Fuel Effects on Gas Turbine Combustion-Liner Temperature, Pattern Factor, and Pollutant Emissions

A H Lefebvre*
Purdue University, West Lafayette, Indiana

An anaytical study is made of the substantial body of experimental data acquired during recent Wright Patterson Aero Propulsion Laboratory sponsored programs on the effects of fuel properties on the performance and reliability of several gas turbine combustors, including J79-17A, J79 17C (Smokeless), F101, TF41, TF39, J85, TF33, and F100 Quantitative relationships are derived between certain key aspects of combustion, notably liner wall temperature, pattern factor, and exhaust emissions, and the relevant fuel properties, combustor design features and combustor operating conditions It is concluded that fuel chemistry, as indicated by hydrogen content and/or aromatics content, has a significant effect on flame radiation and liner wall temperature, but only a slight effect on the emissions of carbon monoxide (CO) and oxides of nitrogen (NO_x) The physical properties that govern atomization quality and evaporation rates affect CO emissions, but other important performance parameters, such as NO_x emissions and liner wall temperature, are sensibly independent of physical properties over the range of fuels studied

Nomenclature

A = area, m^2
C_1 = heat flux from combustion gases to liner wall by convection, W/m^2
C_2 = heat flux from liner wall to annulus air by convection, W/m^2
c_p = specific heat at constant pressure, J/kg K
C/H = carbon/hydrogen ratio of fuel, by mass
D_h = hydraulic mean diameter of atomizer air duct at exit plane, m
D_L = liner diameter or height, m
D_p = atomizer prefilmer diameter, m
f_{pz} = fraction of total combustor air employed in primary zone combustion
k = thermal conductivity, J/ms K
L = length, or luminosity factor
L_e = liner length employed in fuel evaporation, m
L_L = total liner length, m
ℓ_b = mean beam length of radiation path, m
$\dot{m}$ = mass flow rate, kg/s
n = reaction order
P = pressure, Pa
q = fuel/air ratio
q_{ov} = combustor overall fuel/air ratio
q_{ref} = reference dynamic head, kPa
R_1 = radiation heat flux from combustion gases to liner wall, W/m^2
R_2 = radiation heat flux from liner to casing, W/m^2
SMD = Sauter mean diameter of fuel spray, m
T = temperature, K
T_{bn} = boiling temperature at normal atmospheric pressure, K
T_{Lmax} = maximum liner wall temperature for given fuel, K
T_{L0} = maximum liner wall temperature for JP4 K
t_e = evaporation time, s
U = velocity, m/s
V = combustion volume (general), m^3
V_c = total combustion zone volume = predilution zone volume, m^3
V_e = evaporation volume, m^3
ΔP = pressure differential kPa
ΔT = temperature rise, K
ϵ = emissivity
λ_{eff} = effective value of evaporation constant, m^2/s
μ = dynamic viscosity, kg/ms
ν = kinematic viscosity, m^2/s
ρ = density, kg/m^3
σ = Stefan Boltzmann constant ($5.67 \times 10^{-8} W/m^2 K$) or surface tension, kg/s^2

Subscripts

A = air
an = annulus value
F = fuel, or formation
g = gas
L = liner value
pz = primary zone value
w = wall value
3 = combustor inlet value
4 = combustor outlet value

Introduction

THE alternative fuel sources now being sought and the acceptance of a broader specification for avaiation fuels highlight the need for prediction techniques that will allow the impact of any change in fuel specification on hardware durability and combustion performance to be estimated accurately in the combustor design stage Unfortunately the effect of a change in fuel properties is not constant for all combustors but varies between one combustor and another due to differences in operating conditions and differences in design For example, the effect of an increase in carbon/hydrogen ratio on liner wall temperature is much greater for combustors featuring fuel-rich primary zones than for combustors in which the primary zone is fuel weak This is because with rich primary zones most of the heat transferred to the liner wall is by radiation, which is proportional to ϵT_g^4 Thus liner wall temperature is dependent on the flame emissivity, ϵ, which, in turn, is dependent on the C/H ratio of the fuel. With fuel weak primary zones, however, most of the heat transferred to the liner wall is by forced convection Here the dominant term is the gas temperature T_g which is fairly insensitive to changes in C/H ratio In consequence, quite

Received March 21 1984; revision received June 28, 1984

Reilly Professor of Combustion Engineering Member AIAA

large changes in C/H ratio produce only a slight effect on liner wall temperature

Another complicating factor is that the various properties and characteristics of petroleum fuels are so closely interrelated that it is virtually impossible to change any one property without affecting many others However there are several mitigating factors that help to ease the situation For example, atomization quality is influenced only by the physical properties of the fuel namely viscosity, surface tension, and density, all of which are easily measured by standard laboratory techniques Moreover it can also be shown that evaporation rates are closely linked to the physical properties of the fuel; for example ρ_F provides a useful indication of fuel volatility

The basic data employed in this investigation were obtained from a number of programs initiated by the U S Air Force, Army, Navy and NASA, along with engine manufacturers to determine the effects of anticipated future fuels on existing engines As a result of these studies, data have become available that yield new and useful insights into fuel property effects on combustion performance References 1 6 also contain detailed information on all the relevant chemical and physical properties of the fuels employed These fuels were supplied by the U S Air Force for combustion system evaluation They included a current JP4, a current JP8, five blends of the JP4, five blends of the JP8 and, in some cases, a No. 2 diesel fuel The blends were intended to achieve three different levels of hydrogen content, i e , 12, 13, and 14% by mass

The key chemical and physical properties of the fuels selected are listed in Table 1 Additional information on the distillation characteristics of the test fuels is contained in Fig 1

In a recent companion paper[7] attention was focused on the influence of fuel chemistry and physical properties on the combustion efficiency performance, lightup characteristics, and lean blowout limits of gas turbine combustors The objective of the present work is to extend the analysis to include the effects of fuel type, fuel spray characteristics, liner dimensions, and combustor operating conditions on liner wall temperature pattern factor, and pollutant emissions

Fuel Spray Characteristics

A major drawback to the data contained in Refs 1 6 is that they contain very little information on fuel spray characteristics; in particular, no measurements were made of mean drop size for any of the combustors employed in the investigation

In the absence of actual measured values, the mean drop size (SMD) was calculated using one of the following two expressions:

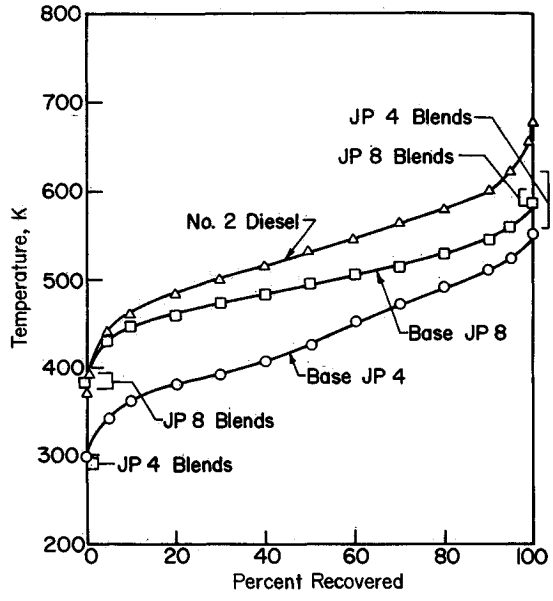

Fig 1 Distillation characteristics of test fuels [2]

For airblast atomizers[8]

$$\frac{\text{SMD}}{D_h} = \left[1+\frac{\dot{m}_F}{\dot{m}_A}\right]\left[0.33\left(\frac{\sigma_F}{\rho_A U_A{}^2 D_p}\right)^{0.6}\left(\frac{\rho_F}{\rho_A}\right)^{0.1} + 0.068\left(\frac{\mu_F{}^2}{\rho_F \sigma_F D_p}\right)^{0.5}\right] \quad (1)$$

For pressure swirl atomizers[9]

$$\text{SMD} = 0.071\,\sigma_F{}^{0.25} v_F^{0.25} \dot{m}_F^{0.25} \Delta P_F^{0.5} \rho_g{}^{0.25} \quad (2)$$

Equation (1) takes full account of variations in fuel properties (σ_F ρ_F and μ_F) air properties (ρ_A and U_A), and atomizer geometry (D_p and D_h) The values of the constants and exponents in this equation were established in a number of experimental studies that covered much wider ranges of fuel and air properties than are needed for the present investigation Thus the main source of error in the use of Eq (1) stems from uncertainties surrounding the values to be assigned to the atomizer dimensions With Eq (2) problems arise in the calculation of ρ_g, since the primary zone temperature cannot be estimated accurately Another potentially serious source of error that applies to both equations is that

