Predicting Exit Temperature Profile from Gas Turbine Combustors

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An analytical model has been developed for predicting exit radial temperature profile from gas turbine engine annular primary combustors. The model assesses the effect of changes in dilution or cooling air distribution, changes in combustor aft end geometry, or changes in combustor operating conditions. The description of the dilution air mixing process includes the effects of mixing in confined flows, closely spaced dilution jets, and elongated dilution ports, the interaction of opposing rows of dilution jets, and the effect of flow area convergence, including nonsymmetric convergence. The cooling air mixing process description includes the effects of combustor turbulence, multiple louvers, and flow area convergence. Comparison of measured exit temperature profile shifts, with the predictions of the analytical design system, shows good agreement in predicting the effects of both dilution and cooling air shifts within the combustor aft end.

Nomenclature

A	= area
C	= coefficient
D	= diameter
H	= height
J	= momentum flux ratio
M	= blowing parameter (mass flux ratio)
S	= spacing, gap
T	= temperature
V	= velocity
W	= flowrate
X	= downstream distance
Y	= penetration distance
δ	= exit temperature profile = local minus exit average temperature
γ	= heat/momentum diffusivity ratio
ρ	= density
ϕ	= temperature profile thickness

Subscripts

ADW	= adiabatic wall
B	= burner front-end
C	= thermal penetration centerline
CA	= cooling air
CS	= coolant supply
CENT	= center plane of dilution port
d	= discharge
DA	= dilution air
EQ	= equivalent
FS	= freestream
ID	= inner diameter
J	= jet
ℓ	= louver
M	= turbulent mixing
Max	= maximum
Mid	= mid-way between dilution ports
OD	= outer diameter
p	= port
ST	= staggered
T	= total
∞	= infinite or freestream condition

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Introduction

CURRENT and future aircraft gas turbine engines operate at high turbine inlet temperatures in order to operate at high engine bypass or compression ratios. This gives higher specific thrust (thrust per unit inlet airflow) and lower engine specific fuel consumption (fuel consumption rate per unit thrust or power). Typical average turbine entry temperatures for current (1975) high bypass ratio commercial aircraft engines or high thrust/weight military engines are at the 1400°C level. Consequently, increasingly stringent limitations are placed upon the temperature profile at the entrance to the turbine, as shown in Fig. 1. Hot-gas temperature profiles must be matched carefully to the turbine blade stress levels if long turbine life is to be attained. For example, an increase in hot-gas temperature at a particular radial location on the turbine first blade of as little as 28°C can either reduce the life of the blade by half, or require an additional ~ ½% engine airflow for turbine cooling, to maintain acceptable blade life. Each 1% additional turbine cooling air can, for example, decrease takeoff thrust by as much as 2¼%.

Historically, the process of designing and building a gas turbine engine primary combustor has always involved guesswork and resulting costly development testing to achieve the desired exit temperature profile. This development testing was required because the analytical tools to describe the various air mixing processes within the combustor were at best rudimentary.

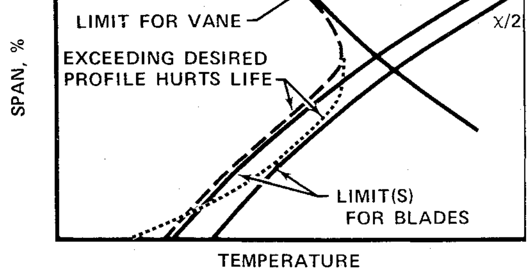


Fig. 1 Turbine life limits set radial profile requirements.

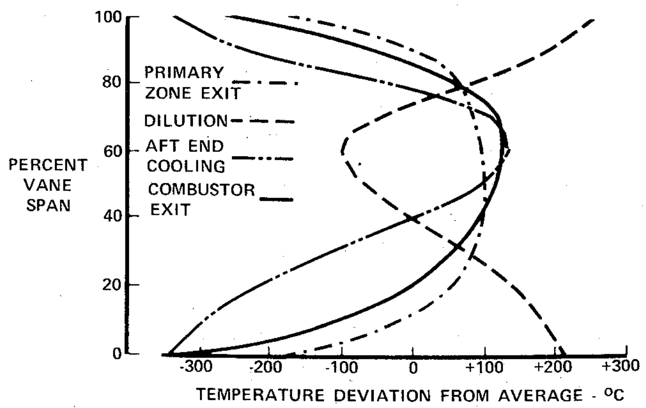


Fig. 2 Temperature profiles contributing to turbine inlet profile.

