Dynamic Modeling and Simulation of a Variable Cycle Turbofan Engine with Controls

Robert W. Buettner
Wright State University
Dynamic Modeling and Simulation of a Variable Cycle Turbofan Engine with Controls

A thesis submitted in partial fulfillment of the requirements for the degree of Master of Science in Mechanical Engineering

By

Robert W. Buettner
B.S.M.E., Wright State University, 2015

2017
Wright State University
I HEREBY RECOMMEND THAT THE THESIS PREPARED UNDER MY SUPERVISION BY Robert W. Buettner ENTITLED Dynamic Modeling and Simulation of a Variable Cycle Turbofan Engine with Controls BE ACCEPTED IN PARTIAL FULFILLMENT OF THE REQUIREMENTS FOR THE DEGREE OF Master of Science in Mechanical Engineering

Committee on Final Examination

Rory Roberts, Ph. D.
Thesis Director

Rolf Sondergaard, Ph. D.

Joseph C. Slater, Ph. D., P.E.
Professor and Chair, Department of Mechanical and Materials Engineering

Rory Roberts, Ph. D.

Mitch Wolff, Ph. D.

Robert E.W. Fyffe, Ph. D.
Vice President for Research and Dean of the Graduate School
Abstract

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Next generation aircraft (especially combat aircraft) will include more technology and capability than ever before. This increase in technology comes at the price of higher electrical power requirements and increased waste heat that must be removed from components to avoid overheating induced shutdowns. To help combat the resulting power and thermal management problem, a vehicle level power and thermal management design and optimization toolset was developed in MATLAB®/Simulink®.

A dynamic model of a three-stream variable cycle engine was desired to add to the capabilities of the power and thermal management toolset. As an intermediate step to this goal, the dynamic mixed-flow turbofan engine model previously developed for the toolset was modified with an afterburner, a variable geometry nozzle, and a new controller to automatically control the new components. The new afterburning turbofan engine model was tested for a notional mission profile both with and without power take-off. This testing showed that the afterburning turbofan engine model and controller were successful enough to justify moving on to the development of the three-stream variable cycle engine model.
The variable cycle engine model was developed using the components of the afterburning turbofan model. The compressor and turbine components were modified to use maps that incorporate the effects of variable inlet guide vane angles. The new engine model and components were sized by attempting to match data from a Numerical Propulsion System Simulation model with similar architecture. A previously developed heat exchanger model was added to the third stream duct of the new engine model. Finally, a new simplified controller was developed for the variable cycle engine model based on the controller developed for the afterburning turbofan model.

The new variable cycle engine model was tested for a notional mission profile for five cases. The first case operated the engine model without power take-off and with the third stream heat exchanger removed. The second case added shaft power take-off. The third and fourth cases did away with the power take-off and added the heat exchanger to the engine model with two different hot-side mass flow rate conditions. The fifth case tested the engine with both power take-off and the third stream heat exchanger. The results were promising, showing that the variable cycle engine model had variable cycle tendencies even with a minimum of controlled variable geometry features. The controller was found to be effective, though in need of upgrades to take advantage of the benefits offered by a variable cycle engine. Additionally, it was found that both power take-off and heat rejection to the third stream impact the entire engine cycle.
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The views and conclusions contained herein are those of the authors and should not be interpreted as necessarily representing the official policies or endorsements, either expressed or implied, of Air Force Research Laboratory or the U.S. Government.

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### Nomenclature (Text)

<table>
<thead>
<tr>
<th>Acronym</th>
<th>Definition</th>
</tr>
</thead>
<tbody>
<tr>
<td>AGATE</td>
<td>alternate generic adaptive turbine engine</td>
</tr>
<tr>
<td>APU</td>
<td>auxiliary power unit</td>
</tr>
<tr>
<td>EPR</td>
<td>engine pressure ratio</td>
</tr>
<tr>
<td>HP</td>
<td>high pressure</td>
</tr>
<tr>
<td>HX</td>
<td>heat exchanger</td>
</tr>
<tr>
<td>LP</td>
<td>low pressure</td>
</tr>
<tr>
<td>LPC</td>
<td>low pressure compressor</td>
</tr>
<tr>
<td>LPT</td>
<td>low pressure turbine</td>
</tr>
<tr>
<td>MEA</td>
<td>more electric aircraft</td>
</tr>
<tr>
<td>NPSS</td>
<td>numerical propulsion system simulation</td>
</tr>
<tr>
<td>SFC</td>
<td>specific fuel consumption</td>
</tr>
<tr>
<td>T2T</td>
<td>tip-to-tail</td>
</tr>
<tr>
<td>TMS</td>
<td>thermal management system</td>
</tr>
<tr>
<td>TSFC</td>
<td>thrust specific fuel consumption</td>
</tr>
<tr>
<td>$T_{t4}$</td>
<td>turbine total inlet temperature</td>
</tr>
<tr>
<td>$T_{t6}$</td>
<td>nozzle total inlet temperature</td>
</tr>
<tr>
<td>VCE</td>
<td>variable cycle engine</td>
</tr>
<tr>
<td>VIGV</td>
<td>variable inlet guide vane</td>
</tr>
</tbody>
</table>

### Nomenclature (Equations)

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Definition</th>
</tr>
</thead>
<tbody>
<tr>
<td>EPR</td>
<td>engine pressure ratio</td>
</tr>
<tr>
<td>$F$</td>
<td>developed thrust</td>
</tr>
<tr>
<td>$Friction Loss$</td>
<td>power loss due to shaft friction at the design speed</td>
</tr>
<tr>
<td>$I_{shaft}$</td>
<td>combined moment of inertia of shaft and all attached turbomachinery</td>
</tr>
<tr>
<td>$Load_i$</td>
<td>component of loading on shaft in terms of power</td>
</tr>
<tr>
<td>$\dot{m}$</td>
<td>mass flow rate</td>
</tr>
<tr>
<td>$\dot{m}_f$</td>
<td>fuel mass flow rate</td>
</tr>
<tr>
<td>$p$</td>
<td>static pressure</td>
</tr>
<tr>
<td>$Pr$</td>
<td>total pressure ratio across a turbomachinery component</td>
</tr>
<tr>
<td>$Pr_d$</td>
<td>design pressure ratio of a turbomachinery component</td>
</tr>
<tr>
<td>$Pr_n$</td>
<td>normalized pressure ratio</td>
</tr>
<tr>
<td>$p_{t \text{ fan exit}}$</td>
<td>fan exit total pressure</td>
</tr>
<tr>
<td>$p_{t \text{ LPT exit}}$</td>
<td>low pressure turbine exit total pressure</td>
</tr>
<tr>
<td>$R$</td>
<td>ideal gas constant for a given molecular composition</td>
</tr>
<tr>
<td>$RPM_{\text{design}}$</td>
<td>shaft design speed in revolutions per minute</td>
</tr>
<tr>
<td>$RPM_{\text{shaft}}$</td>
<td>shaft speed in revolutions per minute</td>
</tr>
<tr>
<td>$SFC$</td>
<td>specific fuel consumption</td>
</tr>
<tr>
<td>$T$</td>
<td>temperature</td>
</tr>
<tr>
<td>TSFC</td>
<td>thrust specific fuel consumption</td>
</tr>
<tr>
<td>$V$</td>
<td>volume</td>
</tr>
</tbody>
</table>
\[ W_c \quad = \quad \text{corrected mass flow rate} \]
\[ \Delta Pr \quad = \quad \text{pressure ratio interval} \]
\[ \varepsilon \quad = \quad \text{efficiency} \]
\[ \varepsilon_n \quad = \quad \text{normalized efficiency} \]
\[ \omega_{shaft} \quad = \quad \text{shaft speed in radians per second} \]
1. INTRODUCTION

1.1. Problem Overview

Next generation aircraft (especially combat aircraft) will include more technology and capability than ever before. The increases in capability afforded by the addition of new technology come at a cost of higher power and thermal management requirements. Figure 1 shows the trends in power and thermal management requirements as they relate to capability for military aircraft, both current and predicted. It should be noted that the power and thermal axis in Fig. 1 has a break just above the point that the F-22 occupies. This indicates that a significant jump in power and thermal requirements will come with next generation aircraft capabilities.

While not entirely exclusive to combat aircraft, these issues are perhaps most severe for combat aircraft which have other constraints that are not common to commercial aircraft. These constraints include the need to maintain high survivability and low observability. This results in limitations to available heat sinks on the aircraft including limitations to heat rejection due to the usage of composite skin materials. This further complicates the thermal management problem.

Additionally, the move to more electric aircraft (MEA) architectures increases the need for electrical power generation on the aircraft. Traditionally, the two primary sources of power on an aircraft are the engine and auxiliary power unit (APU). The presence of an APU increases the empty weight of an aircraft, which is undesirable as this reduces the payload of an aircraft. In addition, the APU occupies volume that could otherwise be used for other aircraft systems or fuel. For these reasons, it is desirable for the APU to be
as small as possible or non-existent. To eliminate the APU as more electrical power is needed on the aircraft, it is necessary to be able to generate electrical power directly from the aircraft’s propulsion engines. This presents many potential issues, especially as the electrical power demands continue to grow. Included in these issues are the thermal management considerations required for replacing traditional mechanical and pneumatic components with electric equivalents.²

In the interest of exploring new thermal management architectures to combat the ever-rising thermal loads on aircraft, a generic vehicle level thermal management design and optimization toolset has been developed. This toolset has been developed entirely in the Simulink® modeling environment.⁴ The toolset models incorporate system dynamics (i.e. transient features) of various vehicle power and thermal management subsystems,

Figure 1. Power and thermal management requirements for current and future military aircraft as they relate to capability¹
including a dynamic turbine engine model. A generic, transient, mixed-flow turbofan engine model based in the Simulink® model environment was created for use in this toolset.\textsuperscript{5,6} The engine model utilizes various transient phenomena to simulate dynamic operation and avoid computationally expensive iterative solution procedures.\textsuperscript{5,6}

To expand the capabilities of the toolbox to include more advanced propulsion systems, a steady-state three-stream variable cycle engine (VCE) model was obtained. This VCE model was integrated in a tip-to-tail (T2T) aircraft model by Donovan et al.\textsuperscript{7,8} This T2T model was created using components from the thermal management design and optimization toolset. While this steady-state model did extend the capabilities of the T2T model to include a more advanced engine architecture, it removed the ability to account for transient effects in the aircraft’s propulsion system. This consequence is not desirable because transient effects in the thermal management system (TMS) can cause appreciable changes in engine performance that should be accounted for. An effort to create a dynamic model of a three-stream VCE in the Simulink® environment began in order to regain the ability to account for the transient effects in the propulsion system. A transient VCE model also provides the opportunity to investigate the effects of the engine on aircraft power and thermal systems including the possibility of using the engine itself as a heat sink.

Examining and managing the interactions of the engine and aircraft thermal systems is important for all aircraft propulsion systems. These interactions become even more important when considering that rejecting heat to the engine cycle has become the preferred approach to thermal management for modern military aircraft.\textsuperscript{2} One proposed method for such heat rejection to the engine cycle is the use of a third-stream heat
exchanger (HX) in a VCE. The feasibility of using a third-stream HX at a given point in a mission is highly dependent on both the engine operating condition and the temperature of the thermal load. Consequently, because the TMS of an aircraft is powered by the engine in many aircraft, transient interactions between the engine and TMS become even more important.

