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Modeling Techniques for the Analysis of Ramjet Combustion Processes

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Increased demand for higher performance from airbreathing propulsion systems, and in particular ramjets, has resulted in a need to upgrade existing technologies to meet more stringent design requirements. The criteria for higher performance include reduced volume and weight, leading to the use of shorter combustors and high energy, high density fuels. Systems constraints have dictated the use of sudden-expansion (dump) combustors capable of operating effectively at high combustion intensities over wide ranges of conditions; problems of flame stabilization, flame propagation and spray combustion have been encountered. This paper describes how the development and application of modeling techniques can be used to interpret experimental results and to assist in the design of ramjet combustors and combustor components.

Introduction

CONTROL of flame stabilization and flame propagation in a turbulent flow represents a key element in combustion-chamber design. The placement and geometry of fuel injectors, flameholders, and air distribution ports are basic design parameters that govern the performance of a particular combustor. Therefore, it is desirable to compute combustion chamber flowfields in order to understand the phenomena that occur in existing combustors and to predict the performance of new combustor design concepts. In addition to the overall combustor flowfield, modeling can also be used to provide insight into the behavior of portions of the combustor, such as flameholders and fuel injectors, under a variety of conditions. The insight gained through the use of these "unit" analyses can be of substantial use in the planning of a combustor test program and in the interpretation of combustor and combustor-component test data.

The computation of a generalized combustor flowfield is a formidable task, involving a number of complex, coupled physical and chemical processes. The obvious difficulties include not only sorting out the multitude of coupled mechanisms but also involve the typical disparity in characteristic length and time scales in combustion chamber flows. Despite the problems involved, considerable progress has been made in recent years in the development of calculation methods for these flows, using technologies for the solution of the elliptic governing equation and simpler yet physically perceptive "modular" modeling techniques.

A "unified" model of an overall combustion chamber flowfield requires the numerical solution of the elliptic form of the governing equations, since most practical combustion-chamber flows involve large regions of recirculation, in which axial diffusion is important. A considerable amount of research effort has been put into the development of numerical techniques for these problems, and successful comparison of calculation with experiment for recirculating flows with large heat release has been reported by Hutchinson et al.[1] and by Abou Ellail et al.,[2] for example. Significantly, in both cases a careful adaptation of the numerical model to the specific experimental configuration was reported to be required, and in both of these papers it was noted that the details of the computation required careful handling to obtain the accuracy demonstrated. Furthermore, Abou Ellail et al.[2] note that it is not possible to provide sufficient resolution in a detailed combustor flowfield computation to adequately describe processes such as fuel injection for which the mixing process initially occurs on a scale much smaller than that of the overall combustion chamber.

Thus, while the development of numerical models capable of providing a direct solution of the equations governing specific combustion chamber flowfields continues, a need exists for the development of physically perceptive yet mathematically simpler models. This requirement arises from both the need to provide a model which allows reasonably rapid computation of a number of different, complex combustor geometries, and the need to develop models for those processes, such as fuel injection, which occur on scales smaller than can be adequately resolved in a detailed overall flowfield computation. The development of approximate methods, modular models, is a response to the requirements just outlined.

Modular Model Philosophy

The basic interest in the application of approximate techniques is to avoid the complexities inherent in a direct calculation of an elliptic flowfield by making suitable assumptions that allow the flow to be computed using simpler approaches. Clearly the simplest possible procedure is to assume that the flowfield is effectively one-dimensional. A more sophisticated approach is to assume that the combustor flowfield can be broken down into separate zones, each of which can be calculated individually in some detail, and then coupled together in some fashion to obtain an overall computational analog of the combustor flow. Such approaches are termed modular models, examples of which have been reported by Roberts et al.,[3] Swithenbank et al.,[4] and Harsha and Edelman.[5]

The submodels used for the flowfield regions and the assumptions involved in the model formulation in each of the

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approaches described in Refs. 3-5 have been discussed by Harsha and Edelman.[6] In the present approach, the combustor flowfield, represented schematically in Fig. 1, is broken down into three major components: a directed flow, which is treated as parabolic, a recirculation zone, assumed to be represented by well-stirred reactor(s), and a turbulent shear layer along the dividing streamline which separates the other two regions. The shear layer serves as the coupling region between the other two model components; fluxes of species and energy across this shear layer form the boundary conditions on the two computational regions. Finite-rate chemistry, based on the quasiglobal model,[7] is included in the formulation for the directed flow; for the modular-model calculations described in this paper, the recirculation region well-stirred reactor formulation has been restricted to a global finite-rate chemistry model. The directed flow is assumed to be fully turbulent, with the turbulent viscosity defined by either an algebraic eddy viscosity,[8] a one-equation turbulence model,[9] or a two-equation turbulence model.[10] For all of the combustor computations reported here, the two-equation turbulence model has been used.

A key difference between this modular model and the models of Roberts et al.[3] and Swithenbank et al.[4] is the provision for the shear layer coupling region in the current model. Through the use of this element of the model, the division of mass flux between the directed flow and the recirculation region is computed iteratively rather than specified empirically. Furthermore, the directed-flow region is computed in detail as a two-dimensional parabolic flowfield, rather than through a one-dimensional approximation, allowing the use of detailed computations of the mixing and chemical reactions in this region of the combustor.

The features of the three modular models discussed in this section are summarized in Table 1. Although the present model is computationally more complex than the models of Roberts et al.,[3] and Swithenbank et al.,[4] and requires an iterative solution, the additional complexity allows the development of an overall combustor flowfield computation which can include far more detailed modeling than is possible with either of these approaches. Furthermore, each element of the present modular model—the well-stirred reactor, the parabolic flow computation, and the shear layer representation—can be systematically developed. For example, the effects of unmixedness can be introduced into both the stirred reactor module and the parabolic directed-flow computation. Multiple well-stirred reactors can also be easily introduced for more detailed modeling of the recirculation region. The detail available in the parabolic flowfield computation allows the inclusion of models for processes such as liquid-fuel injection, and the spread and vaporization of the resulting spray, treated either in bulk or through the use of detailed spray modeling. Thus, modular modeling of the type described in this paper offers considerable potential for systematic development to provide greater insight into the physical details involved in a generalized combustor flowfield.