Table 1 Test fuel chemical and physical properties

Fuel components		Hydrogen contents (H)—	Heating value (net)	Density (ρ_{300K})	Viscosity (ν_{300K})	Surface tension (σ_{300K})	Vapor pressure (P_{300K}),
Base fuel	Blending agents	weight %	MJ/kg	kg/m^3	mm^2/s	mN/m	kPa
JP 4	—	14 5	43.603	752 7	0 924	23 27	12 04
JP 8	—	14 0	43.210	799 5	1 849	25 85	2 15
JP 8	Gulf mineral seal oil	13 9	43.189	801 2	2 071	25 92	1 97
JP 8	2040 solvent	12 0	41 947	852 3	1 809	27 62	1 16
JP 8	Xylene bottoms	13 0	42 724	813.4	1 428	26 38	1 48
JP 8	Xylene bottoms	12 0	42 129	827 6	1.160	26 66	1 33
JP 8	2040	13 0	42 556	825 2	1 804	26 42	1 38
JP 4	2040	12 0	42 203	829 7	1 141	25 22	7 38
JP 4	2040	13 0	42 629	796 3	1 028	23 75	8 61
JP-4	Xylene	12 0	42 196	808 0	0 830	25 21	6 17
JP 4	Xylene	13.0	42 682	786 5	0 835	24 20	9 06
JP 4	Xylene & GMSO	14 0	43 366	769 6	1 057	23 45	10 25
2 D	—	13 1	42 691	837 2	3 245	27 35	1 59

all the experimental work involved in their formulation was carried out under cold, i.e , non burning and fairly quiescent conditions Clearly, drop sizes could be appreciably different in the true combustor environment due to the combined ef fects of high temperature, high turbulence, and strong airflow currents

Liner Wall Temperature

For the purpose of analysis a liner may be regarded as a container of hot flowing gases surrounded by a casing in which air is flowing between the container and the casing Broadly the liner is heated by radiation and convection from the hot gases inside it and is cooled by radiation to the outer casing and by convection to the annulus air The relative proportions of the radiation and convection components depend upon the geometry and operating conditions of the system. Under equilibrium conditions the liner temperature is such that the internal and external heat fluxes at any point are just equal

$$R_1 + C_1 = R_2 + C_2 \tag{3}$$

Internal Radiation, R_1

This is the component of heat transfer that is most affected by a change in fuel type It is given by[10]

$$R_1 = 0\,5\sigma(1+\epsilon_w)\epsilon_g T_g^{1\,5}(T_g^{2\,5} - T_w^{2.5}) \tag{4}$$

The "bulk" or mean gas temperature T_g is obtained as the sum of the chamber entry temperature T_3 and the temperature rise due to combustion ΔT_{comb} Thus,

$$T_g = T_3 + \Delta T_{comb}$$

ΔT_{comb} may be derived from standard temperature rise curves The appropriate value of fuel/air ratio is the product of the local fuel/air ratio and the local level of combustion efficiency Most heat transfer calculations are carried out at high pressure conditions where it is reasonable to assume a combustion efficiency of 100%

For the luminous flames associated with the combustion of heterogeneous fuel air mixtures, the value of ϵ_g for insertion in Eq (4) is obtained as[10]

$$\epsilon_g = 1 - \exp - [0\,29 P_3 L (q\ell_b)^{0\,5} T_g^{-1\,5}] \tag{5}$$

where q is the local fuel/air ratio and ℓ_b is the ' beam length" of the radiating gas. Beam length depends on the size and shape of the gas volume For most practical purposes it is given to sufficient accuracy[10] by the expression

$$\ell_b = 3.4 \text{ (volume/surface area)}$$

The luminosity factor L is an empirical correction in troduced to obtain reasonable agreement between ex perimental data on gas radiation and predictions from Eq (4) Experiments have shown that luminosity factor depends largely on the carbon to hydrogen mass ratio of the fuel[9 10 11]

The original equation for L is[10]

$$L = 7\,53\,(C/H - 5\,5)^{0\,84} \tag{6}$$

Later the following expression was suggested by Kret schmer and Odgers[11]

$$L = 0\,0691\,[C/H - 1\,82]^{2\,71} \tag{7}$$

Another correlation, which is simpler and probably no less accurate is[9]

$$L = 3(C/H - 5\,2)^{0\,75} \tag{8}$$

More recent investigations have tended to emphasize fuel hydrogen content rather than carbon/hydrogen ratio as the property most relevant to flame radiation Figure 2 shows the correlation obtained by Blazowski and Jackson[12 13] between hydrogen content and liner wall temperature for several engines The data shown represent cruise conditions with combustor inlet temperatures ranging from 547 to 756 K The parameter used to correlate the experimental data is

$$(T_{L_{max}} - T_{L_0})/(T_{L_0} - T_3)$$

in which the numerator represents the increase in maximum liner temperature over that obtained with a baseline fuel containing 14 5% hydrogen.

The excellent correlation of data exhibited in Fig 2 could not be duplicated for the results contained in Refs 1 4 as shown, for example, in Fig 3 It is believed that this is because the magnitude of the parameter $(T_{L_{MAX}} - T_{L_0})/$

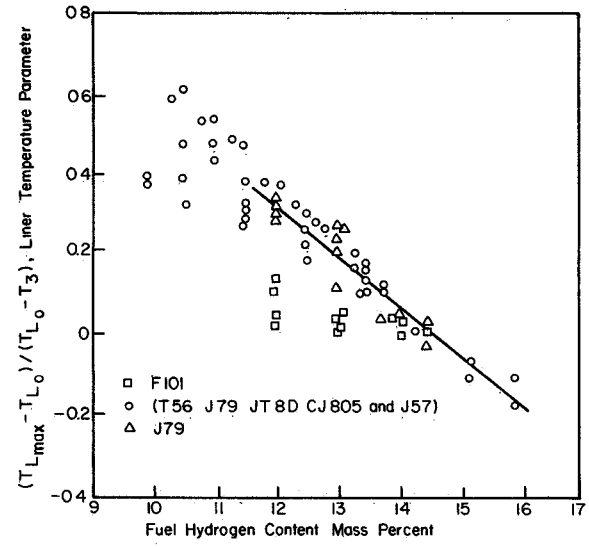

Fig 2 Effect of fuel hydrogen content on liner temperature parameter at cruise operating conditions.[12 13]

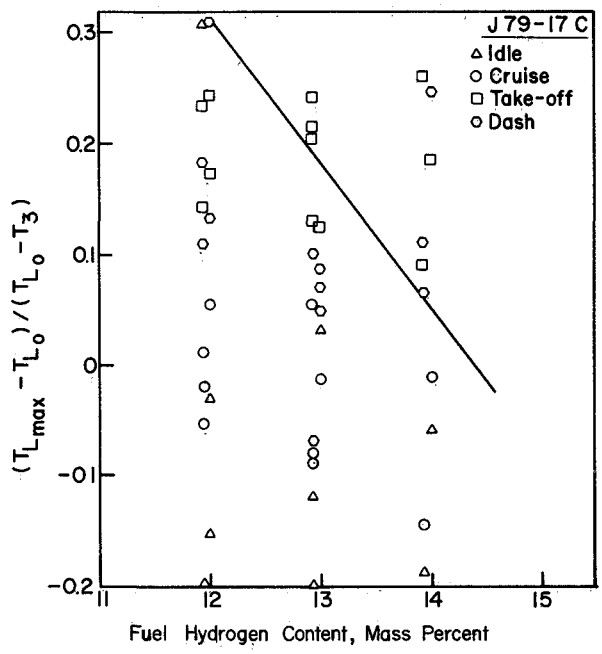

Fig 3 Effect of fuel hydrogen content on liner temperature parameter for J79 17C combustor

$(T_{L_0}-T_3)$ is very sensitive to the value of T_{L_0} Since maximum values of wall temperature are notoriously difficult to determine, this would appear to be a serious drawback to the use of this type of temperature parameter

An alternative approach attempted here was to find a relationship between fuel hydrogen content and luminosity factor Analysis of the experimental data led to the following expression

$$L=336/(\%H_2)^2 \tag{9}$$

Substitution of this value of L into Eq (5) allows calculations of flame radiation to be carried out for all fuels over the entire range of test conditions

External Radiation, R_2

The significance of R_2 increases with liner wall tem perature, and at low values it can often be neglected It can be estimated only approximately due to lack of accurate knowledge of wall emissivities. For this reason it is sufficient to use the cooling air temperature T_3 in place of the unknown temperature of the outer air casing

Values of emissivity for various materials may be obtained from McAdams [14] However, for most practical purposes the following expression, based on typical values of emissivity and diameter ratio, will suffice[9 10]

$$R_2=0\,4\,\sigma(T_w^4-T_3^4) \tag{10}$$

Internal Convection, C_1

Of the four heat transfer processes which together deter mine the liner temperature, this component is the most dif ficult to estimate accurately In the primary zone the gases involved are at high temperature and undergoing rapid physical and chemical change Further difficulty is introduced by the existence within the primary zone of steep gradients of temperature, velocity, and composition Uncertainties regarding the airflow pattern, the state of the boundary layer development, and the effective gas temperature make the choice of a realistic model almost arbitrary.

