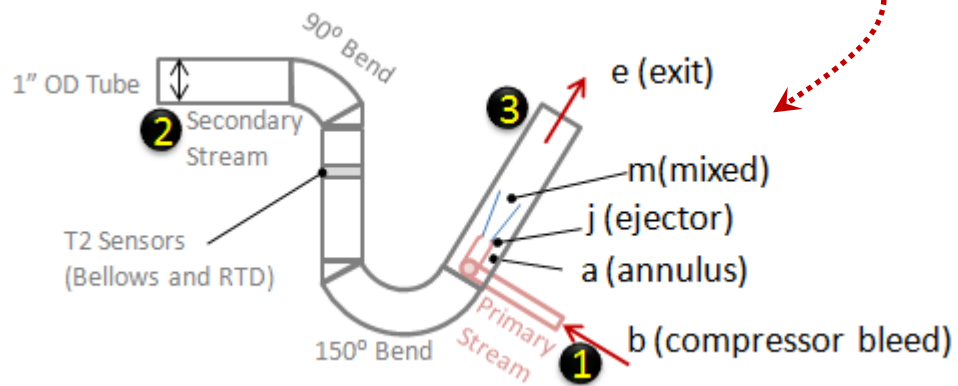
$$PT_4 = PS_4 \left(1 + \frac{\gamma-1}{2}M_4^2\right)^{\frac{\gamma}{\gamma-1}} \quad \text{Equation A-39}$$

Model convergence is determined by comparing the calculated eductor exit static pressure (PS_4 -Equation A-38) to the known value (PS_{exit}). If the error between the known eductor exit static pressure and the predicted exit static pressure is within tolerance then the model is converged. Otherwise, a new mixing region static pressure (PS_3) is guessed and the process is repeated until the predicted eductor exit static pressure (PS_4) matches the known value within tolerance.

Appendix B: Figures

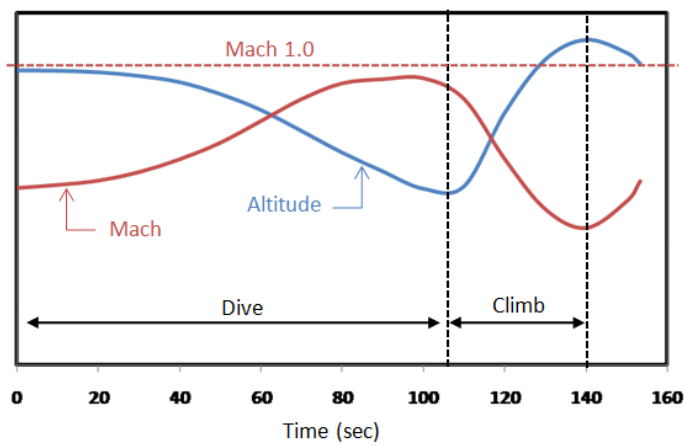


a) Baseline Eductor System

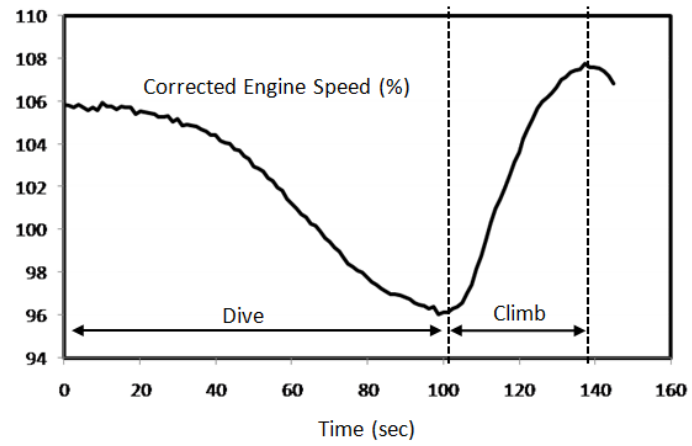


b) Eductor Details

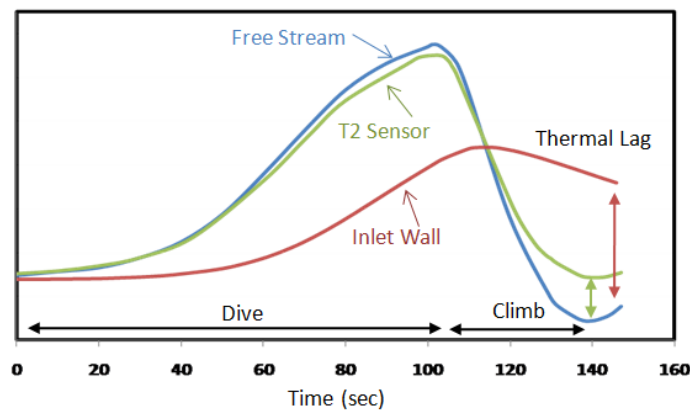
Figure B-1. Baseline Eductor System



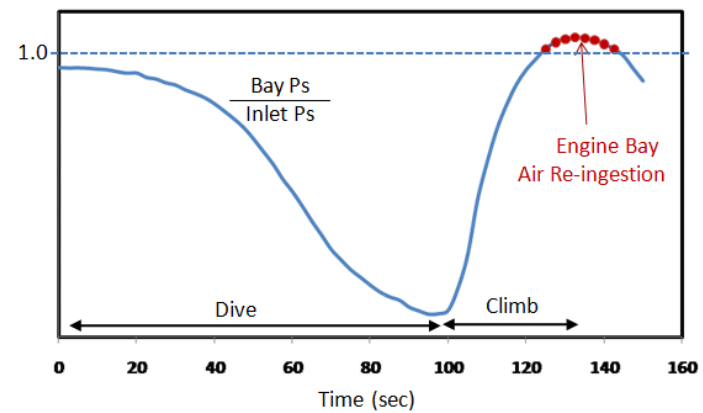
a) Altitude and Mach Number



b) Percent Corrected Engine Speed



c) Total Temperatures



d) Bay Air Re-ingestion

Figure B-2. Representative Dive and Climb Flight Maneuver.

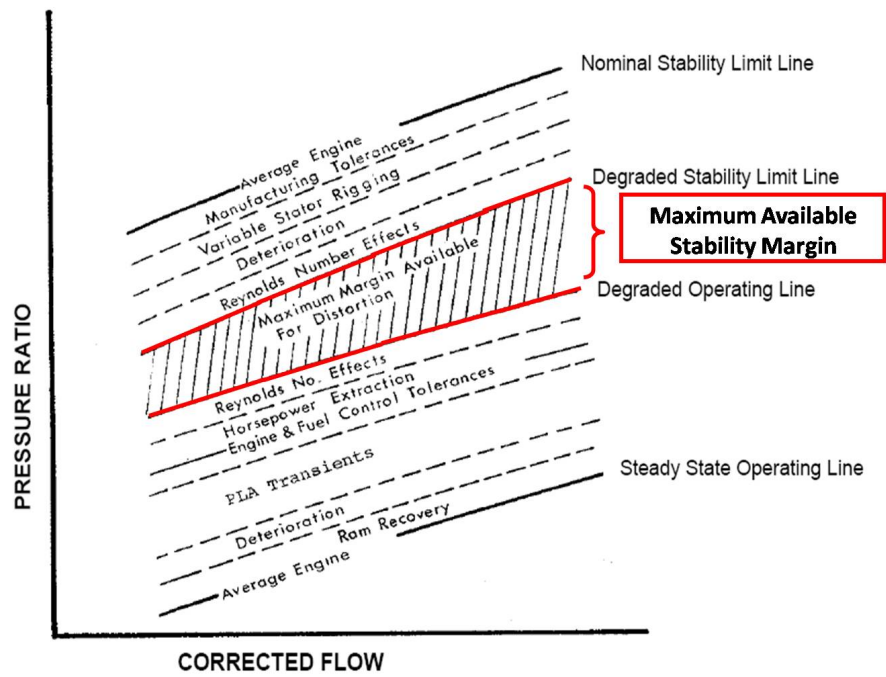
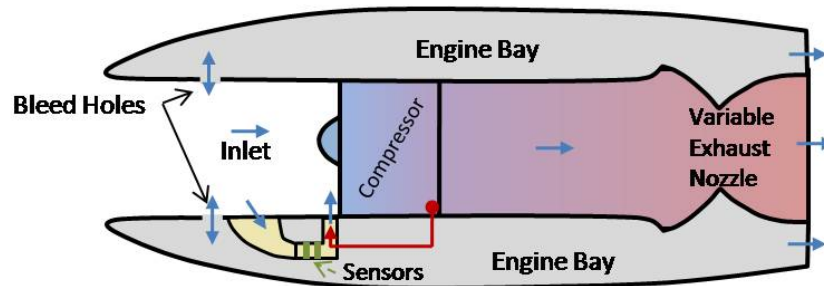
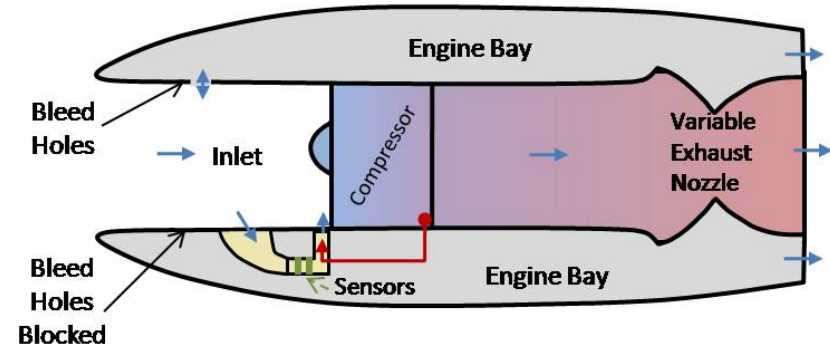


Figure B-3. Typical Compressor Destabilizing Factors (Committee 1999)

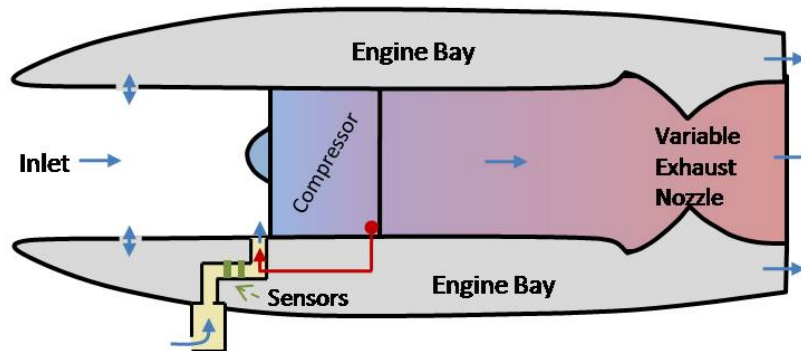
a) BASELINE CONFIGURATION



b) BLEED HOLES BLOCKED



c) SNORKEL CONFIGURATION



d) SCOOP CONFIGURATION

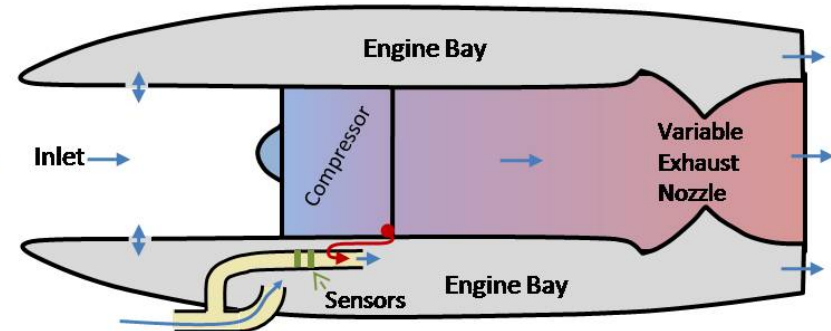


Figure B-4. Baseline and Alternate Eductor Intake Configurations

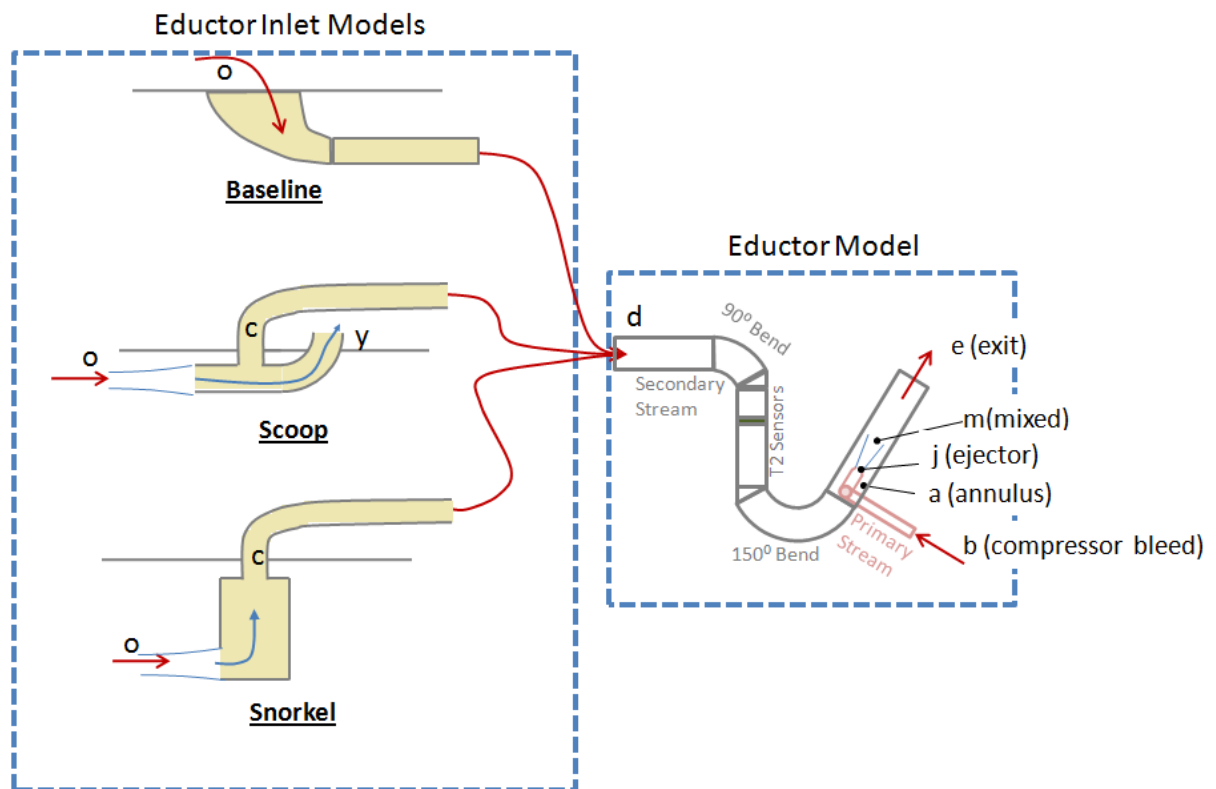
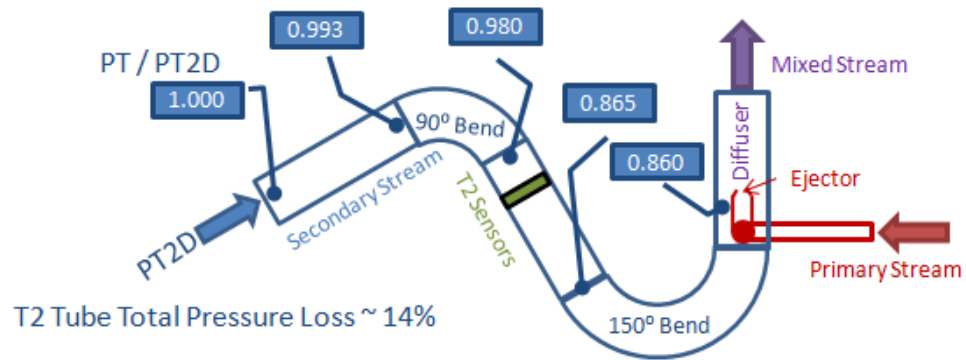
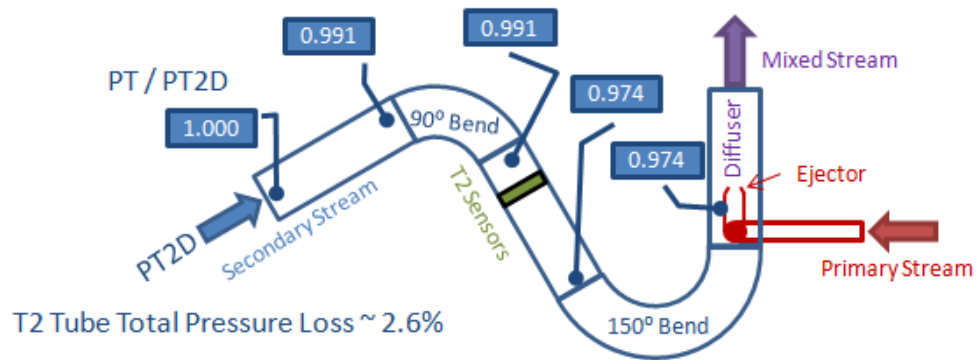


Figure B-5. Schematic of Combined Modeling Approach



a) Choked Ejector



b) Unchoked Ejector

Figure B-6. T2 Tube Total Pressure CFD Results

Pressures are normalized by secondary stream inlet total pressure.

