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AGARD

ADVISORY GROUP FOR AEROSPACE RESEARCH & DEVELOPMENT

AGARD ADVISORY REPORT No. 181

**Propulsion and Energetics Panel
Working Group 13
on
Alternative Jet Engine Fuels**

Volume 2: Main Report

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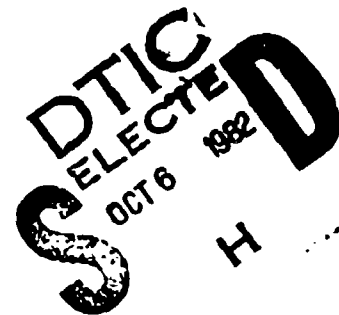
NORTH ATLANTIC TREATY ORGANIZATION
ADVISORY GROUP FOR AEROSPACE RESEARCH AND DEVELOPMENT
(ORGANISATION DU TRAITE DE L'ATLANTIQUE NORD)

AGARD Advisory Report No.181 -- Volume II
PROPULSION AND ENERGETICS PANEL
WORKING GROUP 13
on
ALTERNATIVE JET ENGINE FUELS

Main Report

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 - Continuously stimulating advances in the aerospace sciences relevant to strengthening the common defence posture;
 - Improving the co-operation among member nations in aerospace research and development;
 - Providing scientific and technical advice and assistance to the North Atlantic Military Committee in the field of aerospace research and development;
 - Rendering scientific and technical assistance, as requested, to other NATO bodies and to member nations in connection with research and development problems in the aerospace field;
- Providing assistance to member nations for the purpose of increasing their scientific and technical potential;
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EXECUTIVE SUMMARY

EXECUTIVE SUMMARY

1. INTRODUCTION

It is forecast that during the next twenty years the only economically available fuels for aircraft gas turbine engines will be those from the processing of conventional crude petroleum augmented by similar "synthetic" hydrocarbon components derived from other sources. This report therefore deals only with alternative fuels of this type which may entail considerable changes in fuel properties and relaxation of key items in present specifications to ensure adequate supplies.

Increasing fuel prices, growing demands for middle distillate fuels, including jet engine fuels, and growing difficulties with availability in most NATO Nations characterize the present situation in the aviation fuel market, both military and civil. This situation is likely to deteriorate considerably over the next twenty years. Only a limited amount of crude oil can be converted into aviation kerosine according to present specifications and there is also competition from other product requirements for this portion of the crude barrel. Conventional crudes are becoming heavier and synthetic crudes from tar sands, bitumen, very heavy crude oils, shale and eventually coal will be incorporated into refinery feedstocks. Broadening jet fuel specifications may be the only possible way of increasing the supply of fuel with reasonable economy in both refinery energy and cost. The physical properties as well as the hydrocarbon composition of the fuels would change and there is therefore an immediate need for the prediction of these properties and for research on their effects on handling and storage, aircraft fuel systems and engines.

These needs were recognized by the Propulsion and Energetics Panel and a series of meetings of interested panel members led to a recommendation to the Panel which in 1977 proposed a Working Group on Alternative Jet Engine Fuels with the following objectives:

- To bring together experts from all NATO Nations engaged in research and development work in the field of alternative jet engine fuels.
- To collect information on research and development programs, on their progress and results.
To evaluate the results and impacts on the operation of aircraft engines and aircraft fuel systems.
- To identify gaps which need to be closed and to give pertinent recommendations for future research work.

The Panel also recommended that the Working Group should cover the following subject areas:

- Production methods for alternative aviation fuels with respect to fuel properties.
- Properties of alternative aviation fuels relevant to their use in military and civil engines and aircraft systems.
Influence of alternative fuels on operation of existing engines and aircraft, i.e. performance and operational limits of present hardware systems using alternative fuels as well as modifications and adaptations that could be made to existing hardware systems.
- Application of alternative fuels in new aircraft engines, i.e. problems in combustors, turbines, and afterburners, and methods for their solution.
- Fuel system related problems including engine control and aircraft fuel systems, their impact on engine and aircraft operation, and methods for improvement.

The Working Group was approved by the National Delegates Board in March 1978. The work was divided into three tasks with Panel and non-Panel Members having required expertise assigned to each task group. The three areas were:

- Supply and demand scenarios for aviation turbine fuels in the NATO Nations for the next 20 years.
- The effects of projected quality fuels on current engines, new engines, and aircraft fuel systems.
- Guidelines for research fuels with properties representative of aviation turbine fuels to be expected in NATO Countries around the year 2000.

The following sections of this Report give a summary of the findings of the three Task Groups followed by general conclusions and recommendations. The detailed reports can be found in the Appendices of AGARD Advisory Report AR-181, Vol.II.

2. SUPPLY/DEMAND SITUATION

It is possible to examine the effect of changes in jet fuel properties on availability, energy consumption, and cost of refining by using refinery planning models. Such models are widely used by planners in the petroleum industry, utilizing high speed computers to examine the role of new processes in meeting future product demands and to produce optimum solutions in terms of investments and operating costs. In the case of US civil jet fuel, NASA has contracted to examine five relaxations in product specifications to determine which produces the optimum solution in terms of availability and cost during the next thirty years.

There are many factors which are recognized to adversely affect future quality of all products produced from petroleum or alternative sources. These are discussed more fully in Appendix 1, but predominantly relate to the diminishing supply of high quality crude, the necessity to install processes to convert heavy fuels into lighter products, particularly distillates; and the requirement to meet a future demand pattern of less gasoline and more distillates, including jet fuel.

In order to facilitate understanding of the relationships among various petroleum fuels which are discussed in the following sections of this report, Fig. 1 was prepared to show the approximate distillation limits of the major fuels produced from conventional crude. The limits are dictated by present specifications. It should be noted that NATO F-40 fuel (JP-4) contains a large proportion of gasoline fractions while NATO F-34 (JP-8) is a kerosine distillate similar to civil jet fuel. Other important distillates are diesel and burner fuels. Heavy fuel oils containing residual components are used mainly as boiler or furnace fuels or in large marine type diesel engines. The AGARD Research Fuel, described in detail in the next section and in Table 1, contains not only the F-34 area but also the hatched area which extends its distillation range into the higher boiling range of diesel fuel.

Analysis of products in refining planning studies requires the development of models which simulate by computer the real world of the petroleum refining industry. The types of models are largely determined by geography and marketing patterns. For example, in the US the crude supply and markets differ substantially between the West Coast and the remaining continental US area; these differences affect the type of models and crude inputs. In other NATO countries, similar differences exist. Western Canada differs from Eastern Canada, and Northern Europe from Southern Europe.

Markets in each area are supplied by refineries of varying complexity, depending primarily on historical product demand. For example, the high gasoline demand in the US has created many complex refineries that utilize reforming, catalytic cracking and various heavy fuel conversion processes. On the other hand, the higher fuel oil demand in Europe has emphasized less complex refineries that utilize primarily distillation and reforming. The model combination for each area must provide a different mixture of refineries of various complexities to match actual refineries to satisfy this product demand. The success of a model combination can be verified by comparing computer results with actual product outputs for a particular historical point. Verification is a critical step in model development.

The pattern of crude types and amounts are expected to change significantly during the next 30 years within the framework of overall energy requirements of NATO Nations. During this time period synthetic crudes produced from sources such as tar sands, bitumens, very heavy oils, oil shale and coal are expected to replace or supplement conventional petroleum as refining feedstocks. Each of these presents much more difficult refining problems and tends to make distillate products lower in hydrogen content (i.e. higher in aromatics and naphthalene) than conventional petroleum.

Demand for all products and their expected quality has a considerable influence on jet fuel. For example, dieselization of light trucks and automobiles will spur distillate demand at the expense of motor gasoline. The kerosine cut used for jet fuel is also a refinery blending stock for diesel and burner fuels. It has been suggested by a leading automobile manufacturer that to meet emission requirements, diesel for cars may have to be a light, low aromatic distillate even better than jet fuel. This proposal represents one of the uncertainties that would have to be addressed in computer studies using refinery models.

The quality of products such as cetane number of diesel fuel and sulphur content of burner fuels is controlled in modelling studies by including investment and operating costs for processes such as hydrogen treating and hydrogen generation. When variations in jet fuel quality are explored, the incremental refining costs including investments and return between one case and another are determined by a linear program as the computer seeks to balance the demand for all products vs jet fuel quality shifts for each combination of refinery models.

Although refinery modelling and linear programming studies can produce solutions to the economic vs quality questions sought, it should be recognized that any study involving a 30 year lookahead involves uncertainties. To list just a few of these:

- (1) Will crude quality and supply follow the present downward trends or will new sources unexpectedly develop?
- (2) Will the development of synthetic replacements for crude occur at the rate currently projected?
- (3) Will present inflationary trends in energy cost continue to reduce demand for fuel oil and motor gasolines?
- (4) Will US automotive diesel require high quality fuel better than jet fuel and degrade the remaining distillate fuel for aircraft use?

- (5) Will the US Air Force and other NATO forces convert completely from JP-4 to JP-8 thus increasing demand on distillates?

A complete study for all NATO areas along the line of that presently being carried out for NASA for the US will be the subject of Working Group 16, approved in September 1981 to examine future supply and demand scenarios for aviation turbine fuels.

3. AGARD RESEARCH FUEL

There is a need for some guidance to aircraft, engine, and component manufacturers on the types and characteristics of aviation turbine fuel likely to be available in the year 2000 so that they can proceed with research work aimed at satisfactory operation on these fuels. It is possible from all the sources envisaged for the future to produce fuels meeting present specifications, but the cost and energy penalties for doing so are considered in most of the NATO Nations to be prohibitive. Therefore some deterioration in fuel properties seems inevitable and the efforts of fuel producers, equipment manufacturers, and fuel users will be required to establish the most economical and energy efficient route to be followed. In this context it was felt by the Working Group that the effort should be made to identify for AGARD a fuel to guide research activities. The properties included in Table 1, which define such an AGARD Research Fuel, represent a reasonable range based on present knowledge of expected fuel trends to the year 2000. These research fuel properties provide an envelope within which research in combustion, engine performance, fuel handling, etc. should be conducted.

The Working Group considered the possibility of including more gasoline fractions in an AGARD Research Fuel which would broaden the distillation range and dilute the effects of heavy ends and higher aromatics. However, more gasoline fraction would lower the flash point of F-34 below current specification levels and it was felt that such a fuel would be inconsistent with the present trend to convert military aircraft from F-40 to F-34 for safety and vulnerability reasons.

It is most important that, when expensive full-scale engine testing is undertaken, maximum benefit should be derived from the results by ensuring that the fuel properties are well documented so that they may be related to performance. In smaller scale studies on aircraft and engine components, combustion studies, experiments on spray formation, evaporation, ignition, blow-off, etc., it is equally important that the fuel characteristics be fully detailed to allow correlation with laboratory specification test methods and full scale trials.

Since relatively rapid developments are occurring in fuel sources, fuel processing, additives, etc., it is difficult to forecast the situation twenty years ahead and it is recommended that the AGARD Research Fuel property guidelines should be reviewed at regular intervals in the light of further developments.

It should be noted that reference to 'alternate' and 'emergency' fuels below does not imply that such fuels comply with the requirements of fuels currently so designated in other NATO publications. Rather an 'alternative' fuel in this report is one that could conceivably be a primary aviation fuel in the Year 2000, such as the AGARD Research Fuel, and an 'emergency' fuel is any not too closely related liquid fuel such as leaded gasoline or diesel fuel available at such time.

4. EFFECTS OF PROJECTED FUELS ON ENGINES AND FUEL SYSTEMS

A detailed assessment has been made on the effects of potential variations in hydrocarbon fuel properties on the performance, operating envelope, exhaust emissions, durability, maintainability, reliability, and safety of gas turbine aircraft. The assessment considered four specific subject areas affected by changes in fuel properties:

- Combustor Heat Transfer and Exhaust Pollutants
- Combustor Spray Formation, Evaporation, Ignition, and Blow-off
- Fuel Thermal Stability
- Fuel Flow Characteristics

Each subject area considered the fuel properties having the most significant influence on those particular design and operating criteria for gas turbine powered aircraft corresponding to the potential changes in fuel characteristics suggested by the AGARD Research Fuel. The fuel properties subject to change in the future having the greatest influence on gas turbine aircraft include: (1) hydrogen content (aromatics), (2) viscosity, (3) volatility, (4) storage and thermal stability, and (5) freezing point. The sensitivities of various components of gas turbine aircraft to variations in fuel properties and potential penalties in performance and durability are presented along with needs for advanced technology to counteract potential penalties in future aircraft.

4.1 Combustor Heat Transfer and Exhaust Pollutants

In the course of the study, a smoke emissions index correlation has been suggested which may allow extrapolation of existing data to fuels of lowered hydrogen content burning at increased combustor pressures, with some quantitative accuracy. Further, weight percent hydrogen appears to be the governing fuel property with respect to smoke emissions

and flame radiation as long as naphthalene content remains at the current low level. However, recent data suggest that high naphthalene content may affect smoke and radiation, and should, therefore, be included in the correlation along with hydrogen.

Predictions of a linear wall temperature parameter, in terms of correlations developed over twenty years ago are surprisingly good in terms of the extrapolations in fuel property values. The exceptions here involve modern lean primary zone combustors, where the predictions are conservative. In either case, the increase in liner wall temperature with decreasing fuel hydrogen content can lead, particularly in low pressure ratio engines, to substantial decreases in liner durability. For example, the correlation which relates liner life to fuel hydrogen content predicts that durability would be decreased by as much as 50 percent for a fuel hydrogen content decrease from 14.5 percent which is typical of present F-40 fuel to 13 percent for that of the AGARD Research Fuel.

Combustor data for gaseous emissions and combustion efficiency generally show only a small dependence on the various fuel properties, although at some increased viscosity level, finite droplet evaporation times will lead to increased idle emissions of carbon monoxide and unburned hydrocarbon, with a corresponding decrease in combustion efficiency. Oxides of nitrogen would tend to increase with fuels containing nitrogen compounds, but such fuels generally need to be denitrogenated to meet thermal stability requirements.

Further verification and development of the smoke emissions correlations is recommended and could result in reduction in the need for rig and engine testing, particularly at high combustor pressure levels with fuels difficult to obtain in quantity.

To provide reliable predictions of carbon in exhausts, it is recommended that a standard technique be developed. The measurement will involve isokinetic sampling and may require gravimetric determination.

It is recommended that careful experiments be conducted to determine effects of hydrocarbon structure (e.g. multi-ring aromatics) on carbon formation and flame radiation.

It is further recommended that heat transfer correlations be updated to include modern lean primary zone combustors. If the existing correlation is shown to apply to lean burning combustors, then it should be extended to fuels of low hydrogen content.

Continuing work is required to improve atomizer and primary zone combustor designs and film cooling techniques to protect liner walls. These measures would minimize the impact of decreased hydrogen content on liner durability.

4.2 Combustor Spray Formation, Evaporation, Ignition and Blow-off

The combined effect of increased viscosity and decreased volatility results in increased evaporation times and a considerably worsened ignition behaviour, particularly at low temperatures characteristic of cold starts and altitude relight. Blow-off characteristics of a combustor are influenced only slightly.

Viscosity and volatility are the main fuel properties which affect mixture formation in combustors - spray formation, droplet distribution, and evaporation - as well as ignition. Surface tension and liquid fuel density are of secondary importance. An increase in fuel viscosity leads to large drop size, the magnitude of which depends on the type of fuel injector used. For the maximum viscosity in the proposed AGARD Research Fuel, the Sauter mean diameter of fuel sprays from pressure swirl atomizers will increase by about 10 percent, from air blast atomizers by about 5 percent, while sprays from rotary injectors are insensitive to viscosity. To decrease the Sauter mean diameter of fuel droplets and to keep evaporation times at present levels, it is recommended that energy input for atomization be increased. This can be achieved either by air blast atomization or increasing the fuel supply pressure. An external air supply is needed if combustor pressure loss, and hence, engine performance is not to be compromised. This is true, in particular, for engine start-up at low ambient temperatures. For pressure swirl atomizers, the fuel pressure has to be increased by roughly 80 - 100 percent, which causes problems with weight penalties for the high pressure fuel system and nozzle size. However, pressure swirl atomization with spill return flow seems to offer a possibility for changing drop sizes independent from fuel mass flow.

It is recommended that experiments be conducted to define to what extent combustor hardware is sensitive to increased viscosity and decreased volatility of fuel. Improved fuel injectors and control procedures should be developed to minimize these effects.

It is further recommended that improved ignition devices should be developed which allow reliable ignition, in particular for cold starts and high altitude relight.

4.3 Fuel Thermal Stability

Thermal stressing and long term storage, particularly of the AGARD research type fuels, could lead to the production of fuel insoluble compounds. Higher pressure ratio engines and supersonic aircraft will exacerbate this situation through their inherently higher thermal loadings on the fuel system of both engine and aircraft. The presence and

deposition of these insolubles within the fuel system, particularly in the atomizer, will lead to fuel maldistribution problems, which in turn will cause engine starting and relight difficulties, and excessive temperature scatter in the turbine section. Adherence of fuel degradation products on moving parts can also result in fuel system component malfunction and failure.

Deposition within the heat exchangers will also reduce engine life due to lowered cooling capabilities, so that engine lubricating oil temperatures may rise thereby affecting the bearings. Deposition within the combustor and subsequent spalling, as well as the fuel maldistribution described above, will both lead to increased smoke emissions and thus engine signatures.

In order to reduce these effects, a quantitative definition of factors influencing thermal stability is of great interest, for both aircraft and engine fuel systems with AGARD research type fuels. This requires the identification and definition of laboratory specification-type methods for evaluating fuel thermal stability in line with engine experience (which may require a 'rig' to act as an intermediate step between the engine and laboratory testing). In addition, a method for quantifying fuel storage stability may also be required if the thermal stability tests do not continue to assure adequate storage stability.

It is further recommended that methods be sought to improve the thermal stability of the fuel by refinery processing and additives. Also, improved design and location of fuel system components should be investigated to minimize heat input problems and reduce the vulnerability to deposits.

4.4 Fuel Flow Characteristics

The major problem associated with flow behaviour and pumpability within aircraft fuel systems at low temperatures is the potential blockage of flow passages or 'hold-up' of fuel as a result of increasing freezing point. Analytical and experimental data indicate that long-range commercial aircraft may reach fuel tank temperatures as low as -43°C . Certain long duration military missions may experience even lower tank temperatures. For commercial aircraft, allowing for a 3°C margin of safety, a fuel with a freezing point greater than -46°C might require some form of inflight fuel heating to avoid corrective flight manoeuvre that lead to increased fuel consumption. Short-range aircraft may be able to use fuels with higher freezing points without the use of inflight heating or, at most, through the use of ground pre-heating. Recent analytical and experimental research programs have studied the pumpability of higher freezing point fuels under simulated flight conditions both in unheated and heated fuel systems. Experimental studies of low-temperature fuel system behaviour should continue in small scale, and eventually, in full-scale installations to evaluate advanced methods for fuel heating and wing tank insulation. Additives such as pour point depressants should be explored to determine the feasibility and limits of two-phase flow for fuels near the freezing point. This research should be supported by fundamental research addressed to the development of improved correlations relating freezing point and viscosity to the chemical composition of non-ideal multi-component hydrocarbon fuel mixtures. Other fuel system research should be devoted to the study of the effects of potential changes in fuel properties on lubricity, material compatibility, aircraft safety, and system cleanliness.

4.5 Alternative Fuels in Existing Engines and Aircraft

The above analysis describing the sensitivity of aircraft fuel systems to fuel properties suggests that existing aircraft and engines are likely to have difficulties in operating successfully on an alternative fuel that approaches AGARD Research Fuel in properties. Engines might prove more difficult to start and to relight, exhaust smoke is likely to increase, and temperature patterns in the turbine section may show rapid deterioration. Aircraft scheduled for long duration flights might require flight diversions.