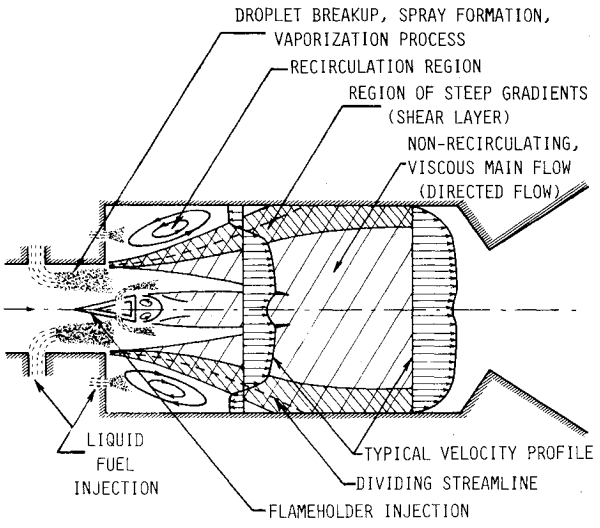

Fig. 1 Schematic of sudden-expansion (dump) burner.

Modular Model Formulation

The formulation of the detailed modular combustor model is fully described in Refs. 5 and 6. There are three basic elements to the model: a parabolic, finite-difference solution procedure which is based on the method described by Boccio et al.,[11] applied to the directed flow; a simplified well-stirred reactor model based on the approach outlined by Harsha and Edelman,[6] which uses a global finite-rate chemical kinetics formulation, applied to the recirculation region; and a simple, linear growth shear layer model, which provides the boundary condition for the two other major flow regions. It should be noted that the modular concept is not restricted to the models described above: in particular, both a full finite-rate chemical kinetics formulation for the stirred-reactor region and a more detailed treatment of the shear-layer region represent logical extensions of the current approach. However, as will be seen from the results presented in this paper, the present simple formulation provides reasonably accurate results for a variety of flows.

Coupling relations between the directed flow and recirculation regions are provided by the shear-layer model. It is assumed that a shear-layer region lies along the dividing streamline and that this region can be modeled as a region of width $\ell(x)$ across which all dependent variables vary linearly. The width of the shear layer is itself assumed to be specifiable by the relation

$$\ell = ax + b \tag{1}$$

in which a and b are constants. Then, along the dividing streamline,

$$\left(\frac{\partial \phi}{\partial r}\right)_W = \frac{\phi_P - \phi_R}{\ell} \tag{2}$$

where the subscripts P, R, and W refer to conditions in the directed flow immediately outside of the recirculation zone, in the recirculation zone itself, and along the dividing streamline, respectively, and ϕ is any dependent variable: species mass fraction α_i, temperature T or enthalpy H,

Table 1 Comparison of modular models

	Roberts et al.[3]	Swithenbank et al.[4]	Present model
Elements	Plug flow reactors	Plug flow/perfectly stirred reactors	Parabolic flow/stirred reactors
Coupling conditions	Empirical flow division	Empirical flow division	Iterative solution for flow division
Turbulent flow characterization	None	None—enters empirical flow division	Turbulent kinetic energy model
Chemistry model	Quasiglobal H/C kinetics	Global H/C kinetics	Quasiglobal H/C kinetics

turbulent kinetic energy k, and turbulence energy dissipation rate ϵ. The boundary condition for the velocity for the directed-flow region is handled differently, however: in this case

$$\mu_T \frac{\partial u}{\partial r}\Big)_W = \tau_W = \tfrac{1}{2}\rho_\infty u_\infty^2 C_{FW} \tag{3}$$

where C_{FW} represents a "skin friction" coefficient along the dividing streamline and $\rho_\infty u_\infty^2$ is evaluated as the average of the values taken by this quantity along the centerline and at the point P. The dividing-streamline viscosity, μ_T, is obtained from the usual two-equation model formulation[10]

$$\mu_{TW} = C_\mu \rho_W k_W^2 / \epsilon_W \tag{4}$$

with $C_\mu = 0.09$.

A number of parameters must be specified, either empirically or through comparison of model predictions with experiment, for a modular model computation to proceed. These include the size and shape of the recirculation zone, the coefficients a and b in Eq. (1), the recirculation zone values of k and ϵ, and the dividing-streamline skin-friction coefficient C_{FW}. While this is a fairly lengthy list of coefficients, and is in addition to the inlet and wall boundary conditions that must be specified in any formulation, experience has shown that good results can be achieved using a reasonably limited range of values of these coefficients. However, it should be stressed that the modular approach is not a predictive tool in the sense that it can be relied upon in the absence of experimental data; instead, the model is one which, given a limited amount of available data, can be used to interpret the phenomena occurring within the combustor under test, and to provide a means for scaling limited test results to cover a more general range of interest to the combustor designer.

The overall flowfield computation using the modular approach proceeds as follows. A dividing streamline shape $R_C(x)$ is assumed, and the shear-layer width expression and shear-stress distribution is defined. An initial state for the stirred reactor computation is assumed, resulting in values of α_{iR} and T_R. With these data available as boundary conditions, the parabolic mixing calculation is carried out to the end of the recirculation zone. This calculation defines the species mass fraction and temperature gradients at $\psi = \psi_W$, and these values are used to obtain the stirred-reactor feed rates. A new stirred-reactor computation is carried out using the new feed rates, resulting in a new specification of α_{iR} and T_R, and the parabolic computation is repeated. The procedure is repeated until the changes in the stirred-reactor composition from computation to computation become small, at which point the coupling iteration has converged. The parabolic calculation is then carried out to the end of the combustion chamber, completing the solution.