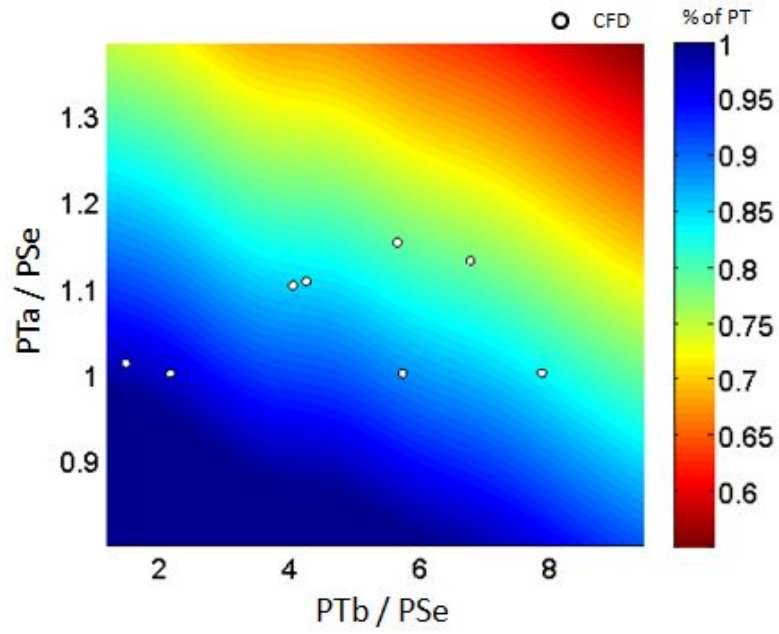


Figure B-7. Secondary Stream Total Pressure Losses

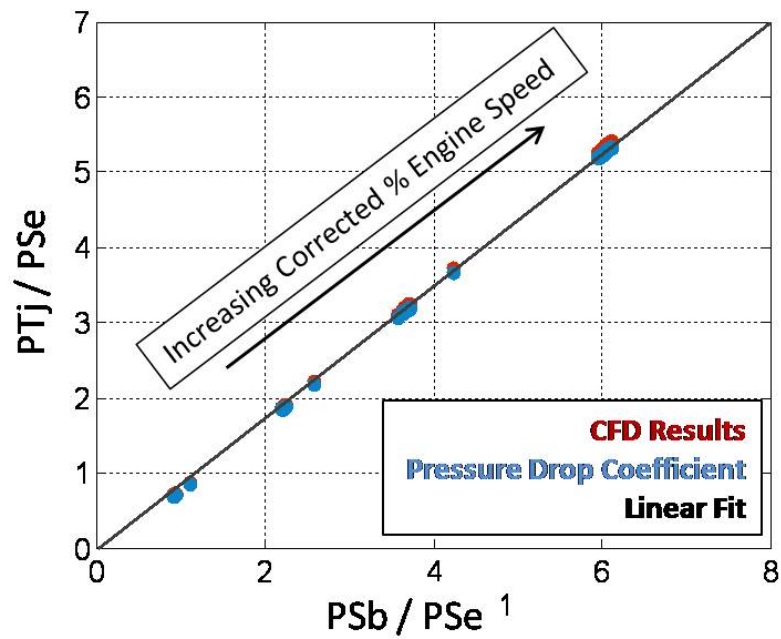
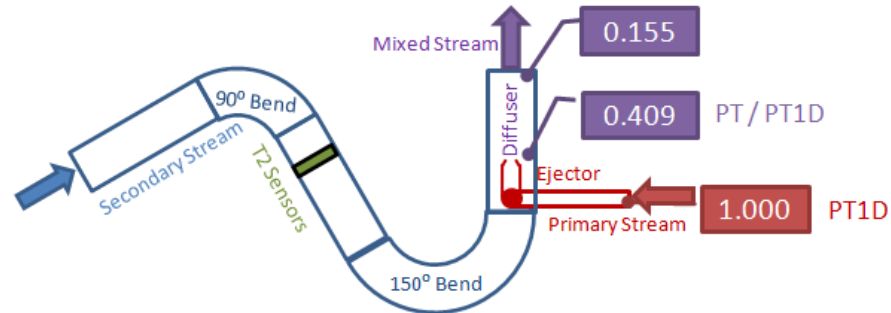
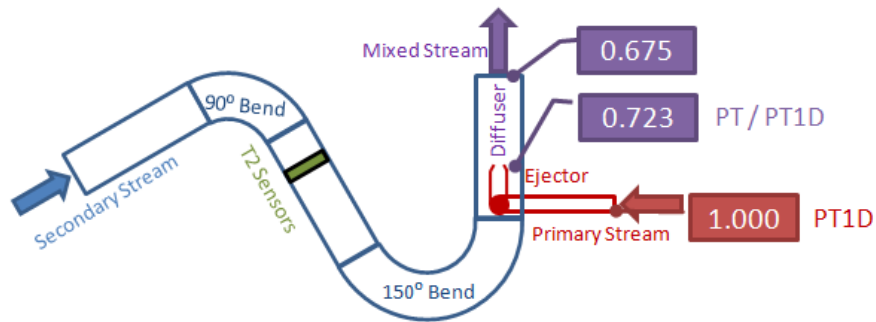


Figure B-8. Comparison of Primary Stream Total Pressure Loss Calculations



Diffuser Total Pressure Loss = $0.155/0.409 \sim 62\%$ (Oblique Shocks)

a) Choked Ejector



Diffuser Total Pressure Loss = $0.723/0.675 \sim 6.6\%$

b) Unchoked Ejector

Figure B-9. Ejector Mixing Region Total Pressure CFD Results

Pressures are normalized by primary stream inlet total pressure.

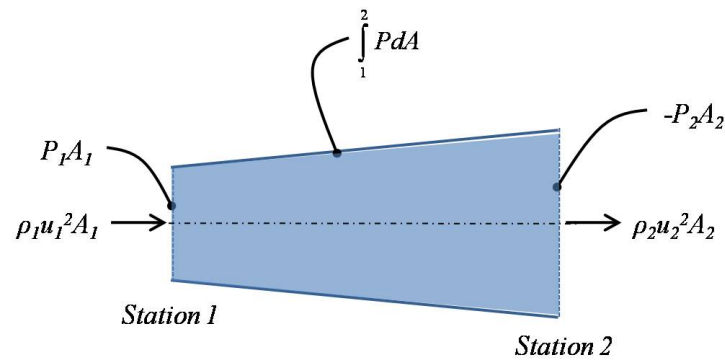
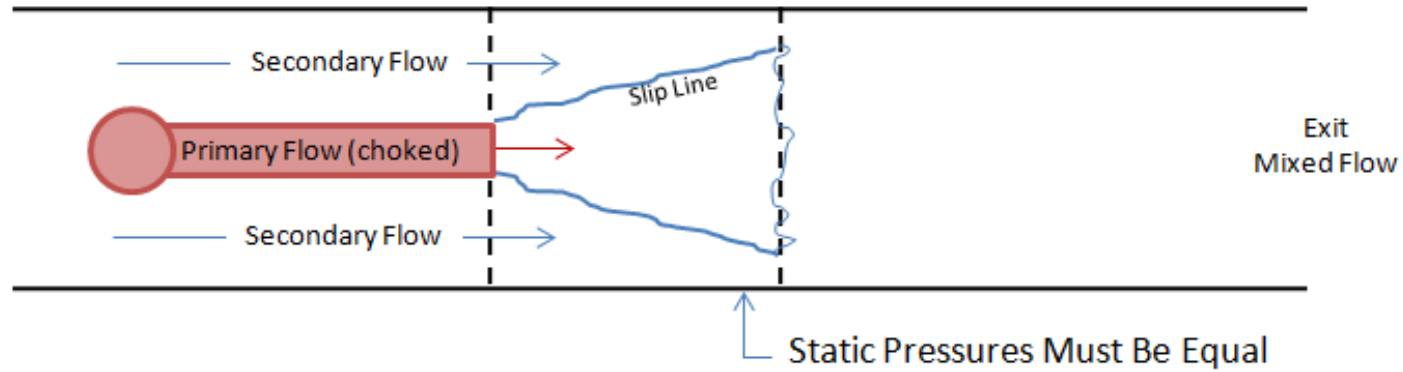
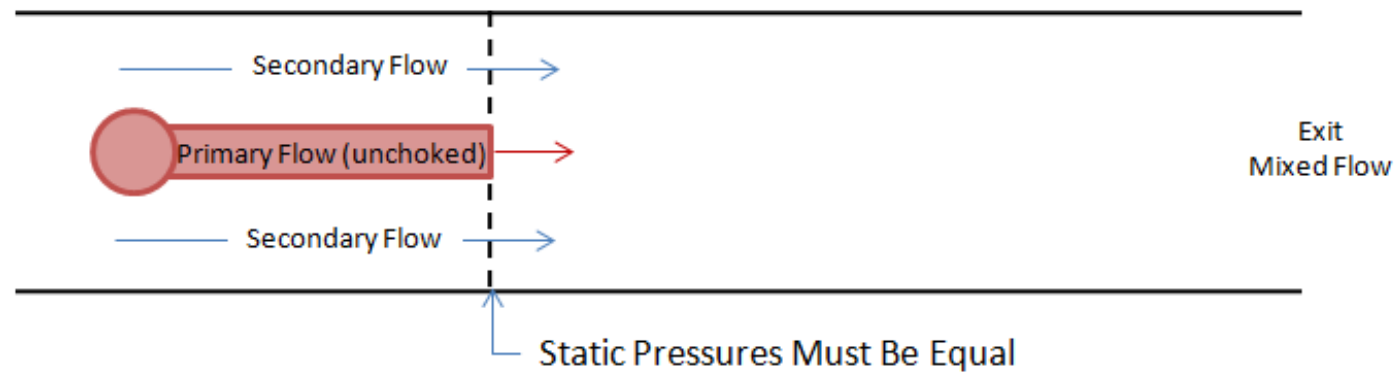


Figure B-10 Quasi One-Dimensional Flow Model.



a) Supersonic Ejector



b) Subsonic Ejector

Figure B-11. Ejector Static Pressure Constraint

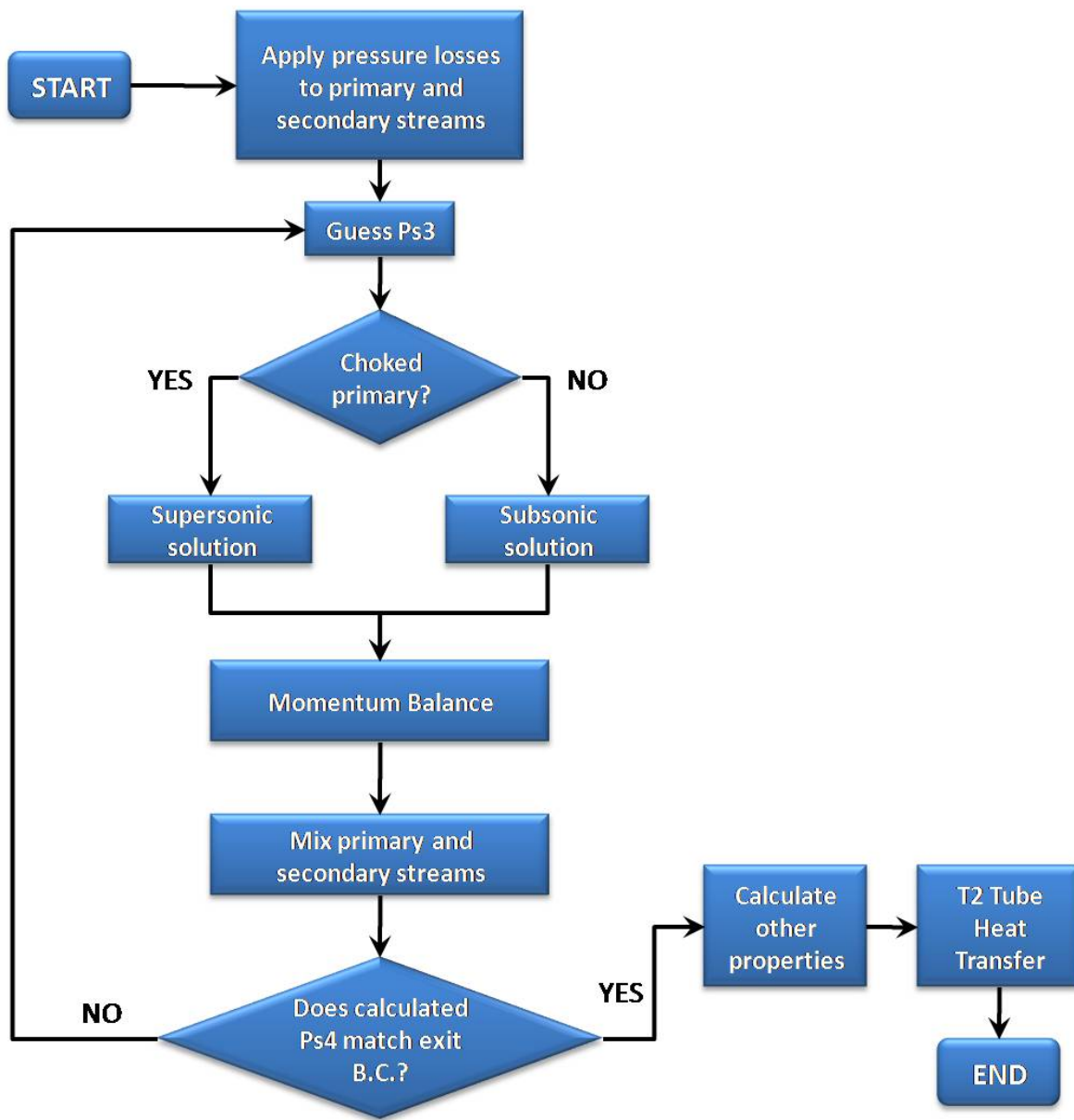


Figure B-12. One-Dimensional Model Flow Chart

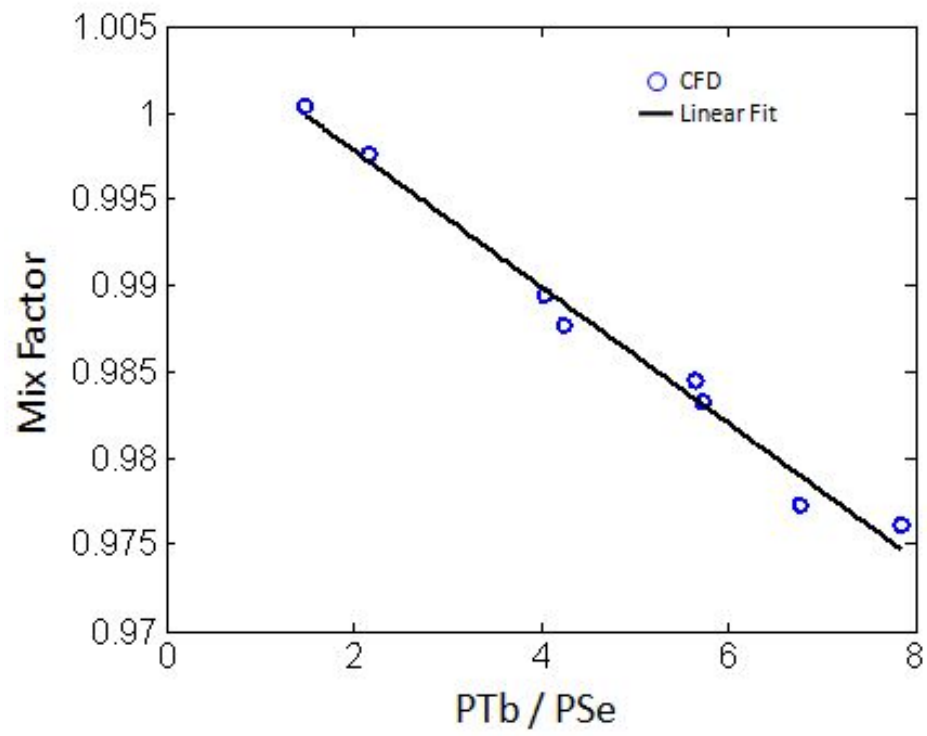


Figure B-13. Model Calibration Factor

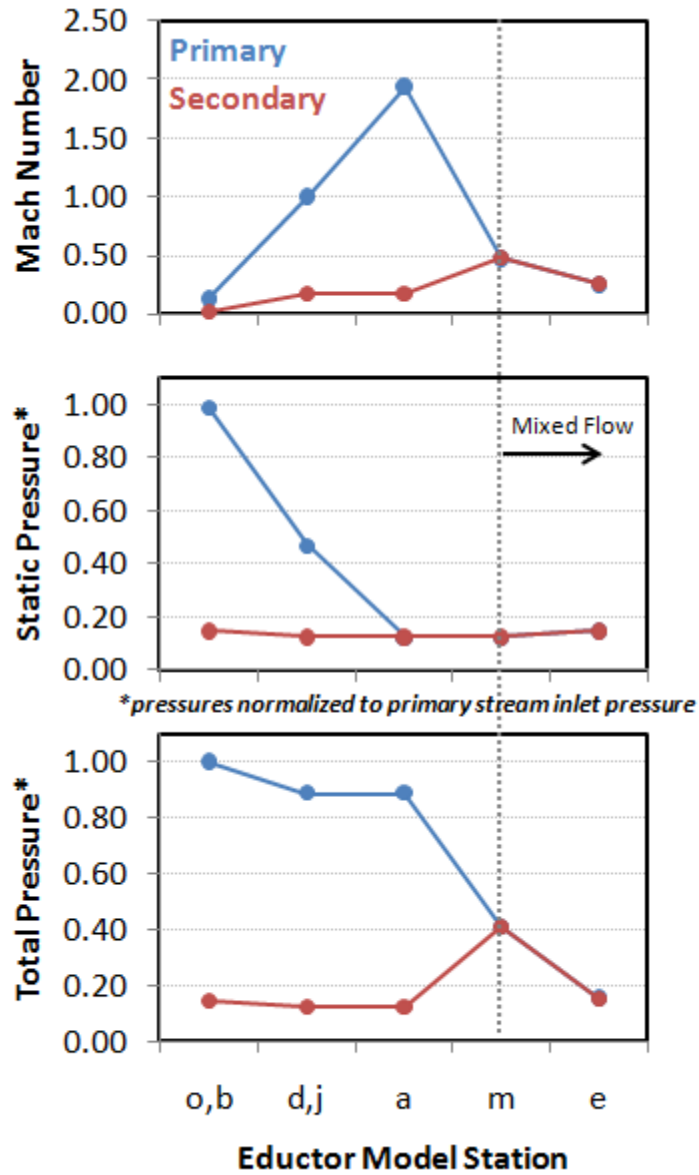


Figure B-14. Example Eductor Model Predictions (Choked Ejector)
 Pressures are normalized by primary stream inlet total pressure (PT_b).