On the other hand, successful research using AGARD Research Fuel could show how to overcome operating difficulties with new engines and existing engines retrofitted with improved combustors and fuel systems. Whether a retrofit of existing systems or the installation of new engines is the more cost effective path to follow cannot be answered without the research suggested by this analysis.

5. CONCLUSIONS AND RECOMMENDATIONS

Increasing shortages of crude feedstocks and the threat of disruptions in supply caused by world economic and potential international conflicts may require a broadening of current jet fuel specifications to improve availability and to allow, at minimum cost and maximum energy efficiency, the use of feedstocks from alternative sources including heavy petroleum crudes, tar sands, coal liquids, and oil shale. To prepare for an uncertain future in jet fuel quality and availability, research is needed to more fully understand the effects of varying fuel properties on engine and aircraft fuel system performance, reliability, and durability, and to build the technology base that would allow greater fuel flexibility in future aircraft.

Since current production engines will still be in service at the turn of the century, research and advanced technology to accommodate possible future fuels must include cost effective retrofit options for current production aircraft as well as new engine and aircraft fuel systems. The following conclusions can be drawn concerning the impact on performance of

operating current aircraft on an AGARD research type fuel

- *Smoke emissions and flame radiation* are controlled principally by fuel hydrogen content

Hydrogen content thus has a major affect on *liner durability*. Calculations show that reductions in hydrogen content from 14.5% typical of F-40 fuel to 13% could lead to as much as a 50% reduction in current liner life although the implication of this prediction on maintainability depends on the engine system.

Low-temperature ignition under cold start and altitude relight conditions will be degraded by increased viscosity and decreased volatility expected in future fuels unless evaporation times can be maintained at present levels by improved atomization. Present atomizers do not appear capable of this requirement.

- Fuels of inadequate thermal stability form *carbonaceous deposits* on fuel system hot surfaces resulting in both fuel system malfunctioning and possible fuel maldistribution and poor temperature patterns in the engine hot section. Future fuels of lower hydrogen content and decreased volatility tend to exhibit lower thermal stability.

Fuel *pumpability* will be a problem for long duration flights with the higher freeze point temperatures typical of AGARD research type fuels.

Finally, the Working Group recommends that

- Supply and demand analyses be conducted to assess the trends in military and commercial aviation fuel usage and refinery product yields, energy consumption, and costs; and furthermore the fuels outlook be reassessed periodically to keep abreast of rapidly changing refinery product demands and processing capabilities. The first such analyses will be the product of Working Group 16.

Research programs such as those described in this report be aggressively pursued among AGARD member nations to determine the effects of fuel property variations on current service and future engines and aircraft fuel systems, and to develop and demonstrate advanced combustor and fuel system technology to accommodate poorer quality fuels likely to be encountered in the future.

- The AGARD Research Fuel, defined in this report, be adopted as a baseline fuel to ensure a consistent data base among participating researchers. Research programs should include, but not be limited to this AGARD Research Fuel, and a complete fuel property analysis should be performed and documented when reporting research results.

Fuels research focusing on property and combustion characterization be conducted which relates chemical properties inherent in fuels from alternative sources to physical properties (both specified and unspecified), and finally to engine performance and durability.

TABLE I
Properties of AGARD Aviation Turbine Fuels

Type	Wide-Cut		Kerosine		
NATO Code	F-40		F-34, F-35		Proposed
	Spec Limit	Typical Values	Spec Limit	Typical Values	Test Fuel ARF(1)
Composition					
Acidity, total (mg KOH/g), max.	0.015	0.006	0.015	0.004	(0.015)
Aromatics (% vol), max.	25.0	11.4	25.0	17.2	R
Olefins (% vol), max.	5.0	0.8	5.0	1.5	R
Sulfur, total (% mass), max.	0.40	0.042	0.30	0.048	(0.30)
Sulfur, mercaptan (% mass), max.	0.001	0.0005	0.001	0.0003	R
Hydrogen content ¹ (% mass), min.	13.6	14.4	13.5	13.8	13.2 ± 0.2
Volatility					
Distillation, IBP, °C	R	59	R	166	-
10% recovered at °C, max.	R	94	205	183	205
End point °C, max.	270	237	300	253	-
Flash point, °C, min.	-	-	38	52	(38)
Density at 15°C (Kg/L), min.	0.751	.763	.755	.804	R
Density at 15°C, max.	0.802	-	.840	-	-
Fluidity					
Freezing point, °C, max.	-58	-62	-50	-54	-30
Viscosity at -20°C, cs, max.	-	-	8.0	4.3	12.0
Combustion					
Specific energy, net (KJ/g), min.	42.8	45.5	42.8	43.2	R
Smoke point, mm, min.	20.0	27.6	19.0	23.9	R
Naphthalenes (% vol)	-	-	-	1.86	R
Corrosion					
Copper strip, 2 hr at 100°C, max.	1b	1a	1b	1a	(1b)
Thermal Stability					
Maximum tube temp., °C	260	>260	260	>260	230
Contaminants					
Existent gum (mg/100 ml), max.	7.0	0.8	7.0	0.9	(7.0)
Water reaction interface, max.	1b	1b	1b	1b	(1b)
Water separation index (mod.) with/ without corrosion inhibitor, min.	70/85	90	70/85	94	(70/85)
Conductivity					
(pS/m), min.	200	-	200	-	(200)
max.	600	-	600	-	(600)

(1) ARF represents AGARD Research Fuel with properties defined by Propulsion and Energetics Panel Working Group 13. Values marked R are considered to be limited by other specification requirements and should be reported. Values in parenthesis are carried over from NATO F-34 to maintain SO₂ emissions, corrosion or contaminants at present levels.

(2) JFTOT at specified max. tube temperature requires a maximum pressure differential of 25 mm Hg and a maximum Tube Deposit Rating, Spun, of 17 (or less than Code 3 Visual Rating).

DISTILLATION RANGE OF MAJOR FUEL PRODUCTS

(Typical Initial and Final Boiling Points)

ARF = AGARD RESEARCH FUEL

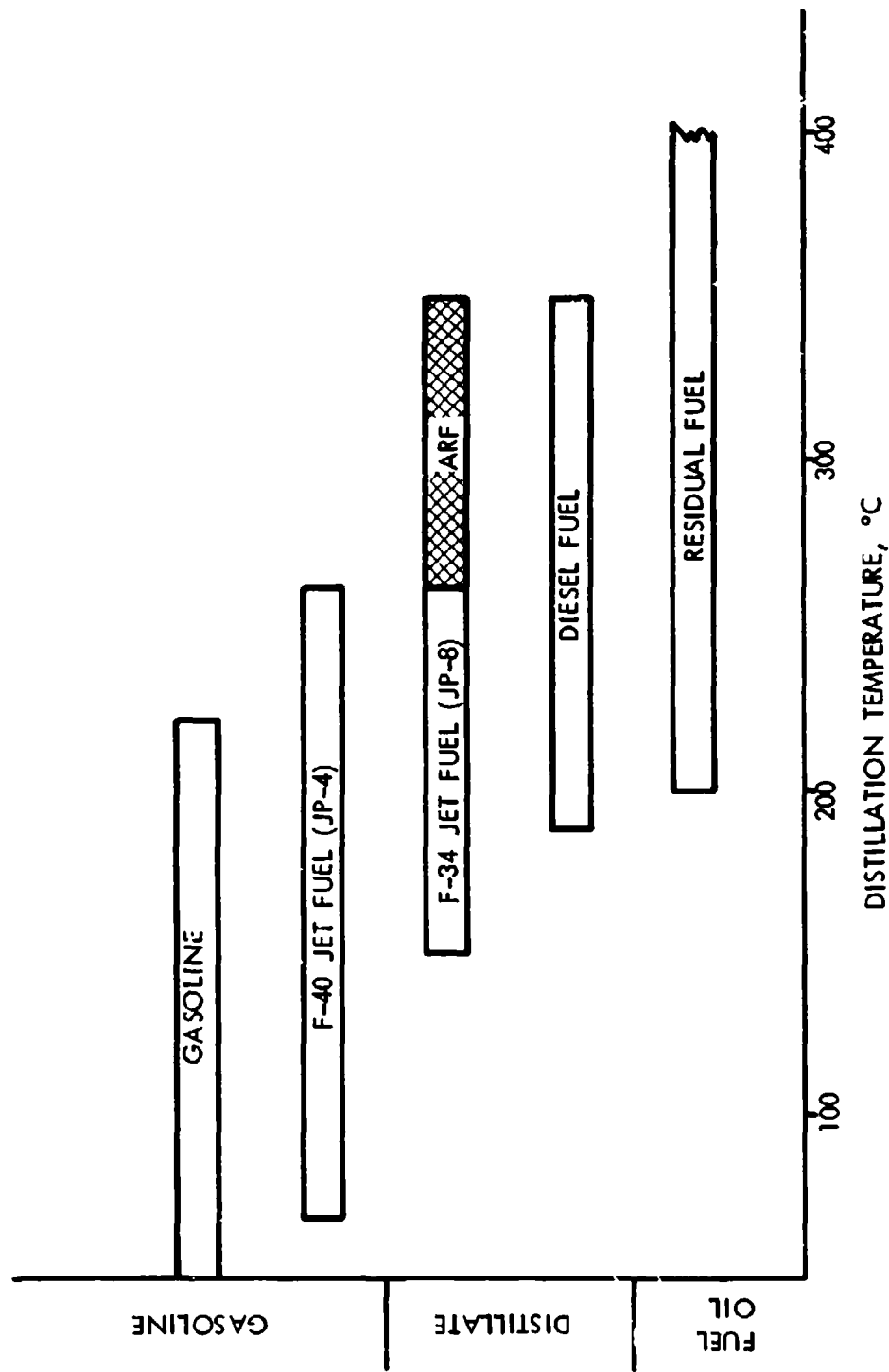


Figure 1

APPENDIX I

SUPPLY/DEMAND FRAMEWORK FOR ALTERNATIVE FUELS

A1.1 INTRODUCTION

Task Group 3 of Working Group 13 on Alternative Jet Engine Fuels was given the assignment of developing the framework for forecasting supply/demand of jet engine fuels for use by the NATO countries in aeronautical research and development efforts.

The Task Group examined the forward projections for supplies of crude petroleum, low quality bitumens and tar sands as well as synthetic crudes produced from oil shale and coal. These projections suggested that the capacity to refine jet engine fuels to current quality may be limited in the future. In particular, the projections indicated that the heavy, hydrogen-deficient crudes required costly and energy intensive refining steps. It appeared desirable to examine the economic tradeoffs that may exist between refining intensity and product quality in order to define an optimum fuel appropriate for future R & D efforts.

The terms "alternative fuels" or "synthetic fuels" are being applied to products from hydrocarbon sources other than conventional crude petroleum that will eventually become significant factors in production of liquid fuels. Today these sources are very heavy crudes, bitumens, heavy residuals and tar sands. In the near future (mid to late 1980s), kerogen from shale is expected to make initial contributions as a hydrocarbon source. In the late 1980s or early 1990s, liquids from coal are anticipated to be introduced as refinery feedstocks.

In preparation for the analysis that will be necessary for studying the availability of an AGARD research fuel, Task Group 3 sought by use of a questionnaire to develop, for each NATO country, year 2000 projections on petroleum demand, crude availability, product distribution, refining capability, civic jet fuel demand, etc. As guided by the advice of Task Group 2 which examined relationships of fuel properties to aircraft performance a set of parameters for an AGARD research fuel were established.

A1.2 METHOD OF ANALYSIS

The analytical methods best suited to projecting the availability and cost of ARF (the proposed AGARD Research Fuel) into the future involve application of computer programs that can examine trade-offs in ARF limits and processing to optimize future yields, energy consumption and cost. Basically the refinery planning studies are to use models that can be demonstrated by the historical record to have validity.

A partial list of the many factors which are bound to influence refinery planning studies based on trends that are now clearly discernable illustrates the complications involved:

- The increasing cost of crude (and natural gas) is accelerating replacement of heavy fuel oils by coal and nuclear energy in power generation.
 - High quality light low sulfur crudes are diminishing to be replaced by heavy high sulfur crudes. Production of conventional crudes is reaching a plateau and is projected to decrease.
 - Hydrocarbon sources other than conventional crude petroleum, such as heavy crudes, tar sands, bitumens, coal, and shale are hydrogen deficient and require considerable upgrading.
 - Technology to improve automobile efficiency is reducing demand for gasoline but increasing demand for diesel fuel, particularly in the US. Total transportation demands dependent on liquid hydrocarbons will increase.
 - By product hydrogen for upgrading crudes and products will become limited as gasoline reforming decreases.
 - Conversion processes to eliminate heavy fractions by catalytic cracking and coking will increase thus making more hydrogen deficient distillates.
 - Removing sulfur and other impurities from crude or products will utilize hydrogen competing with that needed for aromatics saturation.
 - Emission requirements for diesel fuels may result in removal of desirable fractions from jet fuel blends.
 - Petrochemicals, lubricants and non-fuel uses of oil will grow in demand.
 - Energy consumption in refining, or in producing feedstocks for refining is an important cost factor.
- The cost of producing hydrogen for refining from any fossil fuel source will set the pace for energy costs.
- Alternatives such as alcohols exist for other transportation modes but aviation - either civil or military - must continue to utilize liquid hydrocarbons for at least 50 years.

The analysis required to examine products in refining planning studies can be considered in four steps:

- Model Development
- Model Verification
- Application of the Model to Trade-Off Parameters
- Optimization of Product Yields and Costs

A1.2.1 Model Development

The key to a successful refinery planning study – one that produces results eliciting a high degree of confidence – is a sound model. A single model cannot reflect the obvious differences that exist and will continue to exist in different geographical areas. Hence the number of models to be developed is the first judgmental question to be decided. The Task Force considered the historical petroleum supply and demand patterns in NATO countries and tentatively concluded that at least six models would be necessary. After much consultation with industry, the Task Force recommended the following models as most appropriate for development:

- US West Coast – the PAD 5 district
- US East of the Rocky Mountains – the PADs 1 to 4 districts
- Western Canada
- Eastern Canada
- Northern Europe
- Southern Europe

Preparing the models requires collection of baseline data on crude usage, refinery processing, product distribution, projections in energy, feedstock and product demands as influenced by technology changes.

A1.2.1.1 Crudes and feedstocks

The petroleum industry conducts assays of hundreds of different crudes to characterize the fractions from light gases to residual produced in a true boiling distillation. Many assay data are published and much is held in computerized data banks by companies that conduct planning studies. Multi-national companies and national organizations have data on the crudes used and projected for the various NATO model refineries to be studied. The assays provide information on yields and quality of fractions which are basic to processing and product blending.

Crude usage by area and individual refineries is available from both published data and surveys. Projections of crude usage can usually be developed through refinery questionnaires conducted on a coded or anonymous basis. Such projections would include plans for refining of synthetic sources such as tar sands, bitumens, shale liquids, etc.

A1.2.1.2 Product demands

Current refinery output of the range of petroleum products by area is available in published data. In fact these data illustrate why different refinery models are necessary. For example, the gasoline to distillate ratio in the US has historically been higher than in other NATO countries due to the greater US automobile population. The share of the distillate pool blended to aviation fuels both civil and military is likewise different in the US (which consumes about 50% of all aviation fuel) than in other NATO countries.

The important task of projecting product demand into the future requires consideration and judgement on many factors that affect society and other industries. To illustrate some of the many factors a model maker must consider:

- Economic outlooks as they affect specific industries such as airlines relative to ground transportation modes.
- Conversion of the automobile industry to more efficient cars utilizing less gasoline or more diesel.
- Energy use trends as affected by tax structure incentives toward conservation.
- Substitution of coal in industry for liquid fuels as affected by air quality standards.
- Relative cost of energy from synthetic sources vis-a-vis projected world crude prices.

A1.2.1.3 Refinery processes

Considerable baseline data are published on the processes employed in refineries of all sizes in various geographical areas. Some multi-national companies maintain computerized data banks on world-wide refineries to keep up with announced plans to expand, convert, modify, etc. Government agencies likewise are sources of much detailed information on refineries. Questionnaires are also useful in providing detailed data on processes, expansion plans, etc.

Within each area, refineries vary greatly from very large and complicated types that produce a full range of products to very small refineries that lack sophisticated processes and produce a few simple products. In some areas it might be necessary to study two or three model refineries ranging from simple to complex rather than to develop a composite simulated refinery for the area as a whole.

The technology of operating individual processes to refine various products from different feedstocks has been reduced to computer programs that predict yield quality and energy consumption as a function of feedstock quality and operating conditions. The programs also model process networks so that a product from one step becomes the feedstock to another. Outputs also include data on supporting facilities such as power, water, maintenance, etc. and the facilities investment required.

The typical processes considered in a sophisticated computer program include:

- Distillation, atmospheric (crude and light ends)
- Distillation, vacuum
- Catalytic reforming
- Catalytic cracking
- Isomerization
- Hydrocracking
- Hydrotreating
- Coking
- Hydrogen production
- Alkylolation

The starting point in the application of the computerized process network is crude assay data. For a simple refinery, only two or three processes would be called for such as distillation, reforming and hydrotreating. For a complex refinery conversion of higher boiling crude fractions to gasoline and distillates by cracking processes either thermal (coking) or catalytic is the normal pattern. Conversion processes consume more energy than simple distillation and require more investment. The process network programs thus produce not only yield and quality data but economic factors that are reflected in incremental cost data.

A1.2.2 Model Verification

The test of a refinery model's usefulness in analyzing a future product distribution and quality problem is to verify its ability to simulate a historical product pattern. This is normally done by assembling the record of a geographical area for a prior year in terms of products produced, their specifications or average quality and the crudes processed. The models would then be applied utilizing the crude assay data, the product quality targets and the computerized process network.

If the output did not match a geographical area's historical product pattern when appropriately normalized, the model would be altered and the sequence repeated. By iterative procedures, the model could be adjusted until it provided an acceptable representation of the historical record.

Refinery planners state that model verification is one of the most important and difficult parts of a study. For one thing experience and judgement are needed to assess results and to make adjustments that are realistic. On the other hand, a well-verified model provides the confidence to proceed to the planning studies that will permit sound economic decisions to be made.

In considering forward time periods to year 2000 and beyond, it would be logical to modify the model to reflect changing input data. For example it has been estimated that synthetic fuels could reach 12% of US energy demand by 2000 while use of coal for energy could increase from 19 to 33% in this time period. These factors are likely to be reflected in more conversion processes to make distillates from heavy fuel oil. In Canada synthetic crude from tar sands and heavy oils could reach 50% of total crude supply by 2000. Experience would dictate what model changes would be appropriate for year 2000 studies. Experience would also determine how realistic it would be to assume improvements in current refinery processes in the model; for example the search for improved catalysts is active but even if one assumed a measure of success, experience indicates that years elapse before commercialization produces an impact on industry.

A1.2.3 Application of the Model to Trade-Off Parameters

Application of the models to forward time periods requires projections of crudes, synthetic sources, product demands, and product qualities. Because refinery process energy consumptions and costs are so controlled by the major products, it is necessary to consider and fix such critical quality specifications as the following examples:

- octane quality of motor gasoline
- cetane number of diesel fuel
- sulfur content of distillates and heavy fuels
- flash point of distillates
- heavy ends in heating oil
- diesel quality as controlled by exhaust emissions.

The outlook for distillate demand and quality can have an indirect impact on jet fuel availability and quality. For example, if higher boiling aromatics in diesel or burner fuel must be controlled to minimize potentially hazardous particulate emissions, good quality blend components would be directed from the jet fuel pool and remaining low cost hydrogen would be consumed. This route for distillates would affect jet fuel availability and quality adversely compared with different assumptions about distillate quality.

The scenarios under which these major product qualities are selected can obviously change in forward time periods but may also be so dependent upon political factors that the trade-off studies may have to be carried out with widely differing quality selections.