In addition to the basic components of a parabolic, directed-flow analysis and a well-stirred reactor formulation, the modular concept can be extended to include modules which represent other elements of the combustor flowfield, for example, the fuel injection process. The detail of the computation provided by the use of a parabolic directed-flow analysis is the key feature of the modular model that allows the inclusion of a fuel injection module in the complete analysis. This is particularly true in the case of liquid fuel injection, for at the fuel/air ratios appropriate for ramjet combustor operation, the liquid fuel streams initially occupy a very small portion of the overall combustor cross-sectional area.

The liquid fuel injection model used in the modular formulation described here makes use of a combination of empirical information and turbulent mixing calculations. For example, the fuel jet penetration from the wall is computed through the use of the penetration correlation developed by Catton et al.[12] using the breakup time correlation developed by Clark[13] to compute the downstream distance at which penetration is to be computed. That is, it is assumed that penetration is computed for the point at which the fuel jet has broken up into droplets, as given by the breakup time correlation and the local airflow velocity.

For axisymmetric flow, the individual fuel jets are combined into an annulus whose cross-sectional area is computed based on an assumed fuel spray-bulk velocity. Although the present modular model is specialized to axisymmetric flow, it should be noted that the modular concept can be extended to three-dimensional flow, in which case the fuel injection modules would each represent an individual fuel jet.

Subsequent spreading and vaporization of the liquid fuel jets can be represented either through two-phase flow modeling[14] or through the use of bulk spray correlations.[15,16] In the present model the simple correlation of Ingebo and Foster[15] has been used; the vaporization rate, according to this correlation, is a function of the initial velocity and temperature difference between the fuel spray and the surrounding air stream. Spreading of the fuel jet is computed through use of a turbulent mixing hypothesis as for the mixing process in the remainder of the parabolic flow.

Unit Analyses Applied to Combustor Test Programs

In ramjet development programs, combustor testing is often carried out at elevated inlet-static temperatures. In order to obtain these elevated temperatures in a ground test facility, vitiation of the inlet airstream may be required. Although propane-air vitiation can be used in order to minimize the effects of molecular weight mismatch on the comparison of test results with vitiation to those to be expected in flight, the question of the effects of vitiation on the fundamental phenomena involved in the ramjet combustion process still arises.

One advantage of the modular-model concept is that the elements which make up the model (i.e., the parabolic directed-flow analysis and the well-stirred reactor model) can themselves be used in isolation as models of fundamental parts of the overall flowfield. An example of this is the use of stirred reactor models to assess the affects of vitiation on flame stabilization phenomena.

Flame stabilization can be modeled using the well-stirred reactor as an approximation to the intensely backmixed recirculation region behind a flameholder. The loading parameter for this well-stirred reactor at blowout then becomes a characterization of the flame stability which can be expected. To study the effects of vitiation on flame stabilization, well-stirred reactor calculations were carried out for ramjet test conditions, using both pure and vitiated air inlet streams. Results of these computations are shown in Fig. 2 for one test condition of interest. The effect of vitiation can be seen to be a reduction of the loading parameter at blowoff, because of the effects of the presence of H_2O and CO_2 on the specific heat of the "air" mixture.

These results are presented in terms of a reduced loading parameter as used in Curran[17]; in general, the reduced-loading parameter (RLP) is defined as

$$\mathrm{RLP} = \frac{\dot{n}_A}{VP^{1.8}} \left(\frac{A}{T_i^b}\right) \tag{5}$$

with A and b designated as empirical parameters. In actuality, the exponent on the pressure is itself an empirical parameter. If the nonvitiated results shown in Fig. 2 are considered, a comparison of these data with the available experimental results (from Refs. 18 and 19) indicates that, for a pressure exponent of 1.8, the temperature exponent b varies from 2.0 at an equivalence ratio of 0.4 to 1.46 at an equivalence ratio of 1.0, as shown by the additional points in Fig. 2. However, the data shown in Fig. 2 were obtained at 1 atm pressure, while the computations are for a pressure level of about 2 atm. Thus the effects of the use of a pressure exponent of 1.8 are also included in these results. Further computations, at tem-

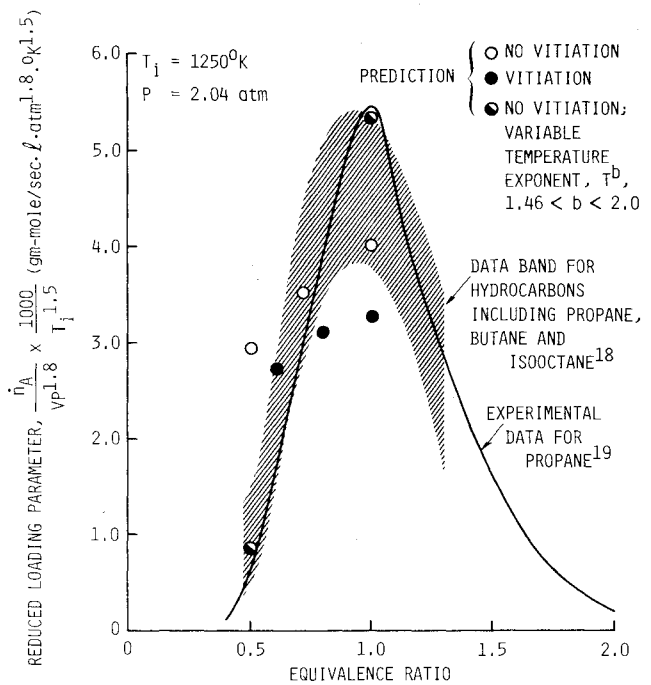

Fig. 2 Comparison of computed blowout results with experimental stirred reactor data.

perature and pressure levels that are elevated with respect to the available data, can serve to indicate the appropriate values for both the pressure and temperature exponents in Eq. (5).