While having a three-stream VCE model is desirable, it is of limited use without a control system model to govern its operation. A large number of variable geometry components combined with the need to account for transients in heat rejection to the third-stream and shaft power take-off without introducing instabilities highlights the need for an advanced control scheme. This advanced control scheme will also be required to enable what is known as flow holding. Flow holding is an engine operation mode in which the maximum possible airflow through the engine is maintained in reduced thrust conditions by modulating the engine bypass ratio. This operation mode reduces off-design inlet drag and fuel consumption as compared to a fixed cycle engine.

The focus of this research is to develop a dynamic model of a three-stream VCE in Simulink® to reintroduce transient effects into the thermal management toolset. The new dynamic VCE model must be computationally efficient to enable fast trade studies when coupled with power and thermal management subsystems from the toolbox. An additional goal of this research is for the developed engine model to be generic such that the model user can easily import their own turbomachinery maps and scale the engine to fit their needs.
1.2. Review of Relevant Literature

1.2.1. Simmons

In 2009, a Ph.D. dissertation concerning a Numerical Propulsion System Simulation (NPSS) based model of a three-stream VCE was submitted by Ron Simmons at Ohio State University. This dissertation presented a theoretical framework and history of VCEs from their earliest roots in the late 1950s and early 1960s to relatively recent efforts of academia. This includes a discussion of the benefits and pitfalls associated with flow holding. These discussions led to the introduction of a novel three-stream VCE architecture featuring variable flow modulation throughout the engine while dispensing with the usage of flow diverting vanes upstream of the bypass ducts. Additionally, this new architecture was configured such that a minimum amount of flow was always maintained through the third stream for cooling purposes. A diagram of this architecture is shown in Fig. 2. This architecture was used as the basis for the architecture of the dynamic VCE model presented in this thesis.

![Figure 2. NPSS VCE architecture](image-url)
Additionally, the dissertation discussed the development of a novel method to investigate
the optimal performance of the VCE for a given mission. The method involved using the
Model Center® process integration environment in conjunction with the NPSS VCE
model to optimize the on-design engine configuration and variable geometry component
settings in off-design conditions for a given mission. To increase the speed of this
process, a genetic algorithm was applied. The details of this algorithm are not important
to the work presented in this thesis and, therefore, will not be discussed here. The
dissertation also presented results for the optimal VCE engines and sub-optimal engine
configurations that decrease engine cycle complexity while maintaining most of the gains
achieved by the optimal engine. Discussions on the most important variable geometry
features based on their contribution to fuel usage reduction for a given mission were
made. The dissertation research found that, for this architecture, “…the use of just three
variable features can yield an astounding 31-34% fuel savings of the [year] 2000 state of
the art engine…”\(^9\). The variable features used to achieve these gains were modulated
cooling of turbine blades, variable high pressure turbine (HPT) inlet area, and a variable
third-stream nozzle. While research indicates that these three features are the most
effective at reducing fuel consumption for the VCE architecture used in the dissertation
research, the engine model developed by this thesis research is to be generic. For that
reason, the final Simulink® dynamic VCE model will incorporate as many variable
gometry features as possible.

1.2.2. Corbett

In 2011, a Master’s thesis concerning the effects of transient loading and waste heat
rejection on a three-stream VCE was submitted by Michael Corbett at Wright State
University. This thesis discussed the origins of the three-stream variable cycle engine as well as the principles that govern its operation. Additionally, Corbett discussed the traditional methods of extracting power from a turbine propulsion engine (i.e. bleed air and shaft power extraction). Also discussed were the trends of moving towards MEA in both military and civilian aircraft and the potential benefits and risks related to this shift. The general effects of bleed air and shaft power extraction are discussed for an individual compressor and the engine as a whole.

The research presented in Corbett’s thesis used an NPSS VCE model integrated with a Simulink® based controller. The VCE architecture discussed by Corbett is very similar to the architecture used by Simmons. Corbett’s thesis then described the modeling of the various components that make up the NPSS VCE model. It also discussed the methods of integration used by NPSS. Additionally, the physics, benefits, and challenges associated with flow holding were discussed. Development of a simplified controller capable of flow holding was also discussed. This controller was mainly based on tabular data generated at multiple steady-state operating points. The details of this control method are presented in detail in Corbett’s thesis. The details will not be presented here because a different control method is used.

Corbett also described the methods used for validation of the model. Results that were obtained showed that the engine model and controller behaved reasonably. The engine model was run over three generic missions. Results showed that the amount of bypassed air was a greater portion of the total engine airflow at reduced thrust settings. Results also showed a correlation between the pressure drop in the third stream duct HX and the corrected mass flow rate through the HX. Additionally, it was found that rejecting heat to
the third stream can require a substantial amount of power in some flight conditions owing to the need to elevate the waste energy to a higher temperature by means of a refrigeration cycle.

1.2.3. Eastbourn

In 2012, a Master’s thesis concerning the dynamic modeling and simulation of a turbofan engine was submitted by Scott Eastbourn at Wright State University. The thesis discussed the primary motivation for the development of a computationally efficient, dynamic model of a mixed-flow turbofan engine model based in Simulink®. This engine model was created for use in a vehicle-level model of power and thermal management subsystems known as a T2T model. This engine model took advantage of volume and shaft inertia dynamics to reduce algebraic constraints and increase fidelity.

The modeling of the components that make up this Simulink® turbofan model were discussed in detail. Comparisons of the new engine components against previously modeled components were performed. Results showed reasonable agreement. Comparisons between the full engine models showed considerable differences in the amount of thrust produced over a mission. Integration of the turbofan engine model with a T2T model was discussed. The average error of the critical parameters of this study were found to be acceptable. Additionally, it was found that the new turbofan engine model significantly reduced simulation times for the T2T model, therefore accomplishing the goal of the research. A design trade study was conducted using the new turbofan engine model with the T2T model to confirm the utility gains achieved by the new engine model. While the results of the trade studies have little relevance to the three-stream VCE modeling efforts, they do show the effectiveness of the modeling techniques used for the
turbofan model at reducing simulation times. Components and techniques from the turbofan engine model developed by Eastbourn will be used to develop the dynamic three-stream VCE.

2. METHODOLOGY

2.1. Additions to the Two-Stream Turbofan Model

The turbofan engine model developed by Eastbourn and Roberts was used as a basis for the VCE model for several reasons. The first reason being that the turbofan engine model uses subsystems to model and organize the various engine components. Figure 3 shows the subsystems modeled in Simulink® as they existed in the turbofan engine model. In this model, the pressures feed backwards from the nozzle to the components that exist before the nozzle in the cycle. This method is also used for the new three-stream VCE model.

The turbofan engine model also uses dynamic effects to reduce algebraic constraints and modeling time, which is the second and most important reason for using the turbofan model as the basis for the VCE model. These effects include shaft inertial dynamics and volume dynamics. Shaft inertial dynamics take into account the power being produced by the turbine that powers the shaft and the power consumed by any compressive or power take-off devices to determine the operating speed of the shaft at any point in time during a simulation. This is accomplished by means of Eq. 1.
\[\text{RPM}_{\text{shaft}} = \frac{30}{\pi} \int \left( \frac{\text{RPM}_{\text{shaft}}}{\text{RPM}_{\text{design}}} \right)^2 \left( \text{Friction Loss} \right) + \sum_{i=1}^{n} \text{Load}_i \right) \frac{l_{\text{shaft}} \times \omega_{\text{shaft}}}{dt}\]  

Where \(l_{\text{shaft}}\) is the combined moment of inertia of the shaft and all of the components (i.e. turbines, compressors, etc.) connected to the shaft. \(\omega_{\text{shaft}}\) is the shaft speed in radians per second. The term \(\sum_{i=1}^{n} \text{Load}_i\) is the summation of all of the loads placed on the shaft where power delivered to the shaft is considered positive and power extracted from the shaft is considered negative. \(\text{RPM}_{\text{shaft}}\) is the operating speed of the shaft in revolutions per minute and \(\text{RPM}_{\text{design}}\) is the design speed of the shaft in revolutions per minute. The \text{Friction Loss} is a power loss term defined at the design speed of the shaft and is corrected for the actual shaft speed by the square of the ratio of the actual shaft speed to the design speed.
speed to the design shaft speed. Note that the integral is multiplied by a conversion factor to obtain the shaft speed in revolutions per minute.

Volume dynamics are accounted for by means of Eq. 2. This equation calculates the static pressure, \( p \), from the ideal gas law and the difference in mass flows into, and out of, the control volume in question. It should be noted that \( R \) is the ideal gas constant for the gas in the control volume and is dependent on the molecular composition of the gas.

\[
p = \int \frac{(\dot{m}_{in} - \dot{m}_{out}) \times R \times T}{V} \, dt
\]  

(2)

The advantages of using Eqs. 1 and 2 are two-fold. First, they allow for the investigation of transient effects within the engine. Second, they remove algebraic constraints and the necessity for the solver to iterate for load balance on each shaft and mass conservation in each control volume. Instead, a non-zero net loading on a shaft will result in a change in shaft speeds which will dynamically drive the net loading to zero. Instead of the solver needing to iterate to achieve mass flow balance for every control volume in the engine, a mass flow difference in a control volume will result in a change in static pressure in that control volume which will dynamically drive the mass flow difference to zero. This has the overall effect of reducing the computational burden of the engine model. Taking advantage of these existing components and methods greatly simplified the process of developing the new VCE model.

The turbofan engine model did not initially have all of the components necessary to construct the VCE model. The missing components included an afterburner, a variable geometry nozzle, and the controls necessary to operate these components effectively and automatically. Rather than constructing the VCE model and adding these components
afterwards, a decision was made to use a new version of the turbofan engine model as a
testbed for the development of the new components.

2.1.1. Afterburner

The first modification made to the turbofan engine model was the addition of the
afterburner subsystem. In order to add the afterburner subsystem, a preliminary
modification was required. As was shown in Fig. 3, both the flows from the exit of the
bypass duct and the Low Pressure Turbine (LPT) enter the nozzle subsystem. Both flows
are mixed in the nozzle subsystem with the relevant components shown in Fig. 4.

![Figure 4. Relevant portion of original nozzle subsystem](image)

As Fig. 4 shows, the flows entering the nozzle are mixed in a subsystem known as the
mixer volume. The afterburner subsystem required that the bypass and LPT exit flows be
mixed before entering the afterburner. To accomplish this, the mixer volume subsystem
was moved out of the nozzle subsystem and into its own subsystem placed before the
The mixer volume subsystem simply combines the two flows and their enthalpies to output the exit temperature and composition of the mixture. With this preliminary modification completed, the afterburner subsystem was created and inserted as shown in Fig. 5.