With respect to aviation fuel, the trade-off parameters to be examined would be derived from the factors suggested by Task Group 1 and listed in Section A1.2.1.2 above. Since the analysis of military fuels of the ARF type was directed to represent compatibility with civil and commercial fuels, the base case would represent NATO Fuel F-34 which is fundamentally civil fuel of the Jet A-1 type defined in ASTM D1655. The trade off studies to be conducted on variations of this base fuel might represent the following changes in aviation fuel quality:

- Base fuel meeting JP-8 specifications (F-34) but of -47°C freezing point which would determine both distillation range and aromatics content.
- ARF fuel of 38°C flash point minimum with a -40°C freezing point but targeted at 13.2% hydrogen; i.e. higher in aromatics.
- ARF fuel of 38°C flash point minimum with a -30°C freezing point and 13.2% hydrogen.

Because the thermal stability target of ARF fuels had been established at 230°C instead of 260°C for F-34, the barriers to inclusion of cracked stocks in ARF blends would be the sulfur limit of 0.3% wt maximum now specified for F-34 and storage stability. Thus process severity in hydrotreating for example would be controlled by sulfur reduction of ARF blend stocks; with cracked components, which tend to be high in sulfur, hydrotreating would also tend to saturate olefins (but not aromatics) leading to increases in hydrogen content. These complicated interactions illustrate why a computerized process network is needed to examine trade-off parameters.

The number of trade-off cases to be examined could be very large if for each model area it was necessary to consider different gasoline/distillate ratios, various levels of diesel quality, different sulfur levels, etc. Output data, including not only yield and incremental cost data for ARF fuels but also other economic factors related to energy consumption, onsite and offsite investment etc., would have to be critically examined to select the group of parameters most appropriate for optimization analyses.

A1.2.4 Optimization of Product Yield and Costs

A refinery linear programming (LP) model is a planning tool used in optimization studies. In most cases the requirements of a refinery can be satisfied in many ways. Alternatives and interactions are so complex that the optimum path usually defies manual analysis. The LP computer program represents a series of equations each of which has so many variables that more than one solution is possible. Since these variables include economic factors as well as product yield data and since the program has the built-in restrictions of product quality, feed availability, equipment limitations, etc., the net result is a particular solution which represents an economic optimum.

Applying the refinery LP computer program to the many models representing different NATO areas is apt to produce a different economic optimum ARF fuel for each area since the program restrictions can differ in each case. This is only speculation however. It is conceivable that because ARF fuel will still be a minor product in every area, the economic optimums may be close together. There is no way to answer this without the analysis just discussed.

A1.3 RECOMMENDATIONS

In the course of its exploration of the best technique to conduct supply/demand studies for NATO jet fuel, the Task Group was advised that NASA had contracted a refinery planning study entitled "Analysis of Trends in Aviation Fuel Refinery Yield, Energy Consumption and Costs". The NASA study was directed at US civil jet fuel and covered the first two geographical areas noted in Section A1.2.1 above.

It appeared logical to the Task Group to suggest that NATO studies of military fuels be conducted as an extension to NASA's contracts since the same type of refinery planning expertise would be needed. Moreover a follow-on program to a US civil fuel study would be the most cost effective way of accomplishing the Task Group 3 objectives. The advantages of this approach were several:

- the baseline data for NATO areas would be largely available in original data sources.
- crude assay data appropriate for US refinery models would largely apply to NATO area refineries.
- refinery process models involve the same units for all NATO areas.
- developing models and verifying them becomes easier with the experience and judgement gained from the US study.
- selecting cases for optimizations from trade-off studies becomes easier with experience gained in model manipulation.
- the value of the output from linear programming studies can be assessed for US civil fuels before extending the system to other NATO areas.

A review of the refinery modeling technique actually being employed by the NASA Contractor suggested to the Task Force another useful possibility, i.e. the economics of converting the military from F-40 (JP-4) to F-34 (JP-8) in both the US and Europe could probably be developed using the same modeling technique. This question was studied by the US Air Force some years ago but, except for the United Kingdom, not implemented largely because serious

availability concerns were identified by fuel suppliers. It was reported that the US Air Force is again interested in analyzing the conversion question. Accordingly, the Task Group recommended that the National Delegate to the US to AGARD be advised that an economic analysis of converting NATO military forces in US and Europe from F-40 to F-34 fuel might be accomplished effectively by extending the current NASA contract programs which are aimed at optimizing civil jet fuel.

To answer the supply/demand questions of AGARD fuels and to optimize an AGARD Research Fuel in terms of availability, energy consumption and cost, the Working Group recommended to the Propulsion and Energetics Panel that the NASA contract programs be extended. This recommendation was accepted and a new Working Group 16 was established to assist NASA in the collection of data for NATO areas and to monitor the contractors with respect to the AGARD fuel studies.

APPENDIX 2

THE EFFECTS OF FUEL PROPERTY VARIATIONS ON JET AIRCRAFT ENGINES AND FUEL SYSTEMS

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A2.1 INTRODUCTION

Appendix 2 presents an assessment of a literature survey conducted for the AGARD Propulsion and Energetics Panel by Working Group 13 on Alternative Jet Engine Fuels. Task Group 2 of this Working Group was given the assignment of assessing the effects of potential variations in hydrocarbon fuel properties on the performance, operating envelope, exhaust emissions, durability, maintainability, reliability and safety of aviation gas turbine aircraft. The survey of fuel property effects is divided into the following four sections:

- A2.2 COMBUSTOR HEAT TRANSFER AND EXHAUST POLLUTANTS
- A2.3 COMBUSTOR SPRAY FORMATION, EVAPORATION, IGNITION AND BLOW-OFF
- A2.4 FUEL THERMAL STABILITY
- A2.5 FUEL FLOW CHARACTERISTICS

The purpose of this study is to indicate what is known about the effects of aviation turbine fuel property variations on both commercial and military aircraft and to identify needs for future research. Each of the four sections of Appendix 2 addresses the fuel property variables having the most significant influence on the particular design and operating criteria being discussed. The range of fuel property variations under consideration was guided by the property differences between Jet A-1 (JP-8) and the proposed "AGARD Research Fuel" (ARF) presented in Table 1 of the Executive Summary. This range of fuel property variations was expanded, within practical limits, to permit a more complete parametric data analysis.

The fuel property variables expected to have the greatest influence on gas turbine aircraft include: (1) hydrogen content (aromatics), (2) viscosity, (3) volatility, (4) storage and thermal stability, and (5) freezing point. The sensitivities of various components of gas turbine aircraft to variations in fuel properties are evaluated, potential penalties in performance and durability are identified along with needs for advanced technology to counteract these potential penalties in future fuel flexible aircraft. Recommendations are presented for research and technology needed to assess the feasibility of using fuels with broadened properties.

The subject of jet aircraft engine and fuel system sensitivity to fuel property variations has, for the most part, been treated generically; that is, specific differences between commercial and military aircraft have not been considered. The level of significance of the various fuel system problems cited in the text may obviously be influenced by specific differences in flight mission requirements, engine operating conditions, engine combustor design and fuel system design for different commercial and military aircraft. Practical limits for fuel property variations in current production aircraft fuel systems are expected to be more restrictive than for future designs that might incorporate advanced technology for broadened property fuels. In general, the discussion of fuel property effects on aircraft fuel systems in this report is mainly concerned with subsonic aircraft. The influence of fuel property variations on supersonic aircraft may be considerably different because of elevated fuel temperatures resulting from aerodynamic heating of wing tanks. It is expected that a major difference would be an increased sensitivity to fuel thermal stability.

A2.2 COMBUSTOR HEAT TRANSFER AND EXHAUST POLLUTANTS

A2.2.1 Scope

This section examines data which are pertinent to heat transfer and pollutant formation/destruction within gas turbine combustors. The objective is to assess the magnitude of the possible effects of introducing the new fuels and to obtain (if possible) information relevant to overcoming possible combustion problems. The literature which has been reviewed has been selected so as to encompass the differences between the F34 and ARF fuels. Indeed, the actual variations considered are considerably wider than these requirements.

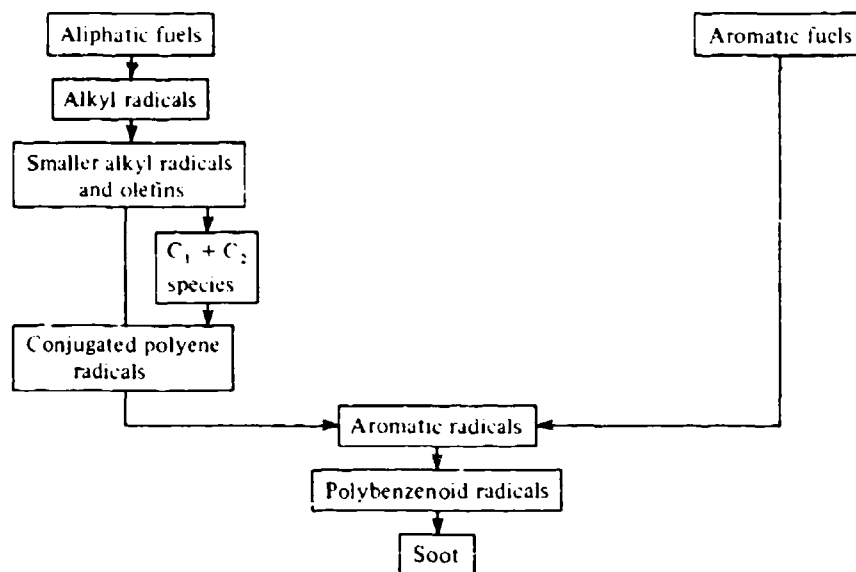
A2.2.2 Carbon Formation and Combustion

Mechanism of Carbon Formation

Soot particles resemble closely, both in structure and composition, the very large polybenzenoid radicals, and to this extent 'carbon' is a misnomer since nearly all soot contains between 1% and 3% hydrogen. Soot is produced in a fuel rich atmosphere at temperatures above about 1300 K. It may be obtained either in premixed or diffusion flames, although at the same conditions, the rate of formation in diffusion flames is said to be about an order of magnitude faster than in premixed flames. A typical route of carbon formation is outlined overleaf.

The route appears to be in agreement with most experimental facts and clearly demonstrates the ease by which aromatic compounds give rise to soot compared with the aliphatic compounds. Further details regarding the route of carbon formation and the mechanisms may be found in [1] to [14]*.

* Numbers in [] correspond to the list of references.



Techniques of Measurement

One of the major problems to be faced in evaluating smoke data is the variety of techniques which have been used for measurement. These range from smoke stack observations through a gamut of exhaust measurement techniques to in-situ measurements within the flame, and to weighing deposits within a combustion system. Although it is about 80 years since it was introduced, the Ringlemann Chart is still used for stack opacity measurements. The method depends upon a comparison of the 'darkness' of a smoke plume with a series of scales on standard charts. It is somewhat inaccurate since (a) it takes no account of plume diameter, (b) different values are obtained depending upon the sky background, (c) stack velocity affects the results and (d) there are variations of sight from person to person.

Many instruments have been developed based upon the interruption of a light-beam passing through the stack or exhaust. These are more accurate than the Ringlemann technique but they are sensitive to particle size distribution and to possible condensates such as fuel vapour and steam.

An extension of the above technique is to sample the stack (exhaust) and to pass the gas through a standard test cell. This type of instrument is typified by the Hartridge and Rolls Royce Smoke meters [5]. These instruments are often used for smoke measurement in gas turbines. The assumption is made that carbon/smoke particulates from gas turbines are of relatively uniform size. Whilst this may be true for the narrow specification range fuels in current use, it will have to be established for the future fuels.

Another technique is to trap the carbon on a filter paper and to examine the degree of staining. A standard volume of gas is passed through the filter, within a known time, and then comparison is made of the stain with known standards. The comparison may be visual or automatic. In the latter case it is usual to employ a reflectance technique. The amount of carbon is determined by comparing the light reading of the sample spot (from 0% to 100% light reflectance) with a reading of the reflectance taken from a clean area on the filter adjacent to the sample spot.

All of the techniques described above are empirical in character and require a calibration of some sort or other. Additionally, there is difficulty in correlating the results given by one instrument with those given by another. The relationships between several methods of determining carbon in exhaust gases are illustrated in Fig. 1. It is stressed that the relations are only approximate since variations occur both with fuel type and with the geometry and operating conditions of the equipment. To use the chart, a known value of one particular piece of equipment is aligned through the reference point. The intersection with the other lines gives the corresponding values. The accuracy is probably no better than about $\pm 20\%$ of carbon content.

The method of obtaining the correlation was to relate the meter performance to the carbon content determined gravimetrically. Briefly, the latter technique involves the filtration of a known volume of exhaust gas from which the carbon content is then determined by the weight gain of the filter. Since the technique has to sample solids from a gas stream, it is usual to employ isokinetic sampling. A simple weighing technique may give rise to errors due to liquid fuel and/or water which can be trapped in the filter. To this end, washing techniques are often employed to remove liquids, the filter being dried afterwards prior to final weighing. In at least one technique, after such cleaning, the sample is combusted and the carbon estimated from the mass gain of a carbon dioxide absorption tube. Used on a semi-micro basis, this latter version of the gravimetric technique offers the additional possibility of determining the hydrogen content of the soot. It is usual for gravimetric techniques to express the result as smoke density (mg/m^3).

It is not surprising that with such a variety of techniques and accuracies available to industry, the comparison of smoke data is rendered difficult. If a wide variety of fuels is to be used in the future it would be well to consider establishing some form of standard isokinetic sampling, calibrated by means of a standard gravimetric technique, in order to obtain the best comparative information for pollutant purposes.

Practical Observations

(a) Point of Incipient Carbon Formation (Premixed Flames)

The point of incipient carbon formation (PICF or threshold of carbon formation) is defined as the condition at which soot (carbon) is first detected. Fig.2 illustrates data taken from Macfarlane et al. [6], Schlader et al. [7], and Blazowski et al. [8]. The apparatuses used were (a) a high pressure burner, (b) a bunsen flame at atmospheric pressure, and (c) a well stirred reactor at atmospheric pressure. There is obviously a large degree of scatter, but still a pronounced trend with hydrogen content. Macfarlane et al. reported that pressure had negligible effect on the PICF but a plot of the various results (Fig.3) does suggest a slight relationship. Both Macfarlane and Blazowski suggest that velocity has little effect upon the PICF, with the possible exception of ethylene which showed a pronounced effect when tested in a stirred reactor. In this latter instance the velocity (or residence time) is represented by mass flow (Fig.4). It is interesting that the value of the PICF corresponding to the lowest value (Blazowski) corresponds closely to that of Schlader et al. (using a Bunsen burner).

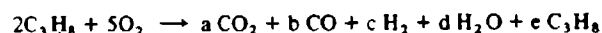
Before leaving the subject of incipient carbon formation, the following facts are of interest -

- (i) One of the claims formerly made for 'walking stick' vaporiser combustor was the absence of smoke. The air/fuel ratio in the vaporiser was of the order of 5/1.

Hence the C/O ratio is given as (Fuel 86% C)

$$C/O = \frac{0.86}{5 \times 0.233} = 0.74 \text{ wt/wt} = 0.98 \text{ atom ratio}$$

- (ii) A spherical combustor using propane/air only produced measurable carbon at equivalence ratios greater than $\phi = 2.0$.



$$C/O = \frac{2 \times 3}{10} = 0.6 \text{ atom ratio.}$$

- (iii) Schlader et al. [7] gives the atom ratio for propane/air as 0.48.

With this in mind a line has been added to Fig.2 corresponding to a C/O atom ratio of 0.5. The agreement between the line and the experimental points is quite suggestive.

(b) Point of Incipient Carbon Formation (Diffusion Flames)

The route of carbon formation is very similar to that of premixed flames except that both the start of formation and the amount of carbon are very dependent upon the local variations of oxygen concentration. For this reason there is no well defined point of incipient carbon formation. However, there must be an equivalent to such a point, since by reducing the pressure of a gas turbine combustor it is possible to obtain a transition from a luminous yellow flame to a transparent blue flame. Similarly, the same change may be obtained within a gaseous diffusion flame by introducing a diluent gas within the fuel stream. It is not clear whether such effects are due to the reduction in oxygen partial pressure, or to possible temperature effects, or to both.

The experimental effects of changes in gas temperature are difficult to analyse. Sometimes an increase in temperature will cause an increase in soot content, at other times a decrease will ensue. This, presumably, is due to the competing effects of soot formation and combustion. A limited review of available data suggests that maximum soot production will occur within the temperature range 1450-1800 K [9 and 10], the precise value being dependent upon the other conditions such as pressure etc.

Because of the dependence upon local oxygen concentration, diffusion flames are very much affected by mixing patterns within the combustor. Hence, it is not surprising that there is a wide scatter in the behaviour of different combustors operated at similar conditions. It is this sort of behaviour that makes the modelling of carbon formation/combustion so very difficult.

Quantities of Carbon Produced

In this section, the concern will be directed towards the net carbon production, (i.e. exhaust carbon content) rather than the local distribution. Very few measurements exist of the carbon within the interior of gas turbine

combustors, at least within published material. To some extent the distribution is represented by the changes in flame emissivity throughout the combustor. Since such carbon (e.g. that in the primary zone) has its largest effects upon heat transfer, consideration of this phenomenon will be reserved until the section dealing with heat transfer is presented.

(a) Premixed Flames

Until recently, almost all work on premixed flames was carried out using laboratory bench scale equipment, bunsen burners and the like. However, in the last year or so, an intensive investigation has been done within a jet-stirred (well-stirred) reactor [8].

As Blazowski et al. [8] and Macfarlane et al. [6] show, the amount of carbon is very dependent upon the hydrogen content of the fuel. In addition, Macfarlane et al. also show a strong dependence upon pressure; the observed values of the proportionality factors were stated to be between 2.5 and 3.0. Both teams also confirm the considerable effect of equivalence ratio, although Macfarlane's results often indicate a peak production area, (for most fuels) between $\phi = 2.0$ and $\phi = 2.5$. This, possibly, is associated with the lowering of temperatures as the mixture is richened.

The relationship of fuel hydrogen content to soot production is well demonstrated by Blazowski et al. (Fig.5). However, these results, plus others given in the same paper, generally correlate well with the carbon/oxygen atomic ratio (Fig.6). This latter appears to eliminate not only fuel composition but also equivalence ratio. Much more evidence is required to prove its general applicability. The result is very interesting and worthy of additional investigation.

(b) Diffusion Flames

The effect of hydrogen content (or carbon/hydrogen) upon carbon formation has been the subject of several investigations over the years. Typical are those given in Refs [11 to 15]. As previously mentioned, the varying methods of determining the carbon, plus incomplete published records, makes the comparison of data difficult.

To overcome this difficulty (at least to some degree) the term C^* is introduced.*

$$C^* = \frac{\text{Carbon formed by any fuel}}{\text{Carbon formed by a fuel having 12.5\% hydrogen}}$$

This ratio eliminates having to obtain the conversion of technique, and facilitates comparison of a number of experiments. Typical results are given in Fig.7 for a number of fuels tested in a range of combustors, both gas turbine and laboratory. It should be noted that the results include both premixed and diffusion flames. There is considerable scatter but the slopes of the individual groups of points remain fairly constant and the line on Fig.7 could be used to predict the effect of a second fuel, providing that a datum point had been established with the first fuel.

Although this sort of correlation is useful, the necessity of a reference point is an inconvenience and it would be better if the actual carbon content could be predicted. The work examined in this report has indicated that the factors of pressure, inlet temperature, hydrogen content and oxygen/carbon ratio are of major influence, with temperature probably having a relatively small effect. Of course, residence time must play a large part, but for much of the data examined in these notes, a fairly constant residence time of about 2 to 3 milliseconds would appear to be appropriate within the reaction zone. Analysis of the available results suggests that

$$E.I._{\text{carbon}} = 1.08 \times 10^{-29} \left[\left(\frac{P}{O/C} \right)^{2.7} (H/C)^{-5.45} \right] T_3^{-4.66}$$

and a plot of the data is given in Fig.8. In this instance all the variables refer to the overall operating conditions. The combustors considered are conventional pressure jet atomising ones.