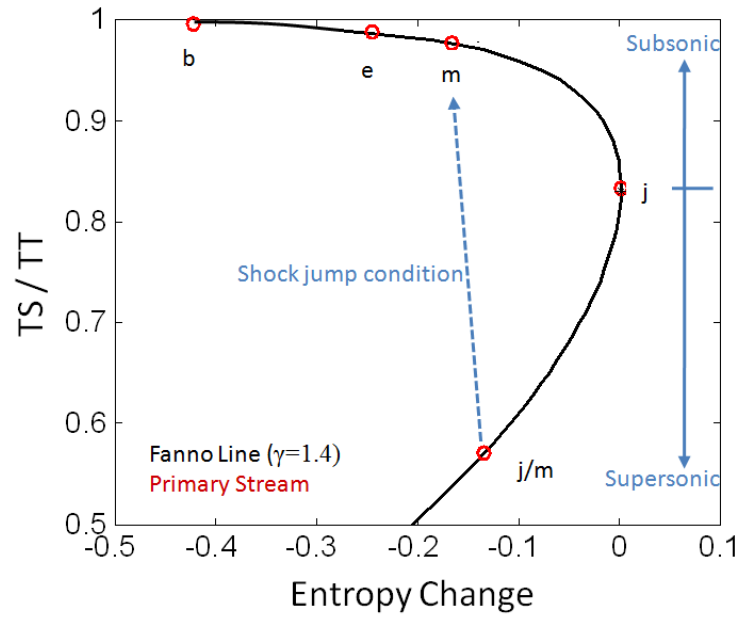


Figure B-15. Fanno Line for Example Ejector Predictions (Choked Ejector)

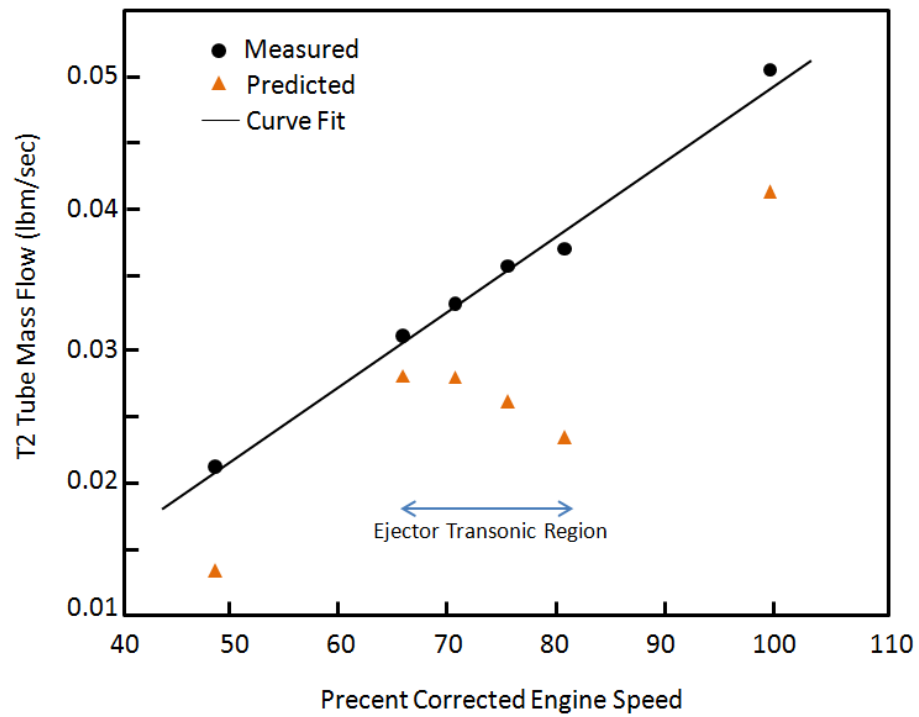


Figure B-16. Comparison of Model Predictions and Experimental Data

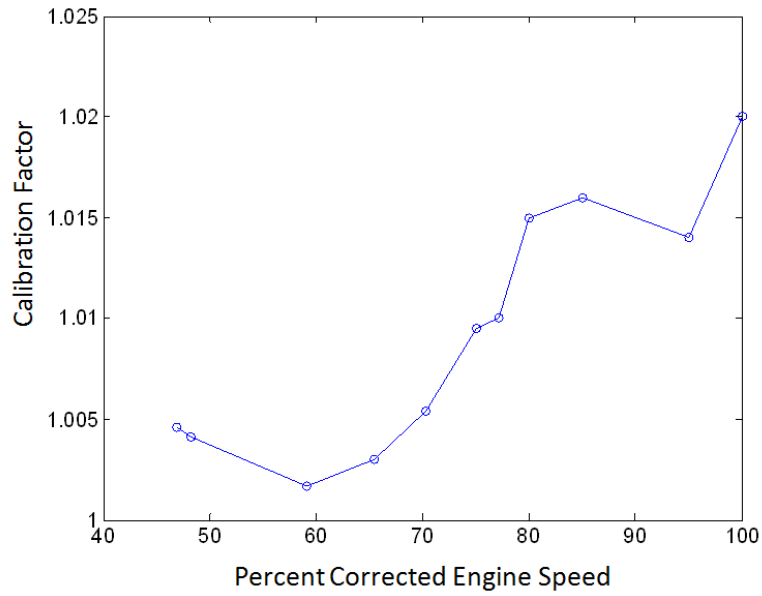


Figure B-17. Model Calibration Factor

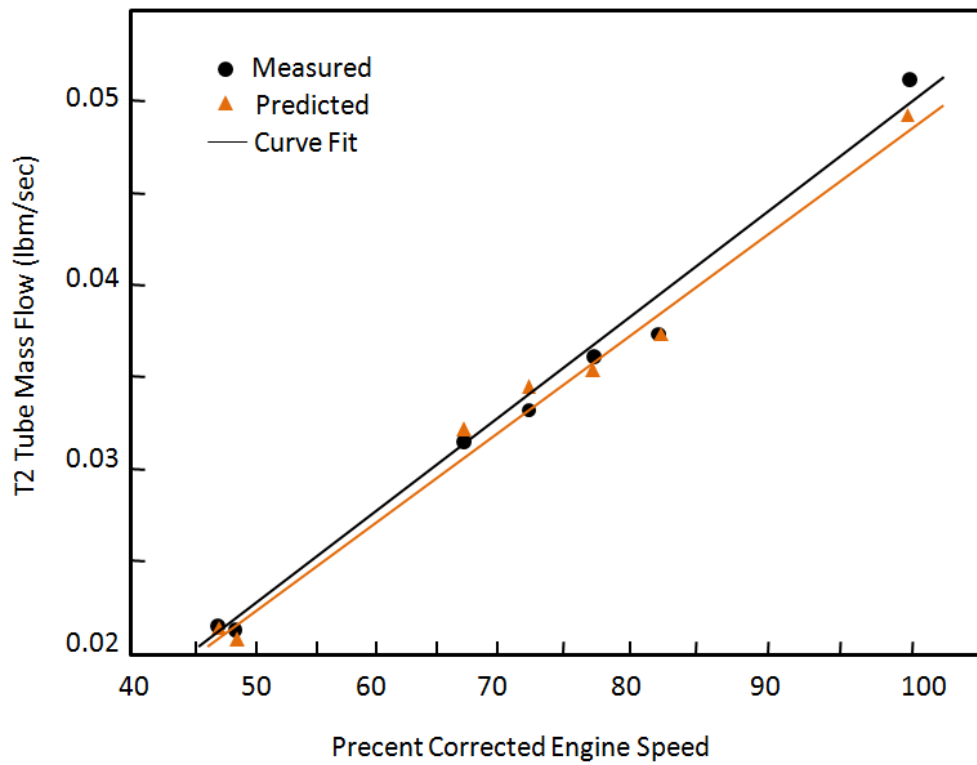


Figure B-18. Comparison of Calibrated Model Predictions and Experimental Data

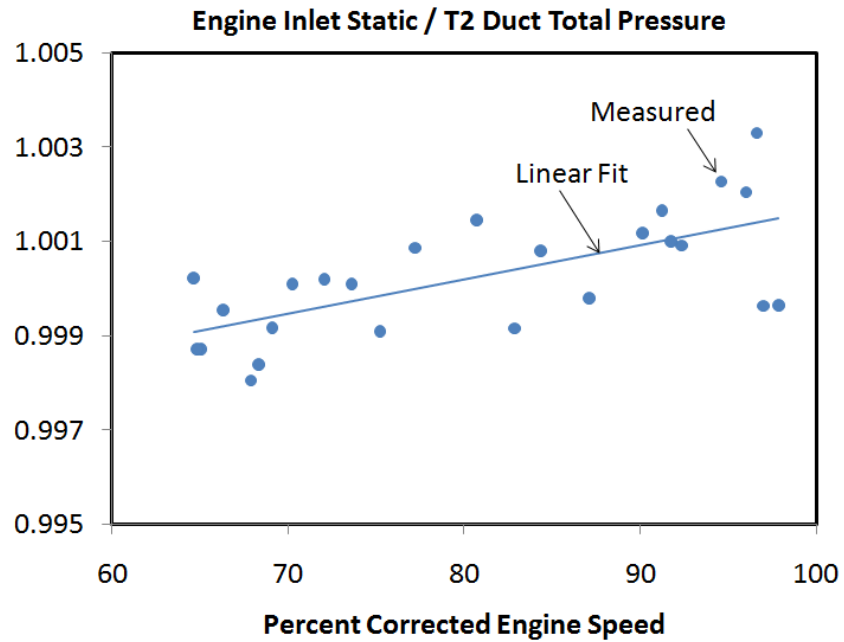


Figure B-19. Baseline Eductor Inlet Model Total Pressure Boundary Condition

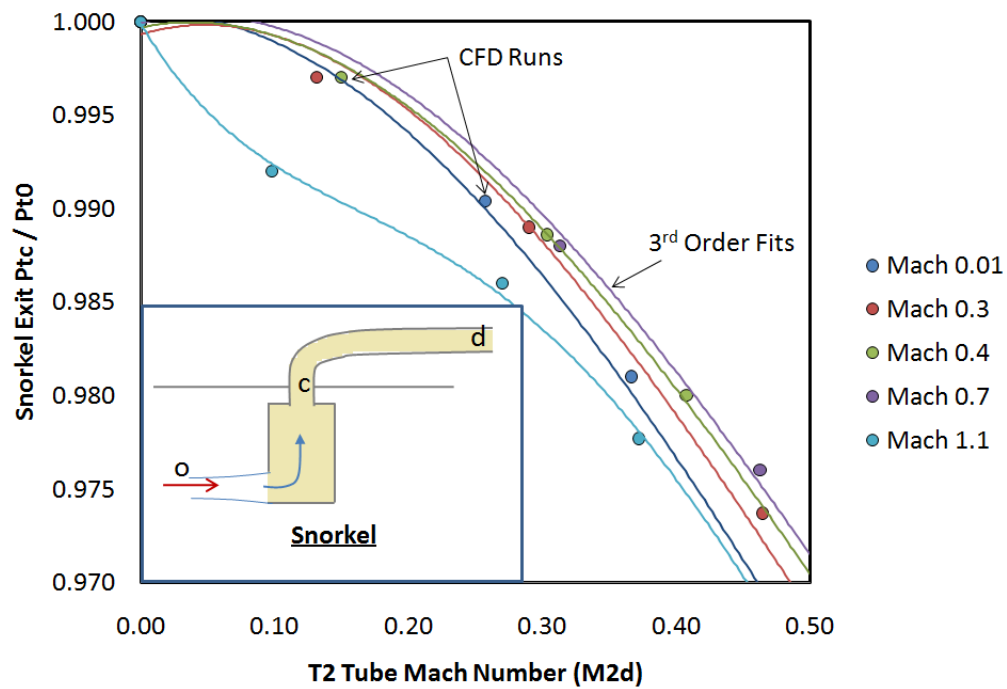


Figure B-20. Snorkel Total Pressure Recovery

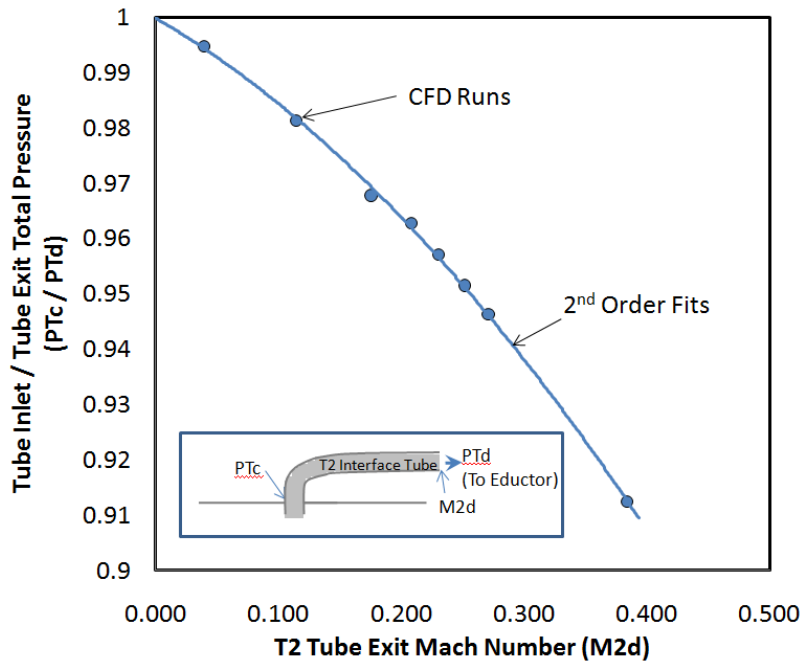


Figure B-21. T2 Interface Tube Total Pressure Loss

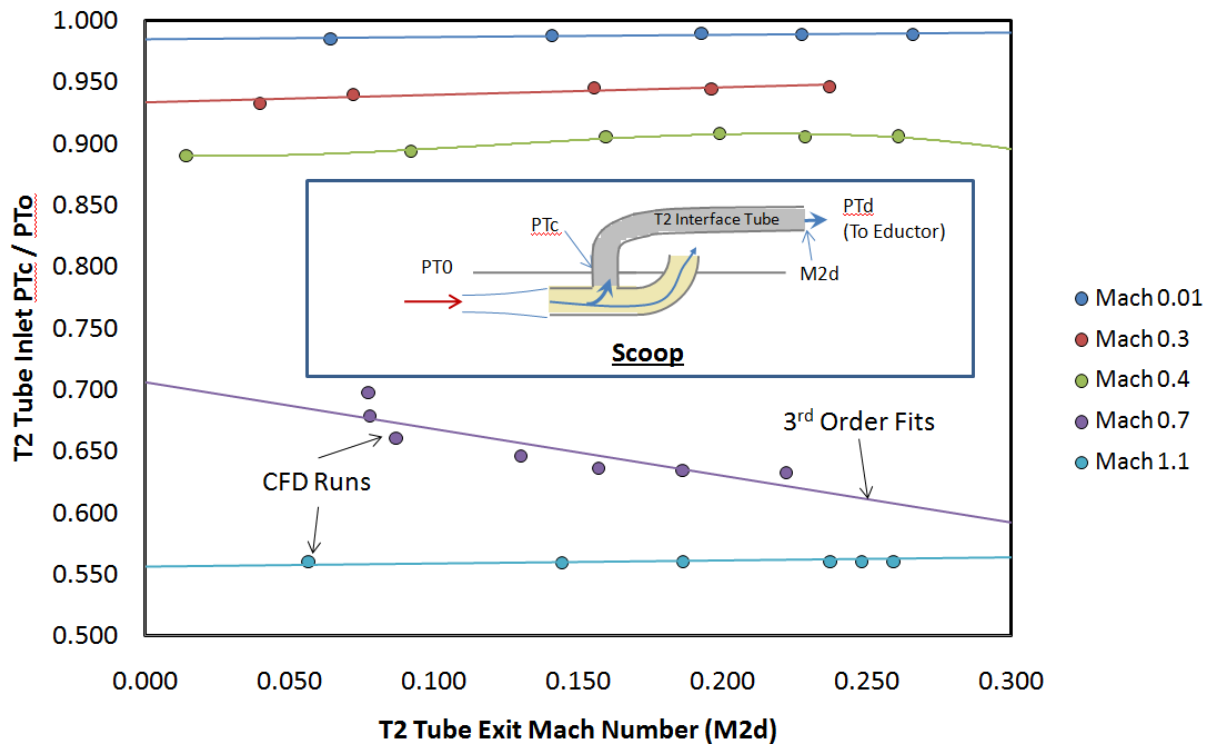
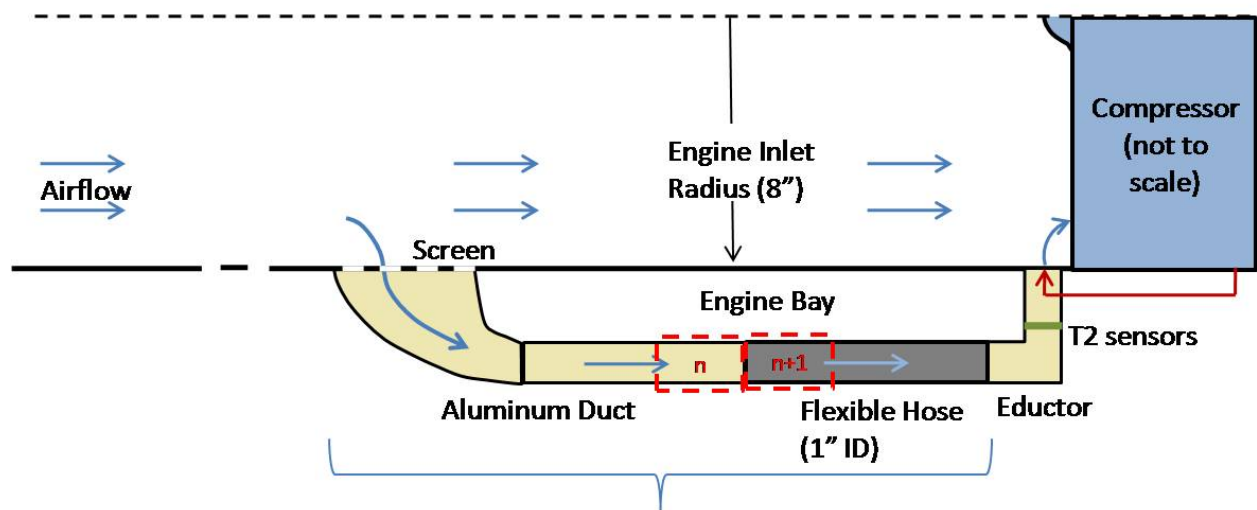