![Figure 5. Engine model subsystems with afterburner](image)

The afterburner subsystem was created using the framework of the combustor subsystem. It is first necessary to understand how the engine model tracks the molecular composition of gasses in order to understand how the combustor subsystem, and thus the afterburner subsystem, functions. The methods used by Eastbourn to develop the turbofan model were retained for this purpose. The gas flows are arranged as vectors containing a molar flow rate (N), the molar composition of the flow as mole fractions (X), and the temperature of the flow (T). This vector, called an NXT vector, is summarized in Table 1. Note that the JP-8 equivalent was found by Eastbourn by analyzing the typical
hydrocarbon composition of JP-8 and using a weighted average based on mole fractions and molecular formulas.\(^5\)

<table>
<thead>
<tr>
<th>Component</th>
<th>Vector Index</th>
<th>Name</th>
<th>Chemical Formula</th>
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<tr>
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<td>2</td>
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<td>C(<em>{10.3})H(</em>{20.5})</td>
<td></td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>Carbon Monoxide</td>
<td>CO</td>
<td></td>
</tr>
<tr>
<td></td>
<td>4</td>
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<td>CO(_2)</td>
<td></td>
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<tr>
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<td>5</td>
<td>Hydrogen</td>
<td>H(_2)</td>
<td></td>
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<tr>
<td></td>
<td>6</td>
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<td>H(_2)O</td>
<td></td>
</tr>
<tr>
<td></td>
<td>7</td>
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<td>N(_2)</td>
<td></td>
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<tr>
<td></td>
<td>8</td>
<td>Oxygen</td>
<td>O(_2)</td>
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<tr>
<td>T</td>
<td>9</td>
<td>Temperature</td>
<td>N/A</td>
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</tr>
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</table>

The NXT vector forms the basis of the combustor’s operation. Two flows enter the combustor. The first of these flows is the air flow from the High Pressure Compressor (HPC) which, for dry air, is assumed to contain only nitrogen and oxygen. The second of these flows is the fuel flow which contains only the JP-8 equivalent. The enthalpy of each flow is calculated based on the molar enthalpies of each component and the molar flow rate of each component. The combustion process is modeled as a chemical reaction between the JP-8 equivalent and oxygen which results in the production of carbon dioxide and water vapor according to Eq. 3. It should be noted that the reaction is assumed to always go to 100% completion. To prevent the combustor from burning more JP-8 than there is oxygen to support, an oxygen checking method was applied. The method used involves setting the upper limit of fuel burned to the product of the mole fraction between the JP-8 equivalent and oxygen for a stoichiometric reaction and the molar amount of oxygen passing through the combustor. If more fuel is supplied to the burner than this limit, the excess fuel simply passes through unchanged. It should be
noted that the engine controller should never allow the fuel flow rate to meet or exceed the stoichiometric value because this would result in a violation of the maximum turbine inlet temperature. The oxygen check remains in place only as a safeguard for the main combustor. The combustor then calculates the heat of combustion for the reaction using the heat of formation of each reactant and product.

\[ C_{10.3}H_{20.5} + 15.425O_2 \rightarrow 10.3CO_2 + 10.25H_2O \]  

(3)

The enthalpy of the products leaving the combustor are calculated in the same method as the reactants, however, the exit temperature is known only from an initial condition at the first time step of the simulation. An energy balance on the reactants, products, and heat of combustion results in the calculation of a new exit temperature which feeds back to calculate the enthalpy of the products for the next time step of the simulation. This does not result in an algebraic loop because the temperature is an integrated quantity. The inlet total pressure of the combustor is calculated by multiplying the exit total pressure simple ratio between the inlet and exit to simulate a total pressure loss due to friction, drag from friction, and heat addition.

The afterburner subsystem applies all of the methods used for the combustor with additional logic. In order to avoid problems with integrators when the afterburner is not fueled (dry operation), it was necessary to use an if-then-else case to switch between fueled (wet operation) and dry operation. When the afterburner is fueled, the afterburner uses the same combustion calculations as the combustor. When the afterburner is not fueled, the subsystem simply calculates the inlet total pressure from the exit total pressure.
and a pressure drop ratio. The pressure drop is kept in place because there will still be drag due to the flame holders in the afterburner.

2.1.2. Variable Geometry Exhaust Nozzle and Controller

In order to avoid an unsustainable mode of operation in the turbofan engine model known as a fan surge condition, an additional modification to the engine was required. Surge, or stall, of a compressive device is a condition in which the device cannot support the pressure ratio it is operating at thus resulting in flow separation on some or all of the blades of a stage. This condition results in a zero thrust condition and combustor flameout.\textsuperscript{11} According to Mattingly, a variable geometry nozzle is required for afterburner operation without affecting the engine components upstream of the afterburner.\textsuperscript{12} The controls for the nozzle are usually configured such that the engine does not “know” whether or not the afterburner is in use.\textsuperscript{12}

Fortunately, the nozzle subsystem of the engine model already contained a means for varying the nozzle throat area, which is the primary means of controlling the backpressure throughout the engine when flow in the nozzle is choked. Because the afterburner is only to be active in high-thrust situations, the flow through the nozzle throat is choked at the transition between wet and dry operation, therefore, the variable nozzle throat area is sufficient to control the backpressure throughout the engine. A simple addition of a proportional-integral (PI) controller was all that was required in order to make use of the variable nozzle throat area. Research on such controllers showed that using the fan surge margin as the controlling factor for the nozzle throat area was a common approach.\textsuperscript{13} There is room for improvement with this controller because maintaining a constant surge margin in the fan may not lead to the highest overall
efficiency for the engine. It is possible that maintaining a surge margin above a safe minimum value by using a scheduled nozzle throat area could result in higher efficiencies.

2.1.3. Turbofan Model Fuel Controller

To make use of the afterburner in a mission, an automated fuel controller was required. This fuel controller must control both the main burner and afterburner fuel flow rates without exceeding realistic engine thermal and mechanical limits. To understand the engine fuel controller, it is first necessary to understand the boundary conditions for the engine. These include the ambient temperatures and pressures (resulting from operation at a given altitude and Mach number) as well as a thrust demand. These variables can, and typically do, change dynamically throughout a simulation. The thrust demand can be set by a vehicle drag-polar model which calculates the thrust demand based on altitude, Mach number, vehicle weight, and the maneuvers being performed by the aircraft (i.e. climb, acceleration, cruise, etc.). The engine controller must react dynamically to thrust demands and changes in engine operating conditions including power take-off.

To create the controller for the new afterburning turbofan engine model, the controller from the original turbofan engine model was used as a starting point. This controller uses a cascade of PI controllers to control the fuel flow to the main burner as illustrated by Fig. 6.

The first PI controller in the cascade compares the thrust demanded by the air vehicle drag-polar model to the thrust being produced by the engine model. This comparison is used to set the low pressure (LP) shaft speed set point. The LP shaft speed set point is
then compared to the LP shaft speed of the engine model in the second PI controller. This second controller is used to set the turbine total inlet temperature ($T_{t4}$) set point. This set point is compared to the actual turbine total inlet temperature in the third PI controller. This third controller is used to set the fuel flow rate to the main burner.

![Diagram of the original turbofan fuel controller](image)

**Figure 6. Graphical depiction of the original turbofan fuel controller**

The original turbofan engine fuel controller required extensive modifications to function with the new afterburning turbofan engine model. The first of these modifications was the creation of a logic controller to determine when afterburner usage is necessary. This was accomplished using Stateflow®, a Simulink® add-on, to handle the logical decision making for the controller. Stateflow®, in essence, is a tool for modeling and simulation of decision logic using state machines and flow charts.\textsuperscript{14} An example of the Stateflow® chart used for the afterburning turbofan engine is shown in Fig. 7.
In Fig. 7, a total of four individual states of operation are indicated. The first state, labeled “Afterburner_Off,” is the default state. The purpose of this state is to prevent the afterburner from activating for a set amount of time at the beginning of the simulation so that it does not become active during start-up transients. This prevents instabilities in the model. Once the set start-up time has passed, the state transitions from the default state to the “Afterburner_Armed” state. It should be noted that the controller does not enter the default state again until the simulation is restarted. The “Afterburner_Armed” state represents the case where the afterburner can become, but is not currently, active. The controller remains in this state until one or more of the main burner PI controllers reaches a set percentage of their saturation limit, which causes the value of “AB_Trigger” to become greater than zero. At this point, the engine is delivering what is considered its maximum safe dry thrust for the current flight conditions and the controller transitions to the state labeled “Afterburner_On”. In this state, the afterburner fuel controller becomes active and the LP shaft speed set point is locked to a pre-set percentage of its maximum

Figure 7. Afterburner logic controller Stateflow® chart
value. Locking the LP shaft speed set point is done to avoid controlling two independent variables that directly affect the thrust produced by the aircraft based on a single parameter (i.e. required thrust). This prevents control interference that would otherwise cause serious instabilities in the model.

Once the required thrust decreases to a point where afterburner operation is no longer required, and the minimum “timeAB” value has been reached or exceeded, the fourth state, labeled “Afterburner_Lockout”, is entered. The “timeAB” value is equal to the amount of time the afterburner controller has been active. This minimum afterburner time is utilized to ensure that the afterburner controller is able to set the fuel demand to a value greater than the lockout value for state transition when the afterburner is required. The “Afterburner_Lockout” state was created to prevent rapid oscillations between armed and active states at the transition point between wet and dry operation. After a preset amount of time, the controller returns to the armed state and is once again allowed to activate the afterburner if the need arises.

The afterburner is not a simple on-off device. It is for this reason that it is not sufficient to know when the afterburner is required; rather the afterburner logic controller causes the activation of a cascade of PI controllers to regulate fuel flow to the afterburner when it is required. The afterburner controller cascade consists of two PI controllers as illustrated in Fig. 8. The first PI controller sets the nozzle inlet total temperature ($T_{t6}$) set point based on the difference between the thrust demand and the thrust produced by the engine model. The second PI controller sets the fuel flow rate to the afterburner based on the difference between the nozzle inlet total temperature set point and the actual nozzle inlet
total temperature. This control scheme permits imposing realistic limits on both the nozzle inlet temperature and the fuel flow rate to the afterburner.

![Figure 8. Afterburner fuel controller cascade](image)

### 2.2. Development of the Three-Stream VCE

#### 2.2.1. Architecture and Benefits

With the development of the afterburning turbofan engine model completed, focus shifted to the development of the three-stream VCE model. The VCE architecture modeled is depicted with flow station numbering in Fig. 9. Note that the yellow stars in Fig. 9 indicate the engine components that are variable geometry features. It should also be noted that flow moves from left to right in the engine diagram and flow stations 0 and 1 are reserved for a supersonic inlet. Table 2 shows a summary of the components and flow station numbers into and out of the components.
To summarize the operation of the three-stream VCE, it is useful to think in terms of a mixed-flow turbofan wrapped in a shell consisting of a fan, an additional bypass duct, and an additional nozzle. Flow enters into the fan at flow station 2 where it is compressed and split between the Low Pressure Compressor (LPC) inlet and the third stream bypass duct at flow station 2.2. The third stream air is then expanded through an independent third stream nozzle. The remainder of the engine, referred to as the core, operates in essentially the same manner as a military mixed-flow, afterburning turbofan engine. Using the engine’s variable geometry nozzles and turbomachinery, flow can be modulated between the core turbofan and third streams. This variable cycle capability is necessary to enable what is known as flow holding.