In Fig.9 additional results have been added including some primary zone data, so-called premixed combustor data, and some very rich premixed hydrocarbon combustion data in a laboratory combustor. It is seen that in general the slope characteristics are similar. The exception is the premixed combustor. In this case the carbon content was so low that clearly discernable trends are not visible.

The real significance of Figs. 8 and 9 is that they suggest that a prediction technique is feasible and that it might well be possible to utilise experimental data from a wide variety of sources. They also indicate the magnitude of the effects of the major variables. The fact that both pressure and the atomic oxygen/carbon ratio have the same exponent suggests that the oxygen partial pressure might be a better correlating function. This has already been noted in the classical work of Appleton et al. [9 and 10], dealing with carbon formation in simple flames and shock-tubes. The same authors also consider the effect of temperature upon both formation and 'burn-out' of carbon. Since overall conditions are only generally available and because of the difficulty in defining carbon formation/burn out zones and conditions in gas turbine combustors no attempt has been made to include these factors. Glassman [15a] postulates that the dominant factors which control carbon formation in diffusion flames are the flame temperature and the fuel structure and that fuel

* Symbols used in the text are defined in Section A2.2.2.5, "Nomenclature".

pressure has only a secondary effect and that the carbon to hydrogen ratio plays no direct role whatever. If this latter is correct, then the data in Figs. 8 and 9 correlate simply because the parameters of pressure and oxygen/carbon ratio are functions of temperature and the hydrogen/carbon ratio a function of temperature and structure. So far, attempts to correlate the data in terms of the theoretical adiabatic maximum flame temperature have not met with any success.

Discussion

The present work indicates that it might be possible (within broad limits) to predict the effects of alternate fuels upon carbon formation in combustors.

What is surprising, is the degree of correlation found between the relatively simple premixed-prevaporised flames and the complex droplet diffusion flames within gas turbines.

The correlation suggested here is not considered to be either sufficiently comprehensive or accurate, but it does suggest a semi-quantitative estimation which might well be usefully modified as additional, and more precise, information becomes available. In particular, results from lean premixed combustors would not be anticipated to lie within the existing scatter band. Similarly, different behaviour may well occur for two-stage or catalytic combustor or other non-conventional types. *The curve must be accepted for what it is - a trend.*

Of the various techniques available for the determination of exhaust carbon, it is felt that some form of gravimetric determination will be required if reliable measurements are to be made.

The role of mixing in the primary (combustion) region of a gas turbine combustor is known to have a large effect upon exhaust smoke. However, it would appear that by the time the average combustor has been fully developed, the quality of mixing is good and that the smoke producing effects have been largely overcome. What, perhaps, is one of the more important aspects worthy of study, is the effect of residence time within the combustion zone.

An interesting implication of the correlation given in Fig. 8 is that it suggests that low pressure test results can be scaled to high pressures. This again is a region which would merit further investigation since if it could be shown that atmospheric pressure test results could be utilised for reliable prediction at high pressures, the cost of combustor development could be significantly reduced.

The relationship between flame luminosity and carbon formation has not been considered in these notes since very few experiments have been done with sufficiently accurate measurements of the two phenomena. This, also, could be a useful area for experiments.

A2.2.3 Heat Transfer Effects

General Considerations

Without the presence of film cooling, the combustor walls receive heat by convection and radiation from the hot gases. The walls lose heat by convection to the air in the combustor casing and by radiation from the combustor wall to the casing wall. If longitudinal conduction is negligible then -

$$R_1 + C_1 = R_2 + C_2 = K_{12} \quad (1) \text{ and } (2)$$

Despite the presence of simultaneous heat and mass transfer (with chemical reaction) as early as 1960, Lefebvre and Herbert [16] were able to predict wall temperatures over a range of conditions.

In almost every real combustor, the situation is further complicated by the use of air for film cooling purposes. The situation considered here is that of a CONVENTIONAL COMBUSTOR burning a LIQUID HYDROCARBON FUEL and using one or other of CONVENTIONAL FILM COOLING DEVICES.

If conduction is restricted to that across the liner wall, then Equations (1) and (2) may be solved by using the following equations [17].

$$R_1 = \sigma \left[\frac{1 + \epsilon_w}{2} \right] \epsilon_g \cdot T_g^{1.5} [T_g^{2.5} - T_w^{2.5}] \quad (3)$$

$$C_1 = 0.017 \left[\frac{\lambda_c}{\mu_c^{0.8}} \right]_{T_c} \left[\frac{M_c^{0.8}}{A_c^{0.8} \cdot d_c^{0.2}} \right] \cdot (T_c - T_w) \quad (4)$$

$$R_2 = 0.4 \sigma [T_w^{2.4} - T_a^{2.4}] \quad (5)$$

$$C_2 = 0.02 \left[\frac{\lambda_a}{\mu_a^{0.8}} \right]_{T_a} \left[\frac{M_a^{0.8}}{A_a^{0.8} \cdot d_a^{0.2}} \right] (T_w - T_a) \quad (6)$$

$$K_{12} = \frac{\lambda_w}{t_w} (T_{w1} = T_{w2}) \quad (7)$$

If the combustor wall is thin, Equations (2) and (7) may be discarded, and in this case $T_{w1} = T_{w2}$. In their paper, Kretschmer and Odgers [17] derive empirical relationships which enable the solutions of Equations (1) to (7) to be made within the various zones of a combustor. These relationships are -

Recirculation Zone Temperature

$$T_{out,RZ} = T_a + \eta_{RZ} \Delta T_{\phi=1} \quad (8)$$

$$\eta_{RZ} = 0.56 + 0.44 \tanh [1.5475 \times 10^{-3} (T_a + 108 \ln P_a - 1863)] \quad (8a)$$

Primary Zone Less Recirculation Zone

$$T_{out,PZ} = T_a + \eta_{PZ} \Delta T_{PZ} \quad (9)$$

$$\eta_{PZ} = 0.71 + 0.29 \tanh [1.5475 \times 10^{-3} (T_a + 108 \ln P_a - 1863)] \quad (9a)$$

$$T_{mean,PZ} = T_a + 0.5 \eta_{PZ} (\Delta T_{\phi=1} + \Delta T_{PZ}) \quad (10)$$

Secondary Zone Temperatures

$$T_{in,SZ} = T_{out,PZ} \quad (11)$$

$$T_{out,SZ} = T_a + \eta_{SZ} \Delta T_{SZ} \quad (12)$$

$$T_{mean,SZ} = 0.5 (T_{in,SZ} + T_{out,SZ}) \quad (13)$$

$$\eta_{SZ} \text{ is given by Equation (17) for weak mixtures} \quad (13a)$$

$$\eta = 1/\phi_{SZ} \text{ for rich mixtures} \quad (13b)$$

Dilution Zone Temperatures

$$T_{in,DZ} = T_{out,DZ} \quad (14)$$

$$T_{out,DZ} = T_a + \eta_{DZ} \Delta T_{DZ} \quad (15)$$

$$T_{mean,DZ} = 0.5 (T_{in,DZ} + T_{out,DZ}) \quad (16)$$

In the secondary and dilution zones, η is given by [18] (Restriction: $\eta < 99\%$)

$$\lg \lg 1/\eta = 0.911 \lg \psi 300 + 4.01 n - 1.097 + D^* \quad (17)$$

In Equation (17), the combustion efficiency term refers to that measured or determined at the outlet plane of the combustor; all other terms refer to the combustion zone, a region which is usually coincident with the primary zone. Equation (17) is specific for fuel lean mixtures. A new value of the constant, D^* , is required in most instances. The value of D^* is best determined experimentally, but if this cannot be done for any reason, then an approximate value, for weak mixtures, may be obtained from:

$$D^* = 0.736 - 0.0173 (P/\Delta P) \quad (18)$$

Equation (18) is based upon very scant data and should be mistrusted accordingly! It is based upon a re-analysis of the data discussed in [18] and taking into account their known pressure losses. Since there are only five combustors in ref. [18] the equation can only be accepted with reservation.

If measurements of combustion efficiency exist (or values of air loadings are required) for inlet temperatures other than 300 K, the following correction is valid for application [17]:

$$\psi T_a / \psi T_{300} = 10^{-3.054 y^{-1.205}} \cdot T_a^{1.2327 y^{-1.205}} \quad (19)$$

where

$$y = \phi \quad \text{when} \quad \phi \leq 1; \quad y = 1 \quad \text{when} \quad \phi > 1. \quad (20)$$

Equation (19) is applicable to a wide range of operating conditions and combustor type (ranging from spherical combustors to practical gas turbines).

Determination of Cooling Gas Temperature

Various models of adiabatic film cooling have been suggested, ranging from those based on theoretical assumptions to purely empirical ones. Nearly all of them use the concept of film cooling effectiveness, which is defined as:

$$\eta_c = \frac{T_g - T_c}{T_g - T_a} \quad (21)$$

Under adiabatic conditions, T_c can be equated to the wall temperature, T_w . Despite the many different premises assumed for the prediction of η_c , the form of most of the correlation parameters is similar to that of Equation (22) [18]:

$$X = \left[\frac{m_g A_a}{m_a A_g} \right]^{0.8} \left[\frac{T_g}{T_a} \right]^{0.6} \cdot \frac{x^{0.8}}{s} \cdot f(s, u_g, u_a, \nu_g, \nu_a)^a \quad (22)$$

where the exponent "a" is of the order of ± 0.2 .

The form of Equation (22) used in this work is that of Winter and Odgers [19]. The parameter is:

$$X = \left[\frac{m_g A_c}{m_c A_g} \right]^{0.8} \left[\frac{T_g}{T_a} \right]^{0.6} \cdot \frac{x^{0.8}}{s} \cdot k \quad (23)$$

$$k = 1.0 \text{ when } 0.8 < u_g/u_c \quad (24)$$

$$k = [u_g/u_c + 0.2]^{-1.25} \text{ when } 0 < u_g/u_c \leq 0.8. \quad (25)$$

Typical correlations of X versus η are given in Fig. 10.

Determination of the Flame Luminosity and Emissivity

In order to specify the amount of radiation from the flame, it is necessary to estimate its emissivity. A considerable amount of laboratory work has been done to measure the emissivity of both transparent and non-transparent gases, but for gas turbine application, one has to resort to convenient semi-empirical equations which have been determined in the field. A very useful set of equations are given in [17].

$$\epsilon = 1 - e^z \quad (26)$$

$$z = -0.286 \cdot P \cdot (f \cdot \ell)^{0.5} \cdot T_g^{-1.5} \quad (27)$$

The radiation beam length " ℓ ", is entirely dependent upon the size and shape of the flame. Conventional combustors may be assumed to be filled with the gas, and, hence, the beam length correlates with the combustor geometry.

For tubular combustors

$$\ell = 0.6 d, \text{ where } d \text{ is the combustor diameter.}$$

For annular combustors

$$\ell = 0.6 (d_o - d_i), \text{ where } d_o \text{ and } d_i \text{ are the outer and inner diameter respectively.}$$

Equation (27) applies to non-luminous flames. When the flame is luminous, the emissivity becomes much more difficult to define. In laboratory flames, and in some furnace applications, it is possible to estimate the change in emissivity by specifying the rate of production and the size of carbon particles within the flame (for example, see [20]). Unfortunately these data do not seem to be capable of general application to gas turbines. Consequently, once again, resort is made to an empirical "fudge-factor", the so-called flame luminosity. Equation (28) specifies the value of flame luminosity as used for the current work. It was derived from data from several sources [16], [21] and [22] and represents the best-fit line to the data available. It has proved satisfactory for most applications to date, but it has obvious limitations. For example, methane apparently has a luminosity less than one. However, it may be used for fuels having a molecular weight of 44 or above, providing that: (a) they are saturated hydrocarbons or (b) they are commercial liquid fuels of a conventional character and ranging from gasoline to fuel oils. With these restrictions, the estimated accuracy of the equation is about ± 25 percent. It is obvious that this is an area demanding further investigations.

$$L = 0.0691 (c/h - 1.82)^{2.71} \quad (28)$$

Correction for luminosity is then made according to [16] by insertion into the emissivity Equation (28)

$$\epsilon = 1 - e^{Lz} \quad (29)$$

Values of Miscellaneous Properties

Metal Emissivity

Values for the metal emissivity are fairly well established and may be found in any good source book of heat transfer or metallurgy. If the combustor has been in operation for some time, the wall emissivity approximates to 0.8, assuming a nickel base alloy.

Values for $\lambda/\mu^{0.8}$

Since, in a real combustor, the film cooling air defines the conduction to the wall, values of the foregoing function are required only for air. They may be conveniently expressed in terms of an equation:

$$\lambda/\mu^{0.8} = 74.811 + 1.674T^{0.75} - 0.0081599T^{1.5} + 2.2539 \times 10^{-5}T^{1.25} - 2.5287 \times 10^{-6}T^3 \quad (30)$$

If an uncooled combustor is used, and there is need to predict wall temperatures, then the appropriate values for the hot gas will refer to combustion products. These may be found in [16].

Metal Thermal Conductivities

The values given in the table below have been derived from a number of source books.

Thermal Conductivity, W/mK

	300 K	1000 K	1300 K
18/8 Stainless steel	17	25	26
Carbon steel	54	46	27
Nickel	90	55	69
Nimonic 75	11	24	30
Nimonic 90	11	25	31
Hastalloy X	9	23	29
Hastalloy 235	9	22	28
Udimet 500	11	22	27
Inconel 625	10	20	23
Inconel 700	12	17	19
Inconel 800	11	24	29

If the hydrogen content is assumed to be the only fuel characteristic which alters heat transfer properties (i.e. different types of aromatics have no effects) then the method of wall temperature prediction described above may be used to predict changes in heat transfer. It is anticipated that the magnitude of such effects would be largely due to changes in radiation properties, since for the same equivalence ratio, the dissociated flame temperatures are within $\pm 1\%$. In order to predict wall temperatures, it is assumed (within the Laval University Combustion Laboratories) that the calculated flame luminosity (Eqn. 28) will be applicable within the recirculation and primary zones. For the secondary (intermediate) zone, because of dilution and carbon burn-out, the value of luminosity is reduced from that of the primary zone by a factor of two. Within the dilution zone the luminosity is assumed to be unity. Making the above assumptions, values of wall temperatures have been calculated (a) for an idling condition and (b) for a series of engine compression ratios (based upon an ambient air temperature of 300 K and a compressor polytropic efficiency of 85%).

Predicted Changes in Wall Temperature due to Changes in Fuel Composition

A hypothetical combustor will be selected having the following characteristics –

Combustor type	– Pipe Chamber	– (see Fig.11)
	– Combustor Diameter	– 230 mm
	– Casing Diameter	– 290 mm
Total air mass flow (@ 1.5 MPa)		– 13.4 kg s ⁻¹
Total fuel mass flow (@ 1.5 MPa)		– 0.27 kg s ⁻¹
Primary zone equivalence ratio		
ϕ_{PZ} (% air in zone = 20.52%)		– 1.0
Secondary zone equivalence ratio		
ϕ_{SZ} (% air in zone = 59.04%)		– 0.5
Overall equivalence ratio		
ϕ_{OV} (% air in zone = 100%)		– 0.3

The film cooling device will be assumed to be a wiggletrip of gap width, 2.5 mm and the calculations will be performed at 25 mm, 50 mm and 75 mm downstream from the slot. Often both wiggletrip and combustor wall are considered "thin" so that conduction effects may be neglected. However the influence of conduction was considered for the present calculations. A maximum difference of about 20 K was observed across the walls. The combustor exit combustion efficiency will be assumed 100% at all operating conditions. Six inlet conditions will apply as follows -

Condition		A	B	C	D	E	F
Inlet temperature	K	300	480	630	723	800	925
Inlet pressure	MPa	0.1	0.5	1.0	1.5	2.0	3.0
Total mass flow (air)	kg/s	1.39	5.48	9.57	13.4	17.0	23.7
Total mass flow (fuel)	kg/s	0.028	0.112	0.193	0.270	0.342	0.477

For each of these conditions the wall temperatures will be assessed for the following fuels having the specified hydrogen contents given below.

Fuel	A	B	C	D	E
%H (mass)	16	14	12	11.11	8
Stoic. A/F	15.11	14.65	14.19	13.73	13.28

In order to attain the same turbine inlet temperatures it will be necessary to adjust the fuel flows so as to maintain a constant equivalence ratio in each section of the combustor. Thus at each condition, the fractional fuel flow will be given by -

Fuel	A	B	C	D	E
Fractional fuel flow	1.03	1.00	0.969	0.937	0.906

This also ensures near to constant temperature conditions within the primary and secondary zones. However, the flame luminosity will change and (using the formula given in [17] this will be (Eqn. 28)) -

Fuel	A	B	C	D	E
c/h (mass)	5.25	6.14	7.33	8.00	11.50
Flame luminosity (L)	1.95	3.65	7.06	9.62	32.45

The metal emissivity will be assumed to be 0.8. For calculation purposes 5% of the total air will be assumed for each film cooling device (wiggletrip), and it must be borne in mind that, since these calculations are solely for comparative purposes, the resultant wall temperatures do not necessarily represent good design practice.

Predictions will be made for the film cooling slots in both primary and intermediate zones. No calculations will be performed for the dilution zone since in this region a constant luminosity factor of unity is assumed. Thus one would expect any changes in this zone to be minor.

The results of the calculations are shown in Figs. 12 to 15.

(a) Flame Emissivity

Fig.12 shows the effect on flame emissivity for the recirculation zone and Fig.13 is pertinent to the secondary, or intermediate zone. The band-spread on the curves has no particular significance except that:

- it indicates the effect of position within the zone relative to the start of the film cooling;
- as the hydrogen content decreases, the width of zone decreases due to the higher "basic" emissivity.

The effect of engine compression ratio is much more pronounced in the secondary zone than within the primary zone. This is to be expected due to the reduction in flame emissivity which emphasises any differences caused by fuel type. No attempt has been made to predict conditions within the dilution zone since it is assumed that all combustion (including carbon burn-out) is completed at the exit to the secondary zone and hence the flame luminosity in this zone is assumed to be unity.

(b) Wall Temperature

Figs. 14 and 15 show the effects upon wall temperatures, expressed in the dimensionless form used by Blazowski [23], where T_{LO} is the wall temperature yielded by a fuel containing a standard amount of hydrogen (in this case - hypothetical JP5: H = 14.5%). As with the emissivity, the band-spread in the wall temperatures has no significance except as it relates to the zone temperature distribution. The magnitude of the effects of hydrogen content are

- much more significant at low pressures and
- are greater within the second zone than the primary zone.

This is in accordance with the changes in flame emissivity previously observed (Figs.12 and 13).

(c) *Heat Transferred by Hot Gas Radiation*

Table I indicates a measure of the change in the flame radiation as a fraction of that given by a fuel containing 14.5% hydrogen. The results are in accord with those noted in Para. (b) for the changes in the wall temperature.

(d) *Radiation from Hot Carbon Particles*

All the above phenomena have been based upon heat transfer from a flame having "mean" properties based upon empirical correlations. Such correlations were obtained by observations of the behaviour of flames in practical combustion systems used in gas turbines and generally using fuels containing not less than 11% hydrogen. Thus, the 8% values represent an extrapolation. In addition, it has been assumed that the behaviour of the fuel is completely defined by the hydrogen content and that the type of compound has no effect upon the carbon formation and/or the heat transfer properties. Suppose that either (a) the extrapolation is not valid or (b) that the latter supposition is untrue. Suppose also that the result is the formation of a large amount of relatively big particles of carbon which burn at their surfaces. Examination of the relevant thermodynamic data suggests that it is possible that the temperature adjacent to (or at the surface of) such particles could be considerably higher than the local hot gas temperature. Under such circumstances the total radiation might suffer a significant increase with a corresponding increase of wall temperature. Currently, there is no known technique for predicting this phenomenon, but with careful attention to experimental details, it might be possible to detect it, if it actually exists.

Experimental Results

(a) *Flame Emissivity*

Very few data are available with respect to the experimental determination of flame emissivities for a range of fuels at various engine conditions. In general, the experimental techniques appear to be limited to the measurement of wall temperatures and flame radiation. The absence of measured hot gas temperatures renders it impossible to deduce the flame emissivities with any real precision.