In the absence of more exact data it is reasonable to assume that some form of the classical heat transfer relation for straight pipes will hold for conditions inside a liner, using a Reynolds number index consistent with established practice for conditions of extreme turbulence This leads to an ex pression of the form[10]

$$C_1=0\,017\left(\frac{k_g}{D_{\kappa\psi}^{0.2}}\right)\left(\frac{\dot{m}_{pz}}{A_L\mu_g}\right)^{0\,8}(T_g-T_w) \tag{11}$$

External Convection, C_2

This is obtained as[10]

$$C_2=0\,020\left(\frac{k_A}{D_{an}^{0.2}}\right)\left(\frac{\dot{m}_{an}}{A_{an}\mu_A}\right)^{0\,8}(T_w-T_3) \tag{12}$$

The fluid properties are evaluated at the annulus air tem perature T_3 In practice the cooling air temperature increases during the passage downstream, but normally this amounts to no more than a few degrees and can reasonably be neglected

Restating Eq (3), for equilibrium

$$R_1+C_1=R_2+C_2$$

Solution of this equation yields the wall temperature, T_w

The value of T_w as determined by the method outlined above represents the liner wall temperature that would be obtained in the absence of internal wall cooling Un fortunately, Refs 1 6 do not contain the detailed information needed to estimate film cooling effects on T_w Bearing in mind the lengthy and tedious nature of the procedure in volved, it was thus decided to calculate 'uncooled' wall temperatures for four combustors only to ascertain if the results obtained reflected anticipated trends in regard to the effect of fuel hydrogen content on liner wall temperature The results of these calculations for the J79 17C and F101 combustors are shown in Figs 4 and 5 for all fuels as plots of T_w vs hydrogen content

It may be noted in Figs 4 and 5 that the calculated values of T_w are generally higher than the corresponding measured

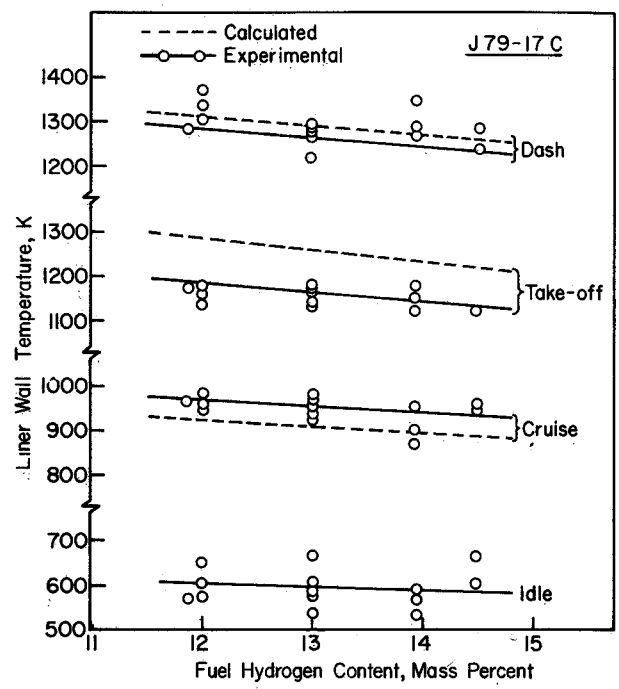

Fig 4 Comparison of measured and predicted values on the effect of H_2 content on liner temperature for J79 17C combustor

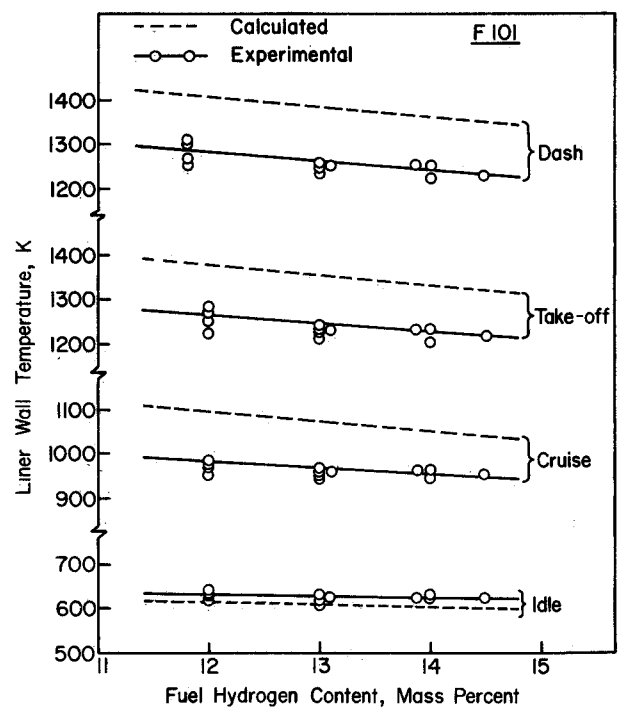

Fig 5 Comparison of measured and predicted values on the effect of H_2 content on liner temperature for F101 combustor

values due to neglect of internal wall cooling Only at low power conditions, where the errors incurred through neglect of internal wall cooling are partially balanced by the assumption of 100% combustion efficiency in the combustion zone, do the measured and calculated wall temperatures roughly coincide

These factors are not considered too serious in a study that is mainly concerned with fuel type because they apply with equal force to all fuels The fact that the measured and calculated values of T_w follow the same trend, as evidenced by Figs 4 and 5, tends to support the validity of using the luminosity factor concept as a convenient means of in corporating fuel hydrogen content into the "standard" equation for flame emissivity Thus Eq (5) may be rewritten as

$$\epsilon_g = 1 - \exp - [97\,44 P_3 (\%H_2)^{-2} (q\ell_b)^{0\,5} T_g^{-1\,5}] \quad (13)$$

Pattern Factor

Perhaps the most important and at the same time most difficult problem in the design and development of gas tur bine combustion chambers is that of achieving a satisfactory and consistent distribution of temperature in the efflux gases discharging into the turbine In the past experience has played a major role in the determination of dilution zone geometry and trial and error methods have been widely used in developing the temperature traverse quality of individual combustor designs to a satisfactory standard Experimental investigations into dilution zone performance carried out on actual chambers have provided useful guidance but very often it has proved difficult or impossible to distinguish the separate influences of all the variables involved Thus although it is now generally accepted that a satisfactory temperature profile is dependent upon adequate penetration of the dilution jets coupled with the correct number of jets to form sufficient localized mixing regions, the manner in which the total dilution hole area is utilized in terms of number and size of holes is still largely a matter of experience

The final mixing process is affected in a complicated manner by the dimensions geometry, and pressure drop of the liner; the size, shape and discharge coefficients of the liner holes; the airflow distribution to various zones of the chamber; and the temperature distribution of the hot gases entering the dilution zone For any given combustor, the latter is strongly influenced by fuel spray characteristics such as drop size, spray angle, and spray penetration, since these control the pattern of burning and hence the distribution of temperature in the primary zone efflux It is known that spray characteristics are strongly influenced by pressure, especially with atomizers of the simplex or dual orifice type, and it is to be expected, therefore that temperature traverse will also vary with pressure, although the extent of this variation will vary from one chamber to another depending on design and in particular on length

Several parameters have been proposed to describe the temperature distribution in the combustor efflux Perhaps the most widely used is the "overall temperature distribution factor' which tends to highlight the maximum temperature found in the traverse and is, therefore of special importance to the design and durability of nozzle guide vanes It is normally defined as

$$\text{Pattern factor} = (T_{\max} - T_4)/(T_4 - T_3) \quad (14)$$

Correlation of Data

Two parameters of crucial importance to pattern factor are liner length which controls the time and distance that are available for mixing, and the pressure drop across the liner which governs the penetration of the dilution jets and their rate of mixing with the products of combustion In a previous analysis of experimental data on tubular tubo annular and annular combustors, it was found that[9]

$$(T_{\max} - T_4)/(T_4 - T_3) = \int (L_L/D_L)(\Delta P_L/q_{\text{ref}}) \quad (15)$$

The data correlation obtained for tubular liners is shown in Fig 6 In connection with this figure it should be noted that the correlation is based not on the L/D ratio of the dilution zone, but on that of the complete liner This was found to provide a better fit to the data

For tubular and tubo annular combustors we have[9]

$$(T_{\max} - T_4)/T_4 - T_3) = 1 - \exp - \left[(0\,070(L_L/D_L)(\Delta P_L/q_{\text{ref}})\right]^{-1} \quad (16)$$

while for annular combustors

$$(T_{\max} - T_4)/(T_4 - T_3) = 1 - \exp - \left[0\,050\,(L_L/D_L)(\Delta P_L/q_{\text{ref}})\right]^{-1} \quad (17)$$