Figure 9. VCE architecture
Flow holding is a means of reducing the spillage drag incurred by an inlet operating at less than its design mass flow rate for the given flight conditions. This occurs mainly in the case where the engine is operating in reduced power conditions. At the maximum dry thrust design point, the engine requires its maximum airflow and the inlet is typically sized for this condition. Figure 10 illustrates the external airflow for a typical engine installation at the maximum dry thrust design point. Note in Fig. 10 that the thrust specific fuel consumption (TSFC), or just specific fuel consumption (SFC), is a measure of the engine’s fuel efficiency and is defined as the fuel mass flow rate, \( \dot{m}_f \), divided by the thrust produced by the engine, \( F \), as in Eq. 4.\(^{11,15}\)

\[
TSFC = SFC = \frac{\dot{m}_f}{F}
\]  

(4)

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<tr>
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<td>2.2 / 2.5, 7</td>
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<td>3 / 4</td>
<td>No</td>
</tr>
<tr>
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</tr>
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<td>10.5 / 11</td>
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</tr>
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*Component modeled such that variable geometry can be implemented at a later date
When the engine is throttled back to a lower thrust setting at the same flight conditions (i.e. same altitude and Mach number), the mass flow required by the engine decreases as illustrated by Fig. 11. The inlet capture area does not change and so the same amount of air is being slowed by the inlet. Not all of this air can be ingested by the engine in typical fixed-cycle designs. This extra airflow, shown by the red lines in Fig. 11, is termed spillage. This flow spillage is capable of producing significant drag, thus contributing to a higher installed TSFC at reduced thrust settings. It is important to note that spillage drag is not captured in the uninstalled engine thrust rating and is considered an installation effect. The reduction of this effect is the primary motivation for flow holding.

Flow holding is a means of reducing spillage drag by matching the inlet captured mass flow to the mass flow rate consumed by the engine. The VCE makes this possible by
controlling the amount of flow that is sent through the third stream bypass duct and nozzle. The flow through the third stream is at a minimum for a given flight condition when the engine is producing its maximum dry thrust. It is increased significantly in reduced thrust settings to maintain the maximum mass flow rate through the fan and minimize spillage drag. Note that no flow modulating valves are present in the engine architecture presented in Fig 9. Instead, flow through the third stream is controlled by the variable geometry third stream nozzle and the variable inlet guide vanes (VIGVs) of the variable geometry turbomachinery components. It is important to note that decreases in component efficiencies at extremely low thrust settings and high third stream flow can diminish the efficiency gains obtained by flow holding.9

2.2.2. Variable Geometry Turbomachinery

The original turbofan engine and afterburning turbofan engine models made use of fixed geometry turbomachinery maps modeled using 2D lookup tables. Due to the source of these maps, similar variable geometry maps could not easily be generated. For this reason, maps were taken from the non-proprietary Alternate Generic Adaptive Turbine Engine (AGATE) NPSS model. The AGATE model is a generic three-stream engine model that makes use of variable geometry turbomachinery maps. It is a modified version of the VCE used by Corbett in 2011.16

Turbomachinery performance maps are typically used in steady-state or quasi-steady-state analyses. Two types of turbomachinery maps are required for an engine model; compressors and turbines. Compressor maps typically make use of an operational line, also known as an R-line or β-line, to resolve points where a given corrected pressure ratio
and corrected shaft speed can result in two possible corrected mass flow rates through the turbomachine. An example of this can be observed in Fig. 12.

Figure 12. Example of a typical compressor map with R-lines

Note how the speed lines hook back downwards near the surge line as the corrected airflow decreases. With this method, the R-line that coincides with surge is typically assigned a value of 1.0 with increasing R-line values corresponding to increasing surge margins. The R-line analysis method typically requires iterative solution procedures. In the case of a variable geometry component, such as the ones used in the AGATE model, an additional variable is required. In this case, multiple maps such as the one shown in Fig. 12 are “stacked”, one for each value of the extra variable.
Using the R-line method would drastically increase the computational time required to simulate a mission. This method would render the engine model unacceptable for use in a T2T model, since it must run faster than real-time to be considered practical for quick trade studies. To meet computationally efficiency requirements, the R-line analysis method had to be abandoned in favor of a slightly less accurate method. In order to accomplish this, the performance maps needed to be converted so that the mass flow rate and efficiency outputs were functions of only the corrected component pressure ratio and corrected shaft speed. In the case of variable geometry turbomachinery, the mass flow rate and efficiency are also functions of the VIGV angle.

In order to make the engine model more flexible for future modification, research was accomplished to automate the map conversion process so that users could insert their own turbomachinery maps into the model with relative ease. This resulted in the creation of MATLAB® scripts for compressor and turbine map conversion. These scripts not only convert the maps, but also add additional resolution using interpolation in order to increase the stability of the simulation. It should be noted that, in order to dispense with the usage of R-lines, it was necessary to substantially change the performance maps near the surge line by eliminating the hook-back. This is a reasonable compromise because the engine will always be required to run with a considerable surge margin (typically 12% or greater) for the simulation to be considered valid. Details of the conversion process are presented in Appendix A.

Simply having variable geometry turbomachinery maps without R-lines is not enough for a successful engine model. The new maps require the implementation of 3D lookup tables in the component models in which they are to be used. This change
required substantial modifications to the fan, compressor, and turbine component models. The first modification changed the 2D lookup tables used for the maps in the turbofan engine model to 3D maps and added an additional input to the turbomachinery models for the VIGV angles. Additionally, a new equation for the normalized pressure ratio, \( Pr_n \), was implemented as presented by Eq. 5. This new equation ensures that the normalized pressure ratios range from zero at a pressure ratio, \( Pr \), of unity to a value of 1.0 at the design pressure ratio, \( Pr_d \). It should be noted that \( Pr_n \) can exceed a value of unity when \( Pr \) is greater than \( Pr_d \). While Eq. 4 is used for both compressive components (i.e. fan, LPC, HPC) and turbine components, the calculation of \( Pr \) is different. For a compressive component, \( Pr \) is the ratio of the exit total pressure to the inlet total pressure of the component. For a turbine, \( Pr \) is the ratio of the inlet total pressure to the exit total pressure. This difference stems from the fundamental differences in the operation of the devices.

\[
Pr_n = \frac{Pr - 1}{Pr_d - 1}
\]  

\textbf{2.2.3. Engine Component Sizing}

In order to use the VCE model, the components had to be sized such that the cycle was viable and the model would be stable. For a three-stream VCE, this can be a time consuming process. This is because of the large number of variables involved. These variables include turbomachinery design pressure ratios, design mass flow rates, nozzle areas, duct areas, and others. Sizing the VCE model is inherently an iterative process and is currently not automated for the Simulink\textsuperscript{®}-based model. To expedite the process, one can use cycle data from the design point of a real VCE or an NPSS model of a VCE with
similar architecture to the Simulink® model. For the purposes of this research, data was obtained from an NPSS model of a VCE with similar, but not identical, architecture to the Simulink® VCE model.

The NPSS VCE model data came in the form of total temperatures, pressures, and mass flow rates through each component of the engine. This data is ideal for use in component test stands such as the LPC test stand shown in Fig. 13. While each component is different in terms of their design variables, inputs, and outputs, the general procedure for sizing them is similar. The conditions on the input side of the component are supplied to the test stand. These input conditions are a combination of the NPSS cycle data, design decisions such as design speeds of turbomachinery, and outputs from other engine components such as the NXT vector. The simulation then runs until the variables on the output side reach an equilibrium point. These values are checked against the NPSS cycle data. Design variables for the component are then altered and the simulation is run again. This process repeats until the component inputs and outputs sufficiently match the NPSS cycle data. It should be noted that, in the case of the cycle used in this research, not every component could be made to match the NPSS cycle data. This was due to a combination of the differences in the two cycle architectures and the differences in modeling techniques. Once a component was sized, it sometimes needed to be resized due to values that were passed to it from other components changing at the design point when a component upstream was changed. This was often the case for components downstream of the main burner. Even still, using the NPSS cycle data greatly accelerated the engine sizing procedure. Once sized, the components were placed in the Simulink® VCE model.
2.2.4. Heat Exchanger

The third stream flow of a three-stream VCE passes only through the engine’s fan meaning that it is substantially cooler than air flowing through other parts of the engine. This makes the third stream flow a good candidate for use as an additional heat sink for the aircraft. This potential heat sink may counter some of the increasing thermal loads on the aircraft. Additionally, this heat sink could be utilized to cool bleed air from the HPC to be used later for cooling the HPT. This would have the advantage of reducing the amount of bleed air needed or increasing the allowable turbine inlet temperature, both of which would increase overall engine efficiency. While these outcomes are tempting, one must take into account that both require the inclusion of a HX in the third stream duct. This will induce a pressure loss in the third stream duct. One must therefore weigh the
gains in thermal dissipation (or efficiency gains) with the efficiency losses associated with the HX.

Including the third stream HX required only minor changes to the VCE model. The HX model used for the three-stream VCE model had previously been implemented in the turbofan engine model. The HX used for this study, as well as previous studies with the turbofan engine model, is a dynamic model of a counter-flow, plate-fin, compact HX. This HX model is a one-dimensional model discretized along the axial flow direction. The performance and weight of this HX are determined based on user provided physical parameters.

The third stream duct is a resized copy of the second stream bypass duct of the turbofan. Implementation of the HX in the third stream duct was therefore accomplished with minimal effort because the duct was already equipped for the HX. The third stream nozzle required a simple modification to account for the energy added to the flow by the HX. This modification included re-implementation of the plenum energy subsystem (from the mixer volume subsystem) into the third stream nozzle. This subsystem essentially calculates the new total enthalpy flow after heat addition from the HX and uses this to calculate the new total temperature of the flow.

### 2.2.5. Controller

A simplified controller was developed for testing the Simulink® based VCE model. This controller did not include a means for varying the VIGV angles of the turbomachinery components and included only a primitive method for controlling the variable geometry nozzles. For these reasons, flow holding with this controller was not possible. The
controller did, however, permit the completion of the mission described in the Testing & Results section.

The simplified VCE controller consists of two parts; a fuel controller and a nozzle controller. The nozzle controller actually consists of two separate PI controllers, one for each nozzle. These PI controllers set the nozzle throat areas for their respective nozzles based on surge margins of the compressive turbomachinery components most effected by the backpressure induced by the nozzles. The third stream nozzle is therefore controlled to maintain a 12% surge margin in the fan. The core nozzle is controlled to maintain a 12% surge margin in the LPC. While these nozzle controllers are highly effective at maintaining an acceptable surge margin in the fan and LPC, it should be noted that the nozzles themselves are constrained devices and have a maximum throat area that cannot be exceeded during a simulation. These limitations mean that surge can still occur in these components if the nozzles are not properly sized for the engine.