Fig. 16 indicates the range of flame luminosity reported by various authors [25]. It will be seen that there is a considerable spread of results and if luminosity is to be retained for prediction purposes it is desirable that further experimental work be done. The definition and significance of flame luminosity has already been discussed.

In the work of Marsland et al. [20] the emissivity of an aviation kerosine was determined over a range of pressures up to about 3 MPa. The results are illustrated in Fig. 17 together with the predictions based upon Equation (28). The agreement is considered to be satisfactory.

Fig. 18 shows some experimental data obtained by Rolls Royce [26]. Although insufficient information was available to enable predictions to be made, the results are typical, and similar, to those of Figs. 12 and 13.

(b) *Wall Temperature Measurements*

A fair amount of work has been published on the effects of fuel composition on wall temperature. All workers agree that diminishing the fuel hydrogen content increases the wall temperature of the combustor. Most of the data appear to correlate against a temperature parameter [23] defined by

$$[Tx] = \frac{T_L - T_{LO}}{T_{LO} - T_3}$$

where T_L is the actual liner temperature

T_{LO} is the temperature yielded by a "standard" fuel (usually 14.5% H)

T_3 is the combustion inlet temperature.

Examples of such data are given in Figs. 19 to 27 taken from references [27] to [33];

Fig. 19 is based upon data from Gleason et al. [27] and plots the temperature parameter against the hydrogen content of the fuel for a number of aircraft combustors. The solid points are for the J79 data reported in [27] and they compare favourably with the primary zone predictions for similar inlet conditions. The predictions are those described in the paragraphs entitled "General Considerations" and "Predicted Changes in Wall Temperature due to Changes in Fuels Composition". The remaining test units do not have their operating conditions specified, but presumably they are not too far removed from the J79 case. It is findings such as these that leads one, tentatively, to accept $[Tx]$ as a general parameter, at least for standard type combustors.

It will be remarked that the above mentioned units are all from engines which are fairly "old". In Fig. 20, data are plotted for a more recent combustor, the F101 [28]. The engine data points lie below the general scatter of the previous engine and also below the predicted values at similar conditions. This is generally accepted to be the result of a lean primary zone mixture compared with those of the older chambers. Since this "leanness" is not given in the reference it

could not be "allowed-for" in the prediction technique (which in this case assumed $\phi_{PZ} = 1.0$) and this may well explain the high prediction value.

All the predictions made in this report have utilised a stoichiometric mixture within the primary zone. Simple observations of the form of Equation 27 indicate that a change in fuel/air ratio will alter the emissivity. A weak mixture will have a lower emissivity than a rich one. That this occurs in practice is well illustrated by Bauserman et al. [29]. The results are illustrated in Table II and Fig.21. To obtain the values cited in Table II and Fig.21 the following procedure was adopted.

The published results were plotted in the form T_w versus $(T_{out} - T_{in})$ and smooth curves were then drawn through the points for each thermocouple position. These curves were then used to yield the data cited here. The effect of the combustor outlet temperature (T_4) i.e. changes in fuel-air ratio are clearly seen, especially within the combustion zone (Fig.21). For comparison, the scatter of the aircraft data (Fig.19) is also included. The significant effects of operating air/fuel ratio suggest that, for the $\{Tx\}$ parameter to be valid, both operating fuel/air ratios and combustor entry conditions should be held as near constant as possible. The experimental findings are in qualitative agreement with the prediction technique but absence of aerodynamic data made quantitative prediction very unreliable.

The results of Bauserman et al. [29] also indicate the zonal influence upon the temperature parameter. Thus the primary-secondary zones show the largest influence, although not necessarily the highest temperature. This implies that the value of the parameter $\{Tx\}$ per se should not be used as an absolute measurement of probable life. It is obvious that at idling conditions a much larger range of $\{Tx\}$ may be tolerated than, say, at full-load take-off.

The dependence of $\{Tx\}$ upon the operating air/fuel ratio (combustor outlet temperature) was also noted by Singh et al. [30] using a $\frac{1}{2}$ -scale version of the Westinghouse industrial gas turbine W251. Their results are reproduced in Table III. The values of $\{Tx\}$ were obtained both as a function of the hydrogen content and the aromaticity. The latter is defined by

$$\%C_A = \frac{100 \times \text{aromatic carbon atoms}}{\text{aromatic carbon atoms} + \text{non-aromatic carbon atoms}} \quad (32)$$

Aromaticity is negatively correlated with hydrogen content as indicated in Fig.22 [31]. There is considerable scatter. The formulae given in Table III indicate that the hydrogen content is a slightly better correlation parameter.

Fig.23, taken from [23] illustrates experimental results which show the variation of $\{Tx\}$ with operating conditions for the T56 combustor. Without the knowledge of the thermocouple positions it has not been possible to make theoretical predictions. The gradients of the curve suggest that the measurements were made in the "secondary" zone. That the measured temperature is sensitive to position in the combustor is illustrated in Fig.24, taken from the same source [23]. The effect is most noticeable for the low hydrogen content fuel and is most likely due to changes in local combustion efficiencies.

Concern has been felt with respect to the effect of the types of compounds present in the fuel. Thus, two fuels having the same hydrogen content might well exhibit different fuel properties due to the structure of the hydrocarbons of which they are comprised. Although a fair amount of experimental work has been carried out, there is little positive evidence to answer this question. It is generally conceded that the worst effects will be given by pure ring structure compounds, whereas those hydrocarbons containing side chains will burn more akin to aliphatic compounds. What is not known, is the amount of such multiple ring compounds which will actually occur in fuels. To-date, the correlation parameter giving overall least-scatter has been hydrogen. This has been demonstrated above (Table III) and Fig.25 [32] illustrates additional results. These latter indicate some of the difficulty of interpreting the experimental results. Further confirmation is provided by Jackson et al. [34], Fig.26.

Using conventional fuels, it is well known that the introduction of premix-prevaporised fuel gives rise to blue, non-luminous flames with fuels which, when burned as droplet diffusion flames, yield the strong yellow of sooting flames. Such premixing would argue insensitivity to fuel composition. Results reported by Rudey et al. [33] are in good agreement with this supposition (Fig.27).

(c) Radiation Measurements

Experimental investigation of the effects of fuel composition upon flame radiation has not been so widely investigated as that of wall temperature measurements. However, sufficient information has accrued to enable broad conclusions to be drawn.

There is a small increase in peak flame temperatures as the fuel hydrogen content decreases. This gives rise to an increase in radiation as indicated in Fig.28. Between 16% H to 8% H, the theoretical increase due to this cause is about 20%. This increase will be considered negligible for the purpose of this report. Schirmer et al. [35] reported the results illustrated in Fig.29. The effects of both fuel composition and pressure (constant inlet temperature) are very significant. At a pressure of 1.5 MPa and an inlet temperature of 703 K the conditions approximate to those used for the example in the paragraph entitled "Predicted Changes in Wall Temperature due to Changes in Fuels Composition" at 1.5 MPa

(723 K). The different sizes and duties of the combustor make a direct comparison impossible, but if the radiation index is used, the two sets of data may be interpreted as a function of the hydrogen content (Fig.30). The radiation index is arbitrarily defined as the ratio of the radiation (R) at any hydrogen content divided by the radiation emitted by a fuel containing 14% hydrogen (R_{14}). Two sets of theoretical data are plotted: those for the recirculation zone and those for the secondary zone. The secondary zone predictions agree very closely with the experimental results, which latter were measured in the secondary region of the Phillips 2-inch Combustor.

Using a similar combustor, Naegeli and Moses [36] examined the effect of fuel molecular structure on radiation. They used a reference fuel containing 14.22% hydrogen and attempted correlation against (a) smoke point (b) total aromatics (c) total aromatic ring carbon (d) hydrogen content and (e) hydrogen together with polycyclic aromatic ring carbon. The results indicated that, in addition to hydrogen, molecular structure played a significant role in effecting flame radiation. For example a 20% naphthalene content increased the radiation by about 20% over that indicated by the simple hydrogen correlation. The better parameter was the fuel smoke point, which seemed to include structure.

In other work [37] Moses et al. studied the flame radiation within a T-63 combustor. These tests are summarised in Fig.31; for convenience the experimental points have been omitted. For these results, the hydrogen content was found to be the best correlation parameter, the detailed hydrocarbon structure apparently having only secondary effects. They also concluded that the Syncrude fuels (derived both from oil shales and coal) fell within the correlation scatter.

Radiation effects within the J79 combustor were investigated by Gleason et al. [27]. Their results are reproduced in Fig.32. The curves indicate that there is some effect due to fuel structure, especially at the higher pressure conditions. Table IV gives the effects of the hydrogen content in terms of the radiation index. The values have been taken from the best-fit lines of Fig.32. For comparison purposes, the theoretical data, calculated as per the section entitled "PREDICTED CHANGES IN WALL TEMPERATURE DUE TO CHANGES IN FUEL COMPOSITION", are given. The agreement is considered to be satisfactory. Within their report [27], the authors conclude that "neither aromatic type (monocyclic or bicyclic) nor final boiling range produced any direct effect on emissions or combustor performance".

(d) Effects upon Combustor Life

Whether or not the detailed structure is of primary or secondary importance in defining the wall temperature, the observed effects due to hydrogen content indicate the possibility of the serious curtailment of combustor life. All the tests described above were of short duration and it follows that the effects upon combustor life have to be inferred from existing knowledge.

Gleason and Bahr [38] have considered the results available for the J79 and F101 engines [27 and 28] and their possible impact upon combustor life. The J79 results were obtained at nominal true engine conditions but the F101 was operated at scaled conditions, and an attempt was made to correct to the true operating pressure conditions.

For the J79 combustor, life predictions were based upon the measured peak metal temperature rise and the lower crack propagation rate indicated in Fig.33. These curves were established experimentally by subjecting a test specimen, simulating a small portion of the combustor liner, to thermal gradients with alternating heating and cooling cycles.

A more complex approach was used for the F101 predictions, using a two-dimensional finite heat transfer model. Near to air-entry holes corrections were applied for local conduction and convection. The corrected temperature data were then entered into a two-dimensional stress analysis programme, and the resultant stresses were then used with the material low-cycle fatigue properties (Fig.34) to predict cycle life to first crack initiation. Engine test experience on the F101 has shown that the combustor life is proportional to the calculated first-crack life.

On an absolute basis the cyclic life of the F101 combustor is considerably greater than that of J79 combustor.

Based upon the above findings, Gleason and Bahr conclude that improved liner cooling facilities will be required in most current technology combustors to accommodate the expected reductions in hydrogen contents of future fuels.

The predicted effects for both engines are given below

% Fuel H (mass)	Predicted Relative Combustor life	
	J-79	F-101
14.5	1.00	1.00
14.0	0.78	0.72
13.0	0.52	0.52
12.0	0.35	0.47

In [30], Singh et al. demonstrated that for fuels containing 10% of hydrogen or above, the combustor temperature rise (subscale W251) was no more than 27 K over their baseline fuel at 1420 K outlet temperature. They see no reason why this should not be tolerated in conventional (industrial) combustors. For fuels of a lower quality, the maximum temperature rise (outlet 1420 K) was about 140 K and this, they conceded, could shorten the life of the combustor unless design changes were introduced.

Recent developments in film-cooling and/or wall insulation techniques give some confidence in meeting the challenge to wall life. One method is to use the coolant to cool the wall by convection prior to injection as a typical film cooling layer. One such technique utilises jet impingement onto the film cooling wall prior to ejection from the slot. The designer is left with a choice of hole size, hole pitch, gap width and material thickness for design criteria. In particular the axial extent of each impingement array has been found to be significant in determining the overall cooling efficiency. Many of the design criteria can be "borrowed" from turbine blade cooling studies.

The convective channel cooling technique utilises a double wall channel to achieve additional cooling prior to the tangential film cooling air injection. This type of cooling may be augmented by artificially roughening the surfaces being cooled (internally). The topic is further discussed by Nealy [39].

Considerable interest has also been shown in transpiration cooling. Theoretically, an ideal transpiration-cooled combustor would require only about 1/4 to 1/6 the air used by a conventionally cooled system. Unfortunately, manufacturing problems, pore-blockage problems, cost, and fundamental material weaknesses all conspire to make an ideal system impractical. Recently, laminated porous materials have been introduced within gas turbines and they offer quite promising economies in cooling air utilisation. The principle is to employ a labyrinth structure, fabricated from a number of diffusion-bonded photo-etched sheets. One difficulty encountered with this type of device is the increase in normal temperature gradient across the structure. Experimental results [39] suggest that, for the same cooling duty, the use of laminated material reduces the air requirement by about a factor of two. Apart from the problem mentioned above, welds may create difficulties and overheating of joints may occur. To overcome the latter, local cooling may be required.

The use of thermal barriers is increasing, particularly in industrial engines, and this certainly enables the combustor to accept a higher wall temperature. Finally, there is the possibility that ceramic combustors could be introduced. To date, however, the life of such combustors has proven to be completely unacceptable. There seems to be little hope for an immediate answer from this source.

A2.2.4 Effects Upon Gaseous Pollutants

The pollutants considered in this section are carbon monoxide, unburnt hydrocarbons, oxides of nitrogen and oxides of sulfur. Since the partially burned products represent a loss in combustion efficiency, this latter is also considered.

(a) Effects Upon Combustion Efficiency

In the early days of gas turbine combustor technology, although pollutants were not considered as such, combustion efficiency was a factor of prime importance. The efficiency was determined in two ways - by pyrometry (thermo-couples) or by gas analysis (generally a gravimetric technique). Reference [40] considered a range of fuels with hydrogen contents ranging from 9.93% to 14.3% (by mass). The tests indicated that the droplet size was the most important parameter in determining the combustion efficiency. No significant influence of aromatic content, or hydrogen content, was noted. The analysis data for the fuels are given in Table V.

Fig.35 shows the measured combustion efficiencies plotted against the droplet diameter. Some additional points for an 1 1/4" diameter combustor have been added [41]. These latter experiments utilised an aviation kerosine. Whilst there is a marked trend with SMD, the wide scatter clearly demonstrates that other (unidentified) factors also influence the combustion process. These include such items as combustor geometry, operating conditions, type of fuel injectors, etc.

Unfortunately, for most of the other reported data, information on droplet size was not forthcoming. However, in [27], at engine idling conditions, a relation was noted between the CO emission index and the temperature corresponding to the 10% ASTM distillation point (T_{10}). This suggests that a similar relationship might exist between T_{10} and the combustion efficiency. In Fig.36, T_{10} is plotted against the ratio $(1 - \eta)/(1 - \eta)_{JP4}$, Refs. [27, 28, 37, 40 to 45]. There is a trend but, as before, the scatter is very large, and very obviously there may be other significant factors. One possibility is that the scatter may be due to the hydrocarbon fraction, rather than the carbon monoxide. It could be that the inefficiency is related to the higher boiling fractions and on the same diagram (Fig.36) the scatter of the experimental points has been indicated against the 90% recovery temperature. If anything, the scatter is less than that of the 10% but there is little to choose between the two.

Ballal and Lefebvre [46] have considered the effect of the Transfer Number (B) of various fuels upon the combustion efficiency. Their results are indicated in Fig.37. An approximate value of B may be obtained from the mean boiling point of the fuel (50% ASTM recovery temperature) [47].

$$B = \frac{1575 - 0.5 T_{50}}{T_{50} - 118} \quad (33)$$

where B = Transfer Number

T_{50} = ASTM Mid-boiling point temperature (K).

Fig. 37 can be only of very limited application since it implies that a combustor having (say) a combustion efficiency of 90% using kerosine (with a swirl atomiser) would have an efficiency of about 150% if iso-octane was substituted as fuel; or less than 40% if diesel fuel was employed. Hence, it can only be regarded in a qualitative sense and as exhibiting that efficiency depends both upon viscosity and volatility. An alternative possibility is that all values exceeding 100% should be taken as equal to 100%. The fact that according to Equation (33), the transfer number (B) is a function of T_{50} implies the possibility of a correlation between $\eta_{\text{loss}}/\eta^*_{\text{loss}}$ and this T_{50} . Examination of the available data suggest a scatter band similar to those of T_{10} and T_{90} .

The evidence indicates that the combustion efficiency is a function of the droplet diameter and volatility, but generally, if the droplet diameter is held constant, much of the loss in efficiency will be restored. Boiler practice suggests that the above remarks are only relevant to distillate fuels, the very heavy fuels having efficiency characteristics which are very dependent upon volatility. All the above remarks are pertinent to idling and low-power conditions. There is no evidence, to-date, of significant effects at high inlet temperature and pressure conditions.

The combustion efficiencies mentioned above have been variously determined. Sometimes the measurements were made by pyrometry, sometimes by gas analysis. When using the latter, sometimes only carbon monoxide and hydrocarbons were determined as inefficiency components, at other times hydrogen was measured. However, the ratio $\eta_{\text{loss}}/\eta^*_{\text{loss}}$ was always calculated using the same technique for individual values of $\eta_{\text{loss}}/\eta^*_{\text{loss}}$.

(b) Effects Upon Carbon Monoxide

Carbon monoxide may be found in the exhaust either as a product of incomplete combustion or as a product resulting from frozen equilibrium. Thus, it may be a function of the available 'burn-out' time, or it may result by quenching mechanisms at cool surfaces etc.

As with the combustion efficiency, Gleason et al. [28] noted a relation between the idle CO emissions and both SMD and volatility. They also reported similar phenomena in [27]. Their results are given in Figs. 38 and 39. Fig. 40 illustrates the trends given in Ref. [27] for CO versus hydrogen content. If all the above trends are correct, then they should also plot in the form of CO/CO^* versus functions of SMD, volatility or hydrogen content where CO is the measured carbon monoxide for any given fuel, and CO^* is the carbon monoxide yielded by a fuel containing 14.5% hydrogen. Fig. 41 shows the data relevant to the J79 and F101 engines [27 and 28]. It is possible to detect a trend at the Idle and Cruise conditions but at Take-off and Dash no correlation is possible. The effect of fuel volatility (Fig. 42) is even less noticeable. Only at the idle condition is there any sort of trend. Similar remarks apply to the effect of droplet diameter upon the carbon monoxide content (Fig. 43). Only when the change is relatively large are any realistic trends observed, and this lends one to deduce that a simple correlation is unlikely to provide other than qualitative information.

Recently, Mellor [48 to 53] has proposed a semi-empirical technique which would appear to have possible application to assessing alternative fuel effects. The following notes are taken from correspondence with Dr Mellor [54]. They indicate the applications of this particular correlation technique.

Gaseous Emissions

As long as heterogeneous effects were negligible, Mellor [49] found that the variations in heating values from fuel to fuel could be correlated simply by use of a characteristic kinetic time and a characteristic fluid mechanic turbulent mixing time. In the T-63 helicopter combustor, however, effects due to heterogeneous processes appeared to be of importance in the carbon monoxide correlation, and successful inclusion of these effects in the model has not yet been accomplished. For other results on emissions modeling, see Mellor and Washam [50] and references therein.

Efficiency

A similar situation prevails in the case of combustion efficiency. Schmidt and Mellor, [53] were able to obtain a model for decrements in efficiency for alternative fuels, provided that the flame was still mixing controlled. Thus again a kinetic and a fluid mechanic time were involved. However, as discussed by Schmidt and Mellor [53], inclusion of heterogeneous processes through some characteristic time associated with the fuel spray has not yet been accomplished.

For all of the foregoing discussion it would appear that, whilst there is a tendency for the exhaust carbon monoxide to relate to hydrogen content and/or droplet diameter and/or fuel volatility, there is no general prediction technique available which yields sufficient quantitative accuracy. For an individual combustor it may be possible to establish an empirical correlation (e.g. Figs 36 and 37) for certain operating conditions (especially idling).