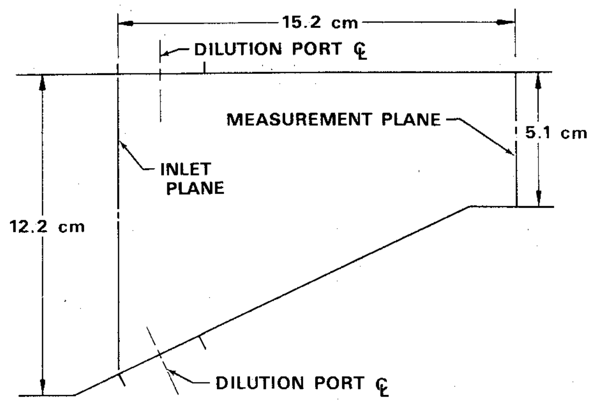


Fig. 3 Dilution zone mixing study test section geometry.

This paper describes a method of predicting the gas turbine engine primary combustor exit temperature profile by predicting the temperature profiles produced by all of the air admission processes. The approach limits the analytical description to the aft end, or dilution zone, of the combustor. The aft end is considered to be the portion of the combustor between the leading edge of the dilution holes and the exit of the combustor. Newly developed correlations for describing the mixing of the dilution air [1,2] form the basis of the analytical model. These correlations are combined with another calculation procedure which determines the contribution of the aft end liner cooling air. An additional contributor to the combustor exit temperature profile is the temperature profile entering the dilution zone from the primary zone. Figure 2 shows, on a representational basis, how the profiles from these separate processes combine to form e.g., the combustor exit radial average temperature profile.

Conceptual Formulation

The generation of the detailed Aft End Design System (AEDS) followed a building-block approach. Each step in the design system is based on a set of empirical correlations which predict one aspect of the dilution and cooling air mixing processes. The temperature profiles resulting from each of the two air-admission processes are calculated independently. The final step consists of combining these separate profiles into one combustor aft end exit temperature profile.

The primary-zone exit temperature profile is treated deductively. The analytical model uses measured combustor exit temperature profile data to derive a primary zone exit profile. This deduced profile is then resubmitted to the design system in order to predict combustor exit temperature profile shifts resulting from changes to the combustor aft end geometry or to the combustor operating conditions.

Sources of Data for Analytical Correlations

Single-Row Dilution Jet Mixing

The National Aeronautics and Space Administration funded an experimental test program (NASA Contract NAS3-15703) to supply the data needed to describe the dilution process by a single row of closely spaced dilution jets.[3] A single row of closely spaced dilution jets was used to inject ambient-temperature air into a heated crossflow confined within a rectangular duct.

Major operation point variables were the diluent jet-to-mainstream density ratio and the diluent jet-to-mainstream velocity ratio. These in turn give the momentum flux ratio

$$J=\left(\frac{\rho_j}{\rho_\infty}\right)\left(\frac{V_j}{V_\infty}\right)^2 \qquad (1)$$

Geometric variations were obtained by varying the dilution port diameter, D, and the spacing of the ports in the row, S_p. These in turn describe the geometry in terms of the duct height-to-port diameter ratio, H/D, and the port spacing-to-port diameter ratio, S_p/D. The range of geometric variables and the experimental operating points are representative of current design practice for Gas Turbine Engine (GTE) combustors.

Dilution-Zone Dilution Jet Mixing

Generation of the procedures for describing the mixing of opposing rows of opposed or staggered dilution jets, in a mixing section with a converging cross-sectional flow area, is based upon the test data from another NASA test program.[4] Figure 3 illustrates the configuration of the test section. The test section featured a rectangular inlet section which discharged heated air into a converging mixing section. The mixing section, in turn, discharged into a rectangular measurement section. The convergence, as shown by Fig. 3, was not symmetric. One wall remained straight, and the entire convergence was formed by the opposite wall. Temperature data for the experimental test program were taken using a fixed thermocouple array.

Dilution air was injected through a series of interchangeable orifice plates located at the start of the convergent mixing section. The orifice plates used rectangular injection slots, and were built so as to be able to test either staggered or directly opposed dilution jets. Testing used a far more limited range of dilution injector geometries than did the test program in Ref. 3.

The major flow variable for the test program was the dilution jet/crossflow velocity ratio; the density ratio was constant at approximately 2. The range of velocity ratio tested included mostly values which were lower than typical GTE design practice. Only one test condition for each geometry exceeded the minimum velocity ratio required, which is 2.5.