The fuel controller used was nearly identical to the fuel controller used for the afterburning turbofan engine model with one major change. Due to the nature of flow holding, future iterations of the controller would not function properly if using the LP shaft speed in the PI cascade that is used to control fuel flow to the main burner. This problem results from maintaining a constant corrected mass flow rate through the fan while reducing thrust, which requires a constant LP shaft speed at a given altitude and Mach number. The LP shaft speed is therefore not a good indicator of engine thrust when flow holding. For this reason, the LP shaft speed portions of the fuel controller were replaced by the engine pressure ratio (EPR). In order to determine the proper flow stations to use to calculate EPR, the main burner fuel flow rate was controlled manually.
and the results were used to find the pressure ratio that best indicated the thrust being produced by the engine. The results showed that the EPR should be calculated based on the exit total pressure of the fan, $p_{t\text{ fan exit}}$, and the exit total pressure of the LPT, $p_{t\text{ LPT exit}}$, as shown by Eq. 6.

$$EPR = \frac{p_{t\text{ LPT exit}}}{p_{t\text{ fan exit}}}$$

(6)

3. Testing & Results

3.1. Two-Stream Afterburning Turbofan

The afterburning turbofan and its controller were tested in isolation from other air vehicle systems for two cases. This was done by placing the engine model and controller model in a Simulink® testbed model which takes mission profile data, thrust demands, and shaft power take-off demands and feeds them to the engine model and controller. The first case was a baseline case to determine how the engine model would perform without external influence (i.e. without any form of power take-off or customer bleeds). The second case included substantial power take-off in the form of mechanical power extracted from the LP and high pressure (HP) shafts. Both cases are important to this research because they will provide insight into what effects power take-off can be expected to have on the three-stream VCE model before actually testing the VCE model. Ensuring that the afterburning turbofan engine model functions properly before the creation of the VCE model reduces the risk of encountering unexpected problems with the new model.

For both cases, the afterburning turbofan engine model was run for the notional mission profile and thrust demand profile depicted by Fig. 14. The mission profile consists of the
altitudes and Mach numbers the engine will operate at as functions of time. The thrust profile was generated using an air vehicle drag-polar model. The mission profile dictates the temperatures, pressures, and ambient velocities that make up the boundary conditions for the engine model. The thrust demands are sent to the engine fuel controller.

Figure 14. Mission profile and thrust profile for the afterburning turbofan

For the test case with shaft power take-off, an arbitrary shaft loading schedule was developed. A graphical representation of the shaft loading schedule is shown in Fig. 15. The power take-off schedule shown in Fig. 15 requires a maximum of 500 kW from each shaft at two points in the mission. Changes in power-take off occur in two or five second rise/fall times depending upon the magnitude of the change. The zoomed portion of Fig. 15 shows the largest loading change during the course of the mission and illustrates the
five second rise time associated with such a large change. It should be noted that this shaft loading schedule is purely notional and is not based off of any existing or planned system architecture.

The results for the baseline case were very much as expected. Figure 16 shows the basic performance graphs for the baseline case. The thrust demands for this case were met reasonably well throughout the mission. The spikes present in the thrust produced by the engine are likely the result of sub-optimal controller tuning in the nozzle controller. Evidence for this claim is presented later. While the controller tuning may not be optimal, the general trends of normal operation are still seen in Fig. 16. The LP and HP shaft speeds stay within a reasonable range. It should be noted that the spikes in SFC occur where the engine is producing very low thrust. This behavior is expected given the definition of SFC. The baseline case uses a total of 25,569 lbm of fuel for the entire mission.

Figure 15. Shaft power take-off schedule

The results for the baseline case were very much as expected. Figure 16 shows the basic performance graphs for the baseline case. The thrust demands for this case were met reasonably well throughout the mission. The spikes present in the thrust produced by the engine are likely the result of sub-optimal controller tuning in the nozzle controller. Evidence for this claim is presented later. While the controller tuning may not be optimal, the general trends of normal operation are still seen in Fig. 16. The LP and HP shaft speeds stay within a reasonable range. It should be noted that the spikes in SFC occur where the engine is producing very low thrust. This behavior is expected given the definition of SFC. The baseline case uses a total of 25,569 lbm of fuel for the entire mission.
Figure 16. Baseline afterburning turbofan performance graphs

Figure 17 shows the fuel breakdown between the main burner and the afterburner as well as the nozzle throat area as a fraction of its maximum permissible value. Figure 17 shows that the afterburner is only active for a short time during the mission where high thrust is required. This behavior is expected and proves that the afterburner controller logic functions properly. The nozzle throat area was saturated so as not to go below a minimum value of 0.4 times the maximum permissible value and to not exceed the maximum permissible value. Evidence of the minimum value can be clearly observed from Fig. 17. Spikes and abrupt changes in the nozzle throat area can also be observed. These spikes correlate well with the spikes in thrust produced by the engine model. Due to the strong coupling between conditions in a jet engine nozzle and thrust production, it is not surprising that these rapid transients would result in thrust spikes. This points to the
possibility of sub-optimal controller tuning in the nozzle throat area controller. Additionally, it should be noted that the nozzle controller’s demand is met instantaneously by the nozzle because no actuator dynamics are modeled. Adding actuator dynamics may smooth the nozzle throat area actuation and reduce the spikes in thrust produced by the engine.

![Figure 18](image)

**Figure 17. Baseline afterburning turbofan fuel breakdown and nozzle throat area**

Figure 18 shows the fan and compressor maps for the baseline case. The blue and black lines that run nearly vertically are the speed lines, and the red line is the surge line. The red circle indicates the location on the map that the turbomachine is operating at the end of the simulation. The black line that runs mostly parallel to the surge line is the path the turbomachine takes on its map throughout the simulation. From these maps, it can be observed that an adequate surge margin is maintained for both the fan and the compressor.
for the entire mission indicating that the turbofan engine controller is effective at controlling the engine when there is no power take-off.

Applying the power take-off schedule as given by Fig. 15 and running the turbofan engine model over the same mission profile with the same thrust demands as the baseline case resulted in the performance graphs shown in Fig. 19. It is interesting to note that power take-off has only a minimal impact on engine performance. In fact, the addition of power take-off reduces the severity of the spikes in thrust production. This translates to a reduction in the severity of spikes in SFC. The total fuel consumption for the mission with power take-off is 25,764 lbm, a 194 lbm increase over the baseline case. While this does not seem like a significant amount, it is important to consider the typical heating

Figure 18. Baseline afterburning turbofan fan and compressor maps
value of JP-8 which is approximately 18,600 Btu/lbm (10,333 cal/g).\textsuperscript{12} The 194 lbm increase in fuel consumption actually constitutes more energy added to the engine than is consumed by the power take-off from both shafts combined. This is to be expected because the engine model incorporates energy losses due to inefficiencies.

\textbf{Figure 19. Afterburning turbofan performance with power take-off}

The reduction in the severity of the thrust spikes in Fig. 19 can be explained by a reduction in the severity of spikes in the nozzle throat area. Figure 20 shows the nozzle throat area graph for the case with power take-off. Note that the abrupt changes in the nozzle throat area have overshoots that are less severe than the baseline case, though they are not completely eliminated. This may be a result of the shaft loading reducing the rate at which the shaft accelerates which may reduce the rate of the transients the nozzle
controller must account for. The remainder of the graphs for the case with power take-off are visually identical to the baseline case and are not shown here.

![Graph of Nozzle Throat Area](image.png)

Figure 20. Afterburning turbofan nozzle throat area with power take-off

### 3.2. Three-Stream VCE

The dynamic three-stream VCE model and controller were tested in a manner consistent with the conditions it is expected to face when running with other aircraft system models. These are a mission with a wide range of flight conditions and thrust demands, large transients in mechanical power take-off, and transients in third-stream heat rejection. This testing was done with the VCE model and controller isolated from other air vehicle systems in a manner similar to that used to test the afterburning turbofan model. A total of five cases were tested. Every case used a mission profile identical to that used in testing the afterburning turbofan.

The thrust demands used for testing the VCE model are shown graphically by Fig. 21. While similar to the thrust demands used for the turbofan model, the thrust demands for the VCE model are slightly different. Early testing found that the VCE model became unstable in low thrust conditions. It was found that manual manipulation of the VIGVs could extend the model’s stability to lower thrust settings, but doing so would be
impractical for testing purposes. Development of an advanced controller to properly, and automatically, control the VIGV angles had not yet been completed. For this reason, the thrust demands used for testing the VCE model were simply scaled and shifted so as to avoid regions of instability. These thrust demands are used for every case.

![Thrust vs. Time](image)

**Figure 21. Thrust demands for VCE model testing**

The first case was a baseline case with no power take-off and the HX removed from the third stream bypass duct. The HX was not present in the duct because, regardless of the presence of any heat rejection to the third stream, it would cause a pressure drop in the third stream duct which would change the performance of the engine and skew the baseline results. The second case used the same mission profile and thrust demands as the baseline case, but also included the power take-off profile used for the afterburning turbofan. The HX was still not present in this case so that the effects of power take-off on the VCE model could be isolated from the effects of the HX.

The third and fourth cases both included the HX and investigated heat rejection to the third stream without shaft power take-off. The addition of the HX to the third stream required minor re-tuning of the engine controller to compensate for the pressure drop. The third case ran a constant mass flow rate through the hot side of the HX. The hot-side
air flow was maintained at a constant temperature of 440.3 °F, a constant pressure of 14.7 psia, and a constant flow rate of 22.04 lbm/s. The fourth case ran a sinusoidal mass flow rate through the hot side of the HX with an amplitude of 13.23 lbm/s, a bias of +17.64 lbm/s, and a frequency of 0.002597 Hz. The fourth case used the same constant hot-side temperature and pressure as the third case.

The fifth case combined power take-off and third stream heat rejection in one simulation. Specifically, this case combined the power take-off schedule used for the afterburning turbofan with the sinusoidal hot-side mass flow rate of the fourth case of VCE testing.

The results for the baseline case for the VCE were mostly as expected. Figure 22 shows the basic performance graphs for the baseline case. The thrust demands were met within reason showing no excessive departures from the demand. A slight exception occurs just before the 600 second point where the engine is not quite able to supply the demanded thrust. This occurs due to limitations imposed on the controller to avoid running out of oxygen to burn in the afterburner. This limit may have been reached due to a potentially sub-optimal limitation of the EPR in the main burner fuel controller preventing the maximum contribution from the main burner when the afterburner is active. This is not considered a major problem from the perspective of this test because of the relatively short duration of the thrust deficit and the fact that checking the suitability of the engine for this mission was not the goal of this study. A small spike in the thrust can also be observed approximately 100 seconds into the mission. This spike is actually a series of intermittent spikes of negligible duration that occur from about 105 seconds into the mission to about 135 seconds into the mission. These spikes are due to sub-optimal controller tuning, evidence of which will be presented later.
Additionally, Fig. 22 shows that the LP and HP shaft speeds stay within reasonable ranges. It should be noted that the LP shaft speed correlates well with the thrust produced by the engine except for where the afterburner is active. This will not be the case when flow holding is enabled. The large increase in fuel consumption during the high thrust production early in the mission is due to the afterburner being active in that portion of the mission. Additionally, the fuel consumption graphs just before 600 seconds combined with the thrust deficit at the same time show that a fuel flow limit has been reached at that time. The total fuel used for the baseline case was 17,623 lbm. It is tempting to make a comparison between the VCE and the afterburning engine at this point. This would be a misleading comparison for two reasons. First, no effort was made to size the afterburning turbofan engine model to match the thrust class of the VCE model. Second, the thrust
demands used for the afterburning turbofan and VCE tests are significantly different. The fuel usage for the baseline case of the VCE model testing is presented here only to serve as a future point of comparison for other VCE model test cases.