Since no information on liner pressure loss factor is con tained in Refs 1 6, this might appear to rule out the use of Eqs (16) and (17) in the present analysis Another drawback to these equations is the inherent assumption that all the liner length is utilized in mixing and combustion while the length occupied by evaporation processes is essentially zero Although this assumption is not unreasonable for highly volatile fuels of low viscosity such as JP4, it is difficult to justify for some of the alternative fuels employed in this program These deficiencies may be remedied by rewriting Eqs (16) and (17) in the following form, where Z=0 07 or 0 05 for tabular and annular liners respectively

$$(T_{\max} - T_4)/(T_4 - T_3) = 1 - \exp - \left[Z(\Delta P_L/q_{\text{ref}})/(\mathrm{L_L} - \mathrm{L_e})/(\mathrm{D_L})\right]^{-1} \quad (18)$$

L_e is the liner length required to evaporate the fuel spray It is obtained as the product of the average predilution liner velocity and the evaporation time, as is shown in Eq 19

$$L_e = U_g t_e = \left(\frac{0\,6\dot{m}_A}{\rho_g A_L}\right)\left(\frac{D_0^2}{\lambda_{\text{eff}}}\right) \quad (19)$$

The constant of 0 6 in Eq (19) stems from the assumption that 60% of the total combustor airflow enters the liner upstream of the dilution zone A more accurate assessment of this constant for each individual liner design would not be justified at this stage due to the larger uncertainty surrounding the value of D_0 In practice it was found that correlation of experimental data could be improved by reducing the value of this constant from 0 6 to 0 33 This may be explained on the grounds that the combustion process does not wait until the fuel has fully evaporated; instead burning commences as soon as sufficient fuel has evaporated to produce a flammable mixture Thus Eq (18) may be rewritten as

$$\frac{T_{\max} - T_4}{T_4 - T_3} = 1 - \exp - \left[Z\left(\frac{\Delta P_L}{q_{\text{ref}}}\right)\left(\frac{L_L}{D_L} - \frac{0\,33\dot{m}_A D_o^2}{\rho_g A_L D_L \lambda_{\text{eff}}}\right)\right]^{-1} \quad (20)$$

where ρ_g is the average gas density upstream of the dilution

zone It is calculated at a temperature T_g which is obtained as

$$T_g = T_3 + \Delta T_g$$

where ΔT_g is the temperature rise due to combustion for a fuel/air ratio of 0 6 q_{ov} A_L is the average cross sectional area of the liner It is estimated by dividing the volume of the liner by its maximum length D_L is the average diameter or height of the liner For a tubular liner it is readily obtained as $D_L = (4A_L/\pi)^{0.5}$

Mean drop sizes for insertion into Eq (20) were calculated using either Eq (1) or Eq (2) Values of λ_{eff} were read off the graphs of λ_{eff} vs T_{bn} contained in Ref 15, using values for T_{bn} obtained from Refs 1 6 at fuel temperatures corresponding to the average boiling point (50% recovered)

For the three tubular combustors examined (namely, the J79-17A, J79 17C, and TF41), values of $Z(\Delta P_L/q_{ref})$ of 0 99, 1 03, and 1 17, respectively, provided excellent correlations of the experimental data, as illustrated in Figs 7 and 8 Reference to Eq (16) shows that these values of $Z(\Delta P_L/q_{ref})$ correspond to liner pressure loss factors for these combustors of 14, 15, and 17, respectively It is of interest to note that the improvement in pattern factor with increase in engine power (due to reduction in evaporation time, as predicted by Eq (20), is fully borne out by the results contained in Figs 7 and 8

The influence of fuel type on pattern factor is manifested through the effects of mean drop size (via viscosity and surface tension) and effective evaporation constant (via T_{bn}) on droplet evaporation time Over the range of fuels examined the effect of fuel type on pattern factor is relatively small, at least at high power conditions where the evaporation time is always a small fraction of the total combustor time, regardless of fuel type. However, if measurements of pattern factor are conducted at operating conditions where the evaporation time constitutes a significant proportion of the total residence time, then a strong effect of fuel type on pattern factor should be expected This, in fact, was precisely the result obtained with the F101 combustor when the effect of fuel type on pattern factor was examined at various simulated engine operating conditions using air supplied at normal atmospheric pressure The results of these tests are shown in Fig 9, where it is of interest to note that the measured values of pattern factor are well correlated by Eq (20) using a value for $Z(\Delta P_L/q_{ref})$ of 2 0 Figure 9 demonstrates a clear effect of fuel type on pattern factor, but it would be erroneous to asume that these data have any relevance to the real engine If Eq (20) is used to calculate values of pattern factor at actual takeoff conditions (including P_3), it is found that pattern factor is virtually in

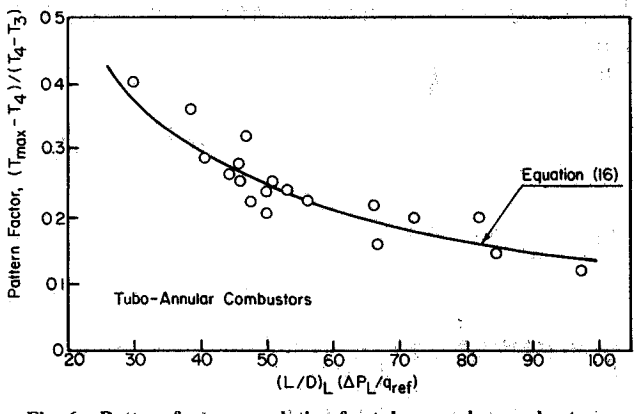

Fig 6 Pattern factor correlation for tubo annular combustors

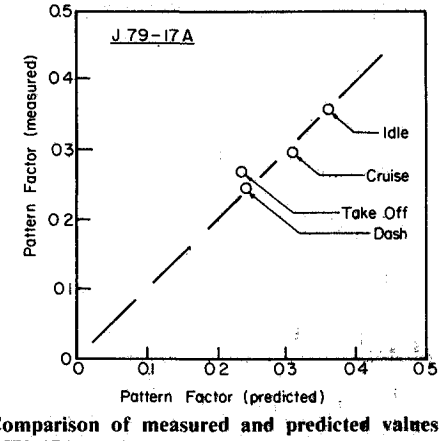

Fig 7 Comparison of measured and predicted values of pattern factor for J79 17A combustor.

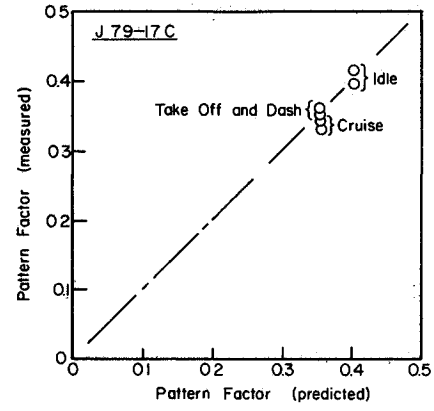

Fig 8 Comparison of measured and predicted values of pattern factor for J79 17C combustor

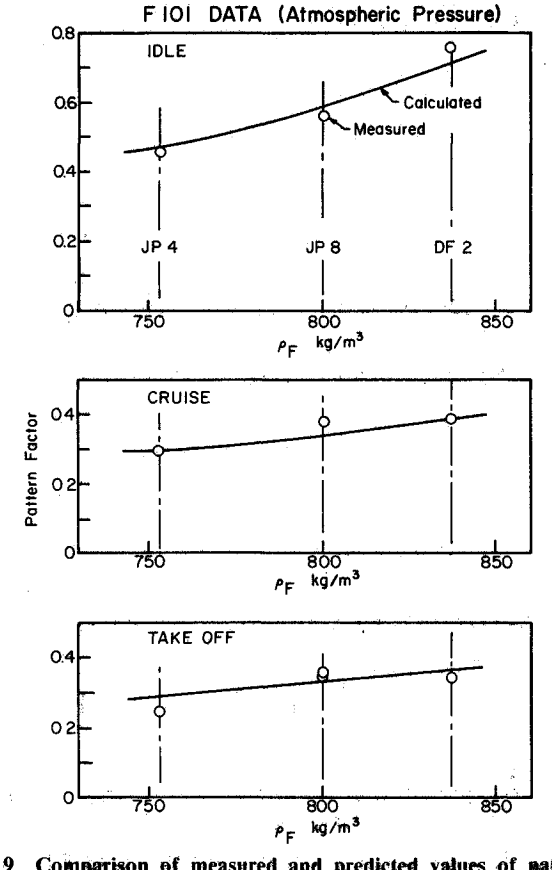

Fig. 9 Comparison of measured and predicted values of pattern factor for F101 combustor

dependent of fuel type, as shown in Fig 10 Thus from a practical viewpoint, the results of the F101 tests are fully consistent with those of all other combustors They all confirm that at the high power conditions where pattern factor is most important to engine durability variation in fuel type has a negligible effect

