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STOVL Propulsion System Volume Dynamics Approximations

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STOVL PROPULSION SYSTEM VOLUME DYNAMICS APPROXIMATIONS

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ABSTRACT

Two approaches to modeling turbofan engine component volume dynamics are explored and compared with a view toward application to real-time simulation of short take-off vertical landing (STOVL) aircraft propulsion systems. The first (and most popular) approach considers only heat and mass balances; the second approach includes a momentum balance and substitutes the heat equation with a complete energy balance. Results for a practical test case are presented and discussed.

INTRODUCTION

An accurate propulsion system simulation for short take-off vertical landing (STOVL) aircraft is a critical element for research on design methodologies for integrated STOVL aircraft flight and propulsion control systems. Such a simulation must, in principle, mimic steady-state and transient sub-system performance and be executable in *real-time*. For modern real-time system analyses, it is important that the STOVL simulation include the dynamics of component feeder ducts, engine inter-component volumes, and the tailpipe.

Early engine simulations on analog computers often included intercomponent volume dynamics modules, but the intent was more often for the purpose of coupling static components than for an accurate solicitation of gas dynamics effects. As such, the traditional approach draws only on the continuity equation and a simplified form of the energy equation. Later, simulations on digital computers relaxed the "complete" form of the initial value problem by dropping intercomponent approximations and employed iterative schemes to balance the engine. The latter approach is not appropriate for simulations on modern real-time computers (such as the Applied Dynamics International AD-100 machine) and is the impetus for the present investigation. Problem formulations for real-time STOVL systems must contain accurate volume dynamics approximations.

Feeder ducts and tailpipes of STOVL engine configurations are likely to exhibit different dynamic behavior than inter-component volumes and therefore may not be accurately characterized by traditional (analog) volume dynamics representations. There is a need for a volume dynamics model with the simplicity and computational speed of the traditional approach, but with the capability to incorporate more detailed heat transfer and fluid-dynamic effects that are likely features of STOVL systems.

In the present work, two approaches to modeling volume dynamics are explored and compared with a view toward application to real-time propulsion system simulation. The first and simplest method employs the traditional approach in the problem formulation; in the second approach a momentum balance is included and the heat equation is replaced with a complete energy balance. These approaches are applied to a typical ejector feeder pipe for a proposed ejector-configured STOVL propulsion system. A third method under development is also mentioned which employs an entropy balance in place of the energy equation.

PROBLEM FORMULATION

Some traditional approaches to the transient volume dynamics problem are represented by the lumped volume work of Szuch et al.[1], the finite difference analysis of Cheng and Bowyer[2] and the control volume approach of Thompson[3].

In order to more easily accommodate multiple inlet and exit flows, the present approximations for mass, momentum, and energy balances are discussed within a control-volume framework. An integral approach allows complicated aero-thermodynamic physics to be more simply and robustly be described by a gross relation involving a *group* of particles rather than, say, a flow streamline approach.

For the intent of the present simulation, it is reasonable to proceed with a one-dimensional gas flow assumption. This sets the stage for simplified control volume equations. Consider the control-volume k shown in Fig. 1. Multiple inlets are permissable at surface i and multiple discharges at j. To apply the assumptions above, first define control-volume mass flux as

$$\dot{m} = -\int_S \rho \underline{v} \cdot \underline{n} dA \tag{1}$$

so, for example, the mass influx is

$$\dot{m}_i = +\sum_{n=1}^{N} \rho_n v_n A_n \tag{2}$$

The governing balances of mass, momentum, energy, and entropy can now be written in the form

$$\frac{d}{dt}\int_V \rho dV = \dot{m}_i - \dot{m}_j \tag{3}$$

$$\frac{d}{dt}\int_V \rho \underline{v} dV = \{v_i \dot{m}_i - v_j \dot{m}_j\} + \{p_i A_i - p_j A_j\} + \mathbf{F} \tag{4}$$

$$\frac{d}{dt}\int_V \rho\left(h_0 - \frac{p}{\rho}\right) dV = \dot{m}_i h_{0,i} - \dot{m}_j h_{0,j} \tag{5}$$

$$\frac{d}{dt}\int_V \rho s dV - \dot{m}_i s_i + \dot{m}_j s_j - \frac{\delta Q_{i,j}}{T} \geq 0 \tag{6}$$

Further simplification of the system of equations hinges on discussion regarding the choice of state variables and the treatment of the time-dependent volume integrals.