Cooling Air Mixing

There are only a few sources of data that deal with the gas temperature profile downstream of a louver cooling slot. The approach taken was to link an approximate profile with the much better documented cooling efficiency data. The main assumption made is that the profile is Gaussian. This has experimental justification in the work of Wieghardt.[5,6] Although the experimental data were taken under less severe conditions than are found in combustors, this appears to be a reasonable approximation. Also required is some velocity profile associated with this temperature profile. This was found in the work of Reichardt,[7,8] who derived a semiempirical relation between the velocity and temperature profile using the ratio of coefficients for heat and momentum transfer. This gives a velocity profile which is Gaussian but has half the width of the temperature profile.

This leaves only the wall value of the temperature profile to be determined. A correlation derived by Juhasz and Marek[9] proved to give satisfactory results and was simple to use. Their experiment was done under conditions similar to those found in combustors, which set it apart from numerous experiments done under low turbulence wind tunnel type conditions. The change in turbulence conditions made marked changes in the value of their mixing coefficient.

A report by Carlson and Talmor[10] considers the effect of turbulence level and mainstream hot-gas acceleration on the decay of the film cooling under conditions like those found in combustors. Both these effects are important in combustors, but the correlating parameter used is very complicated and extremely difficult to use. It also did not lend itself to programming and so was used as a check of the Juhasz and Marek correlation.

Dilution Jet Mixing Analysis

Correlation of experimental data on the mixing of single rows of closely-spaced dilution jets is based upon the experimental observation that the vertical temperature profiles at all axial and transverse locations are Gaussian in shape. They can be expressed in a self-similar form by the proper choice of scaling parameters. The procedures for correlating and predicting the vertical temperature profile scaling parameters as functions of geometric and flow variables, and their applicability under conditions typical of gas turbine engine primary combustor design practice, have been reported previously.[1,2] By themselves, these correlations cannot be directly applied to a workable annular combustor dilution zone design system.

First, the correlations were derived from test data using round dilution ports. Elongated dilution ports are commonly used in gas turbine combustors, and some procedure must be derived to analyze their effect. Contrary to previous experience with single, elongated dilution jets, closely spaced, elongated dilution jets were found to have reduced penetration compared with round ports of the same area.[3] Second, many gas turbine annular combustors inject dilution air through both the inner and outer liners, with the jets meeting in some fashion within the combustor. Third, the combustor annular height required for good ignition and efficient combustion is usually larger than the turbine inlet annulus height, and so some degree of flow area convergence is required.

Corrections for Elongated Dilution Ports

The procedures for correcting the calculated temperatures from single rows of dilution jets for elongated dilution ports use an equivalent jet diameter defined by

$$D_{EQ} = (4A_p/\pi)^{1/2} \tag{2}$$

with

$$D_{EQ,j} = D_{EQ}(Cd)^{1/2} \tag{3}$$

This equivalent jet diameter is substituted in all the correlating equations in place of the jet diameter for round dilution ports. Many of the correlating equations require additional correction using correlations of the form

$$\frac{\text{Corrected quantity}}{\text{Uncorrected quantity}} = F\left(\frac{X}{D_{EQ,j}}\right)^f \tag{4}$$

where F and f are functions of the dilution port aspect ratio. The ratios of the corrected to uncorrected correlating parameters to reduce to identical unity for round ports (aspect ratio of 1). These correcting functions are based upon test data from the NASA-sponsored test program.[3]

Correlation Procedures for Opposite-Wall Injection

Annular combustors used in gas turbine engines customarily inject the dilution air through both the inner and outer liners, although burners have been tested successfully using dilution air injected through one liner only. The procedures, which predict the temperature field produced by a single row of dilution jets, must be adapted to predict the temperature field from rows of dilution jets injected through opposite liners in the burner.

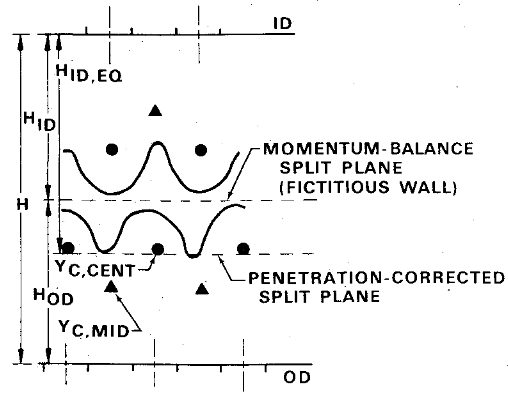


Fig. 4 Correction for opposite-wall injection.