Figure B-22. Scoop T2 Interface Tube Inlet Total Pressure Loss



T2 Tube Heat Transfer Model

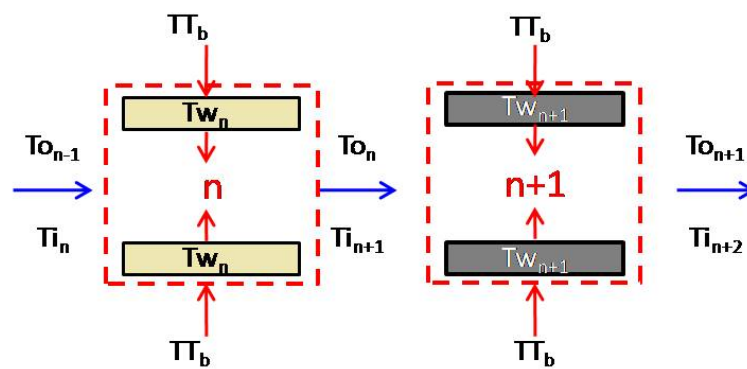


Figure B-23. T2 Tube Heat Transfer Model

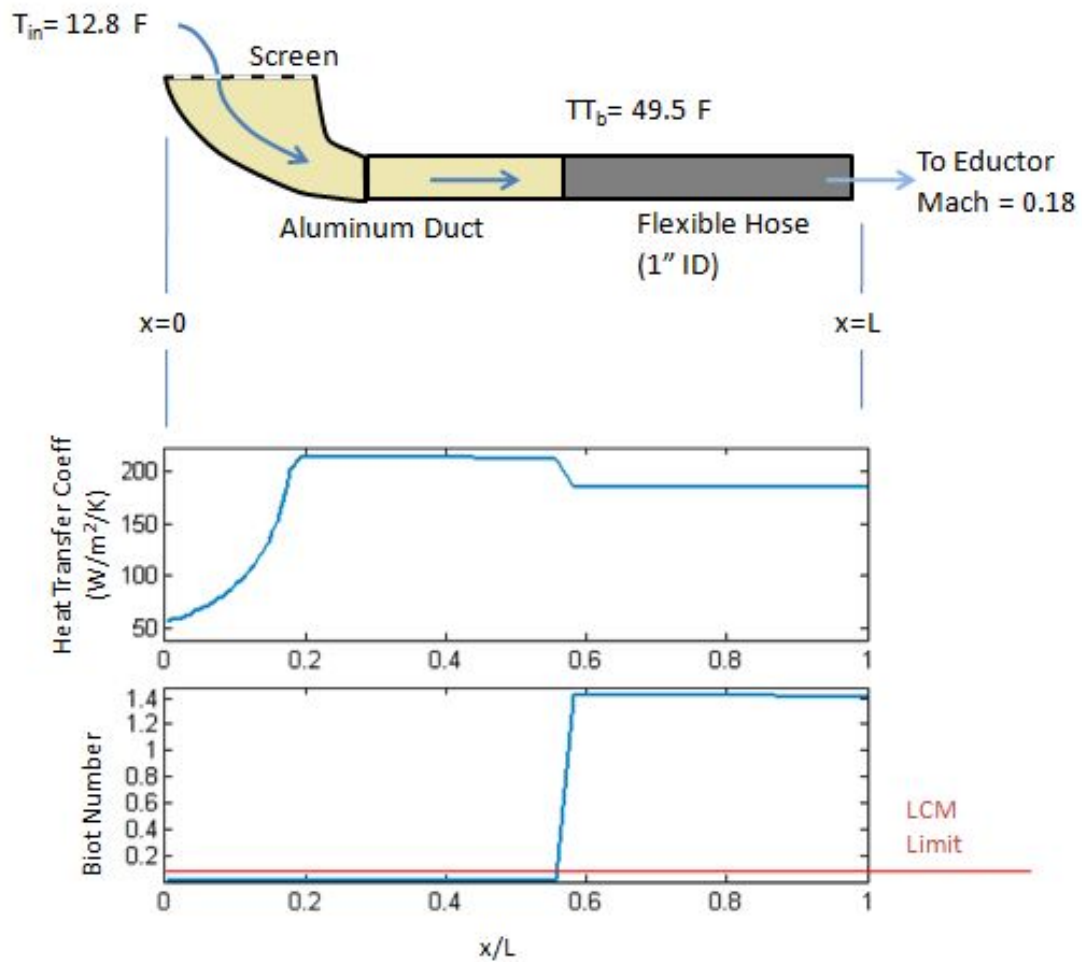


Figure B-24. Predicted Convection Coefficient and Biot Number for T2 Tube

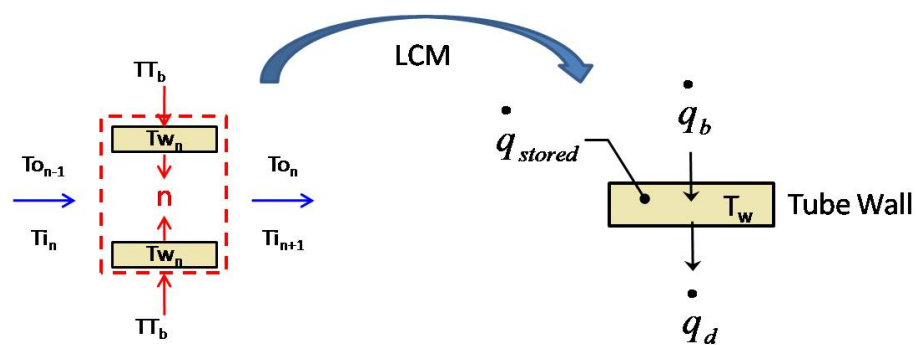


Figure B-25. Lumped Capacitance Heat Transfer Model

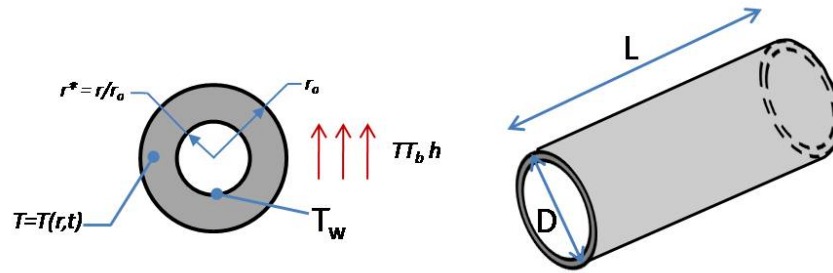


Figure B-26. Heat Transfer Model for Exact Solution to Heat Equation

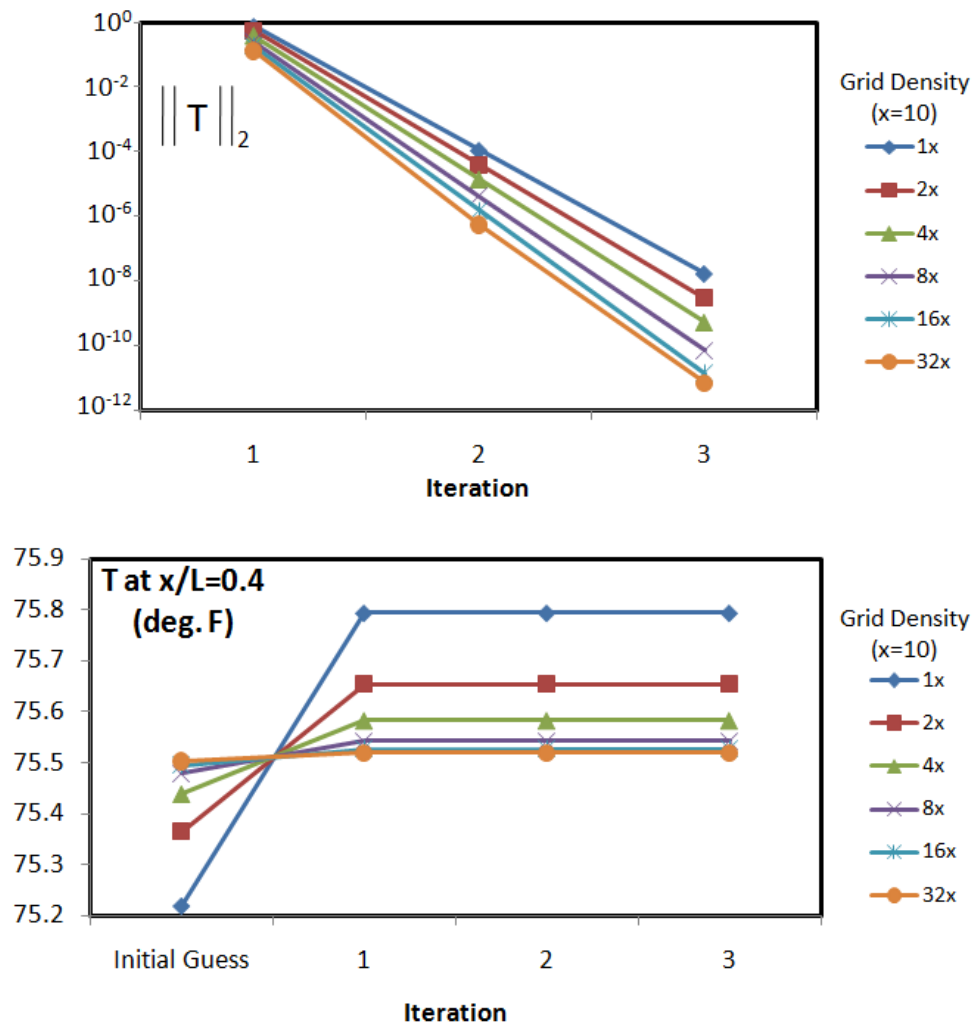


Figure B-27. Heat Transfer Model Convergence

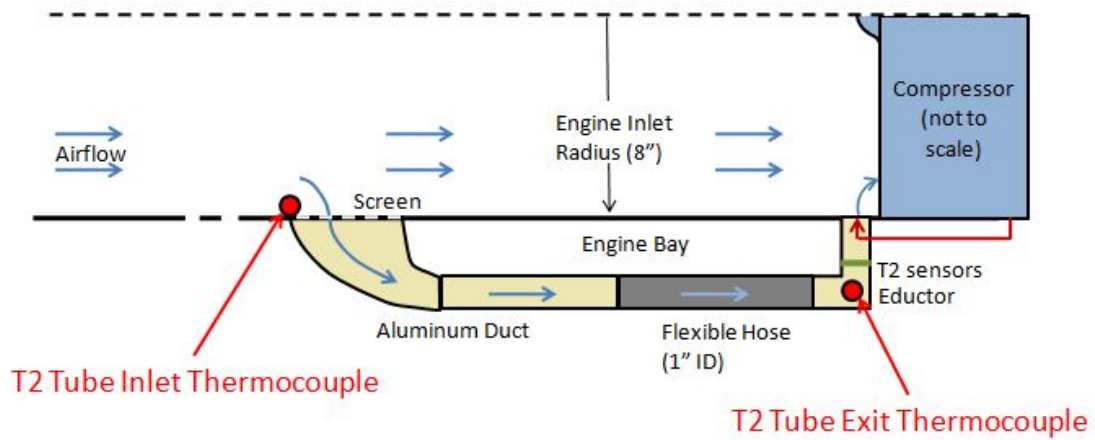


Figure B-28. Thermocouple Locations

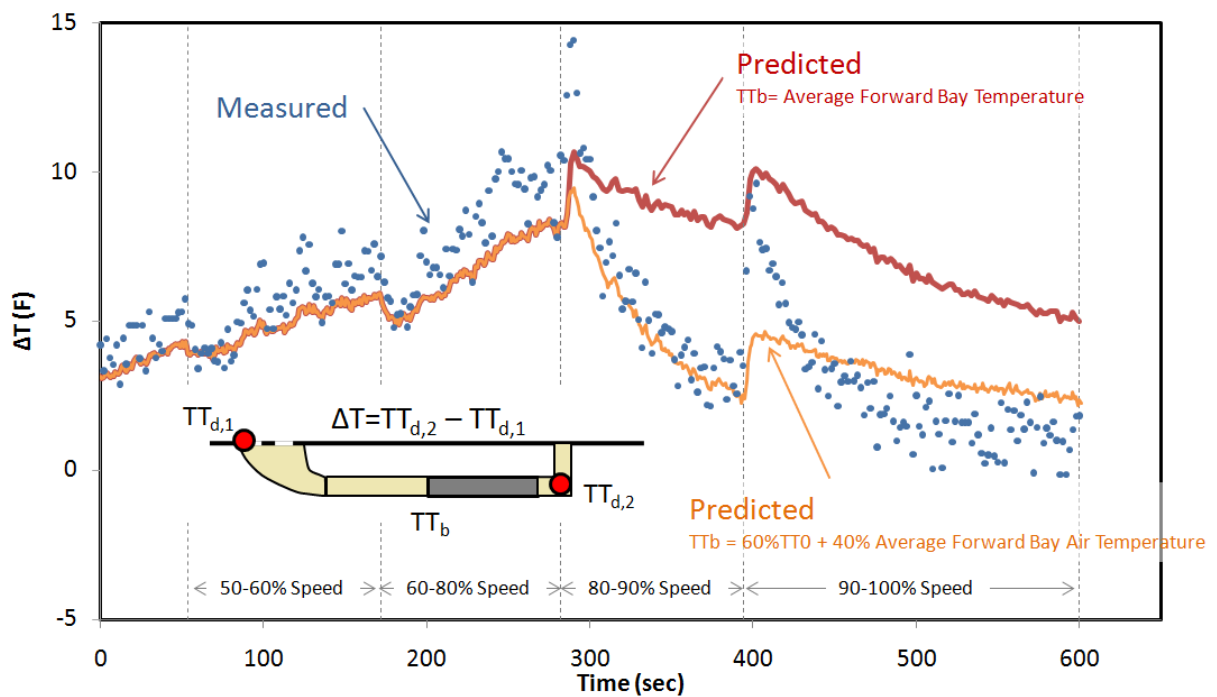


Figure B-29. Comparisons of Measured and Predicted T2 Tube Heat Transfer Components