Pollutant Emissions

In recent years several papers and reports have been issued on the accomplishments made in the reduction of pollutant emissions from gas turbine combustors (see for example, Refs 16 19) Most modeling of emission characteristics has been concerned with oxides of nitrogen, NO_x but efforts have also been made to predict the formation of other im portant pollutant species To be successful, a model must accommodate the complex flow behavior and include a kinetic scheme of the important chemical reactions occurring within the combustor The kinetics of some relevant com bustion processes are, unfortunately not well understood at the present time, particularly for the production of carbon, carbon monoxide, and the hydrocarbon species that are in termediaries in the fuel oxidation process

The primary requirement for a satisfactory emissions model for gas turbine combustors is that it should represent an optimum balance between accuracy of representation utility ease of use economy of operation and capability for further improvement In recent years considerable efforts have been directed toward the development of relatively complex mathematical emissions models that can be applied to gas turbines They have varied in level of sophistication from those with potential to yield a complete description of the relevant thermodynamic and chemical properties as a function of spatial location within the combustor, to others which merely assume that homogeneous conditions exist at all axial stations [20 28]

The high cost and complexity of the more sophisticated mathematical models have encouraged the development of semi empirical models for NO_x and CO emissions Hung's approach to the modeling of NO_x emissions places more emphasis on the physical processes considered to be important and relegates chemical kinetics to a relatively minor role [29] His analysis includes a five region combustor internal flowfield model fuel distribution models for liquid and gaseous fuels a single overall hydrocarbon complete combustion model a nitric oxide formation model based on the Zeldovich mechanism, a diffusion limited complete mixing model, and a model to account for the influence of ambient humidity This model has been used successfully in predicting the influence on NO_x emissions of water injection and wide variations in fuel type [29 32]

Other successful semi empirical models for predicting emissions have been developed by Fletcher and Heywood[20 33] and by Hammond and Mellor [34 36] Useful critical evaluations of both mathematical and semi empirical prediction methods have been made by Rubins and Marchionna [37] Sullivan and Mas,[38] and Odgers [39]

Empirical models can also play an important role in the design and development of low emission combustors They may serve to reduce the complex problems associated with emissions to forms which are more meaningful and tractable to the combustion engineer, who often requires only an in sight and a quick estimate of the levels attainable with the design variables at his disposal They also permit more ac curate correlations of emissions for any one specific com bustor than can be achieved by the more general analytical models discussed previously

In attempting to derive an empirical model for emissions emphasis is placed on NO_x and CO This is because the highly complex and unknown nature of the hydrocarbon oxidation reaction makes it virtually impossible to derive a satisfactory model for unburned hydrocarbons Attempts to correlate soot data have met with some success in regard to the prediction of exhaust smoke levels, but have failed to account properly for the effects of aromaticcs/hydrogen content on soot formation and smoke.[40]

For both nitric oxide and carbon monoxide it may be assumed that their exhaust concentration is proportional to the product of three terms which are selected to represent the following: 1) mean residence time in the combustion zone 2) chemical reaction rates, and 3) mixing rates

Expressions for these three parameters may be derived in simplified form as is now shown

Residence time $= L/U = L\rho A/\dot{m}_A = PV/(\dot{m}_A RT)$ [i e , residence time $\propto (PV/(\dot{m}_A T)]$ It is assumed that reaction rates are a function of pressure and temperature only, i e reaction rate $\propto P^m \exp(zT)$ for NO_x and reaction rate $\propto P^n \exp(cT)$ for CO It is assumed that mixing rates are a function of liner pressure drop Specifically we have mixing rate $\propto (\Delta P/P)^x$ Thus $NO_x = \int$(residence time) (reaction rate) (mixing rate) or

$$NO_x = A\left(\frac{PV}{\dot{m}_A T}\right)\left(\frac{\Delta P}{P}\right)^x P^m \exp(zT)$$

$$= \frac{AV_c(\Delta P/P)^x P^y \exp(zT)}{\dot{m}_A T} \tag{21}$$

where $y = 1 + m$, and A is a constant

Similarly, for CO we have CO $= \int$(residence time)$^{-1}$ (reaction rate) (mixing rate) or

$$CO = C\left(\frac{\dot{m}_A T}{PV_c}\right)\left(\frac{\Delta P}{P}\right)^a P^n \exp - (cT)$$

$$= C V_c^{-1} \dot{m}_A T(\Delta P/P)^a P^b \exp - (cT) \tag{22}$$

where $b = n - 1$ and C is a constant

It is recognized that the above equations have no strong theoretical foundation However, they do embody the main variables of combustor size, pressure loss flow proportions and operating conditions of inlet pressure temperature and air mass flow The effect of variations in overall combustor fuel/air ratio is also included via its influence on primary zone temperature Fuel type affects both flame temperature and mean drop size For NO_x, drop size is unimportant since at the high pressure conditions where NO_x emissions are most

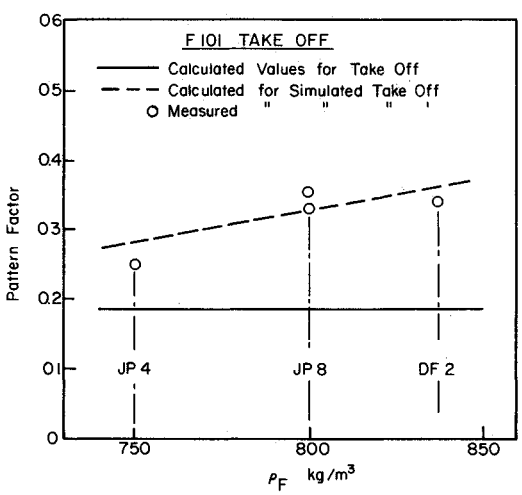

Fig 10 Graphs illustrating the effects of fuel density and combustor operating conditions on pattern factor

prominent, the fraction of the total combustion volume employed in fuel evaporation is so small that wide variations in fuel drop size have a negligible effect on NO_x emissions However at low pressure operation where CO emissions attain their highest concentrations, a significant proportion of the primary zone volume is needed to evaporate the fuel Under these conditions, any factor that influences fuel evaporation rates such as evaporation constant or mean drop size will have a direct effect on the volume available for chemical reaction and therefore, on the emissions of CO and UHC Thus for the correlation of CO data, the effects of fuel type cannot be ignored The manner in which they may be introduced conveniently into the equation for CO emissions is illustrated below From analysis of the experimental data contained in Refs 1 6 it was found for Eq (21) that $A=9\times10^{-8}$ $x=0$ $y=1\ 25$ and $z=0\ 01$ For Eq (22) the results show that $C=86$ $a=-0\ 5$ $b=-1\ 5$ and $c=0\ 00345$

Substituting these values into Eqs (21) and (22) gives

$$NO_x=\frac{9\times10^{-8}P_3^{1\ 25}V_c\exp(0\ 01\ T_{st})}{\dot{m}_A T_{pz}}\ g/kg \qquad (23)$$

$$CO=\frac{86\dot{m}_A T_{pz}\exp-(0.00345\ T_{pz})}{\left(V_c-0\ 55\dfrac{f_{pz}\dot{m}_A}{\rho_{pz}}\dfrac{D_0^2}{\lambda_{eff}}\right)\left(\dfrac{\Delta P_L}{P_3}\right)^{0\ 5}P_3^{1\ 5}}\ g/kg \qquad (24)$$

From Eq (23) it may be noted that the only influence of fuel type on NO_x formation is via the two temperature terms T_{pz} and T_{st} The former is calculated as

$$T_{pz}=T_3+\Delta T_{pz}$$

where ΔT_{pz} is the temperature rise due to combustion corresponding to the inlet temperature T_3 and fuel/air ratio (q_{0v}/f_{pz}) T_{st} is the stoichiometric flame temperature corres ponding to the inlet temperature T_3 Equation (23) suggests that in the combustion of heterogeneous fuel air mixtures it is the stoichiometric flame temperature that determines the formation of NO_x However, for the residence time in the combustion zone which is also significant to NO_x formation the appropriate temperature term is the bulk value T_{pz}, as indicated in the denominator of Eq (23)

Equation (23) is suitable for conventional spray combustors only For lean premix/prevaporize combustors in which the maximum attainable temperature is T_{pz}, it may still be used, provided that T_{pz} is substituted for T_{st} It should also be noted that predictions of NO_x based on Eq (23) tend to be too high when the overall combustor air/fuel ratio exceeds a value of around 100 This is because with diminishing fuel/air ratio the flame shrinks back toward the fuel nozzle and no longer occupies the entire combustion volume V_c However, this is not considered a serious drawback since in practice interest is normally focused on conditions of high fuel/air ratio where NO_x formation rates attain their highest values