The mutual interaction of dilution jets, which enter the burner through opposing, single rows of multiple jets, requires the treatment of three separate aspects of behavior. First, penetration of each row of jets restricts the distance to which the other row can penetrate, and thus results in each row of jets being influenced by an equivalent height, H_{EQ}, which is less than the total height, H. Second, the jets from opposing liners can be either directly opposed or staggered with respect to jets from the opposite liner. Third, the temperature fields produced by each row of jets must be combined in such a way as to give an overall temperature field which is close to the average temperature.

Equivalent Height Formulation

For the particular case of two equal-strength directly opposing single jets, the plane of symmetry between the two has been shown to have the same effect as an opposite wall at the same location.[11] This observation leads to the following procedure for analyzing directly-opposing multiple jets: 1) the plane of symmetry between the opposing jets acts as an opposite wall at the same location, and 2) the location of this fictitious wall within the burner is obtained by a momentum flux balance between the opposing multiple jet rows.

For opposing jet rows, wherein the individual jets are staggered with respect to the jets from the opposing rows, the procedure as given above must be modified. Figure 4 shows a typical contour plot of the temperature field from a single row of jets at a particular axial location. The penetration distance for a particular isotherm is, as expected, greater in the jet center plane-of symmetry. A jet from one row will thus see a greater equivalent height than if it were penetrating into the jet center plane-of-symmetry of the jets from the opposite row.

The procedure selected to handle this equivalent height begins by defining an effective height for directly opposed jets. It then defines two additional increments to the effective height, based upon the ratio of penetration between the jet center and midspan planes-of-symmetry, for a particular isotherm or penetration parameter.

The equivalent heights for opposed jets are given by

$$H_{ID} = \frac{(A_jJ)_{ID}}{(A_jJ)_{ID} + (A_jJ)_{OD}} \times H \tag{5a}$$

$$H_{OD} = \frac{(A_jJ)_{OD}}{(A_jJ)_{ID} + (A_jJ)_{OD}} \times H \tag{5b}$$

Extension of this procedure to the case of staggered jets follows from the conceptual formulation previously outlined.

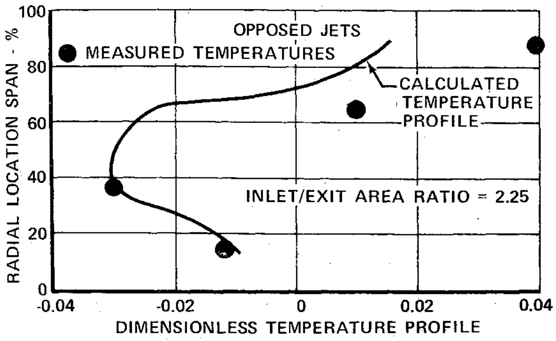


Fig. 5 Substantiation for opposing rows of dilution jets, and for converging flow area.

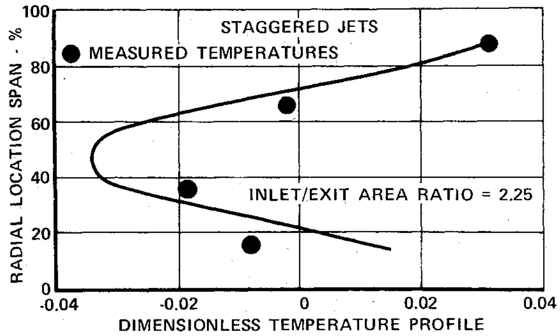


Fig. 6 Substantiation for opposing rows of dilution jets, and for converging flow area.

Temperature Field Combination

The procedure for combining the separate temperature fields produced by the opposing rows of dilution jets, which was found to give the best results, involves first the calculation of each separate temperature field. For directly opposed jets, the region occupied by each temperature field extends from the liner from which the jets entered, up to the momentum-balance fictitious wall. The separate fields are placed together with the dimensionless temperature ratio at the fictitious wall obtained by averaging. For staggered jets, the temperature field for each row of jets is calculated over the entire burner height. The jets are assumed to overlap between their respective centerlines, and the dimensionless temperature ratios in the overlap region are obtained by averaging. Outside of this overlap region, the dimensionless temperature ratios are those of the respective jets.

Calculation Procedures for Flow-Area Change

Most gas turbine engine primary combustors have some degree of flow-area convergence, from the inlet to the dilution zone, to the inlet to the turbine. Two major effects are seen. The first results from the acceleration of the flowfield as the flow area is decreased. The length of time the mixing gases remain in the burner is less than if the flow area were constant, hence, less mixing will occur. Further, the assumption can be made that both the hot and cold streams will be accelerated uniformly once the jets enter the hot-gas flowfield. This latter assumption results in the determination that the controlling momentum flux ratio, for calculation purposes, is at the plane of dilution jet injection. The second is the effect of nonuniform convergence is that, the case wherein one wall of the burner has a greater flowpath change than the other liner.