Possible Solutions to Reduce the Amount of CO

Since at least some of the CO is related to the droplet size, any device which promotes better atomisation will often assist in reducing the problem. In this respect, air-blast atomisers show significant improvement over pressure-jet atomisers, since not only is the droplet size generally reduced, but the intimate mixing of the droplet-vapour-air gives some of the characteristics of premixed combustion. Some idea of the possible effectiveness of using air-blast atomisation may be obtained from Fig.44 [55].

Obviously, the best way to overcome problems of droplet size and volatility is to prevaporise the fuel prior to combustion. The ultimate possibility is the pre-mixed, pre-vaporised combustor. This was proposed as a panacea for all combustion pollutants (sulfur-free fuels) as early as 1970 [56] as also multi-staging. Schematics of both type of combustor are indicated in Fig.45. The difficulties in developing premixed, prevaporised combustors (PPC) are largely associated with the effective vaporisation of the fuel, and mixing it with air prior to combustion in such a manner that fuel deposit effects are minimised and that neither flash-back nor spontaneous ignition occurs. In fact, there is a considerable opinion that the safety risks associated with these difficulties may well preclude their eventual application to aircraft engines. Component testing has very largely verified the performance promise of a PPC and a measure of the pollutant reduction is indicated in Fig.46. This diagram shows not only the potential of the PPC but also the potential of several other possible combustors designed to yield low emissions at idling conditions. The data for all but the premixed systems were taken from [57]. The PPC data have been estimated for this report using the techniques at Laval University [60]. Of the various units, the CF6-50 and JT 9D are current large engines, the CF6-50 Double Annular and JT 9D Vorbix are pseudo-premixed staged combustors, the hot-wall combustor substituted a refractory coating for the cooled wall; the recuperative combustor featured provisions to preheat the air entering the primary zone; the catalytic combustor positioned the catalytic reactor between the secondary and dilution zones. The advantage that the PPC has over the latter three combustors is not only a low pollution potential at the low power conditions but also at all other conditions, including full power. This is particularly true with reference to NO_x (see Paragraph "Oxides of Nitrogen").

(c) Effects Upon Unburned Hydrocarbons

Unburned Hydrocarbons (UBH) present in the exhaust may be due to one or more of several reasons. According to thermodynamic equilibria (with the rare exception of methane) hydrocarbons should not be present at all within the exhaust gases. Thus, it follows that their appearance is (a) due to the probability of poor mixing somewhere within the combustor or (b) the existence of droplets too large to evaporate within the combustion region of the combustor, or (c) quenching or the prevention of evaporation in cool zones (especially near to the walls). It is possible, therefore, that the HC might relate to droplet size and/or fuel volatility and/or fuel hydrogen content. Results taken from [27 and 28] are plotted against these fuel properties in Fig.47. There appears to be little trend, if any, except in the case of the F101 which shows trends both with SMD and volatility. This would suggest that the presence of hydrocarbons in these combustors is much more a function of mixing/quenching than fuel properties. For conditions other than idling there was no possible relation. For many reasons (possibility of poor sampling, line condensation, low concentrations, etc.) hydrocarbons are very difficult to determine with accuracy. Hence, much of the scatter could be experimental. A possible check upon this is to plot the CO emission index against the HC index. This has been done in Figs.48 and 49 [27 and 28]. Although there is a pronounced trend for both combustors, it should be noted that the J79 has a scatter band of HC from 0.5 g/kg to 7 g/kg corresponding to the CO reading of 18 g/kg. The scatter for the F101 data is less, but is still about a factor of two.

It would appear that the correlation/prediction of hydrocarbons in the exhaust is even less tractable than that of CO, and there is but a loose relationship between the hydrocarbon content and fuel volatility and droplet size, unless the latter be comparatively large.

Possible Solutions to Reduce the Amount of HC

Because of the similarity in their modes of formation, those factors which tend to reduce CO formation are also beneficial to the prevention of HC. Air-blast atomisers, two-stage combustion, PPC, all assist, and no further discussion is required here (see Paragraph "Possible Solutions to Reduce the Amount of CO"). The use of a hot-wall combustor is considered to be quite effective.

(d) Oxides of Nitrogen

The oxides of nitrogen produced by combustion fall into three categories – prompt NO, thermal NO_x, and NO_x from fuel bound nitrogen (FBN).

Prompt NO

Experimental measurements on flat flame burners using pre-mixed flames show that, if the NO_x concentration profiles are extrapolated to the initial flame front position, then there remains a certain finite concentration of NO_x. This is particularly true of rich mixture flames. It is generally conceded that this phenomenon may also occur in gas turbines. This residual NO_x consists almost entirely of NO and is termed 'prompt NO'. It was Fenimore [58 and 59] who postulated a mechanism based upon hydrocarbon fragments attacking molecular nitrogen. Possible reactions are -



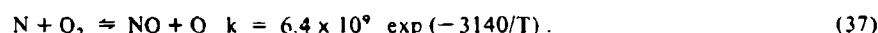
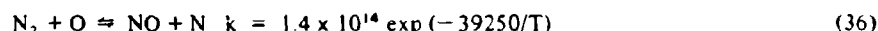
The above equations are (to a limited extent) supported by the observation that (a) rich mixtures given rise to much more NO than weak mixtures and (b) prompt NO does not appear to form in non-hydrocarbon flames such as CO-air, H₂-air. However, the mechanism is not thoroughly understood.

Fortunately for the combustion engineer, the amount of prompt NO generally constitutes only a small part of the total NO_x and the semi-empirical techniques used to predict total NO_x either have the prompt NO 'built-in' or are so inaccurate that it may be neglected. It would seem possible that prompt NO could become a problem if 'rich-lean' combustors are used since, within the rich zone, the amount of prompt NO_x might be expected to be considerably higher than in the existing conventional combustors which generally operate with a slightly lean primary zone. There is no experimental evidence to conclude whether or not the prompt NO is hydrocarbon-type sensitive.

Thermal NO_x

The mechanisms of thermal NO_x are better understood than those of prompt NO, but even here there are some lingering doubts. From purely practical observations, temperature is the major factor (by far) contributing to the formation of NO_x. This is demonstrated in Fig. 50(a) which plots data from a number of combustion systems ranging from well-stirred reactors to aircraft - and industrial-gas turbine combustors [60]. This simple correlation takes the form of the theoretical maximum flame temperature versus the NO_x emissions index (gNO_x/kg fuel). Fig. 50(b) extends the correlation for direct application to gas turbines with inlet conditions which correspond to a compressor polytropic efficiency of 85%.

From the chemical viewpoint, the formation is generally explained in terms of the Zeldovich Mechanism



Obviously the nitrogen and/or oxygen atoms have to come from somewhere. In combustion oxygen atoms may be formed at high temperatures



Many other reactions are possible and it is left to the chemist/engineer to make the best choice to explain the results. Consideration of kinetic data (and experimental results) suggests that pressure also plays a role in NO_x formation and the exponent is of the order 0.4 to 0.7. As with all chemical reactions, another factor is residence time.

Thus, it would seem that any effect of fuel composition upon NO_x formation (nitrogen-free fuel) would result from changes in flame temperature. Based upon [61] Fig. 51 has been produced. If the 'mean' line is used for prediction purposes the error at 8% hydrogen is about $\pm 0.03\%$ (say ± 8 K at 2600 K). This small temperature difference (well within the variation of the fuel calorific value) represents about 10% of the NO_x content at 2600 K.

Figs. 52 and 53 taken from [27 and 28] show the experimental dependence of NO_x upon hydrogen content. If the 14.5% hydrogen is considered as standard then the effects may be plotted as NO_x/NO_x_{14.5}. This has been done in Fig. 54. In addition two prediction curves have been added. The first is based upon the Lipfert data [62] as developed in [60].

$$\log_{10} \text{NO}_{x\text{EI}} = 5.407 \times 10^{-3} T_2 + \frac{\Delta T}{1 + 1.084\theta} - 12.01 \quad (39)$$

and the second is that of Odgers [26] -

$$\text{NO}_{x\text{EI}} = 5.84 \times 10^4 \cdot 10^{-9411/T_f} \cdot p^{0.66} \cdot 1 - \alpha^{-0.257} \quad (40)$$

To obtain the curves of Fig. 54, the NO_x, as determined for the 14.5% H fuel, was used to calculate the temperature ($T_f = T_2 + \Delta T$). Subsequent temperatures for other fuels were then estimated using Fig. 51 and the NO_x prediction made. Neither equation predicts satisfactorily, the one (Eqn. 40) being too low, the other (Eqn. 39) too high. However, the scatter of the experimental data is very large, and about all that one can reasonably deduce is that there is some increase in NO_x as the hydrogen content is reduced and that a mean increase of about 15% could be expected as the hydrogen content is reduced from 14.5% to 12%.

Fuel Bound Nitrogen

If the fuel contains nitrogen compounds, it is a well-established fact that much of the nitrogen can be converted to NO_x during the combustion process. What fraction is converted appears to depend largely upon the total nitrogen content of the fuel. Traces of fuel-bound nitrogen are almost 100% converted, considerably larger quantities (say 0.2%) show about an 80% conversion and at 1% the conversion is about 40%. These figures show considerable variation depending upon the type of compound and the flame conditions.

It is generally assumed that the thermal decomposition of the nitrogen compounds occurs with the formation of low-molecular weight substances such as NO_2 , HCN , CN , NH_3 , N_2H_2 , etc. These compounds then oxidise rapidly in those parts of the flame which are oxygen-rich. This simple concept gives an immediate clue as to how to prevent NO_x from fuel-bound nitrogen. Burn at rich conditions! The kinetic mechanisms are complicated and still not well understood. References [64-67] illustrate typical thoughts on the possible mechanisms.

As already shown, a decrease in the hydrogen content of fuels will result in an increase in carbon formation. Thus control concepts for the two pollutants (NO_x and C) appear to be in conflict. Blazowski et al. [67] find that the optimum equivalence ratio which yields minimum carbon formation and minimum fuel-bound NO_x is closely associated with the first appearance of the total hydrocarbon in the gases. This latter appears to break through in the range $\phi = 1.5$ to $\phi = 1.8$. These values are in some conflict with equilibrium considerations (which suggest $\phi = 2.4$ or richer), and although Blazowski et al. have developed a possible explanation, there is little experimental proof to date.

De Soete's work [64-65] has been based upon premixed flames doped with nitrogen gases (such as ammonia or cyanogen). The work shows the importance of HCN molecules as intermediates in the NO mechanism.

Typical experimental results for conventional combustors are given in Figs. 55-57 [68-71]. They substantiate the above statements. Fig. 56 also indicates that for low fuel nitrogen contents, the degree of conversion increases with the degree of premix, thereby obviating this form of combustion as direct solution for fuel derived NO_x .

Possible Solutions

Prompt NO

There appears to be no published literature on this topic. Generally it is accepted that since the quantities are small, the effect is of little significance, and priority has been given to alleviating other forms of NO_x . Its similarity of formation to fuel NO_x suggests that rich mixture combustion might be an answer. Against this, it is generally stated that prompt NO_x forms more easily in rich mixtures. However, for most fuels the peak formation corresponds to $\phi \approx 1.4$. Richer mixtures produce a sharp fall-off [72]. At $\phi = 1.4$ gasoline yields a maximum of about 80 ppm prompt NO ; at $\phi = 1.8$ this approaches zero. Thus, the solution to prompt NO , if required, would appear to be either very lean combustion or very rich combustion.

Thermal NO_x

Most gas turbines have a reaction time of about 3 ms, and Fig. 58 [73] represents the calculated NO_x according to Equation (40) versus the equilibrium NO_x at the same inlet conditions. At most conditions the amount of NO_x formed is well below equilibrium. Fig. 58 may be used to estimate NO_x for both premixed flames and for conventional liquid-fuel fired gas turbines. In the latter case, the temperature used is that corresponding to the stoichiometric dissociated flame temperature at the appropriate conditions. If steam or water is added, the NO_x may be estimated by calculating the new maximum flame temperature (again assuming $\phi = 1.0$). Use of this curve immediately suggests that a premixed-prevaporised combustor is a possible solution to the NO_x problem.

Reference to Fig. 58 shows that with a reaction (primary zone) time of 3 ms and an engine compression ratio of about 10/1, current NO_x limitations for industrial engines would be met with a combustion system operating with a reaction zone equivalence ratio of about $\phi = 0.7$. Much lower NO_x levels can be accomplished if the operating primary zone equivalence ratio is reduced to $\phi = 0.5$.

There are many papers published on this topic, but perhaps one of the most useful from the viewpoint of performance estimation is that of Anderson [74]. These studies show that very low NO_x emissions are achievable by premixed, prevaporised systems operating at weak mixture conditions. For residence times of 1.5 ms or above, the carbon monoxide content of the exhaust corresponds closely to that of the equilibrium composition. At all the experimental conditions investigated, the overall combustion efficiency was in excess of 99%. Unburnt hydrocarbons were less than $\text{E.I.} = 1.0$ at all but one test point. Recent work at Laval University [75] ($T_1 = 300 \text{ K}$, $P = 0.1 \text{ MPa}$) not yet published, shows sensible agreement with the CO findings (except that a longer residence time is required due to the low inlet temperature), but the HC content was significantly higher. These results, plus a developed analytical model, have been produced for a PhD thesis. Further discussion of the advantages and disadvantages of premixed prevaporised combustors may be found in [75-78].

(c) Combustors Designed for Pollutant Reduction

Hot-Wall Liner Combustor [79]

Much of the CO and HC found in combustor exhaust has been attributed to the quenching effects of the wall film cooling. In the hot-wall concept, film cooling is abolished by the application of a refractory coating to the liner. If this is combined with some form of 'droplet' premix, the resultant NO_x should be reduced at idling. In fact, the NO_x was about the same as for the conventional combustors (see Fig. 46). It would appear that this type of system would have to be combined with a premixed prevaporised combustor to cater for all conditions.

Two-Stage Combustion ('clean' fuel)

This type of combustor is fuel staged. The first stage is optimised for low engine power and is operated alone at idling conditions. At high engine power settings the second stage fuel is added. Because it is fed with the products of the first stage, the subsequent vitiation lowers the peak temperature, thereby limiting the NO_x content. During the simultaneous operation of both stages, the first stage is scheduled to burn rich, thereby minimising thermal NO_x. The system is of little use for fuels containing nitrogen compounds. Various attempts have been made to develop these systems, but to-date the NO_x reduction has never exceeded about 50% and is generally of the order of 10% to 30% less than the equivalent conventional system.

Typical examples of this type or system are those of Lefebvre et al. [80] and Sjöblom et al. [81].

Exhaust Recirculation

This utilises the same principle as water injection [82], namely a reduction in peak reaction temperature. Because of the lower specific heat of the exhaust gases which are recycled, it is not so effective as water/steam, but it does offer a means of NO_x reduction in regions where water is not readily available.

Air-Blast Atomisation

From time to time it has been claimed that the application of air-assist/air-blast atomisers can produce some reduction of NO_x at some conditions. Typical data are cited by Lefebvre et al. [83]. The results with fuel bound nitrogen are dubious. If there are any gains with either of these systems they do not seem to be significant enough to be a major factor in NO_x reduction.

Fuel-Bound NO_x

It would appear to be unlikely that fuel-bound NO_x will present a serious challenge to most aircraft combustors since the requirement of a thermally stable fuel will necessitate a low nitrogen content fuel. However, if for any reason, fuels containing 'bound' nitrogen have to be used some form of two-stage combustor could well be required, especially for civil aircraft. There could also be a requirement for a catalytic stage. Considerations of such combustors may be found in [83-91].

(f) Sulfur Oxides [90]

Sulfur compounds are not anticipated to present any serious problems for future aircraft fuels, but the introduction of heavy fuels for use in industrial gas turbines may give rise to some problems. In heavy oils the sulfur content may be 2% or more, and it has been well established that at least 80% to 90% of this is converted to SO₂ or SO₃ during the process of combustion. There seem to be no means of preventing this oxidation under conditions of normal, lean mixture operation, and either the sulfur will have to be removed during the refining process, or else it will have to be removed by treatment of the exhaust gases using an absorption technique. The former procedure, although expensive, is to be preferred, since the second method is equally expensive and perhaps not so efficient. In addition, if the sulfur is left in the fuel, should there be a rich mixture region within the combustor, some of the sulfur may be converted to hydrogen sulfide. This could cause sulfidisation, a phenomenon which plagued some of the earlier gas turbine and shortened the life of the turbine hardware. Sulfidisation is most prevalent when sodium is present, either as an ash component or in the atmosphere (as would be the case with a plant situated near to the sea).

A2.2.5 Nomenclature

A	cross-sectional area	m ²
A/F	air/fuel ratio (by mass)	
B	transfer number	
C	heat transferred by convection	W m ⁻²
C*	carbon formed by any fuel	
	Carbon formed by the 'standard' fuel (H = 12.5%)	
C _A	aromaticity = $\frac{100 \times \text{Aromatic carbon atoms}}{\text{Aromatic carbon atoms} + \text{non-aromatic carbon atoms}}$	
CO	carbon monoxide	g CO/kg fuel
CO*	carbon monoxide formed from a 'standard' fuel (generally H = 14.5%)	g CO/kg fuel
c/h	carbon-hydrogen mass ratio of the fuel	
d	a diameter	m
d*	hydraulic mean diameter	m
D*	a constant	
E.I.	emissions index	g pollutant/kg fuel
f	fuel-air mass ratio	

H	% hydrogen in a fuel, (mass)	
HC	Hydrocarbon present in an exhaust	gH ₂ O/kg fuel
HC*	Hydrocarbons present in an exhaust formed from a 'standard' fuel, generally H = 14.5%	gH ₂ O/kg fuel
H/C	Hydrogen-Carbon atomic ratio	
k	a constant	
K	heat transferred by conduction	W m ⁻²
ℓ	effective radiation beam length	m
L	flame luminosity	
m	mass flow	kg s ⁻¹
n	reaction order = 2ϕ for $\phi \leq 1$ = $2/\phi$ for $\phi > 1$	
N	molar air mass flow	mol s ⁻¹
NOx	mixed oxides of nitrogen	
NOx _{EI}	NOx emission index	gNOx/kg fuel
O/C	oxygen/carbon atomic ratio	
P	pressure	Pa
P ₃	air inlet pressure	Pa
R	heat transferred by radiation	W m ⁻²
R _F /R _{FO}	ratio of heat transferred by radiation for any given fuel to the heat transferred by radiation for a baseline fuel (14.5 percent hydrogen) at the same operating conditions	
SMD	Sauter Mean Diameter	μ
s	Slot height	m
t	thickness	m
T	temperature	K
T _L	combustor temperature for any fuel	K
T _{LO}	combustor temperature for a reference fuel (usually H = 14.5%)	K
T ₂	combustor inlet temperature	K
T ₃	air inlet temperature	K
T ₄	combustor outlet temperature	K
T ₁₀	temperature corresponding to 10% distilled (ASTM)	K
T ₅₀	temperature corresponding to 50% distilled (ASTM)	K
T ₉₀	temperature corresponding to 90% distilled (ASTM)	K
T _f	flame temperature	K
T _{max}	maximum temperature of a flame -- with dissociation ($\phi = 1.0$)	K
T _{max} *	maximum flame temperature caused by a reference fuel -- generally H = 14.5%	K
[T _x]	$\frac{T_L - T_{LO}}{T_{LO} - T_3}$	
u	velocity	m s ⁻¹
V	volume	m ³
X	film cooling correlation parameter	m ^{-0.1}
x	distance downstream from cooling film injection point	m
y	ϕ for $\phi \leq 1$ 1 for $\phi > 1$	
β	humidity	gH ₂ O/g air
ΔT	theoretical temperature rise	K
ε	emissivity	
η	combustion efficiency	
η _{loss}	combustion loss = $1 - \eta$	
η _{loss} *	combustion loss of a 'standard' fuel, generally H = 14.5%	
λ	thermal conductivity	W m ⁻¹ K ⁻¹
μ	dynamic viscosity	Pa s
ρ	density	kg m ⁻³
σ	Stefan-Boltzmann constant (5.69×10^{-8})	W m ⁻² K ⁻⁴
τ	time	s

ϕ	equivalence ratio	
ψ	fuel loading ($m_f/V p^n$)	$kg s^{-1} m^{-3} Pa^{-n}$
Π	engine compression ratio	

Subscripts

1	inside wall surface	w	wall
2	outside wall surface	DZ	dilution zone
a	air	PZ	primary zone less recirculation zone
a	outer annulus air-outer annulus	RZ	recirculation zone
c	film cooling air-film cooling slot	SZ	secondary zone
g	hot gas	OV	overall

A2.2.6 References

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Additional Reading

Further references may be found in Report No. j. o. 98 *Alternative Fuels - A Bibliography*, Universite Laval, Dep. de Genie mecanique, Laboratoire de Combustion, February 1980.