The excellent correlation of experimental data on NO_x provided by Eq (23) is illustrated in Figs 11 15 Equally good correlations have been demonstrated for all other combustors except the J85 for which the measured values are too low to correlate [40]

The formation of CO in the primary combustion zone takes appreciably longer than the time required to produce NO_x In consequence the relevant temperature is not the local peak value adjacent to the evaporating fuel drops but the average value throughout the primary zone namely T_{pz} Also because CO emissions are most important at low pressure conditions where evaporation rates are relatively slow, it is necessary to reduce the combustion volume V_c by the volume occupied in fuel evaporation V_e This was evaluated

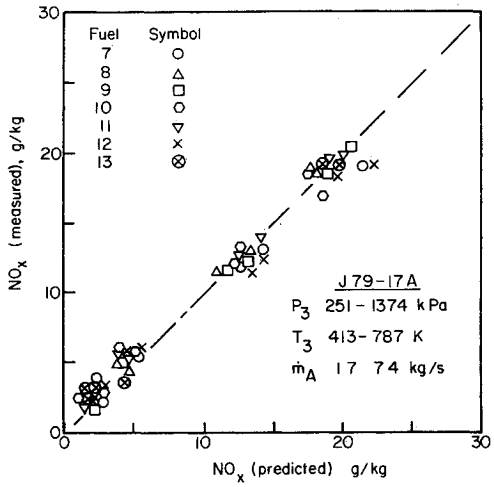

Fig 11 Comparison of measured and predicted values of NO_x emissions for J79 17A combustor (fuels 7 to 13)

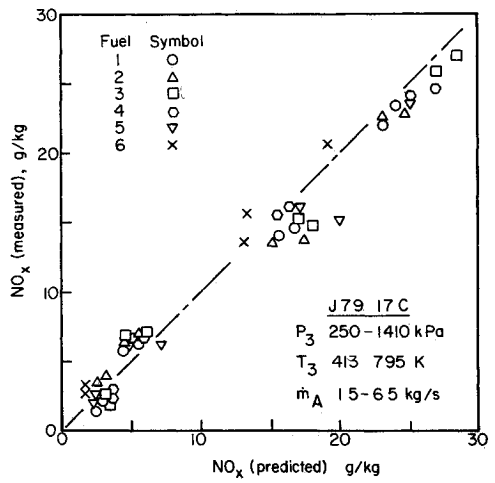

Fig. 12 Comparison of measured and predicted values of NO_x emissions for J79 17C combustor (fuels 1 to 6)

elsewhere[7] as

$$V_e=0\ 55 f_{pz}\dot{m}_A D_o^2/\rho_{pz}\lambda_{eff} \qquad (25)$$

It is of interest to note the inclusion of a pressure loss term in Eq (24) This suggests that the higher turbulence created by an increase in liner pressure drop promotes better mixing in the combustion zone and helps to eliminate the rich and weak pockets of fuel air mixture, both of which are conducive to high rates of CO formation

The very satisfactory correlation of experimental data on CO emissions obtained with Eq (24) is illustrated in Figs 16-20 for J79 17A, J79 17C F101, TF41, and F100 combustors, respectively

Discussion and Summary

I iner Wall Temperature

The most important factor governing liner wall temperature is the combustor inlet temperature T_3 Inlet pressure is also significant due to its influence on the concentration of soot particles in the flame, and hence on the magnitude of the luminous radiation flux to the liner wall At maximum power conditions where liner wall temperatures are of most concern evaporation rates are so high that the physical properties of the fuel appear to have a negligible influence on T_w Chemical affects are also quite small as shown in Figs 4 and 5 However, even small increases in maximum values of liner wall temperature can seriously curtail liner life Thus for the

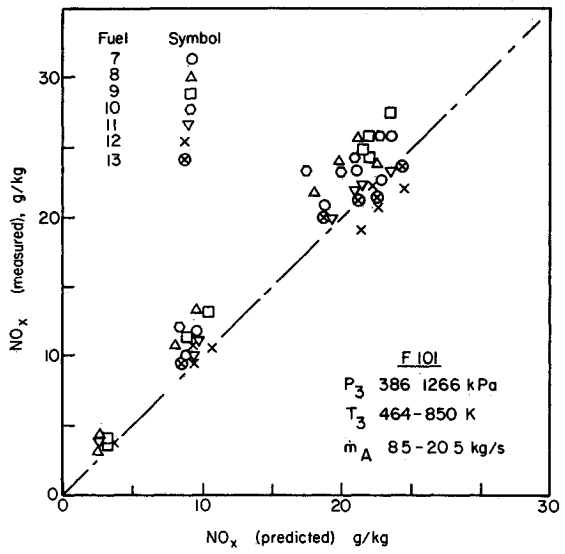

Fig 13 Comparison of measured and predicted values of NO_x emissions for F101 combustor (fuels 7 to 13)

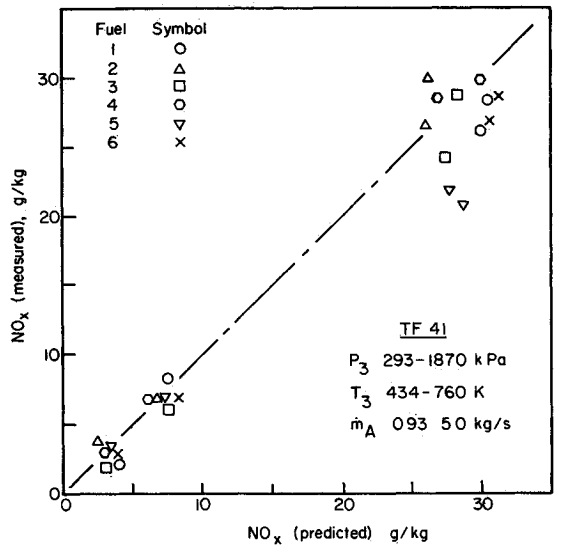

Fig 14 Comparison of measured and predicted values of NO_x emissions for TF 41 combustor (fuels 1 to 6)

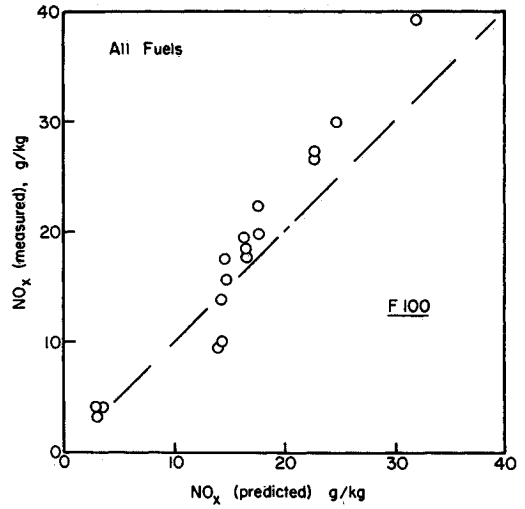

Fig 15 Comparison of measured and predicted values of NO_x emissions for F100 combustor

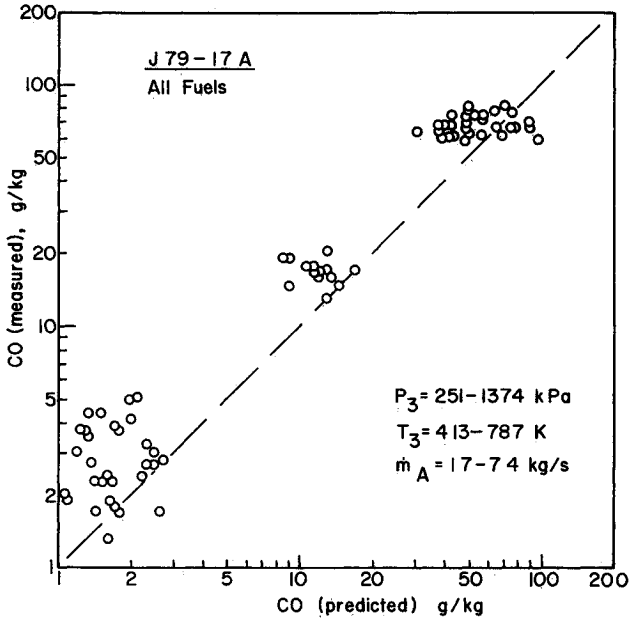

Fig 16 Comparison of measured and predicted values of CO emissions for J79 17A

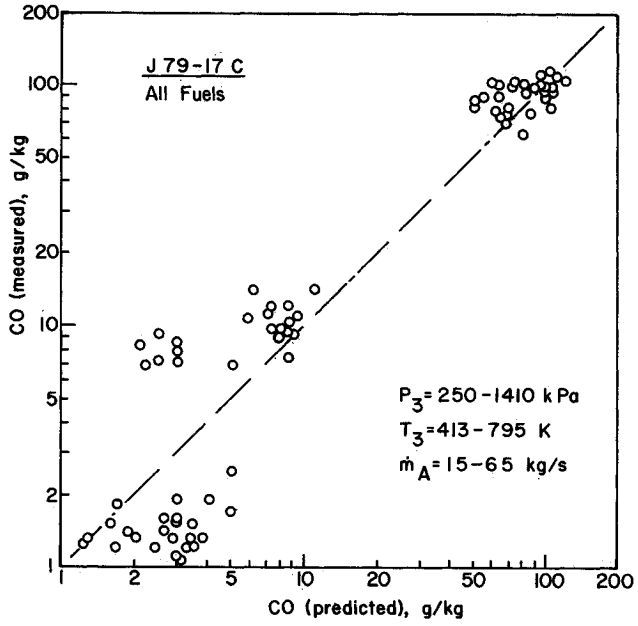

Fig 17 Comparison of measured and predicted values of CO emissions for J79 17C combustor

range of fuels covered in this investigation, fuel type must be considered important to liner durability