Due to the nature of the three-stream VCE model, there are additional features that must be tracked beyond those of the turbofan engine model in order to get a full picture of the engine’s features and functionality. The first of these features that become more important for the VCE are the bypass ratios for both the second and third stream ducts. These bypass ratios are depicted in Fig. 23. The bypass ratio for a duct is defined here as the mass flow rate through the bypass duct divided by the mass flow rate through the gas generator portion of the engine. The presence of bleed air turbine blade cooling muddies the definition of the gas generator mass flow rate, but, for the purposes of this study, it is taken to be the mass flow rate exiting the LPT. Note that “Core Bypass” is used in Fig. 23 to refer to the second stream duct. Figure 23 shows that the third stream bypass ratio increases in low thrust conditions and decreases in high thrust conditions. The opposite trend is observed for the core bypass stream. Due to the relative magnitude of the two bypass ratios, the overall bypass ratio is dominated by the third stream and therefore increases for low thrust conditions and decreases for high thrust conditions. This is the behavior that is expected for a three-stream VCE. This result is interesting because it shows that the VCE tend towards, but does not achieve, flow holding even with a minimum of variable geometry features and a very simplistic controller. Note that flow holding cannot be achieved using the simplified controller because of the simple method of controlling the third stream nozzle and the inability to automatically manipulate the VIGV angles of the turbomachinery components.
The key to explaining these variable cycle tendencies (i.e. the changing bypass ratios) lies in the variable throat area nozzles. Figure 24 shows the nozzle throat areas for both the core and third stream as fractions of their maximum permissible values for the baseline case. Comparing the core and third stream nozzle throat areas with their respective bypass ratios shows a strong correlation between the two values. An exception can be observed in the core nozzle throat area. This occurs when the afterburner becomes active and the core nozzle must open in order to maintain sufficient surge margin in the LPC. While other researchers have reported that the variable throat area core nozzle is not necessary for flow holding, this result indicates that a variable core nozzle is necessary for proper afterburner functionality. This was an expected result.

A small spike can be observed at approximately 100 seconds into the mission in the core nozzle area graph. This spike is actually a series of small magnitude spikes that correspond precisely with the times of the spikes in the thrust graph. This is not altogether surprising because the thrust generated by the engine is strongly dependent on the conditions in the nozzles. These spikes are very likely due to improper core nozzle controller tuning. Similar to the afterburning turbofan, these spikes were not considered a
detriment to the success of this study and no effort was made to correct them. This is further justified by the fact that a new, advanced controller will need to be developed in the future to properly model the effects of all the features and internal effects associated with flow holding. This new controller may or may not use the same method to control the nozzles. Spending time fine-tuning these controllers at this stage in testing is therefore not justified.

The changes in nozzle throat areas follow naturally from attempting to maintain fan and LPC surge margins of 12%. Figure 25 shows the surge margin tracking for the fan, LPC, and HPC for the baseline case. Surge margin tracking is implemented as a visual aid here instead of displaying the operating path on the maps due in part to the nature of the variable geometry compressive devices. The maps change with the VIGV angle setting, thus rendering the original method impractical whenever these VIGV angles are changed.

![Figure 24. VCE baseline case bypass nozzle throat areas](image)

The changes in nozzle throat areas follow naturally from attempting to maintain fan and LPC surge margins of 12%. Figure 25 shows the surge margin tracking for the fan, LPC, and HPC for the baseline case. Surge margin tracking is implemented as a visual aid here instead of displaying the operating path on the maps due in part to the nature of the variable geometry compressive devices. The maps change with the VIGV angle setting, thus rendering the original method impractical whenever these VIGV angles are changed.
Additionally, presenting turbomachinery maps for multiple cases would simply take up too much space when considering that three compressive devices are used in the VCE model and that they would only be used to illustrate the surge margins.

The nozzle controllers were not able to maintain a perfect 12% surge margin in the fan and LPC, but it is clear from Fig. 25 that an adequate surge margin is maintained for both components throughout the mission. The spikes in the LPC surge margin can be attributed to the sub-optimal core nozzle controller tuning discussed earlier. The HPC surge margin shows considerable variation throughout the mission. This is not surprising because the simplified controller does not include any means to control this parameter. Even without being controlled, a reasonable surge margin in the HPC is maintained for the entire mission. It is for this reason that no effort to control the surge margin in the HPC was made. Implementation of such control would not be complicated and would require only the implementation of a simple controller for the variable bleed valve. The variable bleed valve capability was built into the HPC subsystem for the turbofan engine model for the purposes of controlling the HPC surge margin and was retained in the new variable geometry HPC subsystem.

Applying only the power take-off schedule to the VCE model for the second case resulted in performance graphs that were visually similar to those produced by the baseline case. For this reason, these graphs are not included here. This case used 242 lbm more fuel than the baseline case. Again, even though the same mission profile and power take-off schedule were used, caution should be exercised when making a comparison between the increase in fuel usage for the afterburning turbofan and the VCE. At first glance it seems that the VCE model is more sensitive to power take-off than the afterburning turbofan
model. This comparison may not tell the whole story at this stage in testing and more tests should be performed to obtain a more even comparison.

![Graphs of Fan Surge Margin, LPC Surge Margin, and HPC Surge Margin]

**Figure 25. VCE baseline case surge margin tracking**

The bypass ratios and nozzle throat area graphs for the second case with power take-off are also visually similar to the baseline case. Surge margin tracking for the fan and LPC showed only changes in the magnitude of the spikes induced by sub-optimal nozzle controller tuning. For these reasons, the bypass ratios, nozzle throat area tracking, fan surge margin tracking, and LPC surge margin tracking are not presented here. The HPC surge margin tracking is substantially different from the baseline case and is shown in Fig. 26. Comparing this to the baseline case shows substantial reductions in the power take-off case HPC surge margin where power take-off from the HP shaft is at its
maximum of 500 kW. Smaller reductions in the surge margin can be observed in the sections where 100 kW are drawn from the HP shaft. Due to the HPC surge margin not being controlled, this result follows the expected trends concerning power take-off and turbomachinery. While there are marked decreases in the HPC surge margin, the value never decreases below 8%. For this reason it was not considered necessary to implement HPC surge margin control, though such control may be desirable in the future should it become necessary to extract more power from the HP shaft.

Incorporating the HX in the third stream duct and running the constant hot-side mass flow rate for the third case gave performance results visually similar to the baseline case. For this reason, the performance graphs are not presented here. The third case used approximately 231 lbm of fuel more than the baseline case. This is only slightly less than the power take-off case and indicates the sensitivity of the VCE model to flow obstructions in the third stream duct.

The bypass ratios, nozzle throat area graphs, and surge margin tracking for all three compressive components are visually similar to the baseline case and do not warrant inclusion here. However, with the HX in place, there are new variables that require tracking. These variables are heat rejection to the third stream and pressure drop across

![HPC Surge Margin Tracking](image)

**Figure 26. VCE power take-off case HPC surge margin tracking**
the cold-side (third stream side) of the HX. These values are graphed in Fig. 27. Heat rejection to the third stream varies between 150 to nearly 250 kW over the course of the mission. Interestingly, the heat rejection to the third stream is not well correlated with the third stream bypass ratio. This is because the heat rejection to the third stream is also function of the pressure and temperature in the third stream. These vary greatly with altitude and Mach number. The total pressure loss in the third stream duct ranges from approximately 0.4 to nearly 3 psi. Similarities between the shape of the third stream bypass ratio and the pressure drop can be observed here. This is because the pressure drop through the cold side of the HX is strongly a function of the flow velocity through the HX which is strongly a function of the mass flow rate through the duct.

![Graph showing heat rejection and pressure drop](image)

**Figure 27. VCE constant hot-side mass flow HX variables**

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50
Running the VCE model for the fourth case resulted in graphs with few visible differences from the baseline case. The principle differences lie in the variables tracked for the HX as shown in Fig. 28. The heat rejection to the third stream is nearly sinusoidal in shape illustrating a strong dependence of heat transfer on the hot-side conditions. The fact that it is not perfectly sinusoidal shows that the heat transfer rate is also dependent on cold-side conditions, as was also observed from the constant hot-side mass flow case. It is interesting to note that the pressure drop for the sinusoidal hot-side mass flow case is nearly identical to the constant hot-side mass flow case. This observation indicates that the third stream mass flow rate has a much greater effect on the pressure drop than heat rejection to the third stream. In fact, at first glance it appears that the heat rejection has no effect on the pressure drop. A closer look reveals evidence to the contrary, showing that there is a sinusoidal oscillation of very small magnitude in the pressure drop. Figure 29 shows this oscillation. The sinusoidal hot-side mass flow rate case used 236 lbm of fuel more than the baseline case. The small difference in fuel consumptions between the constant hot-side and sinusoidal hot-side mass flow cases indicates that the increase in fuel consumption over the baseline case is driven by the pressure drop and not heat rejection to the third stream duct. It is important to remember, however, that the heat rejection does drive the pressure drop to a small degree.
The fifth and final case was run with both the HX and power take off. The performance graphs for this case are shown in Fig. 30. Note that the spikes that were present in the baseline case are more evident here due to a much larger amplitude. Similar observations could be made in the nozzle areas and the surge margin tracking graphs. Again, this is not considered a major problem because this will not be the final controller for the engine.

Figure 28. VCE sinusoidal hot-side mass flow HX variables

Figure 29. VCE sinusoidal hot-side mass flow third stream pressure drop zoom
model. The combined HX and power take-off case uses approximately 458 lbm more fuel than the baseline case. This large increase in fuel consumption is actually less than the sum of the increases in fuel consumption for the sinusoidal hot-side mass flow case and the power take-off case. This could be the result of changes to the controller tuning when implementing the third stream HX.

The HPC surge margin graph was visually and numerically similar to the case with only power take-off and it is not presented here. The HX graphs were visually and numerically similar to the sinusoidal hot-side mass flow case and are not presented here. An argument can be made here that the power take-off and third stream heat rejection are largely independent in the effects they cause on the engine. This conclusion is not entirely accurate. Effects of heat rejection and shaft power take-off can be observed in nearly

Figure 30. VCE combined HX and power take-off performance

The HPC surge margin graph was visually and numerically similar to the case with only power take-off and it is not presented here. The HX graphs were visually and numerically similar to the sinusoidal hot-side mass flow case and are not presented here. An argument can be made here that the power take-off and third stream heat rejection are largely independent in the effects they cause on the engine. This conclusion is not entirely accurate. Effects of heat rejection and shaft power take-off can be observed in nearly
every part of the engine’s operation. Showing the graphs for every aspect of the engine’s operation that is effected would be impractical and unnecessary, therefore only one example will be covered here.