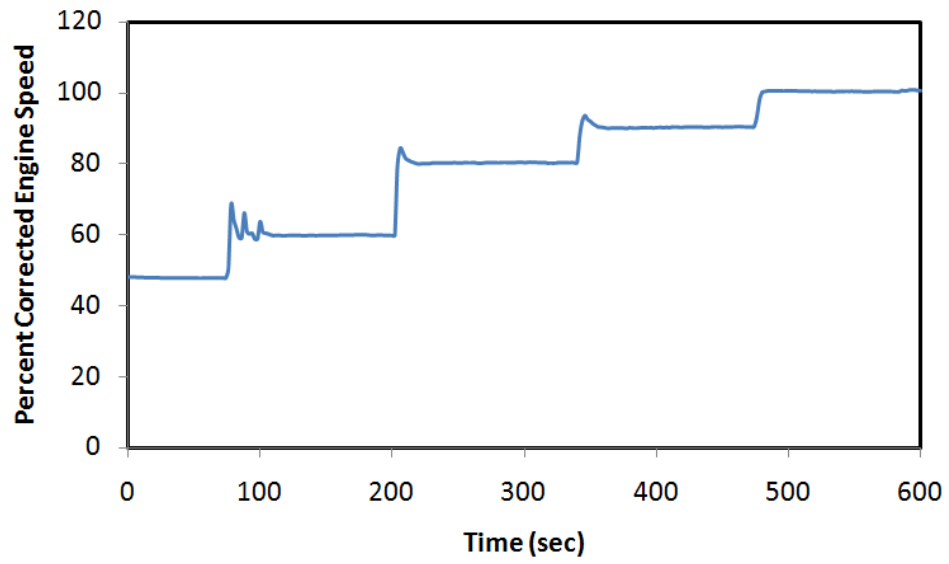


Figure B-30. Representative Sea Level Static Throttle Transient

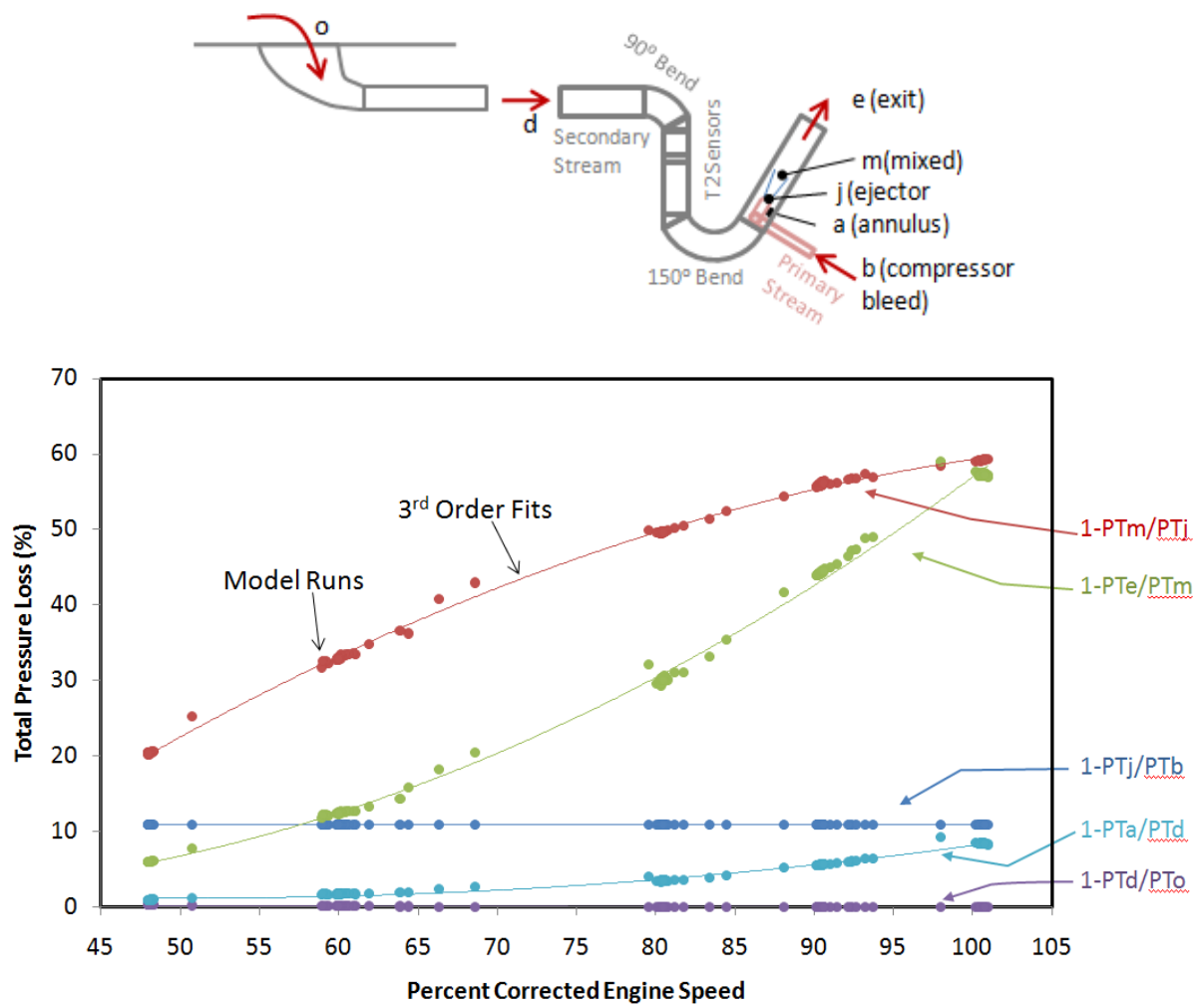


Figure B-31. Predicted Pressure Losses through Baseline Ejector Model (Ground Run)

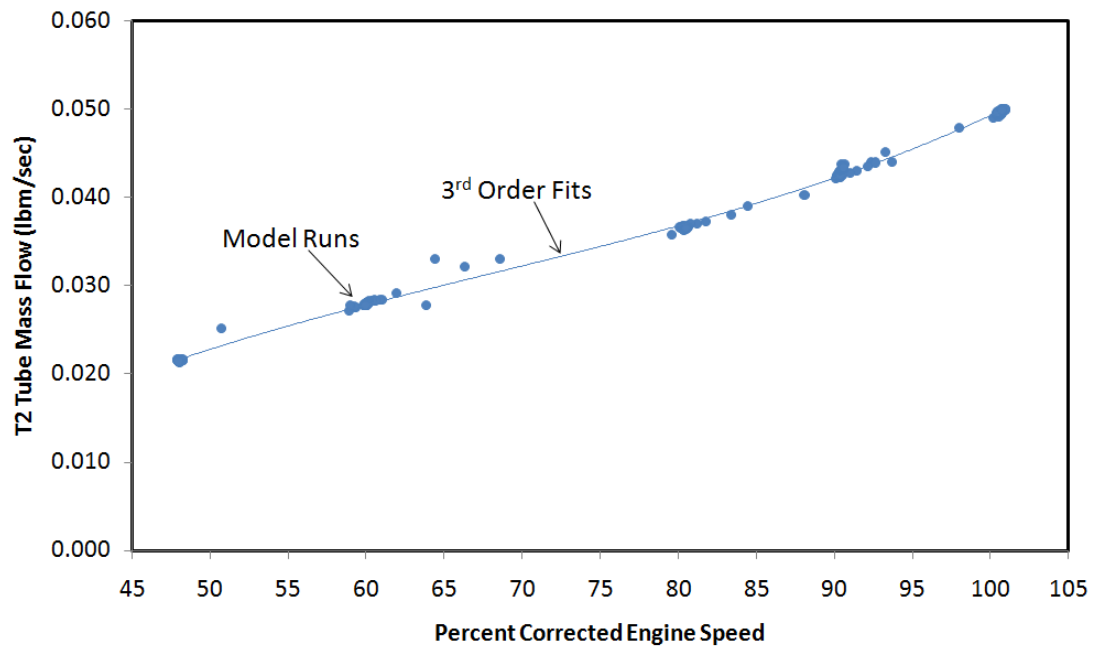


Figure B-32. Predicted T2 Tube Mass Flow Rate (Ground Run)

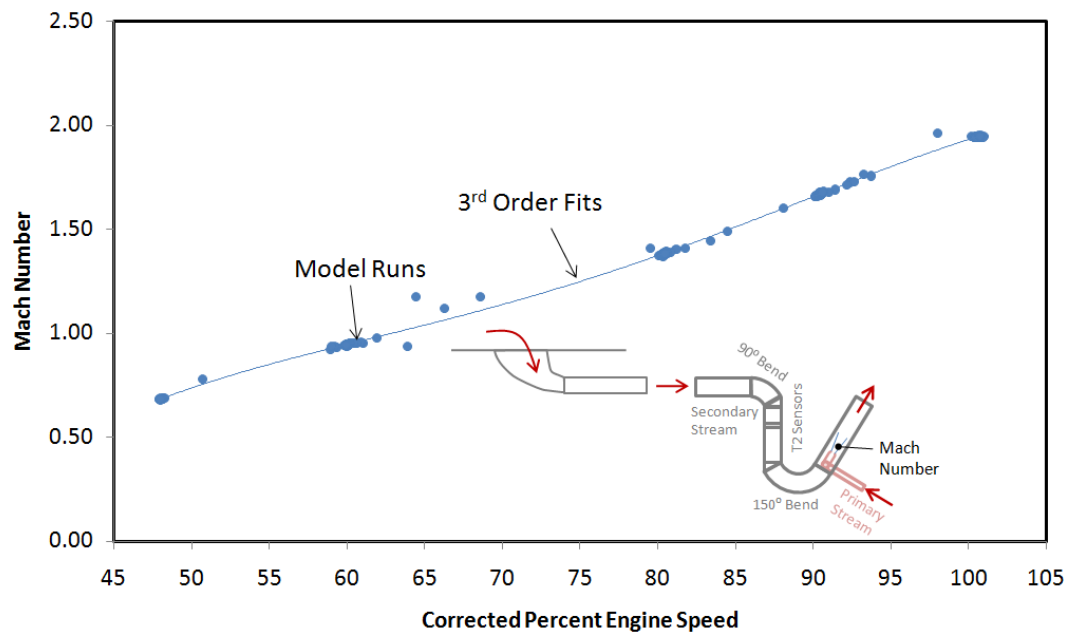


Figure B-33. Predicted Primary Stream Mach Number in Mixing region after Isentropic Expansion

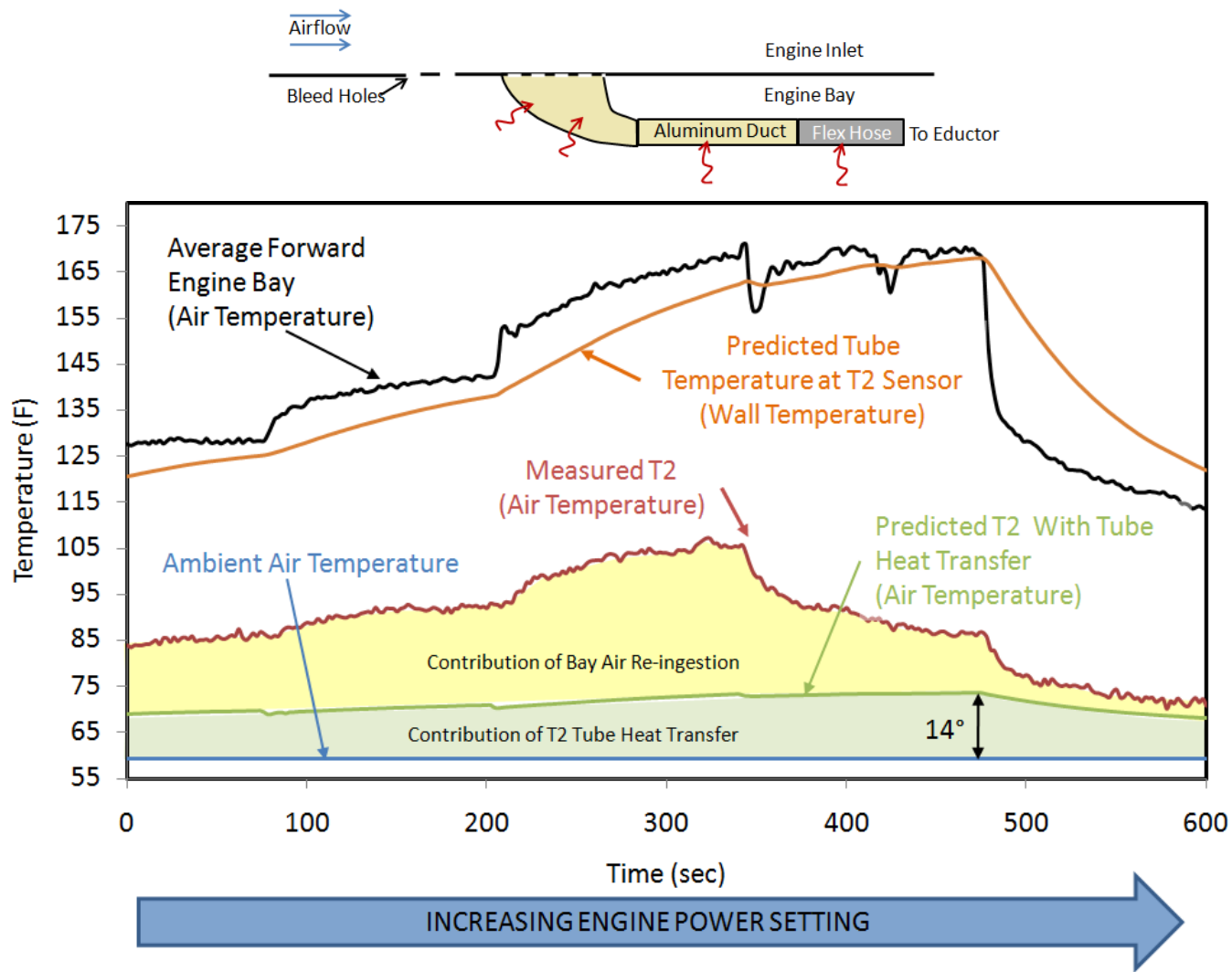


Figure B-34. Baseline Configuration T2 Bias Prediction (Ground Run)

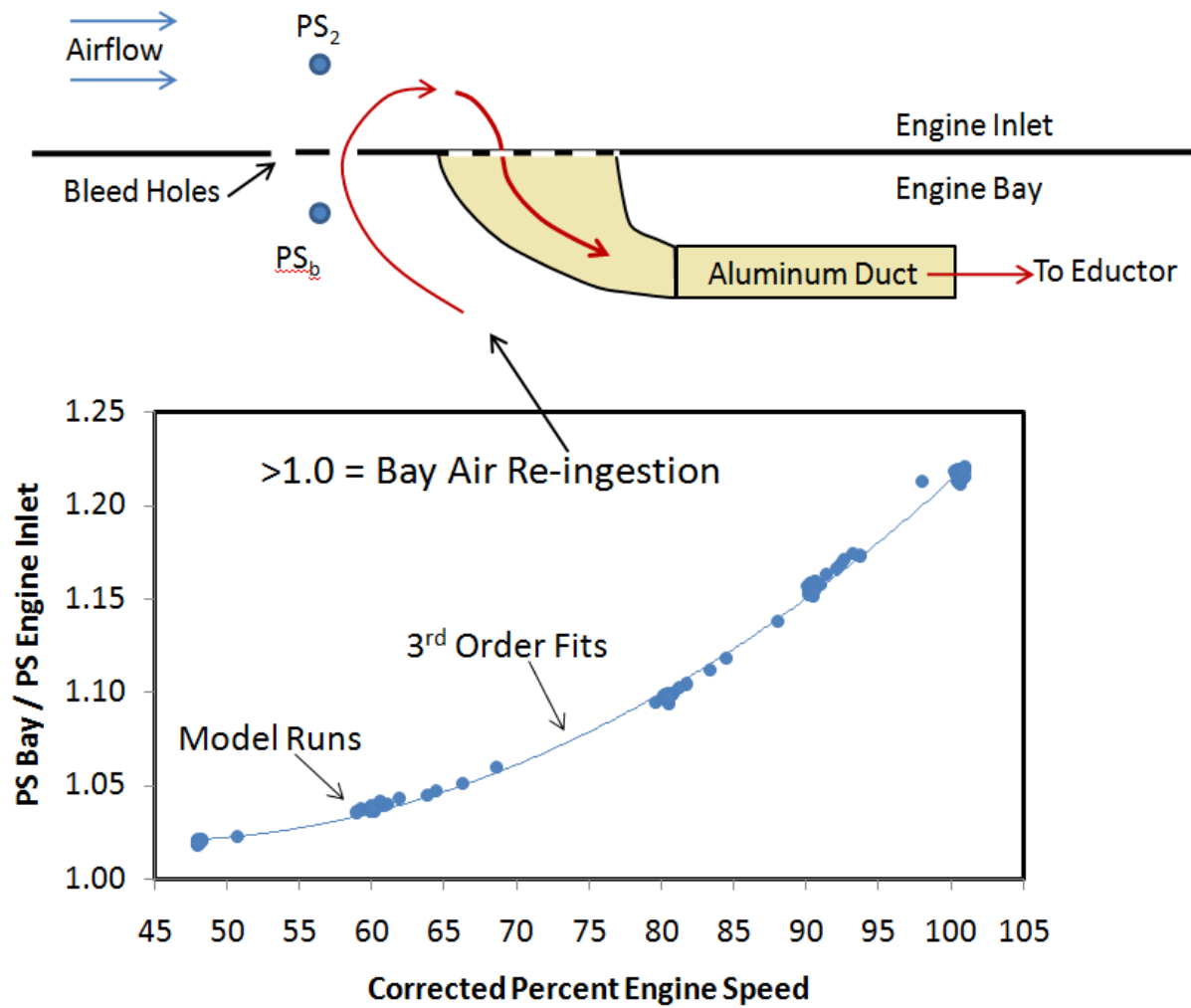


Figure B-35. Delta Pressure Across Engine Inlet Bleed Holes (Ground Run)