In the calculation of liner wall temperatures, the effect of fuel type can be accommodated quite conveniently by in troducing the fuel hydrogen content into the existing equation for gas emissivity This approach leads to the following equation for ϵ_g shown previously as Eq (13)

$$\epsilon_g = 1 - \exp - [97\,44\, P_3 (\%H_2)^{-2} (q\,\ell_b)^{0\,5} T_g^{-1.5}]$$

Pattern Factor

This is described with good accuracy by Eq (20)

$$\frac{T_{max} - T_4}{T_4 - T_3} = 1 - \exp - \left[Z \left(\frac{\Delta P_L}{q_{ref}} \right) \left(\frac{L_L}{D_L} - \frac{0\,33 \dot{m}_A D_o^2}{\rho_g A_L D_L \lambda_{eff}} \right) \right]^{-1}$$

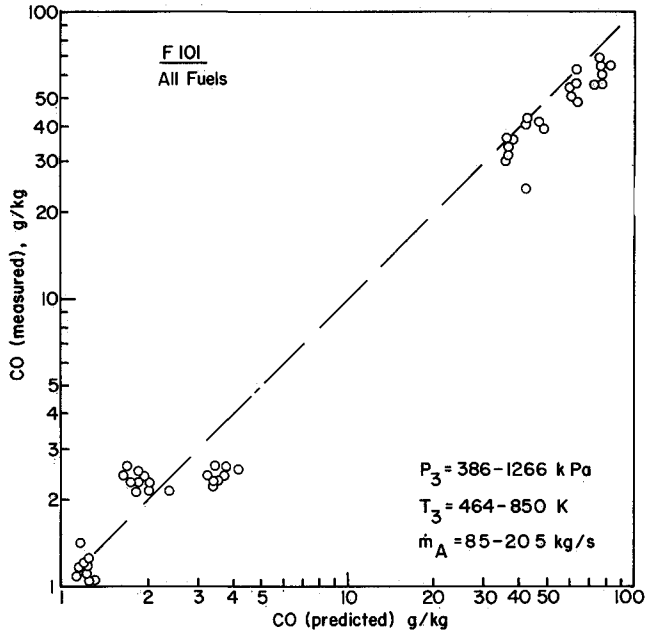

Fig 18 Comparison of measured and predicted values of CO emissions for F101 combustor

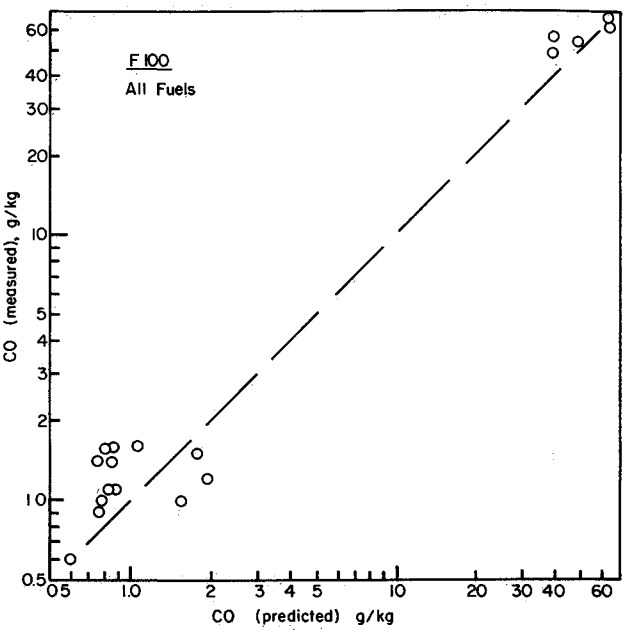

Fig 20 Comparison of measured and predicted values of CO emissions for F100 combustor

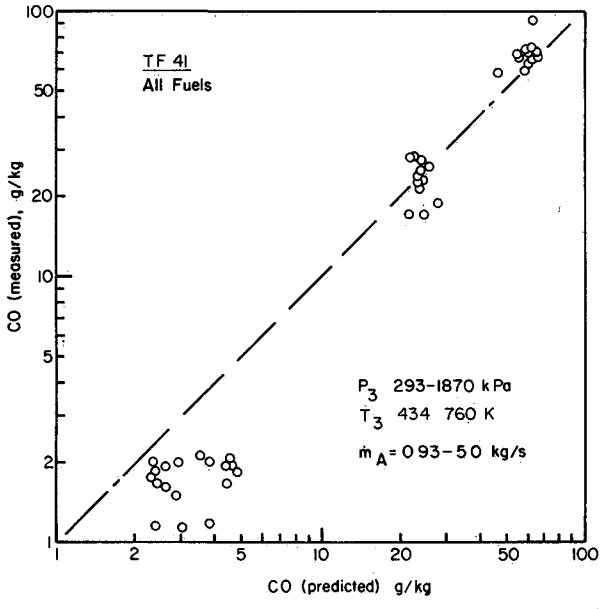

Fig 19 Comparison of measured and predicted values of CO emissions for TF 41 combustor

where appropriate values of Z are 0 70 and 0 50 for tubo annual and annular combustors, respectively The above equation shows that the two main parameters controlling pattern factor are the pressure drop across the liner and the liner L/D ratio It also accounts for the influence of evaporation time in reducing the time available for mixing within the liner At the high pressure conditions where pattern factor is of most concern, the evaporation time is always quite short in comparison to the total residence time of the com bustor and so the dependence of pattern factor on fuel type is fairly small, as illustrated in Fig 10.

With reduction in engine power, the evaporation time increases due to increase in D_o and reduction in λ_{eff} This produces a deterioration in pattern factor as indicated by Eq (20) and also by Figs 7 9, which demonstrate that pattern factor at idle is distinctly worse than at takeoff for all engines These considerations highlight the importance of measuring pattern factor only at the correct combustor inlet conditions of $\dot{m}_A$ P_3, T_3 and q_{ov} corresponding to engine operation at maximum power Tests carried out at simulated conditions at lower pressure levels give misleading results, as shown in Fig 9 First, they yield values that are overpessimistic and second, they show a dependence of pattern factor on fuel type which greatly exaggerates the dependence actually observed at high pressure

NO_x Emissions

It is found that NO_x emissions are very dependent on combustor operating conditions, and also on the size of the combustion zone which governs the time available for NO_x formation The key factor controlling NO_x is the stoichiometric flame temperature which in turn is almost solely dependent on combustor inlet temperature As far as fuel type is concerned physical properties are of little con sequence, except at low power conditions where NO_x emissions are always quite small due to the corresponding low values of T_{st} Fuel chemistry also has little influence on NO_x because it affects only slightly the values of bulk gas tem perature (T_{pz}) and stoichiometric flame temperature (T_{st}) in the following equation for NO_x, Eq 23

$$NO_x = \frac{9\times 10^{-8} P_3^{1\,25} V_c \exp(0\,01\,T_{st})}{\dot{m}_A T_{pz}} \text{ g/kg}$$

CO Emissions

These are correlated by Eq 24:

$$CO = \frac{86\dot{m}_A T_{pz} \exp -(0\,00345\,T_{pz})}{\left(V_c - 0\,55\,\frac{f_{pz}\dot{m}_A}{\rho_{pz}}\,\frac{D_0^2}{\lambda_{eff}}\right)\left(\frac{\Delta P_L}{P_3}\right)^{0\,5} P_3^{1\,5}} \text{ g/kg}$$

It is again observed that combustor size and operating conditions play a prominent role in determining the level of CO emissions Special importance is attached to inlet tem perature and primary-zone fuel/air ratio, due to their combined effect in resolving the primary zone temperature As in the case of NO_x emissions the influence of fuel chemistry is small and is manifested through slight variations

in T_{pz} with changes in lower calorific value However since CO emissions attain their maximum values at low power conditions, where a sizeable proportion of the total residence time in the combustion zone is occupied by evaporation processes the influence of those physical properties that affect evaporation rates, namely μ_F σ_F and T_{bn}, becomes important On this basis it would be anticipated that fuels of high viscosity would be characterized by slightly higher levels of CO emissions, and the experimental data generally confirm this expectation.