BASIC HEAT EQUATION ANALYSIS

Two particularly simple differential equations for the basic heat equation (BHE) analysis result from the application of two assumptions. The first assumption is that changes in flow kinetic energy are negligible; this reduces the first law of thermodynamics to the heat equation. A very *basic* analysis follows from the second assumption in which momentum changes are neglected. Since many mechanical effects that influence the flow are discarded, we expect the approach is most appropriate for short ducts of nearly uniform cross-section.

For a spatially uniform density approximation the volume integral in eqn.(3) can be evaluated. If it is also assumed the volume of region k is of fixed size, the governing equation for the density derivative is

$$\left(\frac{d\rho}{dt}\right)_k = \frac{\dot{m}_i - \dot{m}_j}{V_k} = \frac{\dot{m}_i - \dot{m}_j}{\bar{A}\Delta x} \tag{7}$$

where an overbar indicates a volume-average quantity.

In the absence of mechanisms to influence momentum loss, state changes arise through changes in thermal energy, not mechanical energy. For a zero momentum deficit, the pressure within a given control volume is subsequently uniform (quasi-steady), and the computation for P is simply

$$P = \rho RT \tag{8}$$

Changes in pressure are therefore a direct result of changes in density and temperature.

When the mechanical power equation (the dot product of the velocity and Navier-Stokes equation) is subtracted from the general power equation (the first law of thermodynamics, eqn.5) there results the *heat equation*; in integral form the adiabatic result is

$$\frac{d}{dt}\int_\Omega \rho e dV = -\oint_S \rho e \mathbf{v}\cdot\mathbf{n}dA - \oint_s p\mathbf{v}\cdot\mathbf{n}dA \tag{9}$$

In the ideal case of a perfectly mixed one-dimensional flow of small axial temperature gradient (and a uniform pressure throughout), an average enthalpy $\bar{h}$ can be introduced. This yields the traditional result for the temperature derivative

$$\left(\frac{dT}{dt}\right)_k = \frac{\gamma - 1}{R\dot{m}_{CV}}\{\dot{m}_i(h_i - \bar{h}) + RT(\dot{m}_i - \dot{m}_j)\} \tag{10}$$

Equations (7) and (10) constitute the differential equations for the basic heat equation analysis, and are the typical equations one might see in early analog simulations of engine inter-component volumes.

MOMENTUM/ENERGY EQUATION ANALYSIS

The momentum/energy equation (MEE) approach to the duct analysis includes a balance of momentum and replaces the simple heat equation of the BHE approach with the general energy equation.

Flow momentum changes in ducts are generally attributable to variations in cross-sectional area or the friction associated with real flows; the governing differential equation is a vector expression, but with the flux momentum nomenclature of Fig. 1 in mind (see [3] for details), there results the scalar balance

$$\frac{d}{dt}(\rho v V)_k = M_i - M_j + \hat{F} \tag{11}$$

where the scalar form of the momentum is

$$M = v\dot{m} + PA$$

In this work an idealized expression for the internal resistance of the duct shown is given by

$$\hat{F} = \overline{P}\,dA \cong \frac{1}{2}(P_i + P_j)(A_j - A_i)$$

where the absence of frictional effects should be noted. Carrying through the differentiation in time and re-arranging results in the following equation

$$\left(\frac{dv}{dt}\right)_k = \frac{1}{\bar{\rho}\bar{A}\Delta x}(M_i - M_j + \hat{F}) - \frac{\bar{v}\,d\rho}{\bar{\rho}\,dt} \tag{12}$$

The characteristic velocity $\bar{v}$ is a weighted average of the inlet and exit control volume velocities

$$\bar{v} = \xi v_i + (1-\xi)v_j$$

where ξ is a weighting parameter ranging in value from 0 to 1.