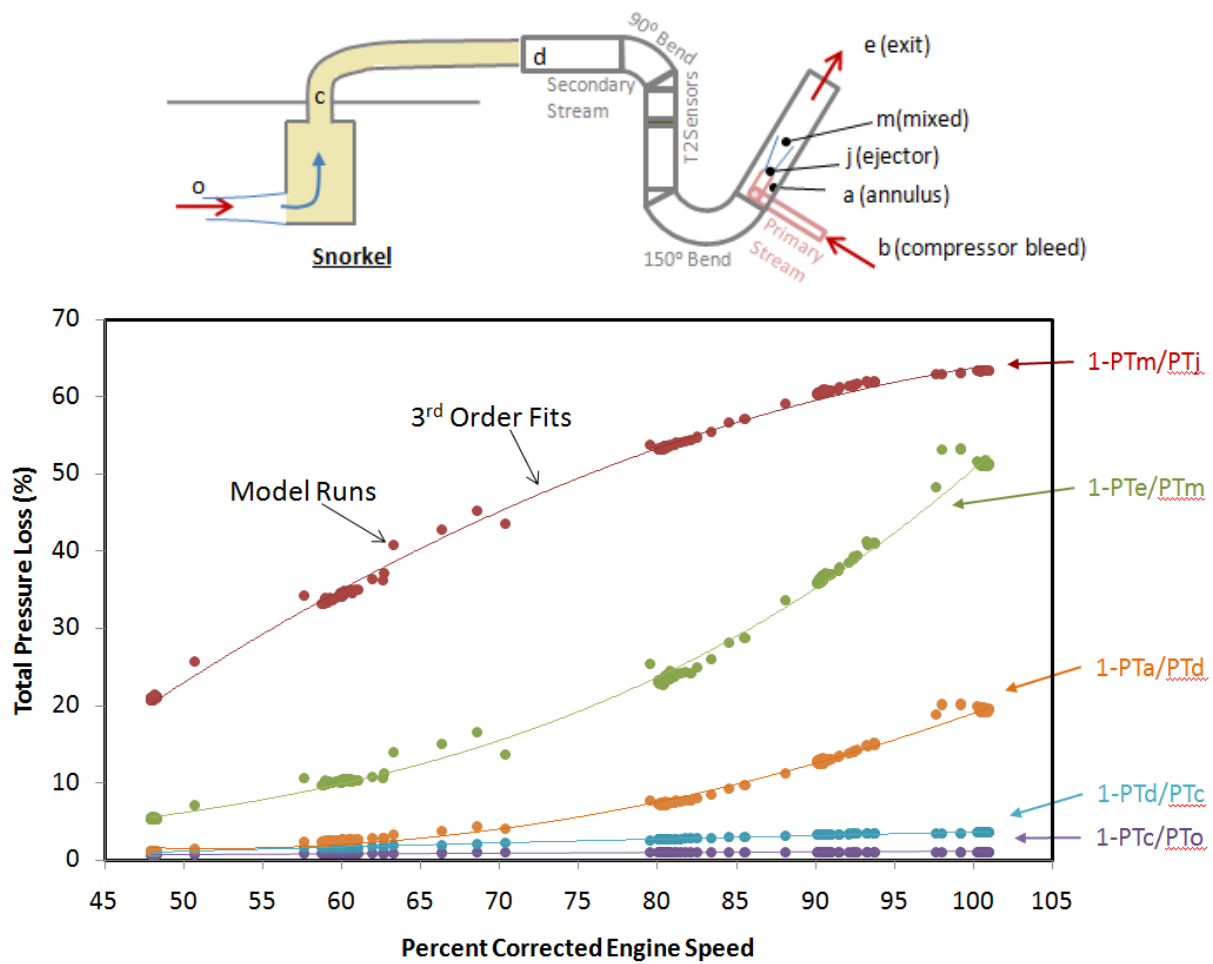


Figure B-36. Predicted Pressure Losses Through Snorkel/Ejector (Ground Run)

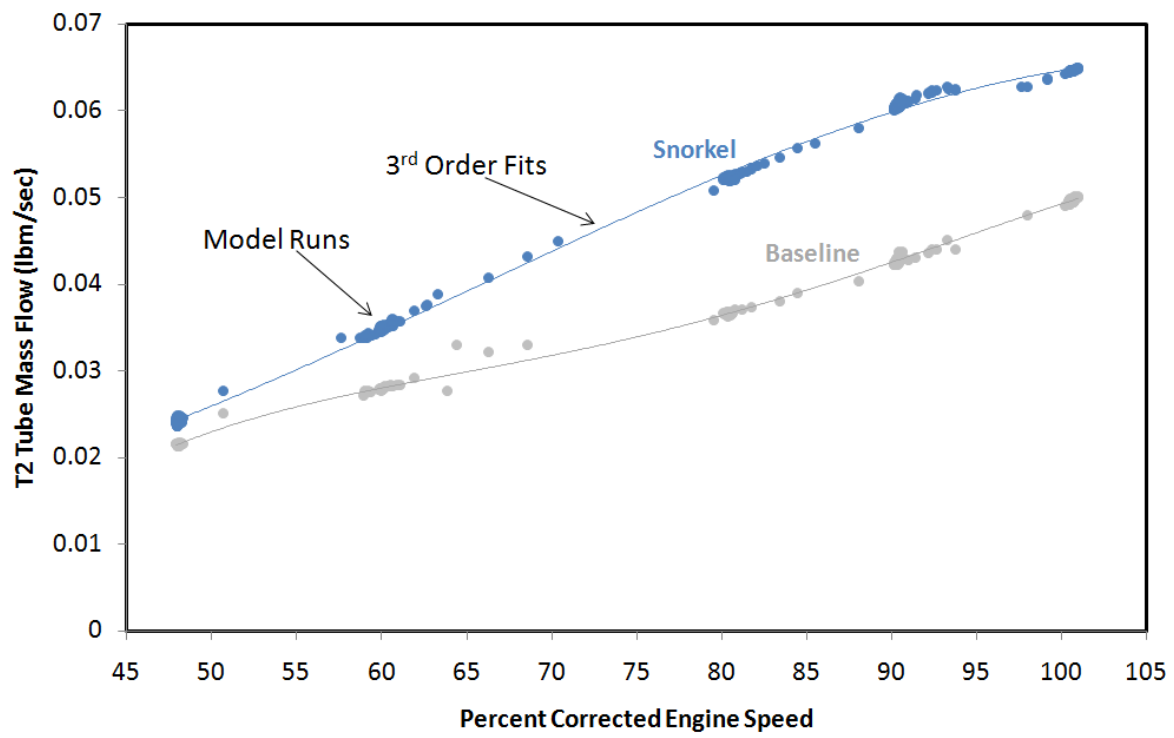


Figure B-37. Predicted T2 Mass Flow for Snorkel/Eductor (Ground Run)

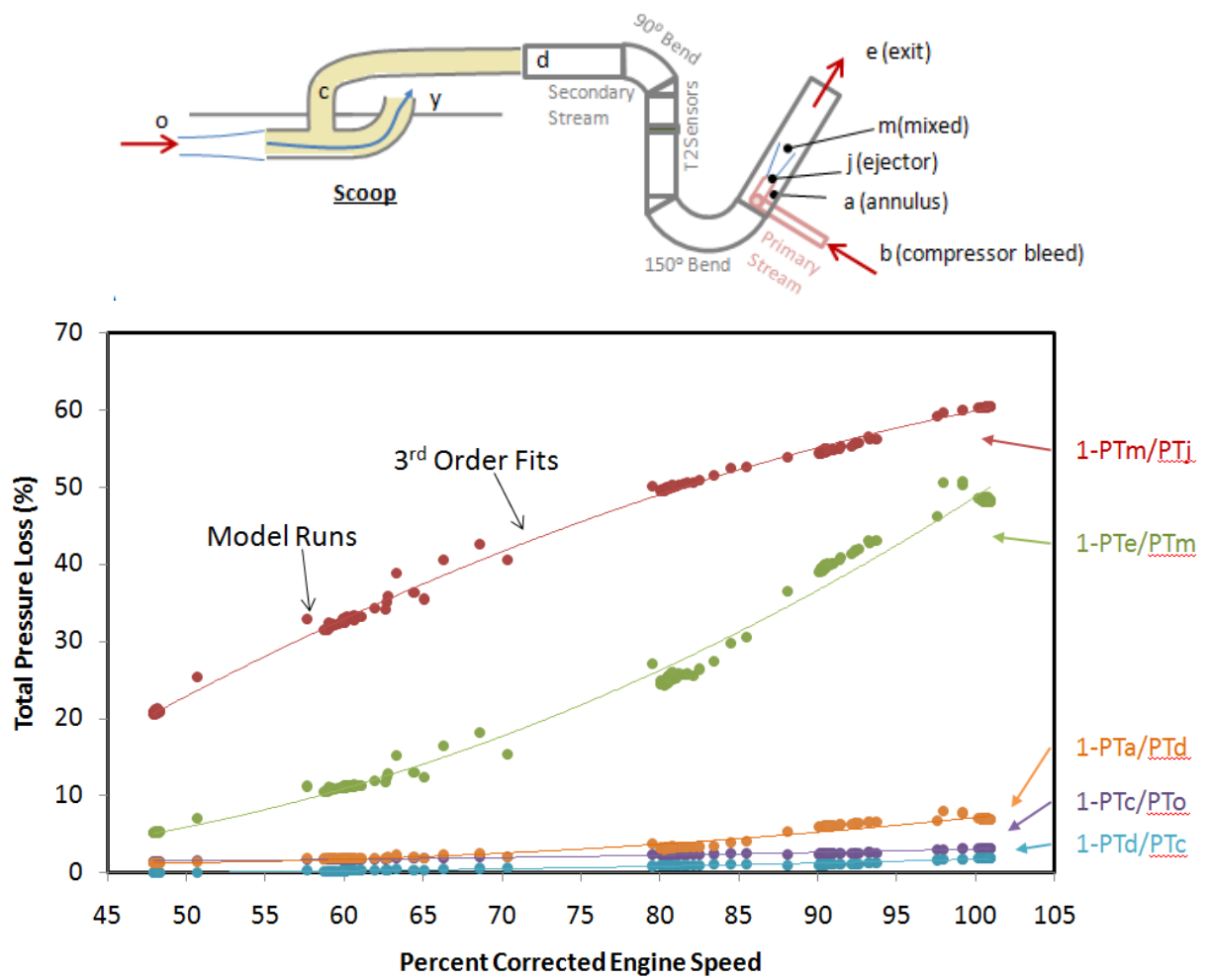


Figure B-38. Predicted Pressure Losses Through Scoop/Ejector System (Ground Run)

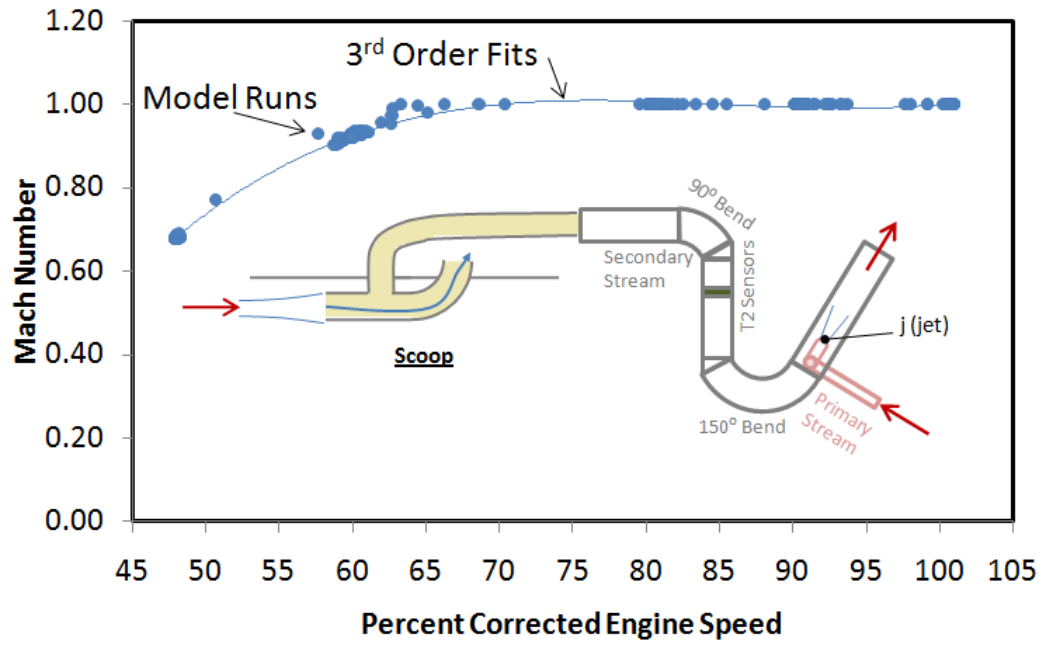


Figure B-39. Predicted Ejector Nozzle Mach Number for Scoop/Ejector (Ground Run)

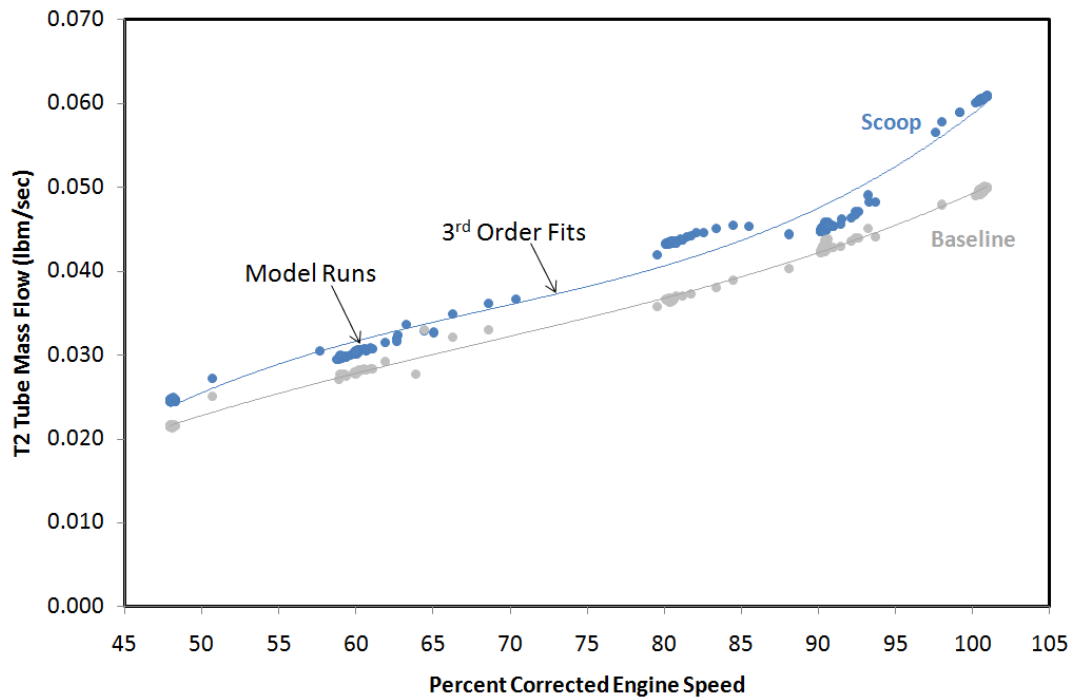


Figure B-40. Predicted T2 Tube Mass Flow for Scoop/Ejector (Ground Run)

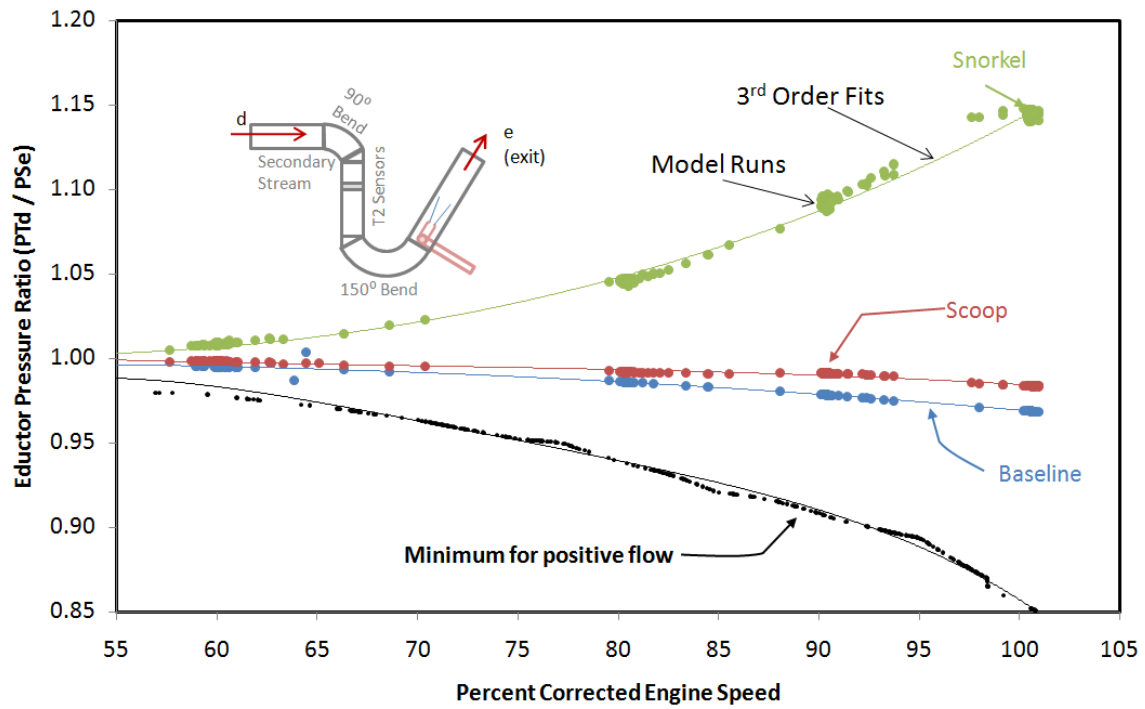


Figure B-41. Predicted Pressure Ratio Comparisons for All Configurations (Ground Run)