Modular Model Validation

Cold Flow

Although one of the objectives of current work is to provide the data necessary for the validation and demonstration of the modular model for ramjet combustor performance, additional sources of data for model verification have also been sought. One such source is the detailed data provided by Chaturvedi[20] for an essentially incompressible, cold flow sudden expansion.

The sudden-expansion configuration investigated by Chaturvedi involved an area ratio of 4.0 with an inlet Reynolds number (based on inlet diameter) of 2×10^5. The inlet velocity was thus of the order of 30 m/s. Chaturvedi's apparatus had an L/D of 25 based on the inlet diameter and was aspirated by a centrifugal pump and butterfly valve assembly at the downstream end; the inlet was a bellmouth intake directly coupled to the expansion section. Because of the differences in Chaturvedi's experimental apparatus relative to a typical sudden-expansion combustor configuration, which include the lack of an exhaust nozzle section, the greater mixing-chamber length relative to a typical combustion chamber, and the lack of an inlet section, the modeling assumptions necessary to match the modular-model predictions with these data are not necessarily appropriate for a sudden-expansion combustor. Nevertheless, Chaturvedi's data, which include axial and radial profiles of mean velocity, axial and radial turbulent intensity components, and total pressure, are detailed enough to provide a critical test of modeling capability.

The parameters, in addition to wall boundary conditions and inlet conditions, that must be specified for a modular computation include: 1) recirculation zone size and shape; 2) shear layer initial thickness and growth rate; 3) dividing streamline shear stress coefficient; 4) recirculation zone turbulent kinetic energy level; and 5) recirculation zone turbulent dissipation length scale. Previous work[5,6] had established that the dividing streamline shape was well represented by a parabolic arc arranged such that at the step,

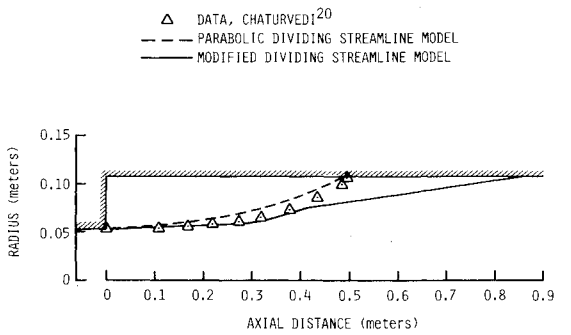

Fig. 3 Dividing streamline shapes.

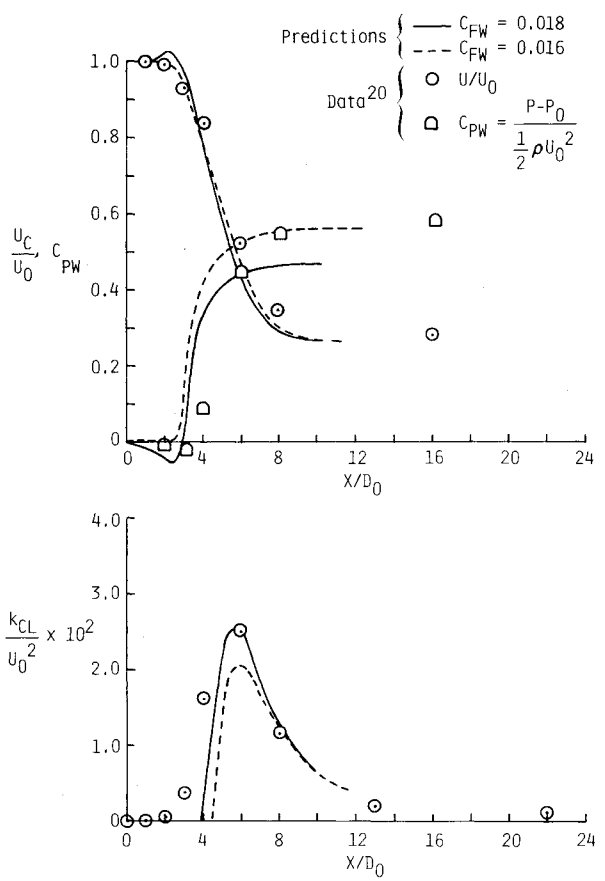

Fig. 4 Centerline velocity and turbulent kinetic energy profiles, and wall pressure coefficient.

$dr/dx=0$, which intercepts the wall at a distance 9.2 step heights from the inlet. The recirculation zone length correlation used here was based on Drewry's results.[21] Previous work also showed that the dividing streamline shear stress coefficient had a strong effect on the predicted static-pressure distribution, while the turbulent kinetic energy dissipation rate length scale assumed for the recirculation zone had a strong effect on the rate of transport into and out of the recirculation region. The shear-layer initial thickness and rate of growth had little overall effect over the range considered in the previous work.[5,6]

Although Chaturvedi's data show a reattachment point at 9.2 step heights from the inlet and indicate an essentially parabolic recirculation zone shape, comparison of the predictions of the model with the data for static pressure and centerline velocity showed that these data could not be reproduced with the parabolic recirculation zone boundary that has been used in other modular model calculations. Instead, an extended recirculation zone shape had to be

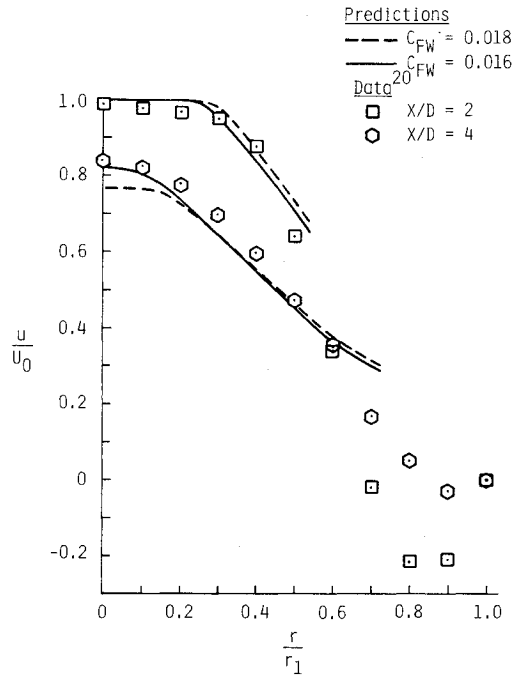

Fig. 5 Radial velocity profiles in recirculation zone regon.