The second case highlighted the effects that power take-off has on the HPC surge margin. While not previously investigated, heat rejection to the third stream also has a slight effect on the HPC surge margin. Evidence of this can be observed by noticing the minute oscillations in the HPC surge margin for the combined HX and power take-off case as shown in Fig. 31. These oscillations are very close in frequency to the sinusoidal heat rejection to the third stream and third stream pressure drops at the same time frame in the mission. Due to the difference in controller tuning, a direct comparison between this case and the case with only power take-off should not be made. It is logical, however, to assume that the presence of the HX in the third stream, regardless of the amount of heat transfer, would also have an effect on the HPC surge margin. Due to the dynamic nature of the engine model and the fact that every change in the engine model effects the entire model, it would be erroneous to assume that there would not be any dynamic interactions between the effects of power take-off and heat rejection to the third stream. More testing would be required to determine the full extent of these interactions, but such efforts would be beyond the scope of this research.

![Figure 31. VCE combined HX and power take-off HPC surge margin zoom](image)

Figure 31. VCE combined HX and power take-off HPC surge margin zoom
4. Conclusion & Recommendations

A previously developed dynamic, Simulink®-based model of a mixed-flow turbofan engine was modified to add an afterburner and a variable geometry nozzle. This was done as an intermediate step towards the development of a dynamic, Simulink®-based model of a three-stream VCE for use in quick power and thermal management design trade studies. Upon completion of the afterburning turbofan model, an automatic controller was developed to control fuel flow to the main burner and afterburner based on thrust demands. The afterburning turbofan model and controller were tested over a notional subsonic mission with thrust demands determined by an air vehicle drag-polar model. Two cases were performed; the first with the engine model operating with only the thrust demands and the second with a notional shaft power take-off schedule. Upon confirming the effectiveness of the new engine model components and fuel controller, focus shifted towards the development of the VCE model.

The three-stream VCE model was based on the architectures used by Simmons and Corbett. Development of the VCE model required the development of variable geometry turbomachinery components based on the fixed geometry turbomachinery components that had been used in the turbofan model. Maps for the new turbomachinery components were converted from the maps used in the AGATE NPSS VCE model to remove the need for R-lines and iterations. Engine components were sized using data from an NPSS VCE model of similar architecture. A previously developed HX model was implemented in the third stream duct to examine the effects of heat rejection to the third stream. Additionally, a simplified controller was developed based on the afterburning turbofan engine controller.
Testing of the afterburning turbofan consisted of two cases, both of which used the same mission profile and thrust demands. The first case tested the engine alone on a test stand. The second case applied shaft power take-off to the model. The results of these tests showed that the turbofan engine and controller were capable of maintaining the thrust demands within reasonable tolerance throughout the mission with and without shaft power take-off. These results also showed that the engine controller was capable of adjusting to substantial transients in power take-off from both the HP and LP shafts.

Testing of the VCE model consisted of five cases, all of which used the same mission profile and thrust demands. The first case was a baseline case with the third stream HX removed and no power take-off. The second case was the power take-off case with the third stream HX removed. The third case tested the engine with the third stream HX using a constant hot-side mass flow rate and no power take-off. The fourth case tested the engine with the third stream HX using a sinusoidal hot-side mass flow rate and no power take-off. The fifth case tested the engine for simultaneous power take-off and third stream heat rejection with a sinusoidal hot-side mass flow rate.

Testing of the VCE model revealed variable cycle tendencies even with the simplified controller and only two controlled variable geometry features. The third stream bypass ratio was shown to increase in low thrust conditions and decrease in high thrust conditions. The VCE model does not achieve true flow holding, but it does show that the bypass ratio of the engine can be controlled. Additionally, it was found that the simplified VCE controller was capable of running the engine through the notional mission without any major problems even when operating with substantial transients in power take-off and third stream heat rejection.
Testing of the VCE model with third stream heat rejection showed that the pressure drop introduced by the third stream HX varies throughout a mission based mainly on the conditions in the third stream duct with very little dependence on the amount of heat rejection. This result is in agreement with the findings of Corbett. Additionally, the dramatic increase in fuel consumption over the baseline case for the heat rejection cases reveal the sensitivity of the investigated VCE architecture to pressure drops in the third stream duct. Testing also revealed that power take-off and third stream heat rejection impact nearly every aspect of engine operation, as was expected. This result suggests that there could be dynamic interactions between the effects of power take-off and third stream heat rejection. These interactions may adversely affect engine operation and should be investigated to determine if they are present.

The work presented in this thesis should be considered as a foundation for future work. While the VCE model discussed here is functional, it requires refinement before it can be used for design trade studies. Among the refinements, a priority should be placed on the development of an advanced controller with the capability to automatically manipulate the VIGV angles in order to achieve flow holding. This controller should also be able to distinguish between conditions where flow holding is advantageous and when component efficiencies decrease to the point where flow holding is no longer advantageous.

In order to develop the new controller, it will be necessary to first develop a dynamic inlet model that is capable of calculating spillage drag. The calculation of spillage drag will enable calculation of the installed SFC of the engine which can then serve as an optimization parameter for the new controller. Due to the versatility of the three-stream VCE, this inlet model should have supersonic capability.
Overall, this research has provided a proof-of-concept for a dynamic three stream VCE model based in Simulink®. The engine model that has resulted from this research will provide an ideal platform on which to base VCE control research. Additionally, the built-in capabilities and computational efficiency of this engine model should prove to be invaluable for future design trade studies of air vehicle power and thermal management systems once a suitable controller is developed. The generic, scalable nature of the dynamic VCE model will enable collaboration between researchers in industry, academia, and government.
Appendix A

Turbomachinery Map Conversion

The nature of the Simulink® turbomachinery models used in the dynamic afterburning turbofan and VCE models typically requires that maps from outside sources be converted to simpler form. This is especially the case for compressive components such as the fan and LPC. This is because compressor maps typically require the usage of R-lines to resolve points where a compressor can have two corrected mass flow rates at one pressure ratio and corrected speed. In order to dispense with the need for iterative solution procedures, the R-lines must be removed and the map adjusted near the surge line such that there is only one possible corrected mass flow rate for a given pressure ratio and corrected speed.

Compressor Map Conversion

A MATLAB® script was developed to automate this process for a typical stacked variable geometry compressor. This MATLAB® script is also capable of remedying some of the common problems that would otherwise prevent the procedure from being completed. Additionally, this script is capable of increasing the resolution of the maps by interpolation. It should be noted that this interpolation is not meant to increase the accuracy of the maps, but is instead used to increase the computational stability of the
maps by preventing errors with look-up tables in the Simulink® turbomachinery models near surge. This will be explained in more detail.

The conversion process for a compressor map begins with loading the data from the original maps. For a 3D (variable geometry) compressor map, this data includes a matrix of pressure ratios with dimensions $m \times n \times l$. The $m$ dimension corresponds to an array of increasing R-line values. Note that the R-line values used here do not follow the convention and instead decrease with increasing surge margin. The $n$ dimension corresponds to an array of increasing corrected speeds. The $l$ dimension corresponds to an array of increasing VIGV angle settings. For a 2D (fixed geometry) compressor map, there is only one VIGV setting and $l = 1$. The data also includes a similar matrix for the corrected mass flow data with identical dimensions to the pressure ratio matrix. The dimensions of the mass flow matrix correspond to the same arrays as the pressure ratio matrix. Additionally, the data includes a matrix of efficiency values of the same dimensions as the pressure ratio and corrected mass flow ratios. A fourth set of data is also required. This data consists of two $l \times n$ matrices. The first of these matrices contains the corrected mass flow rate at surge for each VIGV angle setting and corrected speed value. The second of these matrices contains the pressure ratio at surge for each VIGV angle setting and corrected speed value. The $l$ dimension corresponds to the VIGV angle settings and the $n$ dimension corresponds to the corrected speeds to be consistent with the other matrices.

Once the data has been loaded, a pre-processing procedure is employed. This procedure first checks the map to ensure that, for each VIGV angle setting and corrected speed, the pressure ratio is a function of the R-line value. This is done by insuring that the pressure
ratios only increase as the R-line values increase. If this is not the case, a method to correct the problem is employed at every R-line value above which the pressure ratio is not a function of the R-line value. This method replaces the pressure ratio at the point where functionality breaks down with the sum of the previous pressure ratio and half the difference between the two previous pressure ratios as shown in Eq. A.1. Note that the $k$ index was used for the $m$ dimension. This method was used so that the increase in pressure ratio for an increase in R-line value decreases successively without becoming negative and breaking the functional relationship.

$$P_{r_k} = P_{r_{k-1}} + \frac{P_{r_{k-1}} - P_{r_{k-2}}}{2}$$  \hspace{1cm} (A.1)

The second portion of the pre-processing procedure creates a high resolution array of pressure ratios. This is done by first finding the minimum pressure ratio, $P_{r_{min}}$, and maximum pressure ratio, $P_{r_{max}}$, for the component pressure ratio matrix. Additionally, the interval for the array, $\Delta P_{r}$, is computed. This can be done by finding the minimum pressure ratio difference between consecutive points for a given corrected speed in the original matrix, or it can be hard-coded at a set value. It is recommended that this value not be below 0.001 to avoid creating an unreasonably large amount of data. Note that increasing the resolution does not necessarily increase accuracy due to the resolution limitations of the original map. The new pressure ratio matrix is then populated as shown in Eq. A.2. Note that the final pressure ratio in the array is 1.2 times $P_{r_{max}}$ to allow for a margin of safety in the converted maps.

$$P_{r} = [P_{r_{min}} \ P_{r_{min}} + \Delta P_{r} \ \ldots \ \ 1.2 \times P_{r_{max}}]^T$$  \hspace{1cm} (A.2)
The final portion of the pre-processing procedure is to increase the number of corrected speedlines in the map. It is worth restating here that this is done to increase the computational stability of the converted maps. The increase in resolution should not be interpreted as an increase in accuracy. The reason the computational stability depends on the number of speedlines is due to an inherent quirk in using compressor maps in the Simulink® model. This quirk is demonstrated by Fig. A.1. Figure A.1 shows a generic lookup table similar to those used for the compressor corrected mass flow rates in the Simulink® afterburning turbofan and VCE models. The red line indicates the surge line of the notional compressor in question. To save computational time, the lookup tables in the models are configured to use linear interpolation. For this reason, the interpolated corrected mass flow rate at the corrected speed of 45 (indicated by the placement of the vertical arrow) and pressure ratio of 2.005 (indicated by the placement of the horizontal arrow) is 20. However, noticing a pattern in the table, one would expect a value of 25. In an actual compressor map, the error can be even larger. To avoid this, a more advanced interpolation method is used in the pre-processing procedure so that better accuracy is obtained in the simulation while maintaining the low computational costs of linear interpolation methods.
The first step in increasing the number of speedlines is to define the desired number of speedlines (i.e. corrected speeds). This number will define the size of the lookup tables and a balance should be struck between the need for higher resolution and memory requirements when the maps are loaded prior to the simulation. The next step is to create a new array of corrected speeds ranging from the original maximum to the original minimum. The MATLAB® “linspace” command is ideal for this task because it creates a linearly spaced array with a specified number of elements ranging from the minimum specified value to the maximum specified value. The next step is to increase the \( n \) dimension of the corrected mass flow, efficiency, and pressure ratio matrices so that they match the length of the new corrected speed array. This is done using a nested loop and the MATLAB® “interp1” command. The outer loop runs over the length of the VIGV angle setting array and corresponds to the \( l \) dimension of the original matrices and the new interpolated matrices. The middle loop runs over the length of the new corrected speed array. 