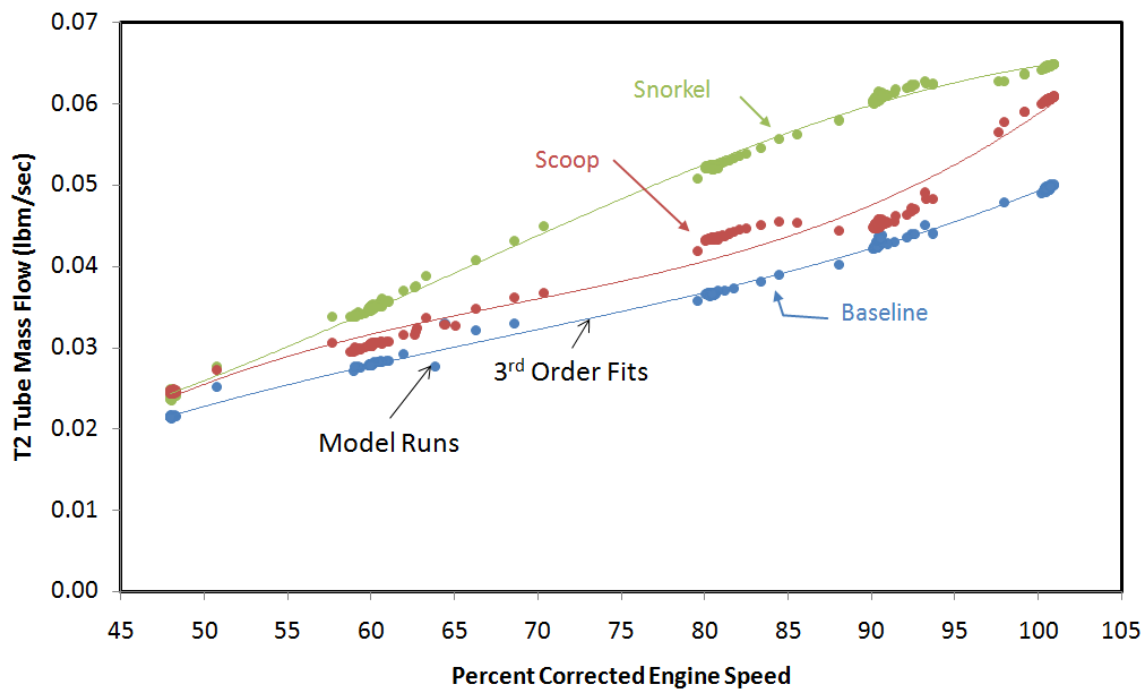


Figure B-42. Comparison of T2 Tube Mass Flow for All Configurations (Ground Run)

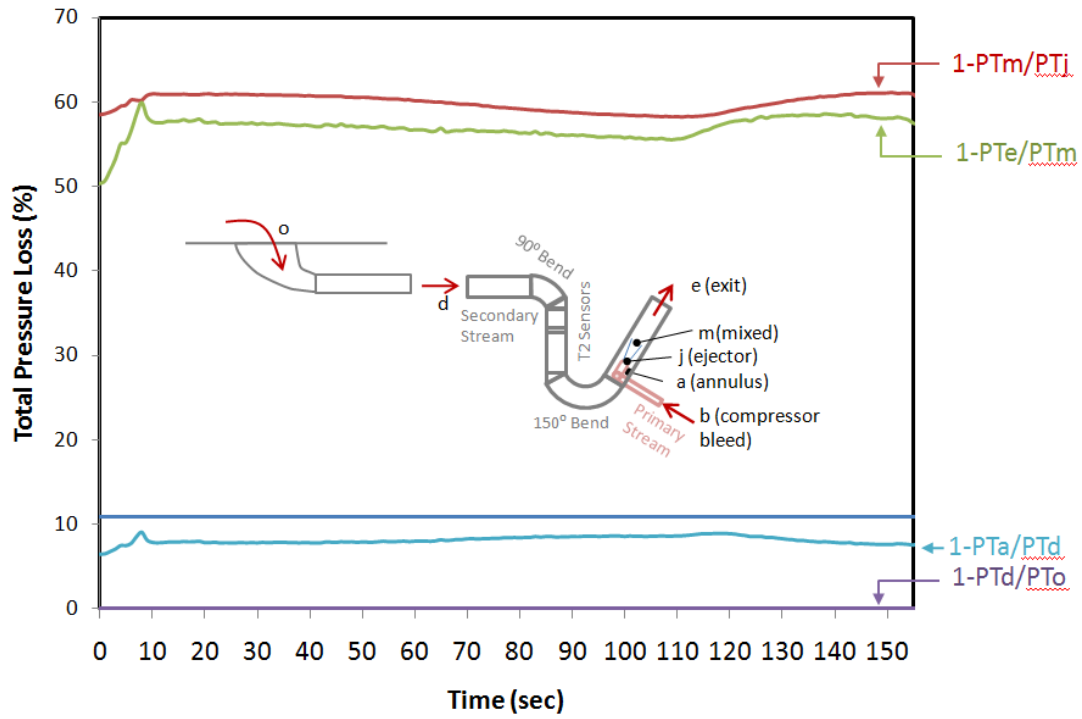


Figure B-43. Predicted Pressure Losses Through Baseline Ejector Model (Flight Transient)

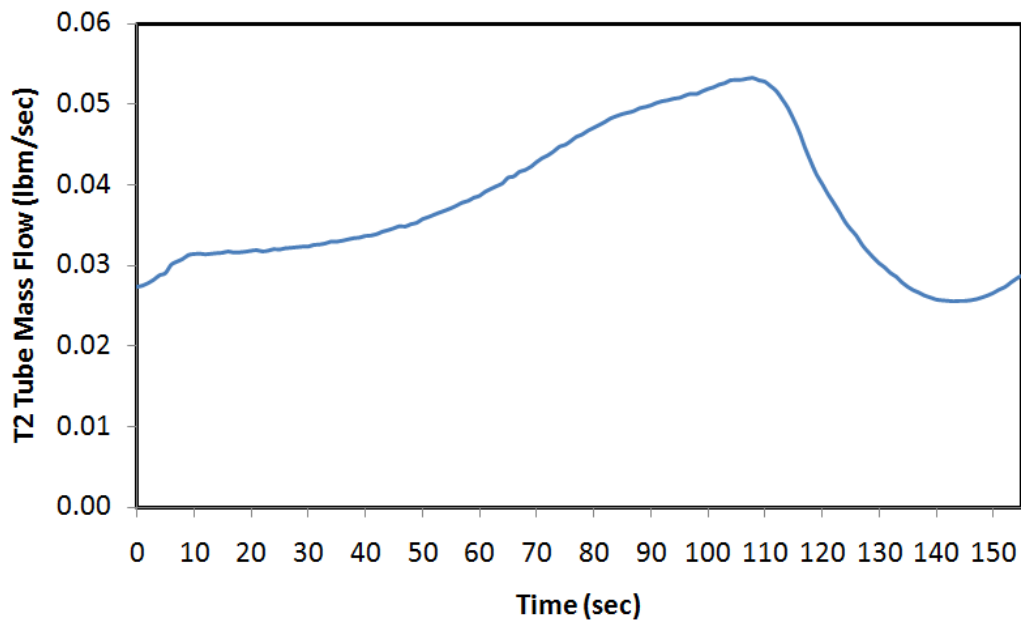


Figure B-44. Predicted T2 Tube Mass Flow (Flight Transient)

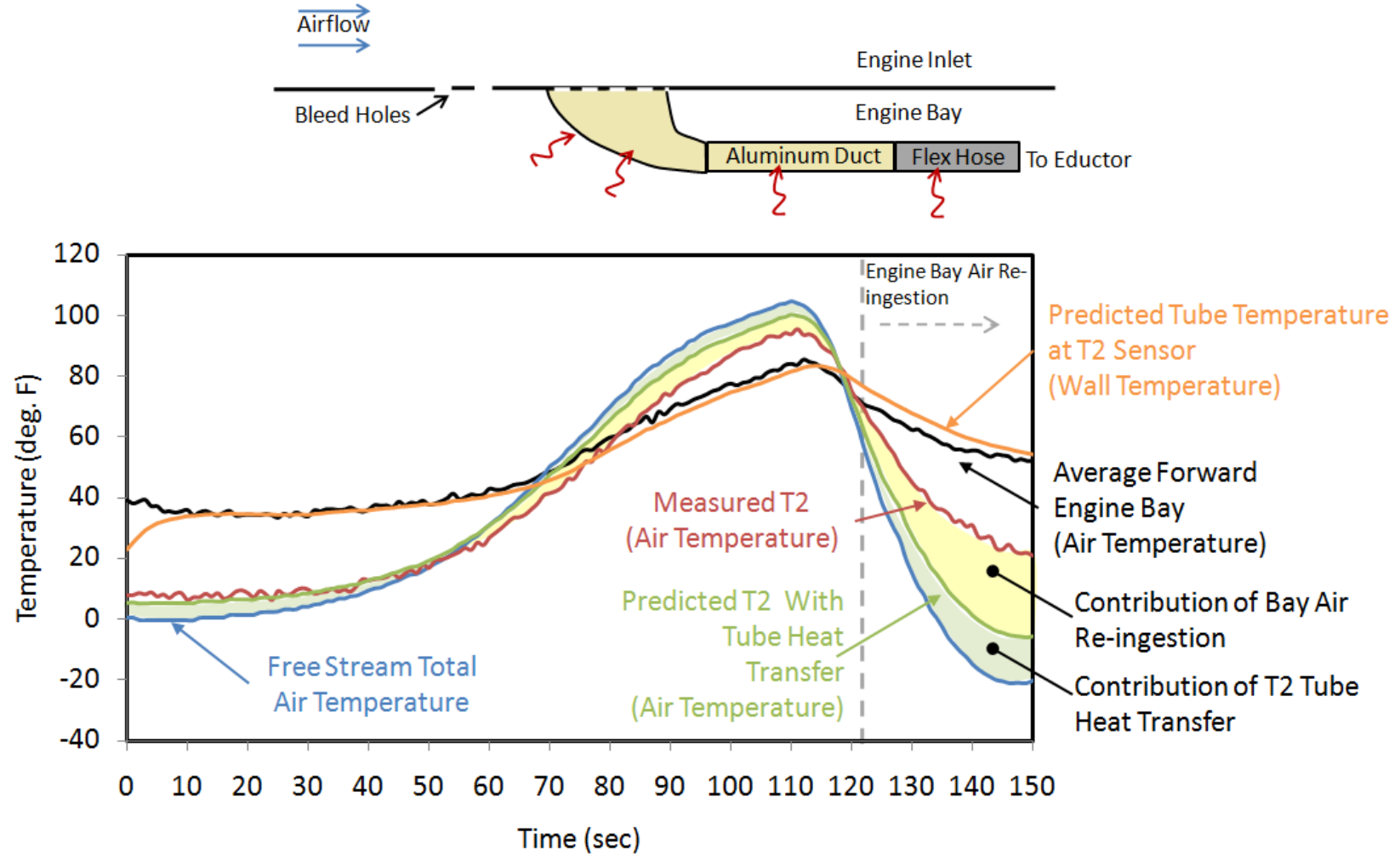


Figure B-45. Baseline Configuration T2 Tube Heat Transfer Predictions (Flight Transient)

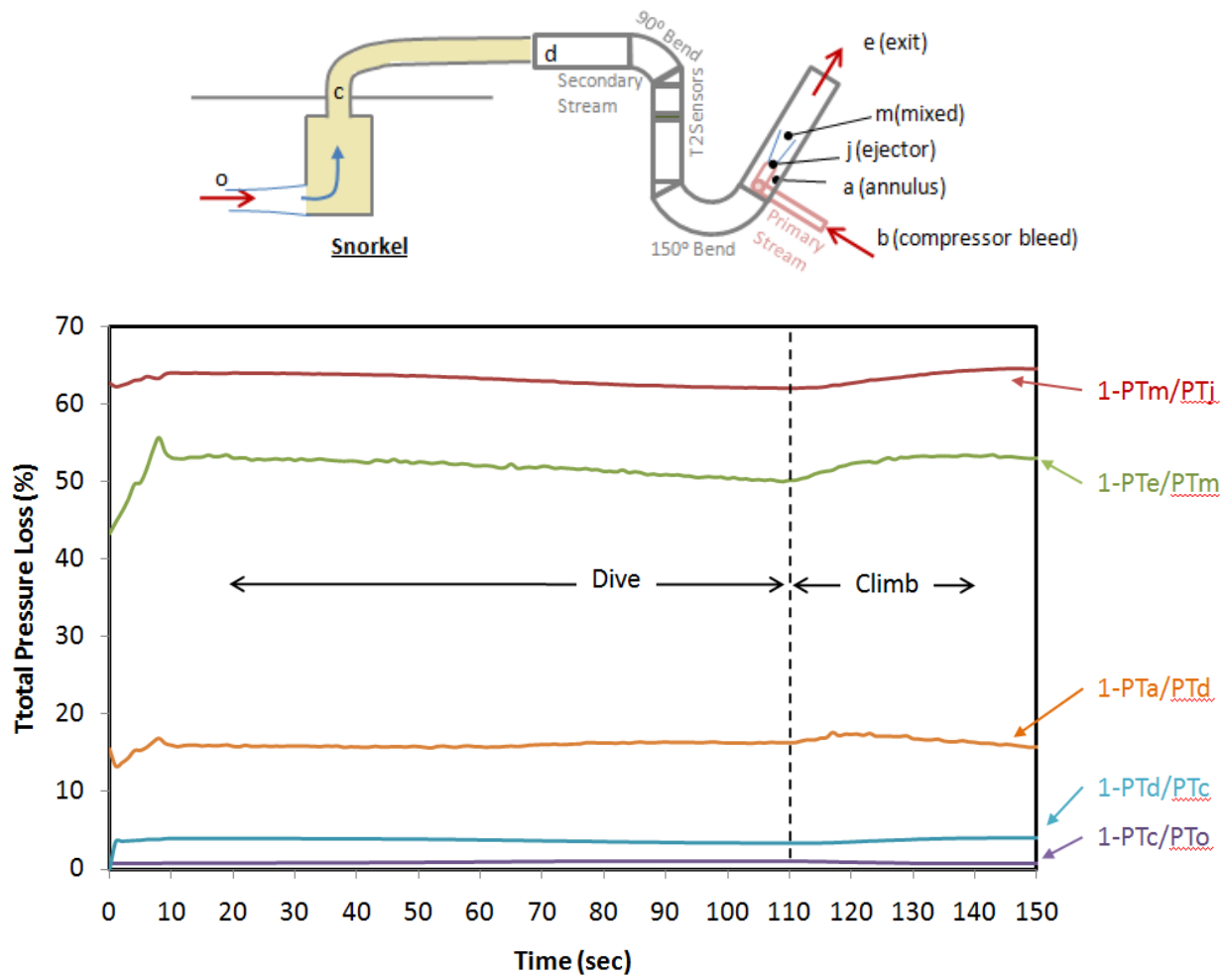


Figure B-46. Predicted Pressures for Snorkel/Ejector (Flight Transient)

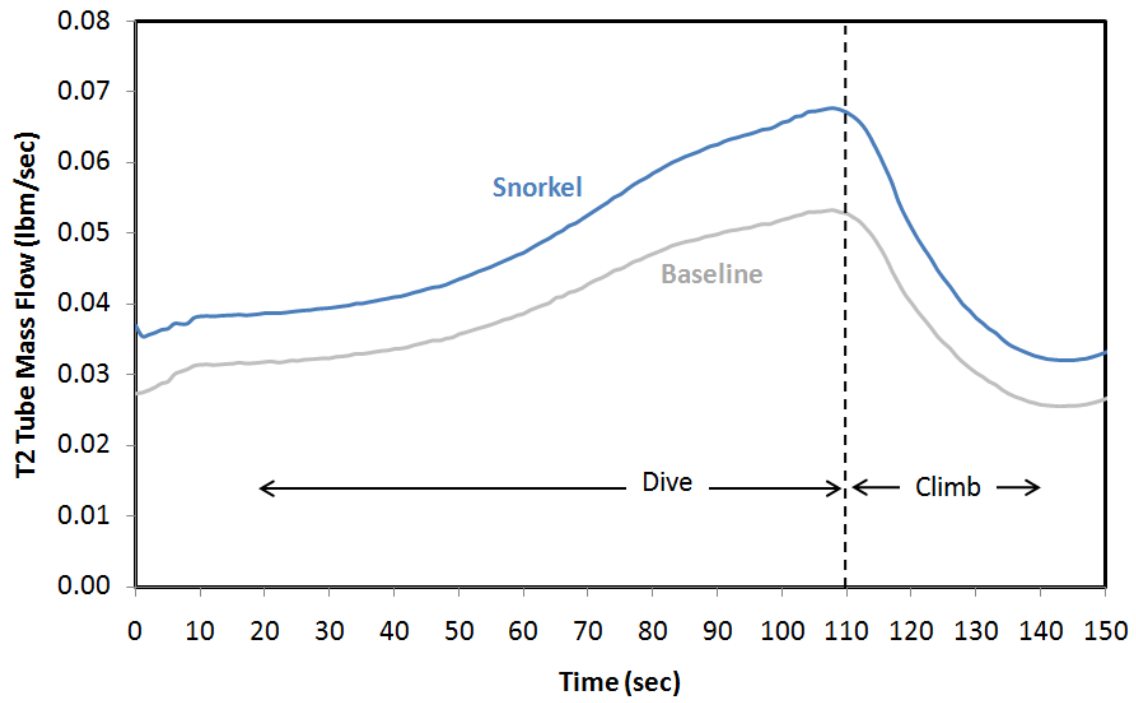


Figure B-47. Predicted T2 Tube Mass Flow for Snorkel/Eductor (Flight Transient)