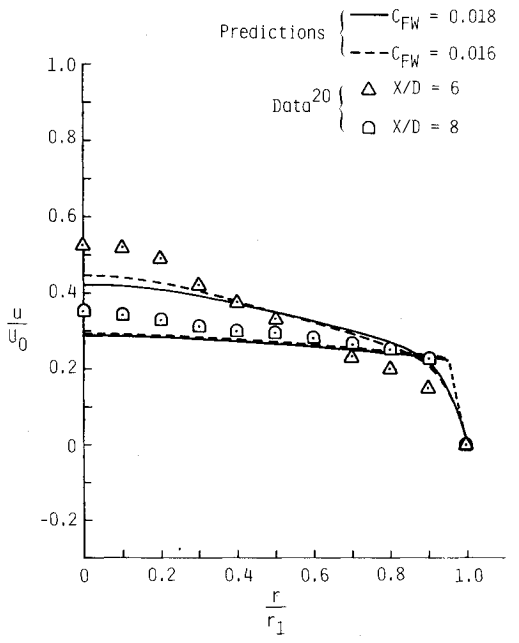

Fig. 6 Radial velocity profiles downstream of recirculation zone.

adopted. Figure 3 illustrates the experimentally determined recirculation zone shape, and the parabolic and modified recirculation-zone shapes adopted for the computation. Note that when the extended shape is used, the effective recirculation zone length for purposes of computing the stirred reactor volume is kept at the 0.491 m distance indicated by the data; thus the extension represents a crude model of a region over which the displacement effect of the shear layer bounding the dividing streamline relaxes to that of a wall-boundary layer.

Centerline velocity and turbulent-kinetic energy profiles and wall static pressure distributions are shown in Fig. 4 in comparison with Chaturvedi's data.[20] For these calculations, the initial velocity was taken to be uniform across the inlet, at 30 m/s, the initial turbulent-kinetic energy level was $10^{-4}U_0^2$, based on Chaturvedi's data, and the initial turbulence energy

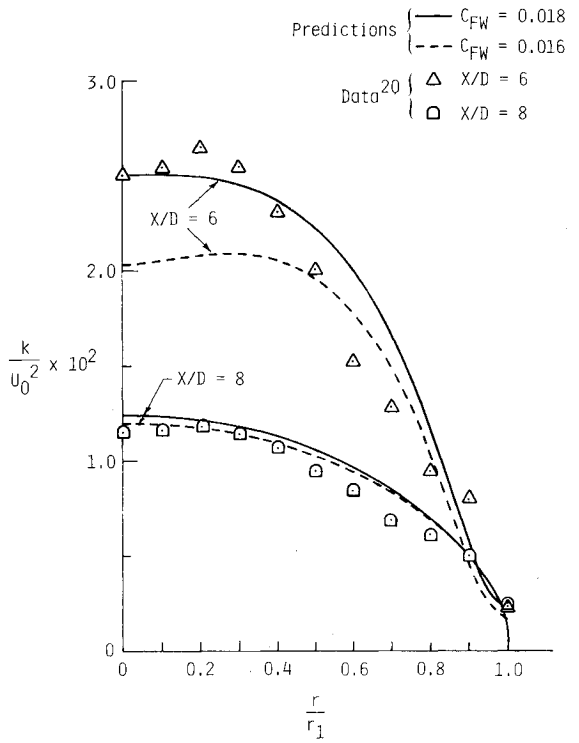

Fig. 7 Radial turbulent kinetic energy profiles.

dissipation rate was obtained from $\epsilon = 1.69k^{1.5}/\ell_k$ with $\ell_k = 0.1r_0$. A standard set of two-equation model coefficients have been used for all of the flowfield computations reported in this paper; thus:

$$C_\mu = 0.09;\ C_{\epsilon 1} = 1.40;\ C_{\epsilon 2} = 1.95;\ \sigma_k = 1.00;\ \sigma_\epsilon = 1.22$$

Results are shown for two values of the effective shear-stress coefficient along the dividing streamline, with $C_{FW} = 0.016$ providing the best agreement with the data for wall-static pressure and centerline velocity, but underpredicting the centerline kinetic energy data. Note that the initial increase of the centerline-kinetic energy is not predicted by the turbulence model; this phenomenon, which is associated with the closure of the potential core region of the flow, has also been observed in free jets. The other model parameters used in this calculation are $a = 0.03$, $b = 0.09$, $k_{RZ} = 0.005\ U_0^2$, and $\ell_{RZ} = (h + L_{RZ})/2$, where the latter two variables represent the effective turbulent kinetic energy and dissipation-length scale within the recirculation region. In the expression for the recirculation-zone length scale, h is the step height and L_{RZ} the recirculation zone length; since $L_{RZ} = 9.2h$, effectively $\ell_{RZ} = 5.1h$.

The predicted velocity profiles are compared with the experimental data in Figs. 5 and 6. Overall, the agreement is reasonably good, although it is clear that the flowfield mixing rate is slightly overpredicted. For $X/D = 2$ and $X/D = 4$, the predicted velocity profiles end at the dividing streamline location, since the modular model does not provide any detail of the recirculation region flowfield. Figure 7 shows a comparison of predicted radial turbulent kinetic energy profiles at axial locations downstream of the recirculation zone; again the overall level of agreement is reasonably good.