The general power equation includes representations of thermal *and* kinetic energy,

$$\frac{d}{dt}\left\{\rho\left(h + \frac{v^2}{2} - \frac{p}{\rho}\right)\right\}_k = \dot{E}_i - \dot{E}_j \tag{13}$$

where

$$\dot{E} = \rho\left(h + \frac{v^2}{2}\right)\mathbf{v}\cdot\mathbf{n}A = \dot{m}\left(h + \frac{v^2}{2}\right)$$

The time-dependent term can be rearranged to read

$$\frac{d}{dt}\left\{\rho\left(h + \frac{v^2}{2} - \frac{p}{\rho}\right)\right\}_k = \left\{\frac{1}{\gamma - 1}\frac{dp}{dt} + \bar{\rho}\bar{v}\frac{dv}{dt} + \frac{\bar{v}^2}{2}\frac{d\rho}{dt}\right\}_k$$

and from this an expression for the pressure derivative obtained

$$\frac{1}{\gamma-1}\left(\frac{dp}{dt}\right)_k = \{\dot{E}_i - \dot{E}_j\} - \left\{\bar{\rho}\bar{v}\frac{dv}{dt} + \frac{\bar{v}^2}{2}\frac{d\rho}{dt}\right\}_k \qquad (14)$$

Equations (7), (12), and (14) are the differential equations of interest in the momentum/energy equation analysis.

REMARK ON ENTROPY ANALYSIS

Traditionally, the entropy equation is employed as a check on the validity of multiple solutions (subsonic flow, supersonic flow) extracted from the general energy balance. However, except for the mathematical inconvenience of the inequality in eqn.(6), there should be no reason the entropy equation cannot replace the general form of the energy equation. An analysis is in-progress to investigate replacement of the lost-work-energy term so that

$$\bar{A}\Delta x\frac{d(\rho s)}{dt} - \dot{m}_i s_i + \dot{m}_j s_j = C_1 \geq 0 \qquad (15)$$

where C_1 is an empirical constant (that replaces the lost-work term) associated with a specific duct configuration. When a reference entropy point has been established, the ideal gas relations permit evaluation of entropy at any other desired state. Here, the proposed benefit is in condensing the otherwise scattered real flow effects (friction, heat transfer) into one constant, expediting computations for a given duct. It remains to establish whether computations for entropy at each point is faster than the quadratic solution previously required for the velocity (from the energy equation).

RESULTS

Duct system response to step-function inputs are considered in this work since these changes in duct flowrate are coincident with typical STOVL flight scenarios. In the present work, flow conditions representative of previous NASA Lewis ejector tests provide baseline data for a realistic test case. We therefore consider a step change in flowrate from 8.48 Kg/s to 9.91 Kg/s that occurs with an increase in static pressure from 106 kPa to 108 kPa and at a constant inlet temperature of 400K. A constant area duct of 0.102 m^2 is used for all calculations; also, all numerical calculations proceeded with a time step of 1 ms. Two cases presented are where the duct volume is first contained within one finite-volume and then partitioned into several finite elements.

Our first result points out a potential hazard in simplifying the time derivatives of the control-volume integrals with the "characteristic" field variables ($\bar{v}$, $\bar{\rho}$). Figure 2 illustrates predictions of exit flowrate for ducts of 1.5m and 4.6m in length. Note the absence of momentum exchange in the BHE approach essentially provides a quasi-steady duct flowrate response. The MEE predictions require twice as much computer time and seem more reasonable, but the discharge of the 1.5m duct which lags its 4.6m counterpart reflects the inaccuracy of the assumed value of the weighting scalar $\xi = 0.5$ for very long ducts; after one time step the incoming fluid has penetrated only 2% of the 4.6m duct, not the 50% implied in the calculation. When $\xi = 0.9$ the situation corrects to the shorter duct reaching a steady-state before the longer duct does.

In Fig. 3 it appears the pressure response of the BHE approach is flawed by the absence of momentum exchange; pressure changes must inadequately communicate through the heat equation. Since Fig.3 is based on $\xi = 0.5$ the trend in pressure response for the MEE approach is intuitively reversed, but, as mentioned above, corrects when $\xi = 0.9$. The cautionary note from

Figs. 2 and 3 is that although the BHE calculations appear *stable* in the analysis of very long ducts, this by no means implies the results are necessarily accurate. If, however, analytic interest is focused *only* on accurate representation of flowrate, use of the BHE approach may then be appropriate.

Figure 4 illustrates flowrate predictions with the MEE approach for a 3m long duct divided into 5 control-volumes. In this case the reduced length of the individual control volumes partially compensates for the inaccuracy of results for the assumed $\xi = 0.5$; generally, the "correct" ξ to use is not known apriori. In practice, 5 control volumes require the same multiple of computations that a single-volume computation does. It therefore remains to balance in propulsion applications whether or not the need for machine speed solicits pre-processing ξ for greater single-volume duct analysis accuracy.