![Figure A. 1. Generic lookup table with surge line](image)

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speed array and corresponds to the $n$ dimension of the new interpolated matrices. The inner loop runs over the length of the R-line value array and corresponds to the $m$ dimension of the original matrices and the new interpolated matrices. The new matrices are constructed by interpolating using the original corrected speed array as breakpoints, the original matrices as the data points, and the new corrected speed array values as the input at all indices. For the compressor maps in this research, the “pchip” method was used for all interpolations.

The map conversion process can only begin after the pre-processing procedure has been followed. This process converts the maps from their dependencies on R-line values to depending only on pressure ratios and corrected speeds. The process begins by initializing the new corrected mass flow and efficiency matrices. These matrices have dimensions $m \times n \times l$. The $m$ dimension now corresponds to the newly created pressure ratio array. The $n$ dimension corresponds to the new corrected speed array. The $l$ dimension still corresponds to the VIGV angle settings. Additionally, new surge pressure ratio and surge corrected mass flow matrices were initialized with dimensions $l \times n$.

Populating the new corrected mass flow and efficiency matrices occurred simultaneously in a nested loop. The outer loop ran over the $l$ dimension. The middle loop ran over the $n$ dimension. In the middle loop, the minimum and maximum pressure ratios are determined for each corrected shaft speed from the interpolated pressure ratio matrix. A “pchip” interpolation of the interpolated pressure ratio matrix is also performed at an R-line value just above surge for each corrected speed and VIGV setting. The result of this is taken to be the surge pressure ratio. While this does slightly shift the surge line, the converted map are not be accurate near the surge line anyway.
The inner loop runs over the $m$ dimension and populates the new corrected mass flow, efficiency, and surge mass flow matrices. In each iteration of the inner loop one of three possible cases can occur. If the pressure ratio array returns a value less than the surge pressure ratio on the current corrected speedline, the pressure ratio array value at the current indices is used to find the corresponding R-line value by interpolation of the interpolated pressure ratio matrix and the R-line array. This R-line value is used to populate the new corrected mass flow and efficiency matrices at the current indices by interpolation of the R-line array and the respective interpolated matrix.

The second case occurs when the pressure ratio array returns a value within a range of one half of $\Delta Pr$ of the surge pressure ratio. This case corresponds to values on the surge line. In this case, the new corrected mass flow and efficiency matrices are populated at the current index by interpolation of the respective interpolated matrices at the previously established surge R-line value. Additionally, the surge corrected mass flow matrix is populated by the value of the new corrected mass flow matrix at these indices.

The third and final case occurs when the pressure ratio array returns a value greater than the surge pressure at the current indices. Physically, pressure ratios above the surge line are not sustainable and cause the mass flow rate and efficiency to go to zero. To emulate this behavior, the corrected mass flow rate and efficiency values fall off sharply above the surge pressure ratio. At a fixed corrected speed and VIGV setting, the new corrected mass flow rate, $W'_c$, is calculated by Eq. A.3. Note that the $k$ index was used for the $m$ dimension. The constant scaling factor of 0.6 can be substituted for other values greater than zero and less than one if a different decay rate is desired. Similarly, Eq. A.4
calculates the new efficiency value at a fixed corrected speed and VIGV setting. This completes the conversion process for a compressor map.

\[ W_{c_k} = W_{c_{k-1}} - 0.6 \times W_{c_{k-1}} \]  
(A.3)

\[ \varepsilon_k = \varepsilon_{k-1} - 0.5 \times \varepsilon_{k-1} \]  
(A.4)

These maps will need to be scaled when used in the engine models, a process which will be made easier by normalizing the pressure ratios, mass flow rates, and efficiencies. This is accomplished by choosing a design point and then finding the values of the pressure ratio and corrected mass flow rate at that point. For the compressors used in this research, the design point was chosen to be at a corrected speed equal to 100% of the design shaft speed of the compressor. The design pressure ratio was taken to be the maximum surge pressure ratio at the design corrected speed. This was done to be consistent with the maps used in the afterburning turbofan model. For this research, the design corrected mass flow rate was taken to be a different value for each VIGV setting equal to the corrected mass flow rate at the design speed and pressure ratio. The design efficiency was taken to be the maximum efficiency value in the entire efficiency matrix.

The next step of the normalization process was to perform the actual normalization. For the pressure ratio and surge pressure ratio matrices this simply required the application of Eq. 5. For the corrected mass flow and surge mass flow matrices, this required dividing every value for each VIGV setting by the respective design value. Normalizing the efficiency matrix simply required dividing every element in the matrix by the design efficiency value.
The method used to normalize the mass flow matrix in this research is not correct because it removes some of the dependence of the mass flow rates on the VIGV setting. This error does not affect the results of this research because the VIGV settings were held constant. It does, however, mark an area for future improvement. The simplest solution to this problem would be to take the design corrected mass flow as the corrected mass flow at the design speed and the VIGV setting that corresponds to the design pressure ratio. Every element in the corrected mass flow and surge mass flow matrices would then be divided by the design corrected mass flow. Implementing this fix in the script used to convert the compressor maps would require only minimal effort. However, implementing the new maps in the VCE model would require considerable time and effort to repeat the component sizing procedure. For this reason, the change had not been implemented at the time of writing this thesis. Nevertheless, the fix will be needed in order for this research to continue in the future and should be pursued as soon as time allows.

The final step of the compressor map conversion process included correcting and extending the maps. The correction process simply ensures that the normalized mass flow is a function of the pressure ratio in the nearly vertical region of the speedlines. This is done in a nested loop with the outer loop being for the $l$ dimension, the middle loop for the $n$ dimension, and the inner loop for the $m$ dimension. The inner loop runs in the direction of decreasing pressure ratio for this process. The correction for mass flow occurs only when the normalized pressure ratio is less than the normalized surge pressure ratio and the normalized mass flow at the previous $m$ dimension index is equal to normalized mass flow at the current index. If these conditions are met, the normalized mass flow at the current index is increased by a small non-zero value. The extension of
the mass flow matrix occurs only when the normalized pressure ratio is less than the normalized surge pressure ratio and the normalized mass flow at the current $m$ dimension index is zero. When this happens, the normalized mass flow at the current $m$ dimension index is given a value that is the sum of the value at the previous index and the same small non-zero value used for the correction process. This procedure is done only to ensure simulation stability. The compressor should not enter this extended region during normal operation.

The normalized efficiency matrix is extended only if the normalized pressure ratio is less than the normalized surge pressure ratio and the normalized efficiency, $\varepsilon_n$, at the current $m$ dimension index is zero. In this case, Eq. A.5 is employed. The index $k$ indicates the current index and $k-1$ and $k-2$ are the indices of the two previous values in the inner loop. This concludes the compressor map conversion process.

$$\varepsilon_{nk} = \varepsilon_{nk-1} - 0.5 * |\varepsilon_{nk-1} - \varepsilon_{nk-2}| - 0.00001$$

(A.5)

**Turbine Map Conversion**

The turbine map conversion process is much simpler and is not necessary in all cases. Turbine maps do not have the same problems as compressor maps because there is no surge line and, typically, no R-lines are needed because the corrected mass flow rate is already a direct function of the pressure ratio and corrected speed. In some cases, it may be necessary to increase the resolution of the maps before using them in the Simulink® turbine models. Nevertheless, some turbine maps do use R-lines which need to be eliminated in order to use the maps in the Simulink® models. This is the case covered by the MATLAB® script used to convert the turbine maps.
The first step in converting the maps is to load the data into the workspace. This data includes a corrected mass flow matrix of dimensions $m \times n \times l$. The $m$ dimension corresponds to an array of increasing R-line values. Note that the R-line values used here do not follow the convention and instead decrease with increasing surge margin. The $n$ dimension corresponds to an array of increasing corrected speeds. The $l$ dimension corresponds to an array of increasing VIGV angle settings. Additionally, $l \times n$ matrices of the maximum and minimum pressure ratios for each VIGV setting and corrected speed are required.

Once the data is loaded, the conversion process begins by increasing the resolution of the R-line value array. This is done by simply using the “linspace” command to create an array of R-line values spanning from the minimum value to the maximum with a user-defined number of elements. This research used 200 elements. The resolution in the $m$ dimension of the corrected mass flow and efficiency matrices are then increased to match the new R-line array resolution. This is done by using a nested loop with the outer loop corresponding to the $l$ dimension, the middle loop corresponding to the $n$ dimension, and the inner loop corresponding to the $m$ dimension. In the inner loop, the new corrected mass flow and efficiency matrices are constructed by using the “interp1” command in “pchip” mode to interpolate using the original R-line values as the breakpoints and the original matrices as the data with the new R-line values as the inputs.

A procedure similar to that used to increase the number of speedlines in the compressor maps is used to increase the number of speedlines in the turbine maps. This procedure will not be repeated here. The conversion process began by determining the absolute maximum pressure ratio in the maximum pressure ratio matrix and the absolute minimum
pressure ratio of the minimum pressure ratio matrix. The “linspace” command is then used to create an array of pressure ratios ranging from the absolute minimum pressure ratio to the maximum pressure ratio with the same length as the new R-line array.

New for the corrected mass flow and efficiencies are initialized with the new dimensions of the R-line values and corrected speed arrays. These matrices are then populated using another nested loop. The outer loop runs over the length of the VIGV setting array, the middle loop runs over the length of the pressure ratio array, and the inner loop runs over the length of the corrected speed array. Inside the inner loop, the “interp1” command is used in the default mode to determine the R-line value for the pressure ratio at the m dimension index. This R-line value is then used to populate the new corrected mass flow and efficiency matrices at the current indices by using the R-line array as the breakpoints with the old matrices as the data and the R-line value as the input. Once this loop is complete, the conversion process is complete.

Once again, the maps used in the engine models must be scaled and normalizing them makes the scaling process simpler. A slightly different procedure is used to normalize the turbine maps when compared to the compressor maps. For the turbine maps, the design VIGV setting and R-line value were known in this research. The design speed is once again taken to be the corrected speed that corresponded to the 100% design speed of the turbine. To normalize the pressure ratios, the design pressure ratio is found using interpolation of the pressure ratio array with the R-line array as the breakpoints and the design R-line value as the input. Eq. 5 is once again employed to complete the normalization. Normalizing the corrected mass flow matrix requires finding the design corrected mass flow by interpolating at the design R-line value. Normalization of the
corrected mass flow rates requires dividing the corrected mass flow matrix by the design corrected mass flow. A similar method is used to normalize the efficiency matrix.
5. References


14 The MathWorks, Inc., *Stateflow* [webpage], [retrieved 19 May 2016].