This comparison provides some validation of the ability of the modular approach to model accurately the details of a given flowfield. It is important to note that the adjustment of the model parameters carried out for this case was done with reference to the wall static pressure and centerline velocity measurements: the detailed profile data were not used in the modeling iteration. In the course of the modeling of this flowfield, a number of observations of model sensitivities

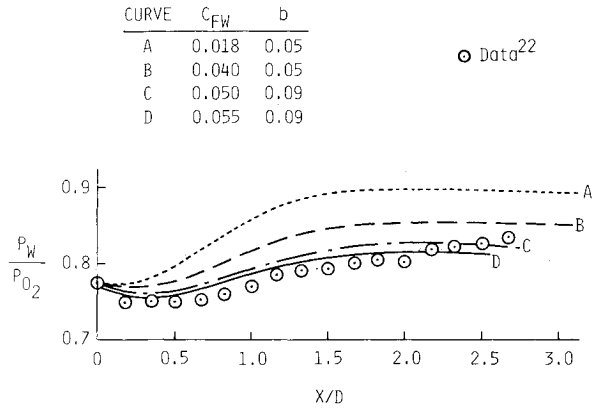

Fig. 8 Wall static pressure distributions.

were made. Although space does not permit including examples of each of these sensitivity observations, for this flowfield the following were observed.

1) The predictions were insensitive to the parameters a and b in the shear layer growth model.

2) The initial level of turbulent kinetic energy had a strong effect on the overall turbulent kinetic energy distribution in the flowfield, but a significantly smaller effect on the wall-pressure distribution.

3) The wall pressure distribution is primarily influenced by the size and shape of the recirculation zone and by the level of the assumed dividing streamline shear stress coefficient, C_{FW}.

The configuration used experimentally by Craig et al.,[22] while also a sudden expansion, is considerably different from that studied by Chaturvedi.[20] The basic configuration studied in Ref. 22 involved a combustor L/D of 3, a dump area ratio A_3/A_2 of 2.25, and an exit nozzle area ratio $A_2/A_3 = 0.40$. Thus it is considerably shorter than Chaturvedi's apparatus, and incorporates an exit-nozzle contraction. Both of these differences can be expected to have substantial effects on the overall flowfield development. A major contribution of this work is the measurement of wall static pressure distributions in both cold flow and reacting flow. This report represents one of the few investigations in which these data, crucial to the comparison of experimental data with analytical model results, have been reported. Supplementing the results reported in Ref. 22, additional unreported static pressure distributions have been made available to the authors by Craig.

Although an appropriate set of model coefficients have been developed for Chaturvedi's configuration, the differences between that apparatus and the combustor used by Craig et al. are substantial. Thus it can be expected that different parameter values would be necessary to accurately characterize this flowfield. In order to establish the necessary values, parametric calculations of one set of cold flow data, for an inlet total temperature of 552 K and an air mass flow of 1.45 kg/s, were carried out. Figure 8 illustrates the results of these computations. Here, a parabolic recirculation zone shape was used, and those model parameters not noted in Fig. 8 retained the values that were arrived at in the course of the computations of Chaturvedi's [20] configuration. The inlet-turbulent kinetic energy level and dissipation rate were established using the same relationships to mean flow and geometric variables as were used in the Chaturvedi calculations.

The differences in model parameters between the Chaturvedi[20] and Craig et al.[22] cold flow configurations are the shape of the recirculation zone and the level of the dividing streamline shear-stress coefficient. Although the comparisons shown in Fig. 8 may indicate that a small further refinement to the parabolic shape used in these calculations could improve the match between the computed and measured pressures over the range $0.5 < X/D < 1.5$,

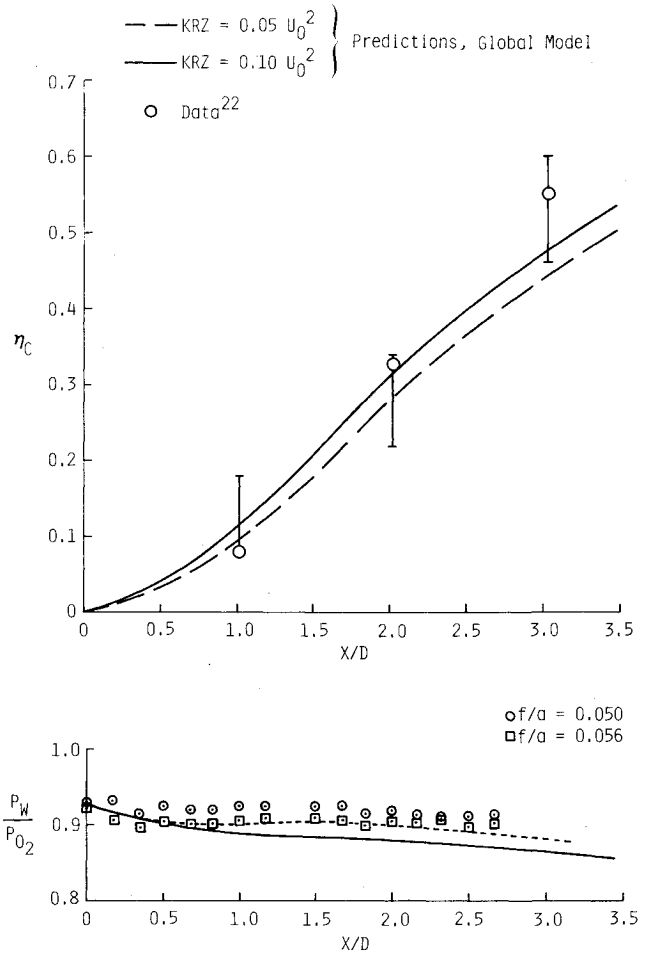

Fig. 9 Combustion efficiency and wall static pressure distributions, global kinetics model.

overall the level of agreement is quite good. The apparent lack of a large transition region between shear layer and wall boundary layer implied by these results, obtained with no transition region, may be a result of the use of a converging exit nozzle in the Craig et al. configuration. A further difference between this configuration and that studied by Chaturvedi is the lack of an inlet section upstream of the dump plane in the latter case, compared to the use of a relatively long inlet section in the former. The computations assume a uniform inlet profile so that the existence of an inlet boundary layer is reflected primarily in the shear stress parameter C_{FW}. Thus the requirement that the skin friction coefficient along the dividing streamline be a factor of 3 higher for the Craig et al. comparison than for the Chaturvedi modeling may be a result of the differences in the initial conditions between the two flows.