TABLE I
Effect of Fuel Composition Upon Flame Radiation

%H	Zone		Conditions					
			Idle	$\Pi = 5$	$\Pi = 10$	$\Pi = 15$	$\Pi = 20$	$\Pi = 30$
16	P R I M A R Y	R_F/R_{FO}	0.63	0.72	0.79	0.83	0.85	0.90
14		R_F/R_{FO}	1.06	1.06	1.05	1.05	1.04	1.03
12		R_F/R_{FO}	1.69	1.38	1.27	1.18	1.12	1.06
11		R_F/R_{FO}	2.01	1.47	1.31	1.20	1.12	1.06
8		R_F/R_{FO}	2.78	1.54	1.33	1.20	1.13	1.06
16	S E C O N D A R Y	R_F/R_{FO}	0.65	0.67	0.69	0.71	0.74	0.77
14		R_F/R_{FO}	1.15	1.15	1.15	1.16	1.17	1.16
12		R_F/R_{FO}	2.15	1.99	1.88	1.79	1.73	1.61
11		R_F/R_{FO}	2.86	2.51	2.26	2.09	1.98	1.77
8		R_F/R_{FO}	7.74	4.67	3.41	2.77	2.41	1.97

TABLE II
Calculated Values of $[T_x]$, From Ref. [29]
 $T_3 = 590\text{ K}$

T/C location	Dome		# 1		# 2		# 3		# 4		# 5		# 6	
	$T_w\text{ K}$	$[T_x]$	$T_w\text{ K}$	$[T_x]$	$T_w\text{ K}$	$[T_x]$	$T_w\text{ K}$	$[T_x]$	$T_w\text{ K}$	$[T_x]$	$T_w\text{ K}$	$[T_x]$	$T_w\text{ K}$	$[T_x]$
900	Fuel*													
	A	825	-	870	-	870	-	860	-	790	650	-	770	-
	B	830	0.02	875	0.02	875	0.02	805	0.02	795	650	0.00	770	0.00
	C	970	0.62	1050	0.04	1040	0.61	960	0.37	850	680	0.50	800	0.17
1150	D	850	0.11	890	0.07	900	0.11	875	0.06	795	655	0.08	775	0.03
	A	960	-	995	-	1030	-	1070	-	1025	920	-	1000	-
	B	965	0.01	1000	0.01	1035	0.01	1075	0.01	1030	925	0.02	1000	0.00
	C	1135	0.47	1195	0.49	1225	0.44	1240	0.35	1160	1030	0.33	1060	0.15
1400	D	990	0.08	1015	0.05	1070	0.09	1090	0.04	1035	950	0.09	1000	0.00
	A	1135	-	1170	-	1195	-	1310	-	1270	1170	-	1320	-
	B	1140	0.01	1175	0.01	1200	0.01	1315	0.01	1275	1175	0.01	1320	-
	C	1340	0.38	1375	0.35	1415	0.36	1520	0.29	1151	1390	0.38	1375	0.08
1400	D	1170	0.06	1190	0.03	1250	0.09	1325	0.03	1300	1220	0.09	1325	0.01

* Fuels (A) DF II (B) II-Coal (C) SRC II Blend (D) Paraho Shale

TABLE III

Influence of Fuel Hydrogen and Aromaticity on $[T_x]$ (After [30])

Burner Outlet Temperature K	Influence of Fuel Hydrogen*	Influence of Fuel Aromaticity
839 (1050°F)	$[T_x] = -0.129 (\% H) + 1.67$ ($\sigma = 0.19$)	$[T_x] = 0.012 (\% C_A) - 0.22$ ($\sigma = 0.25$)
1172 (1650°F)	$[T_x] = -0.092 (\% H) + 1.19$ ($\sigma = 0.13$)	$[T_x] = 0.0087 (\% C_A) - 0.16$ ($\sigma = 0.14$)
1311 (1900°F)	$[T_x] = -0.079 (\% H) + 1.02$ ($\sigma = 0.12$)	$[T_x] = 0.0074 (\% C_A) - 0.14$ ($\sigma = 0.14$)
1422 (2100°F)	$[T_x] = -0.078 (\% H) + 1.00$ ($\sigma = 0.08$)	$[T_x] = 0.0074 (\% C_A) - 0.14$ ($\sigma = 0.08$)

* $H_{Ref} = 12.9\%$ $TC_{ARef} = 18.4\%$

TABLE IV

Effect of Fuel Hydrogen Content on Flame Radiation Index, J79 Engine (After [27])

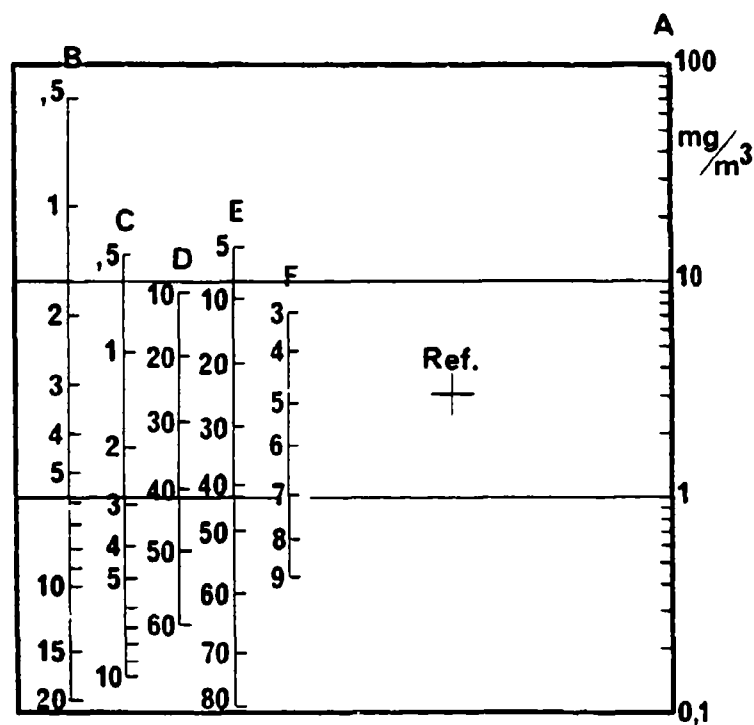
Experimental [13]					Theory	
	Idle	Cruise	Take-off	Dash	--	--
T_3 K	421	559	664	781	480	723
P_3 MPa	0.25	0.47	1.36	1.59	0.50	1.50
R_{12}/R_{14}^*	1.52	1.72	1.64	1.68	1.73† 1.72 1.71	1.54† 1.51 1.47
R_{13}/R_{14}^*	1.26	1.39	1.33	1.37	—	—

* Radiation Index = $\frac{\text{Radiation at } x \% H}{\text{Radiation at } 14\% H}$

† Depends upon position in combustor secondary zone.

TABLE V
Fuel Properties – High Density Fuels [40]

Fuel (RDE/F/KER)			'210	'212	'213	'214	'215
Density kg m ⁻³			0.769	0.898	0.861	0.894	0.965
Flash Point (P.M. Closed) K			254	349	357	378	339
Bromine No.			1.7	8.3	2.4	2.1	6.2
Aniline Point	K		330	303	321	328	–
Cloud Point	K		203	< 233	–	–	249 (FP)
Pour Point	K		195	214	204	206	244
Aromatic Content % Vol.			20.0	53.4	28.3	24.5	87.0
Distillation (I.P.)	In. B.P.	K	330	491	472	497	515
	10%	K	370	502	450	510	518
	50%	K	487	535	506	549	528
	90%	K	490	560	550	588	544
	Fi. B.P.	K	515	568	567	602	548
Ultimate Analysis	C % by mass		85.46	88.60	86.70	87.47	86.64
	H % by mass		14.29	11.17	12.93	12.30	9.93
	S % by mass		0.25	0.23	0.37	0.23	3.43
Kinematic Viscosity Centistokes	@ 298 K		1.01	3.87	2.68	6.77	3.87
	@ 311 K		–	2.88	2.10	4.68	2.38
	@ 323 K		–	2.27	1.71	3.48	2.27
Surface Tension dynes/cm	@ 298 K		23.0	31.2	29.5	31.7	34.6
	@ 311 K		22.2	29.9	27.9	30.4	33.0
	@ 323 K		21.5	28.8	26.5	29.1	31.6
Calorific Value Nett.	kJ/kg		43173	43285	43075	42938	42826
Smoke Point	mm		26	7	13	11	5



- A. Particulate Smoke mg/m^3 ($\mu\text{g/litre}$)
 B. Rolls Royce Photo Smoke Units
 C. Hartridge Smoke Units
 D. Smoke Number, S.N.
 E. SAE Smoke Number
 F. Bacharach Units
 Ref. Reference Point

Fig.1 The approximate relationship between gravimetric exhaust carbon and several instrument techniques

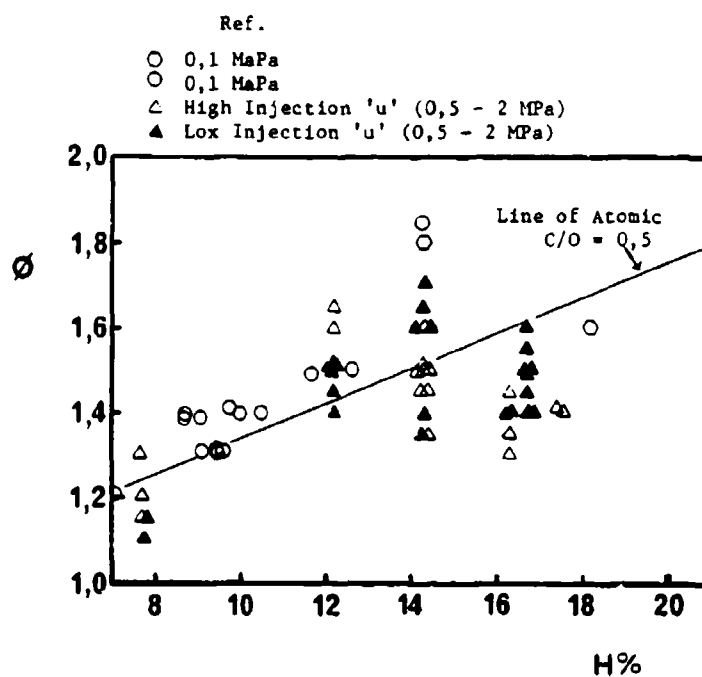


Fig.2 Incipient soot limits

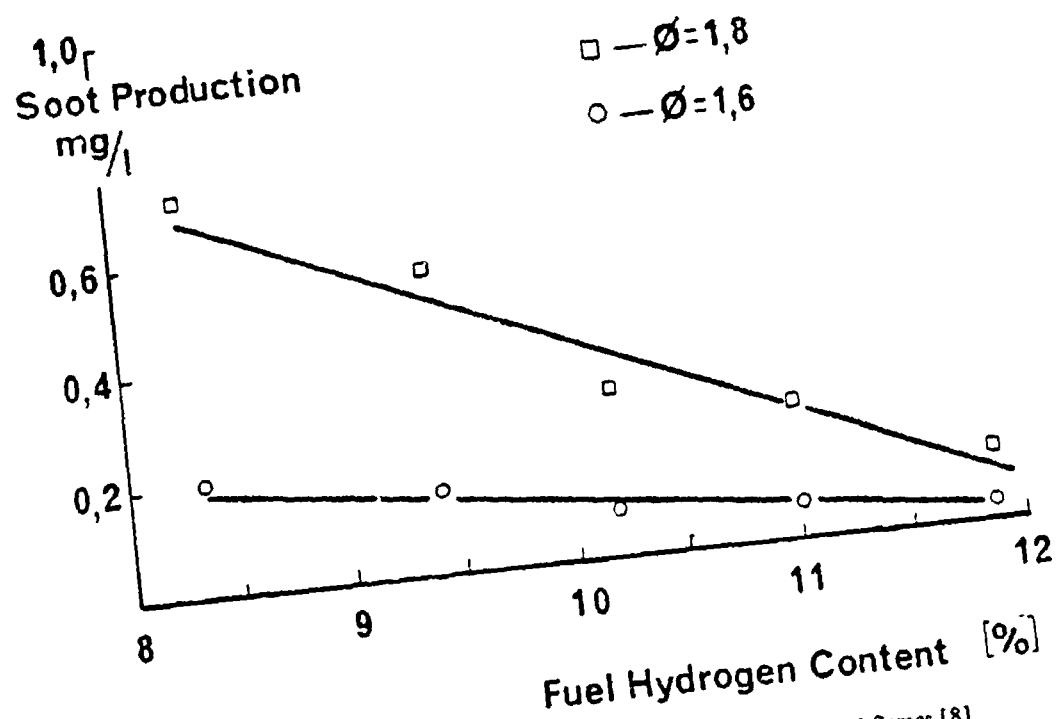


Fig.5 Effect of hydrogen content on soot production in premixed flames [8]