The appropriate means by which to determine the effect of accelerating flowfield has been found by defining an equivalent mixing length, X_{EQ}, which is the volume of the burner between the dilution jet inlet plane and the burner exit plane, divided by the burner cross-sectional area at the dilution jet inlet plane. This definition of an effective length has been used with success in performance calculations for mixing-limited rocket engine combustion chambers. The temperature profiles at the exit of the combustor are obtained by nondimensionalizing the vertical (or radial) locations at the height at the combustor inlet plane.

The effect of a nonuniform convergence considers the nonsymmetric turning of the cross-flow to be reflected in a different equivalent burner height for each separate row of jets. These different equivalent heights depend upon whether the OD or ID jets are those which see the greatest amount of convergence.

Substantiation

Comparisons between experimentally measured and anlytically predicted dimensionless temperature ratios are shown as transverse averages, plotted in the form of locally measured (or calculated) values, minus the average dimensionless temperature ratio.

Figure 5 shows a comparison, using the data of Ref. 4, for the case of directly opposed dilution jets. Although equal amounts of diluent were injected through each liner, the exit temperature profile is nonsymmetric because of the nonuniform convergence of the test section. The analytical procedures accurately assess the effect of this nonuniform convergence and give a predicted profile in good agreement with the measured data.

Figure 6 shows a comparison for the case of staggered dilution jets. Again, the exit dimension temperature profile is nonsymmetric, and the analytical profiles reflect this nonsymmetry.

Cooling Air Mixing Analysis

Single Slot Analysis

To obtain a gas temperature profile from a single louver cooling slot, the following procedure was developed to give a semiempirical approximation. The main assumption made in this procedure was that the profiles downstream of the louver were all of the same Gaussian form

$$\frac{T-T_{FS}}{T_{\mathrm{ADW}}-T_{FS}}=\exp\left[-\frac{\pi}{4}\left(\frac{Y}{\phi}\right)^2\right] \tag{6}$$

To determine the temperature of the profile (T) requires that values be found for the adiabatic wall temperature (T_{ADW}) and the temperature profile thickness (ϕ). To determine the wall temperature, the empirical film cooling correlation of Juhasz and Marek is used.

$$\frac{T_{\mathrm{ADW}}-T_{FS}}{T_{CS}-T_{FS}}=\left[1+C_M\left(\frac{X}{MS}\right)\right]^{-1} \tag{7}$$

To determine the temperature profile thickness, the assumption is made that the enthalpy under the profile remains constant from the initial conditions at the louver lip where the profile is assumed to be a step function.

$$\text{profile enthalpy}=\int_0^{\text{span}}\rho(Y)\;V(Y)\;(T-T_{FS})\mathrm{d}A \tag{8}$$

The density ($\rho(Y)$) is found by ratioing it to the freestream value, which then becomes the inverse of the temperature ratio. An assumption is then made to relate the velocity profile ($V(Y)$) to the temperature profile. Reichardt has derived the following semiempirical relation for free turbulent conditions which is appropriate for the major part of the profile

$$T/T_{\max}=(V/V_{\max})^{1/\gamma} \tag{9}$$

Here, the temperature and velocity scales are adjusted so that their zeros coincide. The ratio of heat to momentum diffusivity (γ) has been experimentally determined to be about 2 under turbulent conditions. This gives the velocity a Gaussian profile with half the width of the temperature profile, but the velocity at the wall, which is the maximum, is the one undetermined value. To arrive at this value, the assumption is made that the mass defect in the profile is constant and the velocity profile at the louver lip is a step function.

$$\text{profile mass} = \int_0^{\text{span}} [\rho(Y)\ V(Y) - \rho_{FS} V_{FS}]\ \mathrm{d}A \tag{10}$$

These assumptions and equations are then sufficient to determine the temperature profile thickness. Combining the assumptions, the conservation equations are then solved simultaneously for the thickness and wall velocity.