It appears from the computations that the BHE apprach is not adequate to describe the essential transient flow features required to accurately simulate propulsion system feeder ducts and tailpipe responses, but *will* suffice for traditional inter-stage engine component coupling. The more detailed (and computationally intensive) MEE approach is necessary for simulating STOVL-specific propulsion system components.

CONCLUSION

The modeling approaches presented provide rapid characterization of ejector/duct field variable time dependencies. These models are potentially useful for real-time STOVL propulsion simulation development efforts. If, however, the basic assumptions of each approach are not appreciated in application, the net result is the rapid computation of the wrong answer.

NOMENCLATURE

A	cross-sectional area
$\dot{E}$	energy flux
$\hat{F}$	internal resistance of the duct
h_0	total enthalpy, $= h + v^2/2$
h	specific enthalpy
KE	kinetic energy
L	total duct length
$\dot{m}$	mass flowrate
m_{cv}	control volume mass
$\dot{M}$	momentum flux
M_{cv}	control volume momentum
P	pressure
R	gas constant
s	entropy
t	time
v	velocity
V	volume of control-volume
x	streamwise coordinate
Δx	duct incremental length
ρ	density of gas
ξ	weighting parameter for average velocity calculation

Subscripts

i	inflow
j	outflow
k	control volume designation
n	dummy index

Superscript

-	averaged quantity

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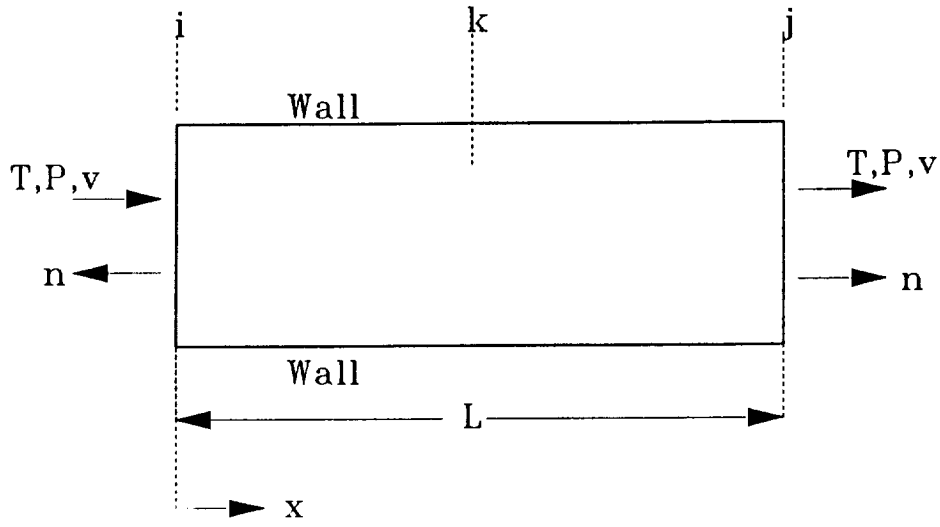

Fig.1 Control-volume approximation.

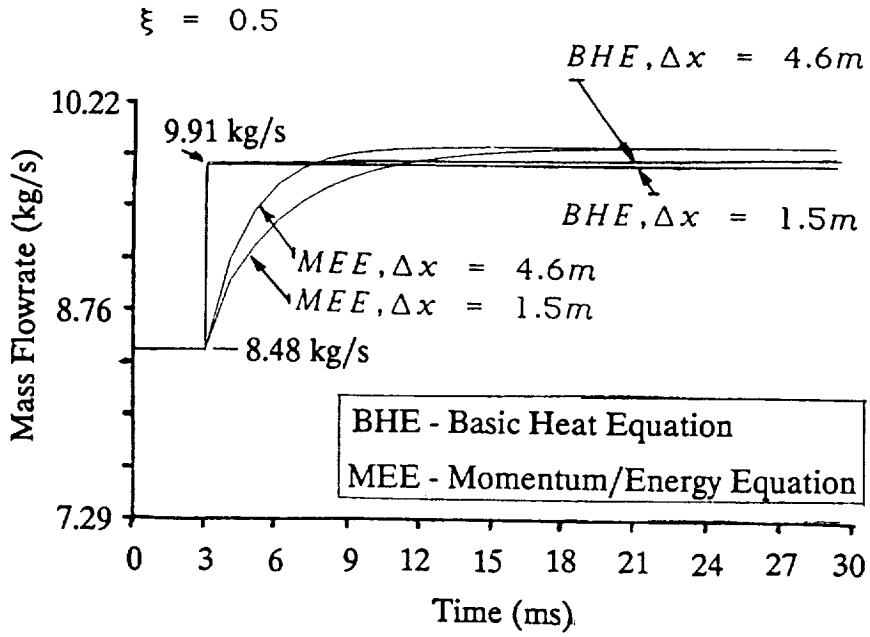

Fig.2 Solution for duct exit flowrate as function of time.