Conclusions

1) Analysis of the experimental data, which cover a wide range of fuel types from JP4 to DF2, shows that fuel chemistry, as indicated by hydrogen content and/or aromatics content, has a significant effect on flame radiation and liner wall temperature

2) The influence of fuel chemistry on CO and NO_x emissions is quite small and stems from the effects of slight differences in lower calorific value on combustion tem perature

3) The physical properties that govern atomization quality and evaporation rates affect CO emissions but, at high power conditions, both liner wall temperature and NO_x emissions are sensibly independent of physical properties over the range of fuels studied

4) Fuel chemistry has no direct influence on pattern factor However, physical properties have an effect that is ap preciable at lower power conditions but that diminishes in importance with increase in engine power, becoming very small at the highest power setting where the effect of pattern factor on vane life is most significant.

Acknowledgments

Support of the work reported here by the Fuels Branch of the Air Force Aero Propulsion Laboratory, Wright Patterson Air Force Base Ohio, is gratefully acknowledged The Air Force Technical Monitors employed on this program were Dr Thomas A Jackson in the initial phase, and Mr Curtis M Reeves for the remainder of the program

References

[1] Gleason, C C Oller T L Shayeson M W and Bahr D W , Evaluation of Fuel Character Effects on J79 Engine Combustion System ' AFAPL-TR-79 2015 June 1979

[2] Gleason C C , Oller T L. Shayeson M W , and Bahr D W , Evaluation of Fuel Character Effects on F101 Engine Combustion System ' AFAPL TR-79 2018 June 1979.

[3] Vogel R E Troth D L and Verdouw A J Fuel Character Effects on Current, High Pressure Ratio Can Type Turbine Com bustion Systems," AFAPL TR 79 2072 April 1980

[4] Gleason C C , Oller, T L , Shayeson, M W and Kenworthy, M J. Evaluation of Fuel Character Effects on J79 Smokeless Combustor," AFWAL-TR 80 2092, Nov 1980

[5] Oller T. L , Gleason C C Kenworthy, M. J , Cohen J D , and Bahr, D W "Fuel Mainburner/Turbine Effects AFWAL TR 81 2100, May 1982

[6] Russel P. L , Fuel Mainburner/Turbine Effects ' AFWAL TR 81 2081, Sept 1982

[7] Lefebvre, A H , Fuel Effects on Gas Turbine Com bustion—Ignition Stability and Combustion Efficiency to be published in *Transactions of the ASME Journal of Engineering for Power*

[8] Lefebvre A H Airblast Atomization ' *Progress in Energy Combustion Science* Vol 6, 1980 pp 231 261.

[9] Lefebvre A H. *Gas Turbine Combustion* McGraw Hill Book Co., 1983

[10] Lefebvre A H and Herbert M W , ' Heat Transfer Processes in Gas Turbine Combustion Chambers ' *Proceedings of the In stitution of Mechanical Engineers* Vol 174 No 12 1960, pp 463 473

[11] Kretschmer, D and Odgers J A Simple Method for the Prediction of Wall Temperatures in a Gas Turbine Combustor *ASME Paper No 78 GT-90* 1978

[12] Blazowski W S Combustion Considerations for Future Jet Fuels *Sixteenth Symposium (International) on Combustion* The Combustion Institute, 1977, pp 1631 1638

[13] Jackson, T A Fuel Character Effects on the J79 and F101 Engine Combustion Systems Symposium on Aircraft Research and Technology for Future Fuels, NASA Lewis Research Center 1980

[14] McAdams W H *Heat Transmission* 3rd ed McGraw Hill Book Co , New York 1954 Chap 4

[15] Lefebvre A H and Chin J S Effective Values of Evaporation Constant for Hydrocarbon Fuel Drops ' *Proceedings of 20th Automotive Technology Development Contractors Meeting* P 120 1982 pp. 325 331

[16] Jones, R E and Grobman, J Design and Evaluation of Combustors for Reducing Aircraft Engine Pollution *Atmospheric Pollution by Aircraft Engines* AGARD CP 125 1973

[17] Bahr, D W 'Technology for the Reduction of Aircraft Turbine Engine Exhaust Emissions " *Atmospheric Pollution by Aircraft Engines* AGARD CP 125, 1973

[18] Henderson R E and Blazowski W S Aircraft Gas Turbine Pollutant Limitations Oriented Toward Minimum Effect on Engine Performance ' *Atmospheric Pollution by Aircraft Engines* AGARD CP-125, 1973.

[19] Jones R E Diehl L A Petrash D A and Grobman J Results and Status of the NASA Aircraft Engine Emission Reduction Technology Program NASA TM 79009, 1978.

[20] Fletcher, R S. and Heywood, J B ' A Model for Nitric Oxide Emissions from Aircraft Gas Turbine Engines " AIAA Paper 71 123 1971

[21] Mosier S A and Roberts R. 'Development and Verification of an Analytical Model for Predicting Emissions from Gas Turbine Engine Combustors during Low Power Operation, AGARD CP 125 1973

[22] Roberts R Aceto L D Kollrack R Teixeira, D P , and Bonnell J. M An Analytical Model for Nitric Oxide Formation in a Gas Turbine Combustor, *AIAA Journal* Vol 10 June 1972 pp 820 826

[23] Mador, R J and Roberts R A Pollutant Emission Prediction Model for Gas Turbine Combustors ' AIAA Paper 74 1113, 1974

[24] Edelman R and Economos C "A Mathematical Model for the Jet Engine Combustion Pollutant Emissions, AIAA Paper 71 714, 1971

[25] Mongia H G., and Smith K An Empirical/Analytical Design Methodology for Gas Turbine Combustors ' AIAA Paper 78 949 1978

[26] Bruce T. W. Mongia H C and Reynolds, R S ' Combustor Design Criteria Evaluation, Vol 1, UASRTL TR 78 55A 1979

[27] Swithenbank, J , Turan, A and Felton P G Three Dimensional Two Phase Mathematical Modelling of Gas Turbine Combustors *Gas Turbine Combustor Design Problems,* Hemisphere 1980 pp 249 314

[28] Pratt D T , Coalescence/Dispersion Modelling of Gas Turbine Combustors " *Gas Turbine Design Problems,* Hemisphere, 1980, pp 315-330.

[29] Hung W S Y An Experimentally Verified NO_x Emission Model for Gas Turbine Combustors ' ASME Paper No 75 GT 71 1975

[30] Hung W S Y Accurate Method of Predicting the Effect of Humidity on Injected Water on NO_x Emissions from Industrial Gas Turbines,' ASME Paper No 74 WA/GT 6 1974

[31] Hung W S Y , A Diffusion Limited Model that Accurately Predicts the NO_x Emissions from Gas Turbine Combustors Including the Use of Nitrogen Containing Fuels ASME Paper No 75 Pwr 11 1975

[32] Hung W S Y , Modeling and Measurement of NO_x Emissions from Burning Synthetic Coal Gas in Gas Turbine Combustors ASME Paper No 75 WA/GT 3, 1975

[33] Fletcher R S , Siegel R D and Bastress E K The Control of Oxides of Nitrogen Emissions from Aircraft Gas Turbine Engines NREC 1162 FAA RD 71 111 Vols I II and III Nor thern Research and Engineering Corp Cambridge, Mass 1971

[34] Hammond, D C Jr and Mellor A M , Analytical Calculations for the Performance and Pollutant Emissions of Gas Turbine Combustors ' *Combustion Science and Technology* Vol 4 No 3 1971 pp 101 112

[35] Hammond D C Jr and Mellor, A M , 'Analytical Predictions of Emissions from and within an Allison J 33 Combustor ' *Combustion Science and Technology* Vol 6 No 5 1973 pp 279 286

[36] Mellor, A M Semi Empirical Correlations for Gas Turbine Emissions, Ignition and Flame Stabilization *Progress in Energy Combustion Science* Vol 6 1981 pp 347 358

[37] Rubins, P M , and Marchionna N. R , Evaluation of NO_x Prediction Correlation Equations for Small Gas Turbines *Journal of Aircraft* Vol 15 Aug 1978 pp 497 502

[38] Sullivan D A and Mas P A ' A Critical Review of NO_x Correlations for Gas Turbine Combustors ASME Paper 75 WA/GT 7 1975

[39] Odgers, J , Current Theories of Combustion within Gas Turbine Chambers, ' *Fifteenth Symposium (International) on Combustion* The Combustion Institute 1975 pp 1321 1338

[40] Lefebvre A H , Fuel Effects on Gas Turbine Combustion AFWAL TR 83 2004 1984