The success of this profile prediction system depends on the prediction of the wall temperature by the film cooling correlation. In using the Juhasz and Marek correlation given previously, it was necessary to modify it slightly so that the prediction of the wall temperature would not go below the temperature of completely mixed flow. If it should, the procedure would be unable to find a real profile width to satisfy the equations. The following equation assumes that the temperature at infinity in the correlation should be the completely mixed temperature in the duct.

$$\frac{T_{\mathrm{ADW}} - T_{FS}}{T_{CS} - T_{FS}} = \frac{1 + (W_{CS}/W_B) C_M (X/MS_t)}{1 + C_M (X/MS_t)} \tag{11}$$

Correlation Procedures for Flow-Area Change

The preceding analysis neglects the effect of a converging duct where the mass flux ratio (M) is changing with distance (X). All that is given is that the mixing coefficient (Cm) has a value of about 0.15 for turbulent combustor type conditions. By using the Carlson and Talmor correlation which will handle accelerating flow, it was found that the same mixing coefficient gives a good approximation if the mass flux for the freestream, at the location where the profile is calculated, is used in determining the mass flux ratio (M). Since the Carlson and Talmor correlation was difficult to use and did not lend itself to programming, this modified form of the Juhasz and Marek correlation was used.

Multiple-Louver Combined Profiles

The above procedure gives a good profile for a single louver. These must then be combined at the end of the combustor. The approach taken is to separate the profile into separate gases at freestream and coolant temperature. The ratio of the mass of these two gases at each point in the profile is given by

$$W_{CS} / W_{FS} = (T - T_{FS}) / (T_{CS} - T) \tag{12}$$

The assumption is then made that the mass of freestream temperature gas is the same at the same profile location for all the profiles to be summed, and also that the ratio of the two gases is the same as in the original profile. The final summed profile is then found by

$$T_{CA} = T_{FS} + (T_{CS} - T_{FS}) \frac{\Sigma(W_{CS}/W_{FS})}{1 + \Sigma(W_{CS}/W_{FS})} \tag{13}$$

This approach will not arrive at temperatures less than the coolant temperature or greater than the freestream temperature. It is therefore a physically reasonable approach.

Since the profiles are to be summed after being calculated individually, the single calculations must ignore the contribution of the other louvers. Therefore, the temperature at infinity used in the calculation of the profile from each slot is the average exit temperature with the enthalpy from all of the louver cooling air removed. But in the calculation of the mass flux ratio, the mass contribution of the other louvers is considered.

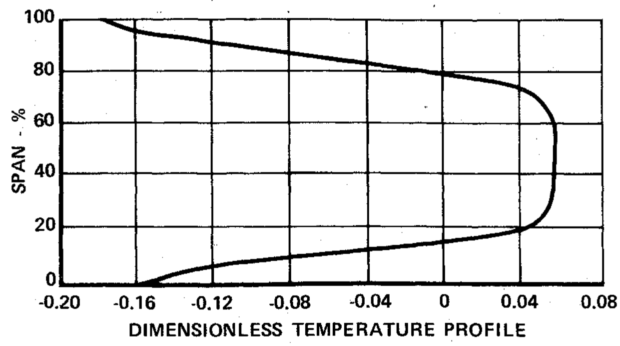


Fig. 7 Calculated combustor cooling air profile.

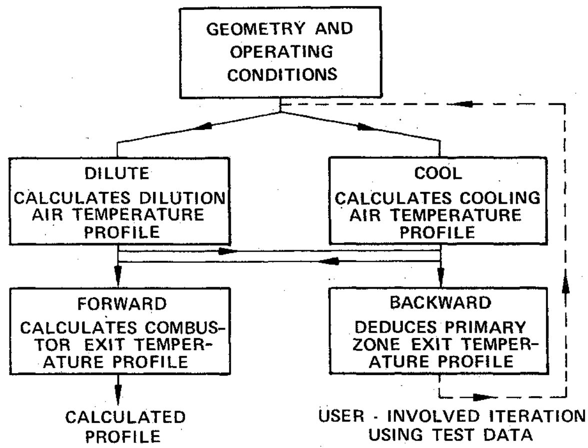


Fig. 8 AEDS computation flow chart.

Substantiation

The substantiation for the procedure used to derive the cooling air profile is very limited. Figure 7 shows a calculated cooling air temperature profile for representative combustor cooling airflows. A significant point of the analysis is the choice of the mixing coefficient for the efficiency correlation. The value chosen for the coefficient has an important effect on the profile so that with the range of values possible there is enough variation to calibrate the prediction system to achieve a reasonable profile for a general combustor. So far, a mixing coefficient of 0.15 has given good agreement between predicted and measured profile shifts.

Design System Synthesis

Approach

Formulation of the unified design system consists of combining the independently generated dilution air temperature and cooling air profiles into the combustor aft end exit temperature profile. Procedures for calculating the contribution of the combustor primary zone to the exit temperature profile are not presently available. Combination of the dilution and cooling air profiles includes a way to deduce the primary zone profile contribution from test data.