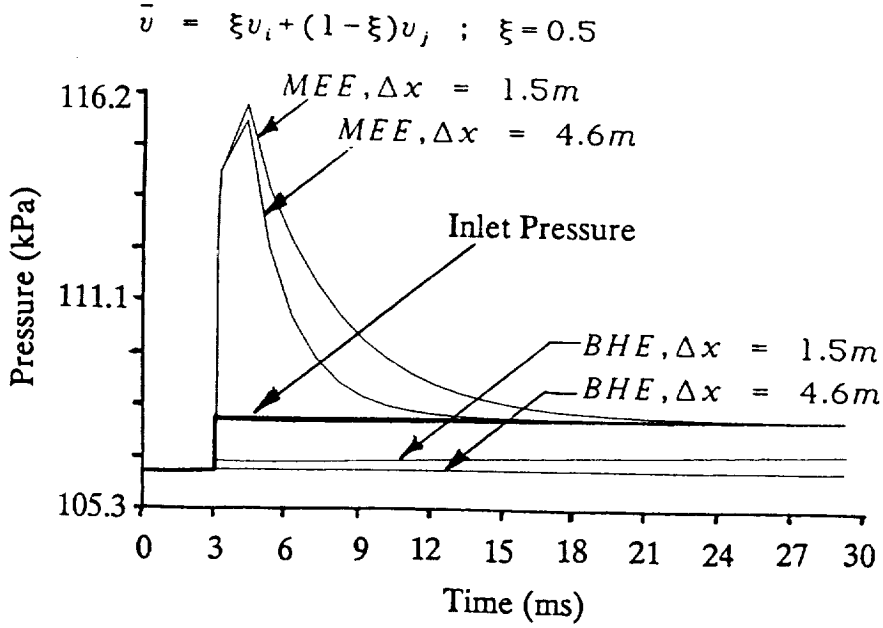

Fig.3 Solution for duct exit pressure as function of time.

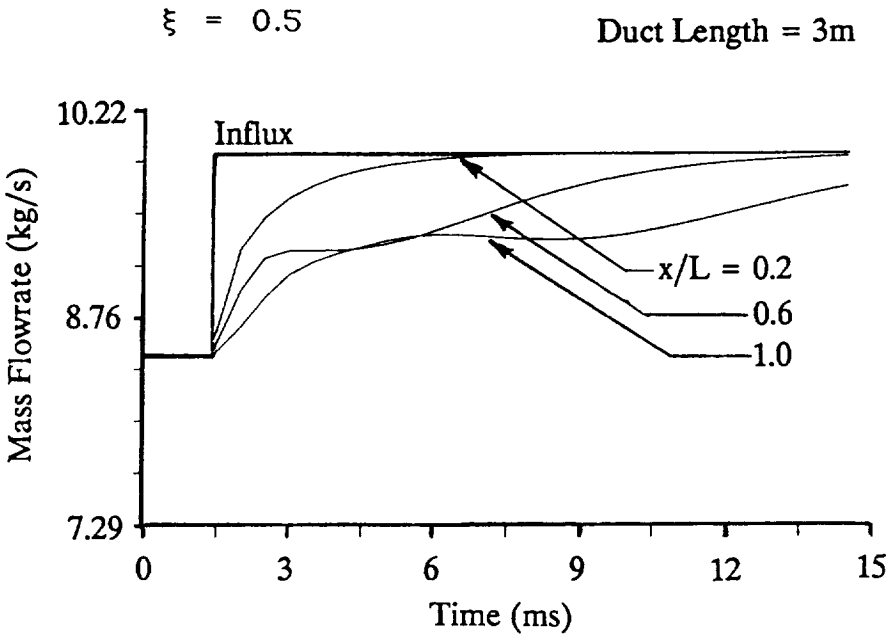

Fig.4 Solution for duct flowrate as function of time for three points along the duct.

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16. Abstract

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