Reacting Flow

Craig et al.[22] also report combustion efficiency and wall-static pressure distribution measurements for a premixed suddden-expansion combustor. The combustor configuration is identical to that used in the cold-flow investigation. To further test the modular-model formulation, computations were made of a premixed dump combustor at a fuel/air ratio of 0.053 for comparison with the data presented in Ref. 22. In the experiments, the fuel used was JP-4, which was represented in the computations by propane. Both a simple one-step global finite-rate chemistry model and the full hydrocarbon-oxidation kinetics as represented by the quasiglobal model[7,23] were used to represent the chemical kinetics processes in the directed flow. Propane was chosen to represent the fuel because previous studies with propane had

resulted in the development of a one-step finite-rate kinetics model that represented fairly accurately the ignition delay for propane-air (although not the overall reaction time) over a range of conditions of interest in this work.

Initial conditions included an inlet total temperature of 554 K and an inlet-static pressure of 1.83 atm. With a mass-flow rate of 1.57 kg/s, the inlet velocity and static temperature were 159 m/s and 543 K, respectively; the inlet Mach number was 0.351. Initial turbulent kinetic energy and dissipation rate values were established in the same manner as for the cold-flow calculations, and the geometry of the combustor and recirculation zone was the same as in the the cold-flow calculations.

Both combustion efficiency and wall static pressure distribution data are available for this configuration, and the results of the modular model calculation of these quantities are shown in comparison with the experimental data in Fig. 9. The combustion efficiency shown was computed from the mass-average temperature at each axial location in the calculation, using the JANNAF temperature-rise combustion efficiency definition, Ref. 24.§ The "ideal" exit total temperature required for this calculation was obtained from the data tabulations provided to the authors by Craig; for these data, T_{T5} (ideal) = 2178 K. Note that in these comparisons, data points are shown for the fuel/air ratio 0.053 value at which the computations were carried out. These data points were obtained from plotted values of combustion efficiency vs fuel/air ratio presented in Ref. 22 for three different values of combustor L/D. The band shown for each data point represents the range of observed combustion efficiencies as a function of distance for all fuel/air ratios tested and is a better indication of the overall trend of the combustion efficiency vs length data than are the individual data points themselves. Some caution is advised in interpreting the combustion efficiency comparison shown in Fig. 9 since three different combustor configurations were involved in obtaining the data. Thus the relationship between recirculation-zone length and combustor length is different for each of the three combustors tested. The static pressure data (for two fuel/air ratios which bracket the fuel/air ratio used in the computation) and the predictions are both for a combustor L/D of 3.0.

The first point that should be noted in reviewing the results presented in Fig. 9 is that the level of agreement obtained was arrived at by increasing the kinetic energy level in the recirculation zone substantially over the levels found to be appropriate for the cold-flow cases already discussed. One possible physical cause for this increased turbulent energy level in the recirculation region is the large-scale oscillation of the recirculation zone that has been observed in a variety of reacting, recirculating flowfields.[23] While the fluctuations that such oscillations produce are not strictly turbulence, two factors combine to suggest that they can be interpreted in the context of an analytical flow model as increased turbulent-intensity levels in the recirculation zone. One is that the oscillations enhance the rate of mixing in the recirculation region and in the shear layer bounding it, and the second is that some of the energy in these oscillations can be expected to provide a source of turbulence energy. For all other factors equal, the increase in turbulent kinetic energy in the model recirculation zone, while not increasing the mixing rate within that region, does increase the effective viscosity along the dividing streamline, and thus the rate of transport across the shear layer. It also changes, both directly and indirectly through the transport mechanism, the rate of transport of turbulent kinetic energy to the recirculation zone. Thus, the increased turbulent energy level in the recirculation zone required to achieve good agreement with the available data for the Craig et al. premixed combustor appears to be plausible on physical grounds.

It is also of interest to note, from Fig. 9, that the lower of the two curves shown in the combustion efficiency comparison provides the better agreement with the observed static-pressure profiles, despite a significant underprediction of the combustion efficiency at $X/D \simeq 3.0$. As has already been noted, the global finite-rate model does not properly predict reaction time for the propane-air system used to model this flow, and thus further computations were carried out using the quasiglobal hydrocarbon kinetics model[7,23] for the same condiitions.

The initial quasiglobal computation of this flowfield, denoted by $F=1$ in Fig. 10, produced a surprising result: blowout was predicted to occur in the directed flow as evidenced by the plateau in the combustion efficiency vs distance curve. To interpret this result, a discussion of the development of the quasiglobal chemical kinetics model for hydrocarbon oxidation is required.

During the development of the quasiglobal model, two major sources of data were utilized. One of these was ignition delay data obtained in experimental configurations approximating the ideal limit of plug flow, and the other was blowout data obtained from laboratory well-stirred reactors. Although data from both sources verified the basic quasiglobal-model concept: that hydrocarbons higher than propane could be grouped into generic classifications and that for each group an overall partial-oxidation reaction to the products CO and H_2 could be written, at least for fuel/air equivalence ratios less than one, it was observed that the effective pre-exponential coefficient in the quasiglobal