Figure 8 shows the overall computation flow charts. DILUTE calculates the dilution air temperature profile; COOL, the cooling air temperature profile. These, in turn, supply their calculated temperature profiles to the final calculation procedure, herein lumped into a FORWARD package and a BACKWARD package.

The FORWARD package accepts the calculated dilution and cooling air temperature profiles, plus a primary zone temperature profile contribution, and calculates the aft end exit temperature profile. The primary zone temperature profile may be input as a constant value or as a deduced temperature array. The constant value would be used at the beginning of the design process, before test data become available. Once test data are taken, they are used to deduce the primary zone temperature profile contribution.

The BACKWARD package handles this process of deducing the primary zone profile contribution. It accepts as input the calculated dilution and cooling air temperature profiles plus the measured combustor exit temperature profile, and returns the deduced primary zone profile contribution.

Calculation of Exit Temperature Profile

Previously, all analyses were for rectangular ducts, and setting the jet spacing was straightforward. In an actual annular combustor having, for example, the same number of ID and OD dilution jets, the ID jets are physically closer together than are the OD jets. The ID jets spread apart on penetrating outward into the combustor. The OD jets converge on penetrating inward into the combustor. Definition of the jet spacing for use in the mixing correlations is no longer obvious. The equivalent spacing for the ID dilution jets is based upon an equivalent surface, area-weighted between the ID liner and the momentum-balance split plane. A corresponding definition holds for the OD jets.

The procedure used to obtain the combined, combustor exit temperature profile from the individually generated cooling and dilution air temperature profiles is the algebraic addition of the separate profiles. Because the cooling air in the aft end of the combustor is a small fraction, ¼ or less, of the total combustor airflow, and because the cooling air is concentrated near the combustor liners, the "first-order" approximation for the profile calculations neglects the effect of the dilution and cooling air upon each other.

For calculation of the dilution-air mixing, the first-order approximation uses a mixed temperature based upon the total combustor airflow, minus the aft-end cooling airflow. The dilution jet temperature is the shroud air temperature. The combustion temperature of the combustor front end is based upon the fuel/air ratio up to the leading edge of the dilution slots and assuming 100% combustion efficiency at this point. For calculation of the cooling-air mixing, the first-order approximation replaces the freestream temperature, T_{FS}, by the mixed temperature defined previously.

Algebraic addition of the separate profiles gives the combustor exit temperature profile about the mixed temperature. The combustor exit profile, relative to the turbine inlet temperature, is then obtained by adding the difference between the mixed temperature and the turbine inlet average temperature.

Substantiation

Substantiation of the Aft End Design System is based upon comparison of predicted and measured profiles for a low bypass ratio military augmented turbofan and a high bypass ratio commercial turbofan. The substantiation procedure, which was followed, was the same in both cases. A baseline test was used to deduce the primary zone exit temperature array. This deduced array was then input, and the effect of combustor dilution zone hardware changes was predicted. The predicted exit average radial temperature profiles are compared with the measured profiles.

Military Turbofan Testing

The military turbofan testing was directed toward improvement of the exit average radial profile by shifts in the dilution air from the OD liner to the ID liner. The test shifted 10% of the combustor air from the OD to the ID. Since the dilution air is roughly 40% of the combustor airflow, this amounted to decreasing the OD dilution airflow, and increasing the ID dilution airflow by 50%.

Figure 9 shows the measured exit average radial temperature profile in the form of a dimensionless temperature

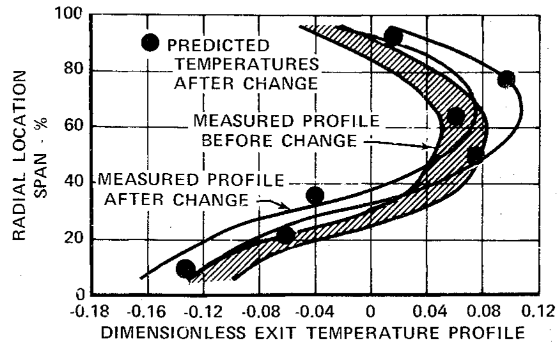
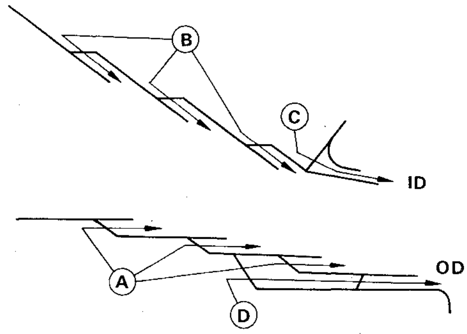


Fig. 9 Predicted profile shift from 10% dilution air shift on a military turbofan engine.