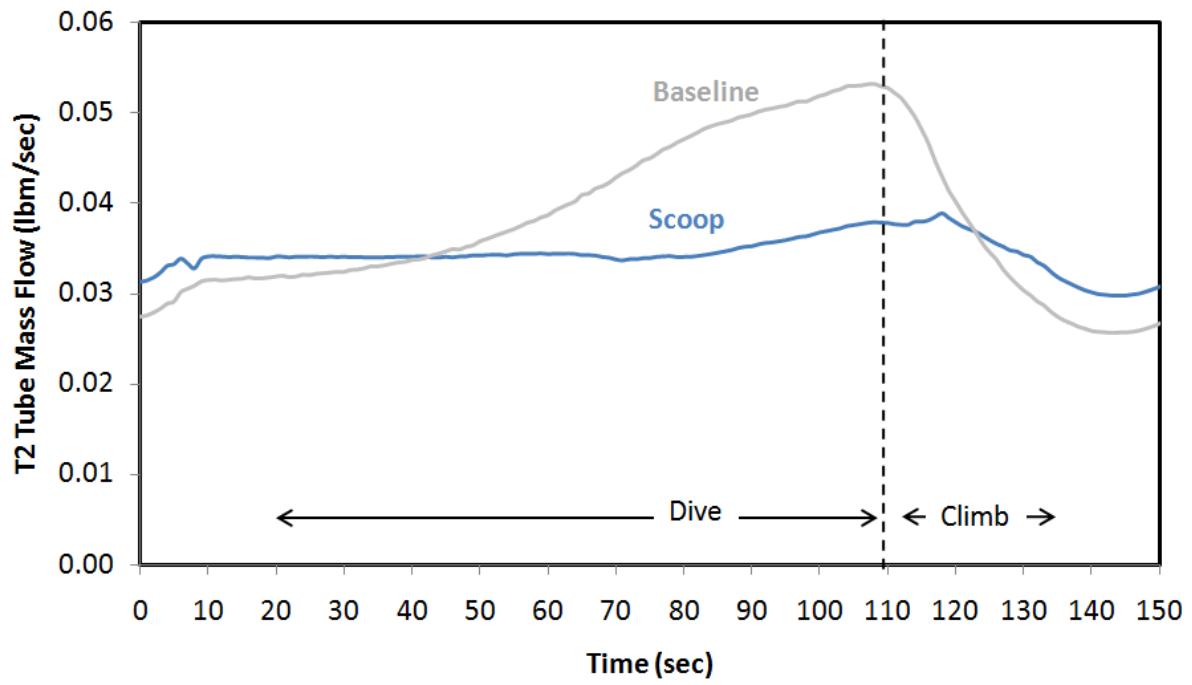


Figure B-49. Predicted T2 Tube Mass Flow for Scoop/Eductor (Flight Transient)

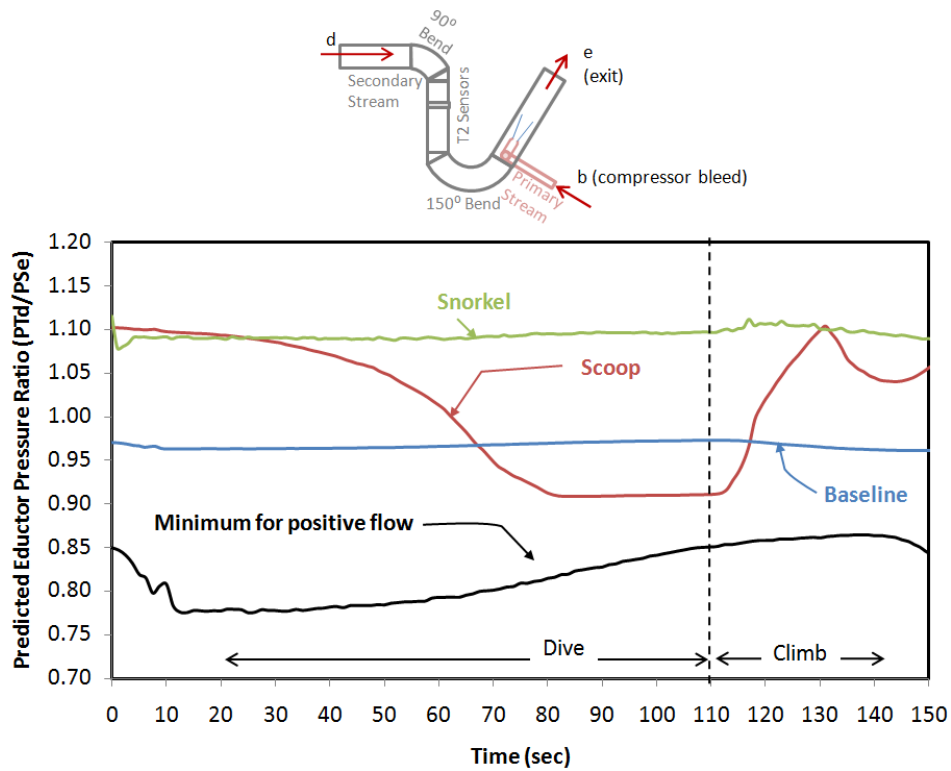


Figure B-50. Predicted Pressure Ratio Comparisons for All Configurations (Flight Transient)

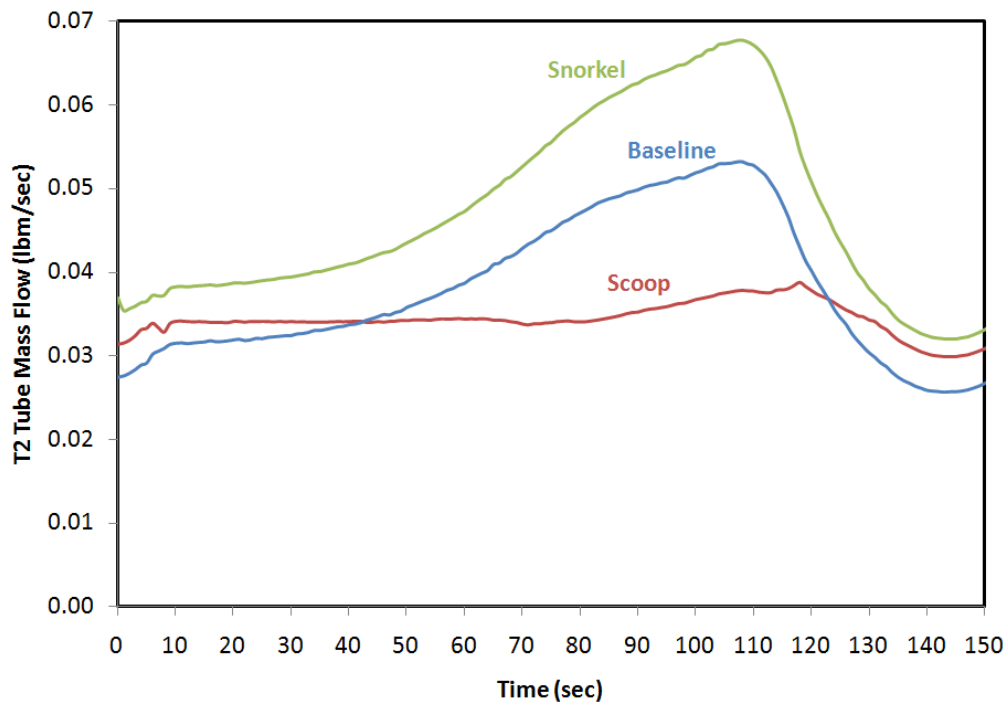
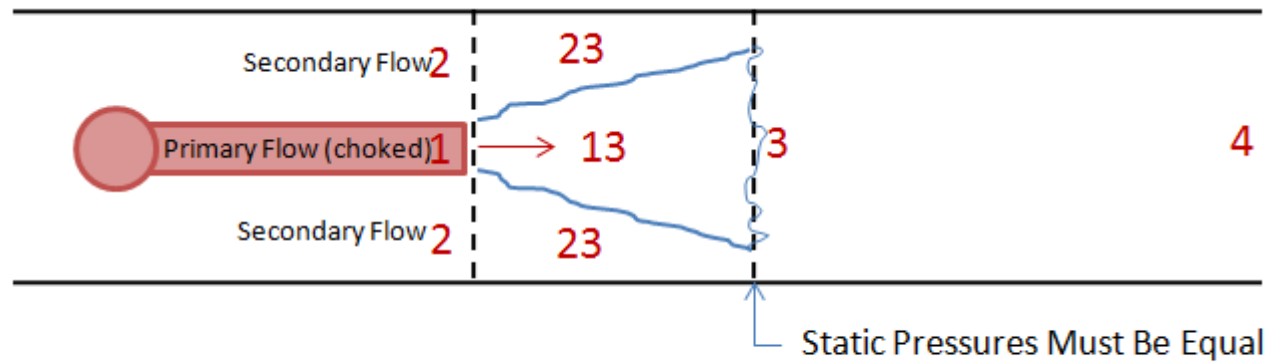
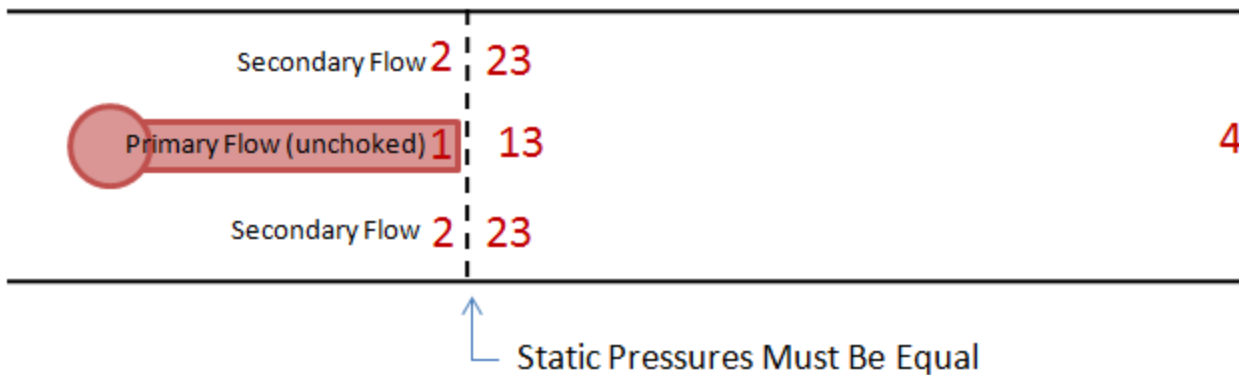


Figure B-51. Comparison of T2 Tube Mass Flow for All Configurations (Flight Transient)



a) Supersonic Ejector



b) Subsonic Ejector

Figure B-52. Station Nomenclature for Derivation of Ejector Model Equations

Appendix C: Tables

Table C-1: Eductor System Total Pressure Losses

Subsystem	Nomenclature	Method
Eductor Tube (Secondary Stream)	PTa/PTd	CFD
Eductor Tube (Primary Stream)	PTj/PTb	CFD/Empirical
Eductor Mixed Stream	PTe/PTm	CFD
Baseline Inlet	PTd/PTo	Historical
Snorkel Inlet	PTc/PTo	CFD
T2 Interface Tube	PTd/PTc	CFD
Scoop Inlet	PTc/PTo	CFD

Table C-2: Representative Model Boundary Conditions

Run	PTj/PSe	PTa/PSe	TTj/TTa
1	2.173	1.002	1.336
2	5.729	1.002	1.766
3	5.653	1.154	1.763
4	7.858	1.002	1.969
5	4.251	1.108	1.618
6	4.046	1.103	1.593
7	6.77	1.132	1.886
8	1.49	1.014	1.456

Table C-3: Flight Conditions for Snorkel and Scoop CFD Runs

CFD Run	Altitude	Mach Number
1	Sea Level	0.01
2	Sea Level	0.3
3	28Kft	0.4
4	25Kft	0.7
5	38Kft	1.1

Table C-4: T2 Bias Components

Configuration	Figure	Engine Bay Air Re-ingestion	Engine Inlet Thermal Boundary Layer	T2 Tube Heat Transfer
Baseline	B-5a	Yes	Yes	Yes
Blocked Bleed Holes	B-5b	No	Yes	Yes
External Snorkel	B-5c	No	No	Negligible
External Scoop	B-5d	No	No	Negligible

Table C-5: Approximation to Infinite Series

No. of Roots (n)	θ^*
1	0.813849
2	0.891339
3	0.905804
4	0.908677
5	0.909174

Appendix D: Method Used to Calculate Pressure Drop Coefficient

The following steps outline the method used to determine the pressure drop coefficient (Equation 2-2 and Equation 2-3) that was used to verify the CFD calculations.

Primary flow pipe data:

- 1) 3/8" aluminum tubing
- 2) 20 inches long
- 3) four 90° bends

Assumptions:

- 1) Pipe entrance is a sharp corner.
- 2) Airflow through the pipe is turbulent.
- 3) The pressure drop is 10% or less than the inlet pressure so that compressible effects can be ignored.
- 4) Relative tube roughness is between 0.00006 and 0.0002.
- 5) Pipe bends have a radius to diameter ratio of $r/d \sim 5$.

Method:

- 1) Calculate Reynolds number. Average $Re = 1.7e5$ for range of operating temperatures.
- 2) Estimate friction factor (f) using chart A-24 from (Crane Valves 1999).

$f = 0.027$ for $Re = 1.7e5$ and estimated relative roughness

- 3) Determine K value

- | | | |
|----|-------------------------------|---|
| a. | $K_1 = f \frac{L}{D} = 1.77$ | pipe loss (Crane Valves, 1999, p. 3-4) |
| b. | $K_2 = 15.5f = 0.41$ | 90° pipe bend (Crane Valves, 1999, p. A-29) |
| c. | $K_3 = 0.5$ | pipe entrance (Crane Valves, 1999, p. A-29) |
| d. | $K = K_1 + 4K_2 + K_3 = 3.94$ | pressure loss coefficient |

Appendix E: Iterative Solution Algorithm

If only the boundary conditions are explicitly known and the mass flow rates and Mach numbers are unknown, an iterative solution approach must be used. The solution steps are outlined below and follow the flow chart shown in Figure B-12.

- Apply pressure losses derived from CFD eductor simulations to primary and secondary streams prior to eductor model
- Determine if primary stream is choked

If the eductor exit pressure is lower than the critical pressure required to choke the primary stream nozzle, then the nozzle will be choked and the primary flow will expand supersonically into the mixing region. This is analogous to the textbook converging diverging nozzle situation. Separate solution methods are used for subsonic or supersonic primary stream.

- Guess static pressure in mixing region (station m)

If the mass flow rates are unknown, the static pressure at the matching station (PS_m) value must be guessed in order to determine the remaining flow variables in the primary and secondary streams. The range of the mixing region static pressure can be intelligently determined based on primary and secondary inlet total pressures. For example, the maximum mixing region static pressure cannot be higher than the primary or secondary stream total pressures. The minimum mixing region static pressure is limited by subsonic or supersonic ejector operation.

- Calculate primary stream Mach numbers and mass flow rate

If the primary stream is choked, then the Mach number at the nozzle is unity and the flow area is the cross sectional area of the expanded jet as illustrated in Figure B-11a. If the flow is subsonic, then station m becomes the exit of the ejector nozzle, the flow area is the exit area of the ejector nozzle and the Mach number must be determined using isentropic relationships with the model inputs and the guessed static pressure at station m . Once the Mach number is determined, the mass flow rate can be determined also (see the appendix for details on the specific calculations).

- Calculate secondary stream Mach numbers and mass flow rate

Once the primary stream flow area is determined from the previous step, the secondary stream flow area may also be determined as the difference between primary flow area and mixing

region cross section. The Mach numbers are calculated from isentropic relations as in step 4 and the mass flow rate can then be determined (details in the appendix).

- Momentum balance

The linear momentum of the primary and secondary streams is calculated at the isentropic station m . Once the individual momentums are known, the mixed momentum can be calculated and mixed flow velocities may be determined.

- Apply mixing correlations to calculate exit properties

The mixing correlations ensure that mass, linear momentum and energy are conserved through the mixing region. Mass is conserved such that the mixed mass flow rate is simply the sum of the primary and secondary stream mass flow rates. Since the total enthalpy must be conserved, the mixed stream enthalpy must be equal to the sum of the primary and secondary stream mass weighted enthalpies. Once the streams have been mixed and the viscous terms have been accounted for in the linear momentum equation, the exit static pressure may be calculated and compared to the exit boundary condition.

- Compare calculated exit static pressure to measured exit static pressure.

If the calculated exit static pressure does not match the exit boundary condition within a pre-defined tolerance, the calculations are repeated until the model converges to the known exit boundary condition. The steps are repeated starting from a new guessed mixing region static pressure (PS_m).

Vita

Brian Binkley was born in Hendersonville, Tennessee on April 11, 1984. He grew up in Springfield, Tennessee and graduated from high school at Pioneer Christian Academy in May 2002 and during the same year he began undergraduate studies at Tennessee Technological University. During his undergraduate studies, he completed one year of the cooperative study program at Denso Manufacturing in Maryville, Tennessee and a second year of the cooperative study program at the Arnold Engineering Development Center at Arnold Air Force Base, Tennessee. Brian completed his Bachelors of Science degree in Mechanical Engineering at Tennessee Technological University in December 2007. After graduation he began his professional career at the Arnold Engineering Development Center while completing his Masters degree in Mechanical Engineering part time at the University of Tennessee Space Institute.

Brian is currently employed by Aerospace Testing Alliance the primary contractor for the Arnold Engineering Development Center.