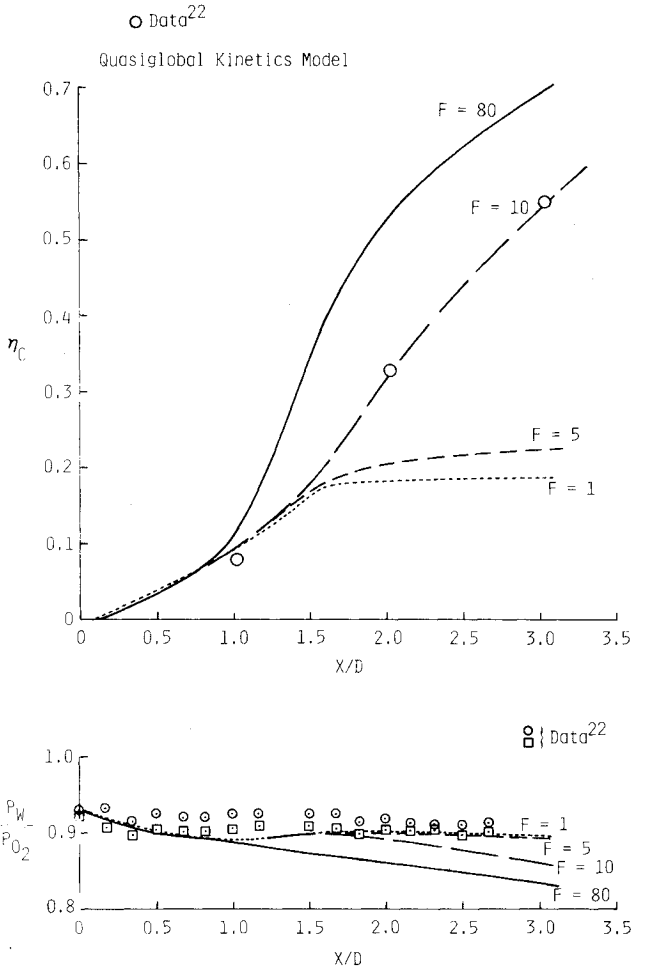

Fig. 10 Combustion efficiency and wall static pressure distributions, quasiglobal kinetics model.

§The definition was modified slightly in that static rather than total temperatures were used, for convenience. At the combustor Mach numbers encountered in these experiments, the difference in temperatures is of the order of 1%.

Ahrrenius rate expression had to be increased by a factor of 80 for well-stirred reactor flows compared to plug flow.

The salient difference between a well-stirred reactor and a plug flow is that in the former the reactants and products of combustion are continuously being mixed with fresh, unreacted, entering species. This, of course, is the situation for a mixing and reacting flow, and it is expected to be particularly significant near blowout. This, a further series of computations were carried out with a multiplicative factor F preceding the pre-exponential coefficient in the quasiglobal-hydrocarbon partial oxidation rate expression.[23] For propane, this expression is

$$\frac{-dC_{C_nH_m}}{dt} = F \cdot 6.0 \cdot 10^4 TP^{0.3} C_{C_nH_m}^{1/2} C_{O_2} \exp(-12{,}200/T) \quad (6)$$

where T is temperature (K), P, pressure (atm), and $C_{C_nH_m}$ and C_{O_2} are concentrations of fuel and oxidizer, respectively (g-mol/cm^3), and values of F from the plug flow limit of 1 to the stirred-reactor limit of 80 were considered. The rationale for carrying out this investigation is that each element of reacting fluid can be considered to behave as a combination of plug flow and stirred reactor flow: in the absence of turbulent transport each element is a plug flow reactor, whereas if turbulent transport dominates then the flow in each element approaches that of a homogeneous-stirred reactor. This idealization is of course an oversimplification but it is an instructive concept.

As is shown in Fig. 10, the well-stirred reactor limit, $F=80$, drastically overpredicts the rate of reaction in comparison with the data, while for $F=5$, blowout is still predicted. When $F=10$ is selected, an almost perfect representation of the data is obtained, but this should be considered to be fortuitous: no representation that $F=10$ is the appropriate value for mixing flows can or should be made.

If the predicted pressure profiles are considered, Fig. 10, an entirely different conclusion could be obtained. Examination of these predictions shows that, while none of the predictions is in exact correspondence with the data, the predictions for $F=1$ and $F=5$ are in better agreement than those for $F=10$ and $F=80$. Significantly, the disagreement in the latter two cases is greatest in the region downstream of the end of the recirculation zone, and is thus not related to the dividing streamline modeling. The data presented by Craig et al.[22] indicate that for the premixed combustor, blowout is observed between a fuel/air ratio of 0.030 and 0.040. The sensitivity of these results to the parameter F is an indication that at the conditions of these data, a fuel/air ratio of 0.053, the flowfield may also be near blowout. In fact, the combination of the agreement with the axial pressure profiles obtained for $F=10$ can be taken to indicate that this combustor flow was, in fact, oscillatory, with a continuous partial blowout and reignition phenomena occurring during the test.

This discussion has emphasized the model sensitivities observed during this phase of the model validation program. It is, of course, also instructive to consider the insensitivities observed. These can be summarized as follows: for the premixed reacting flow, the predictions are insensitive to the details of the shear layer growth rate, as modeled by Eq. (1); the premixed combustor is totally insensitive to initial velocity profile variation as represented by the inclusion in the initial profile of a 1/7 power law boundary layer with a thickness equal to 0.1 times the inlet radius.

Conclusions

While the computational ability to predict in detail the flowfield in a ramjet combustor without empirical approximations appears to remain a long-term goal rather than a short-term prospect, modeling techniques have been shown to be a viable tool in the analysis of ramjet combustion processes. These modeling techniques range from the analysis of plug flow and well-stirred reactor flows, which represent idealizations of aspects of an overall ramjet flowfield, to the modular approach, which represents the only realistic method of introducing a detailed description of a ramjet combustor flowfield into a computational technique. The modular approach is not a completely predictive technique, but no currently available mathematical model of a ramjet combustor can be considered to be totally predictive. What the modular approach, coupled with the use of detailed unit analyses, offers is the ability to analyze and interpret experimental data and to aid in the design and planning of appropriate experiments. A considerable amount of further work is still required to develop all viable ramjet modeling approaches, whether unified or modular formulations. What this paper has endeavored to demonstrate is the insight that can be gained through the use of modeling in both the design of combustors and experiments and in the interpretation of experimental results. It is in this area that modeling holds the greatest promise.

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