	A	B	C	D	TOTAL
BEFORE CHANGE	5.1	3.5	.1.17	0.98	10.75
AFTER CHANGE	3.7	3.7	0.62	0.51	8.53

Fig. 10 Cooling air changes in a commercial turbofan engine.

for the baseline geometry. This profile was obtained from Temperature Sensitive Paint (TSP) applied to the first-stage turbine blades, extrapolated back to combustor exit conditions. The combination of TSP measurements and extrapolation gives a temperature error band. This profile was input to the AEDS to deduce the primary zone exit temperatures.

Figure 9 also shows the comparison between the predicted exit average radial temperature profile and the measured profile band. Qualitative agreement is quite good. The AEDS correctly predicts a profile peak shift toward the OD and an increase in peak temperature. Quantitative agreement is also good. The greatest deviation between the measured and predicted value is about 0.050, at the 36% span location. The average deviation between measured and predicted temperatures is about 0.022.

The "notch" at the 36% and 64% span locations results from the mixing analysis procedures for the dilution air profiles. Further improvements to the mixing analyses require additional development and better test data on the dilution air mixing process.

Commercial Turbofan Engine

The commercial turbofan engine testing examined the effect of changes in the cooling airflows through the final three ID and OD louvers, and in the turbine platform cooling airflow. Profile changes were measured using instrumented first-stage turbine vanes. The changes in the airflow distribution are shown in Fig. 10. Figure 11 shows the measured exit average radial temperature profile for the baseline geometry.

Analysis of the exit temperature profile shifts was complicated by the geometry of the dilution injection ports on the OD liner. The combustor injects the dilution air through directly-opposing rows of jets, with 40 jets/row on the ID and 80 jets/row on the OD. The OD jets are in pairs, inline axially, and spaced approximately two hole diameters apart. Each pair of OD jets directly opposes an ID jet.

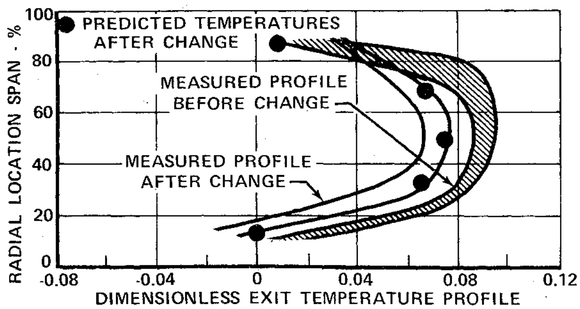


Fig. 11 Predicted profile shift from 2% cooling air shift on a commercial turbofan engine.

The AEDS, as currently formulated, cannot directly handle more than one row of dilution ports per liner. However, Ref. 3 reports that pairs of jets behave like a single row of ports having the same open area and transverse spacing, for moderate-to-high momentum flux ratios.

This argument led to the replacement in the AEDS, of the OD dilution port pairs with a row of single ports, each port having the same total area. The single ports were located at the same axial location as the upstream port in the pairs of ports. Figure 11 shows the comparison between the predicted exit average radial temperature profile and the measured profile following the change in combustor cooling air. Agreement is superb over almost all of the profile. This success is viewed as a confirmation of the validity of the procedures describing the mixing of the combustor aft end cooling air, since the combustor modifications involve cooling flow changes only.

Conclusions

Comparison of the analytical predictions of both the component process analyses in the Aft End Design System and of the AEDS as a whole, with available test data, leads to the following conclusions.

1)The procedures to predict the mixing behavior of opposing rows of dilution jets, in geometries representative of current GTE design practice, give good agreement with the limited amount of available data.

2) The procedures to predict the mixing behavior of the cooling air have no direct data substantiation under ideal conditons. The results appear to be qualitatively reasonable when compared with profile shifts resulting from cooling air changes in actual combustors.

3) The AEDS gives good qualitative agreement between predicted and measured profile shifts resulting from dilution or cooling air changes for a limited amount of testing. Better qualitative agreement requires further improvements to the analytical mixing models.

4) Testing of single rows of multiple dilution ports should be done using nonround ports in a wider range of geometries than the testing described in Ref. 3.

5) Testing of opposing rows of multiple dilution jets should be done under a wider range of geometric and flow conditions than the testing described in Ref. 4.

6) The analytical procedures describing the mixing of opposing rows of multiple dilution jets could be revised and improved to give more reliable prediction of absolute profile levels.

7) Testing of multiple cooling slots under high-turbulence and accelerating flow conditions should be done to obtain mixing behavior data for single or multiple louvers for comparison with the analytical predictions.

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