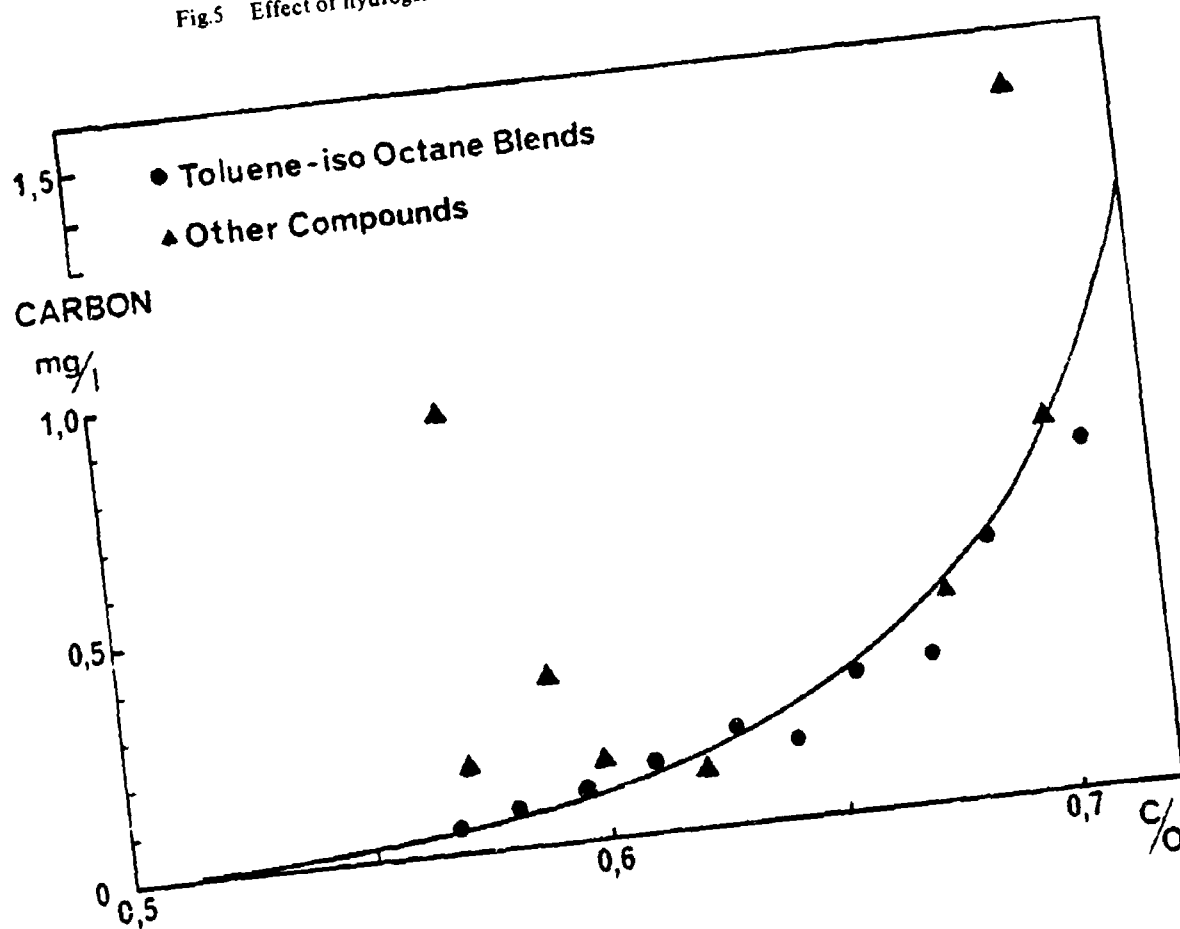


Fig.6 Effect of C/O upon carbon formation data from [6] in premixed flames

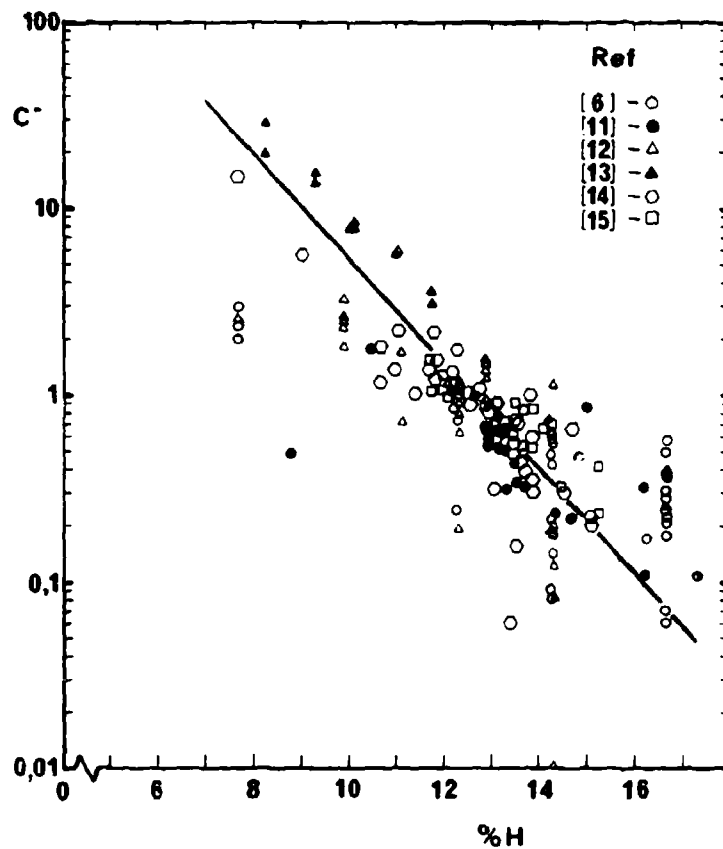


Fig.7 Effect of hydrogen content on carbon formation in various combustors

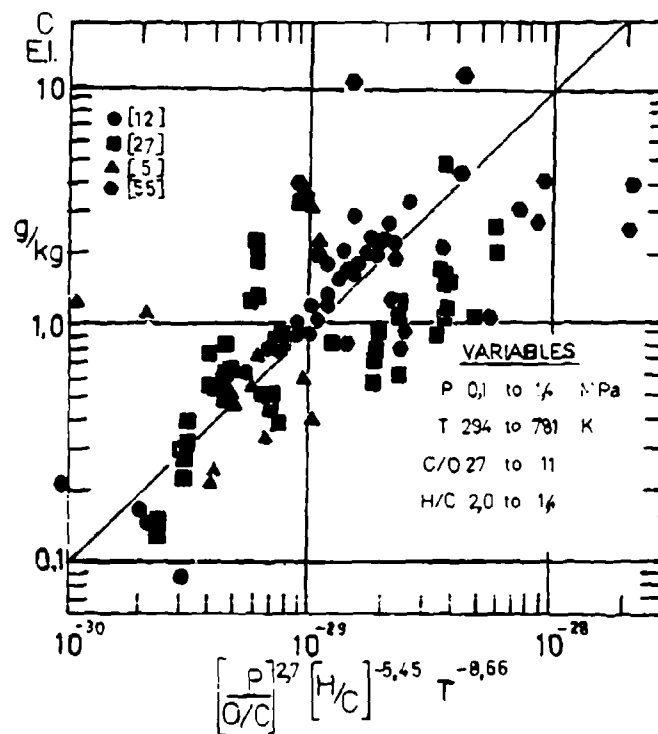
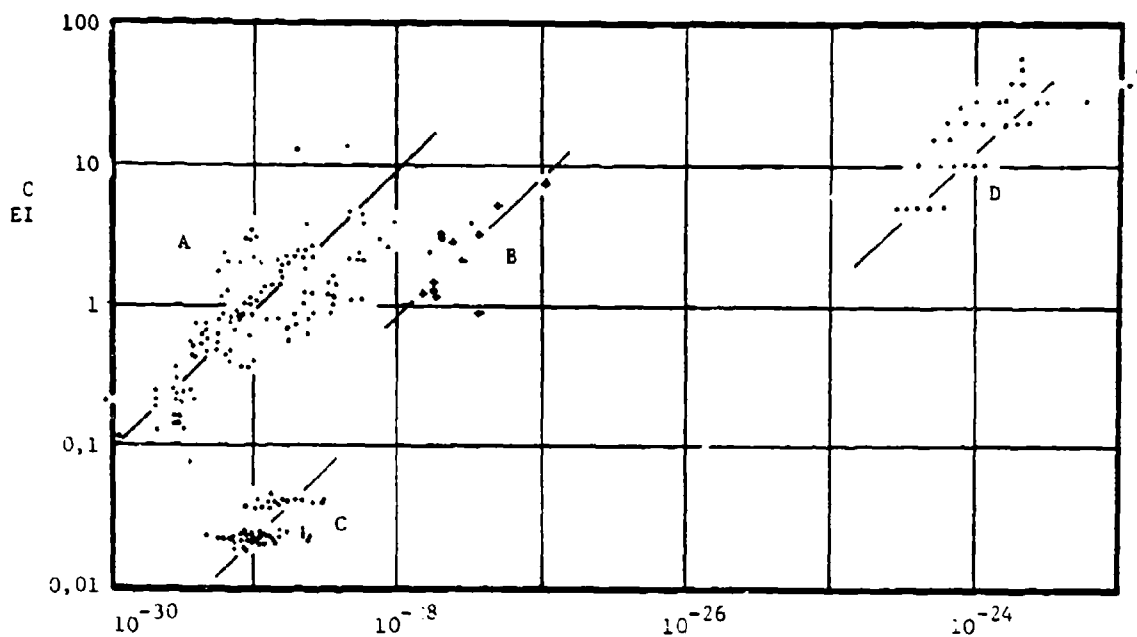


Fig.8 Carbon correlation 'Conventional' combustors



$$\left(\frac{p}{O/C}\right)^{2.7} \left(\frac{H}{C}\right)^{-5.45} T_3^{-8.66}$$

- A Conventional Combustors
- B Semi-premix Combustor
- C Stirred Reactor
- D Premixed Reactor

Fig.9 Correlation for carbon-emission index

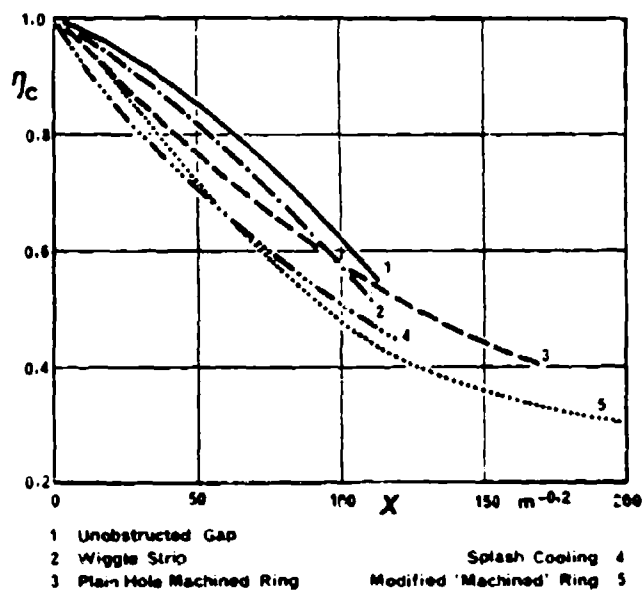


Fig.10 Film-cooling correlation

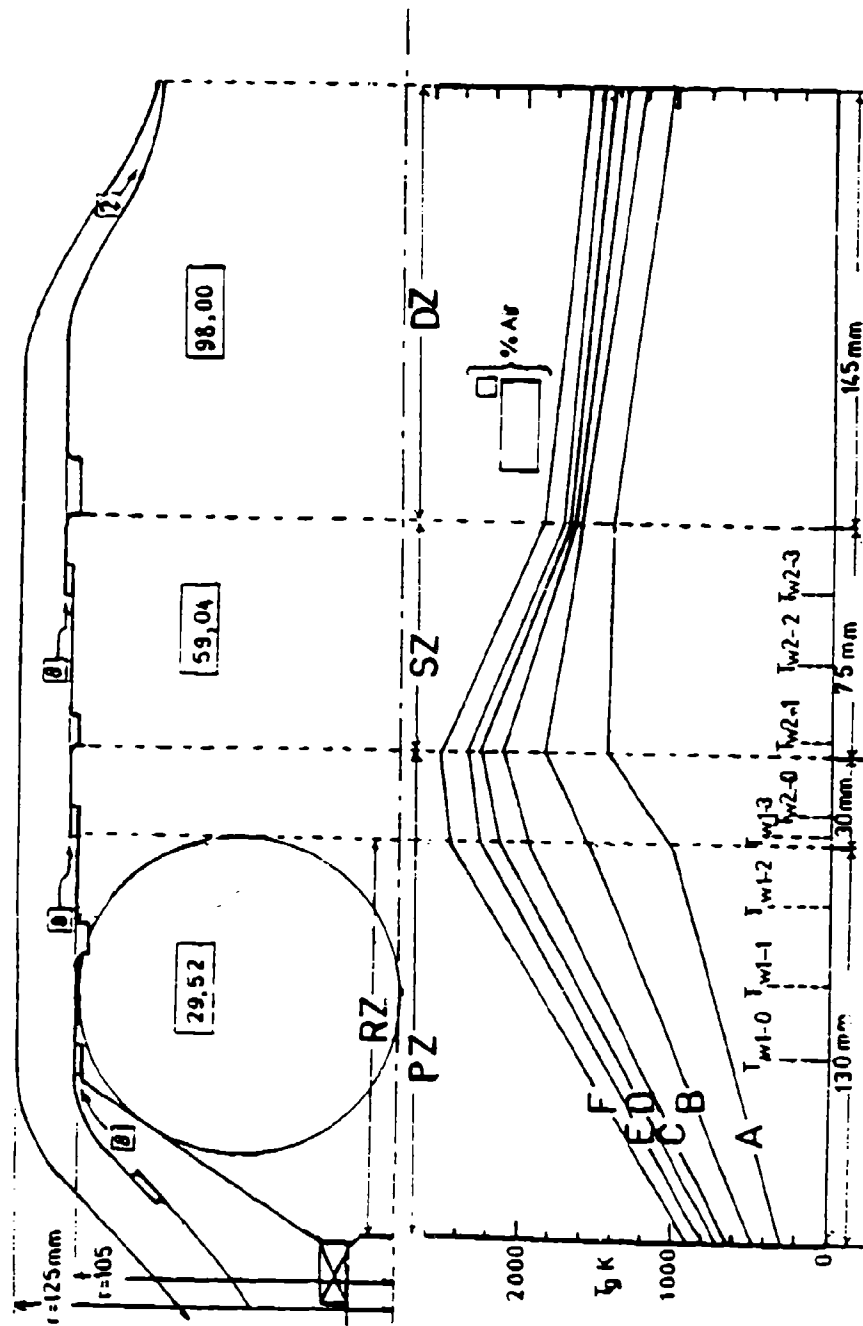


Fig.11 Hypothetical combustor used for theoretical calculations

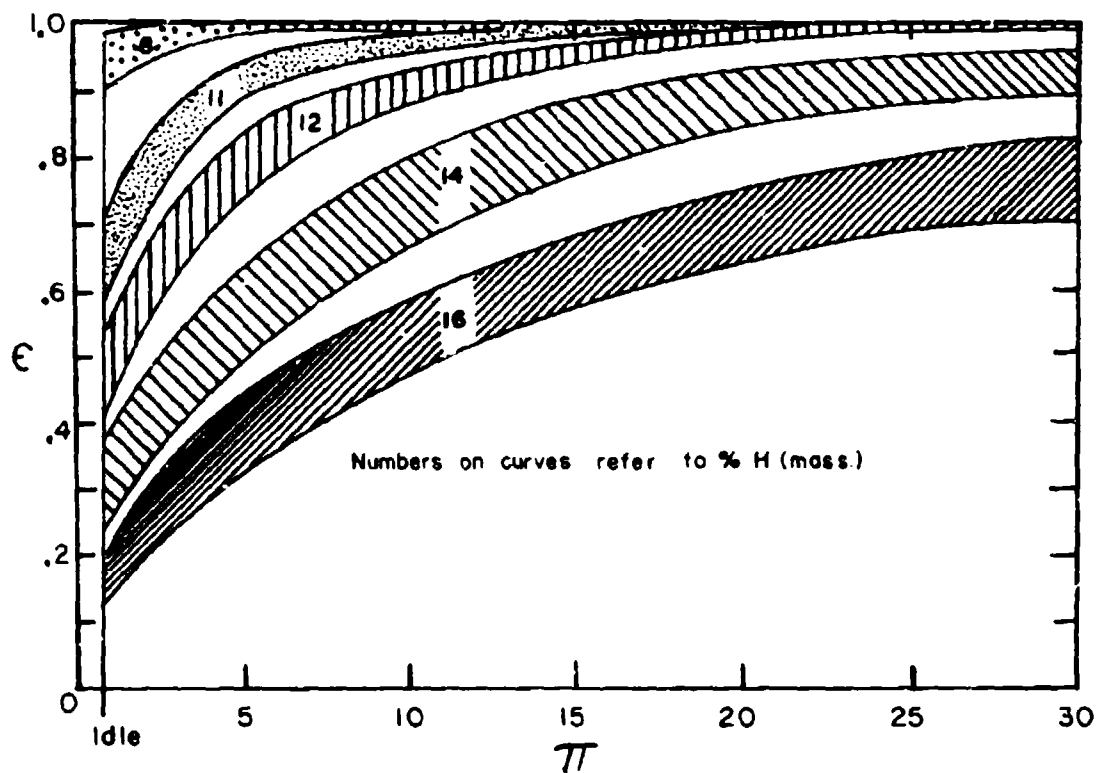


Fig.12 Predicted changes in flame emissivity (ϵ) in the recirculation zone due to operating conditions and fuel composition

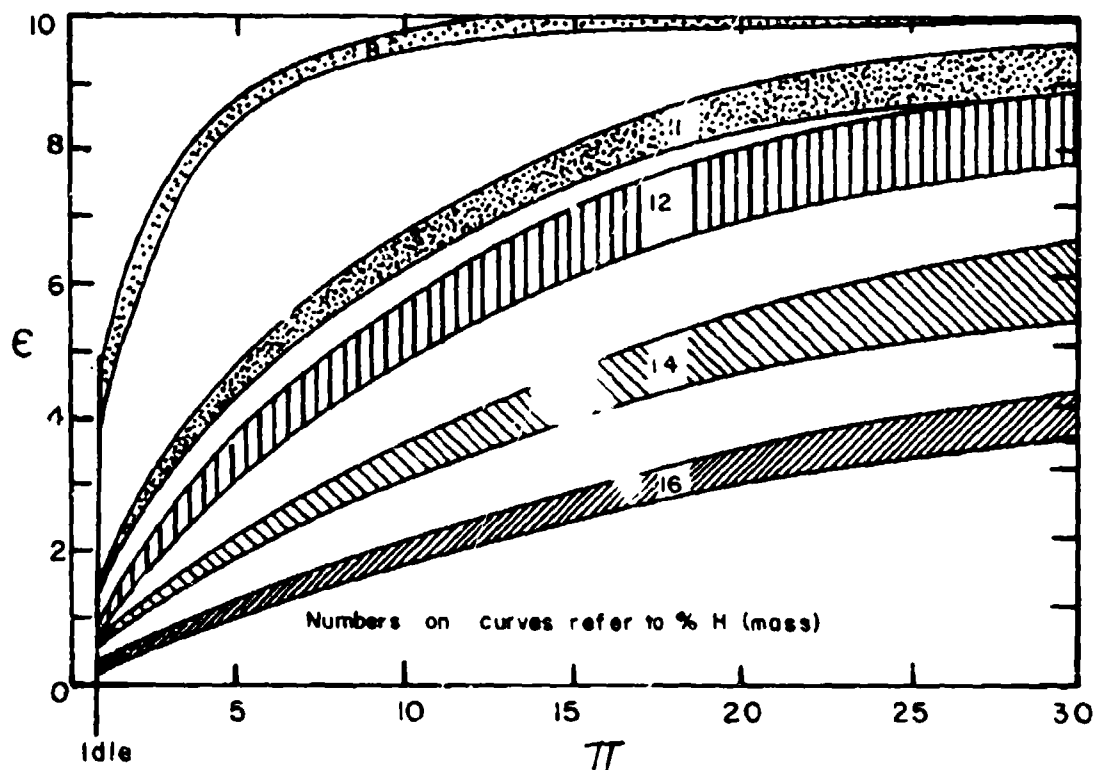


Fig.13 Predicted changes in flame emissivity (ϵ) in the secondary zone due to operating conditions and fuel composition

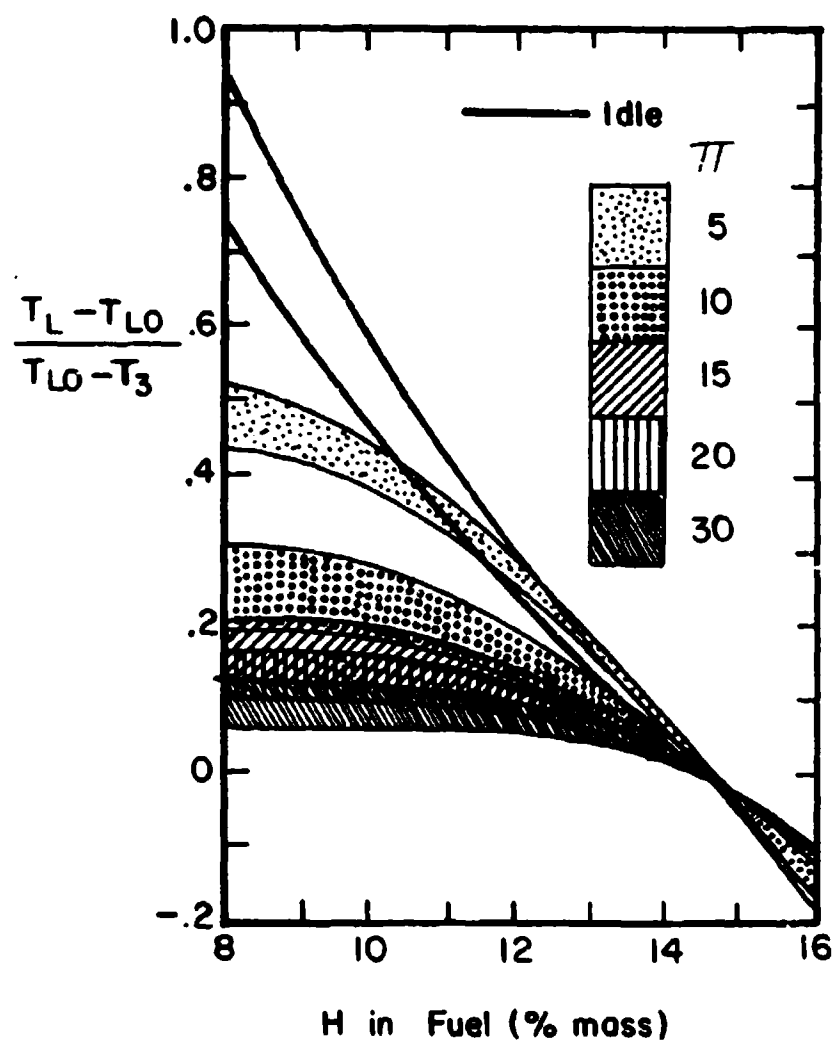


Fig.14 Predicted changes in combustor wall temperatures in the recirculation zone due to operating conditions and fuel composition

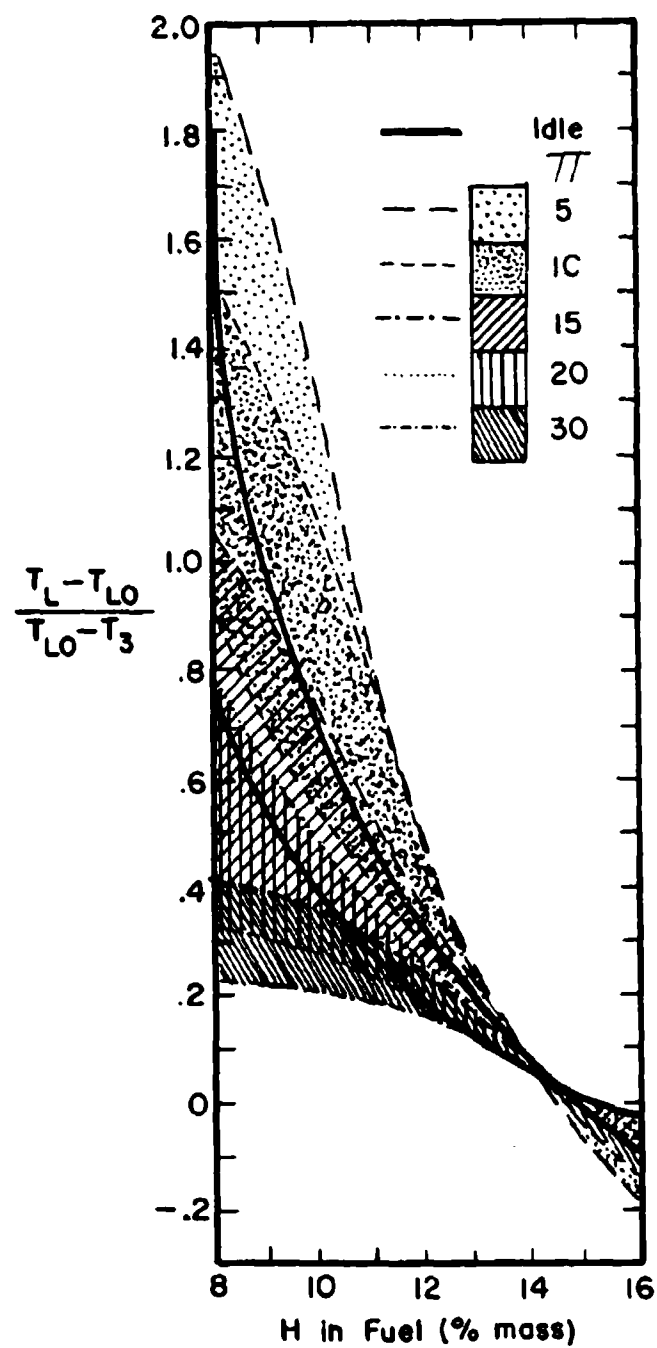


Fig.15 Predicted changes in combustor wall temperatures in the secondary zone due to operating conditions and fuel composition

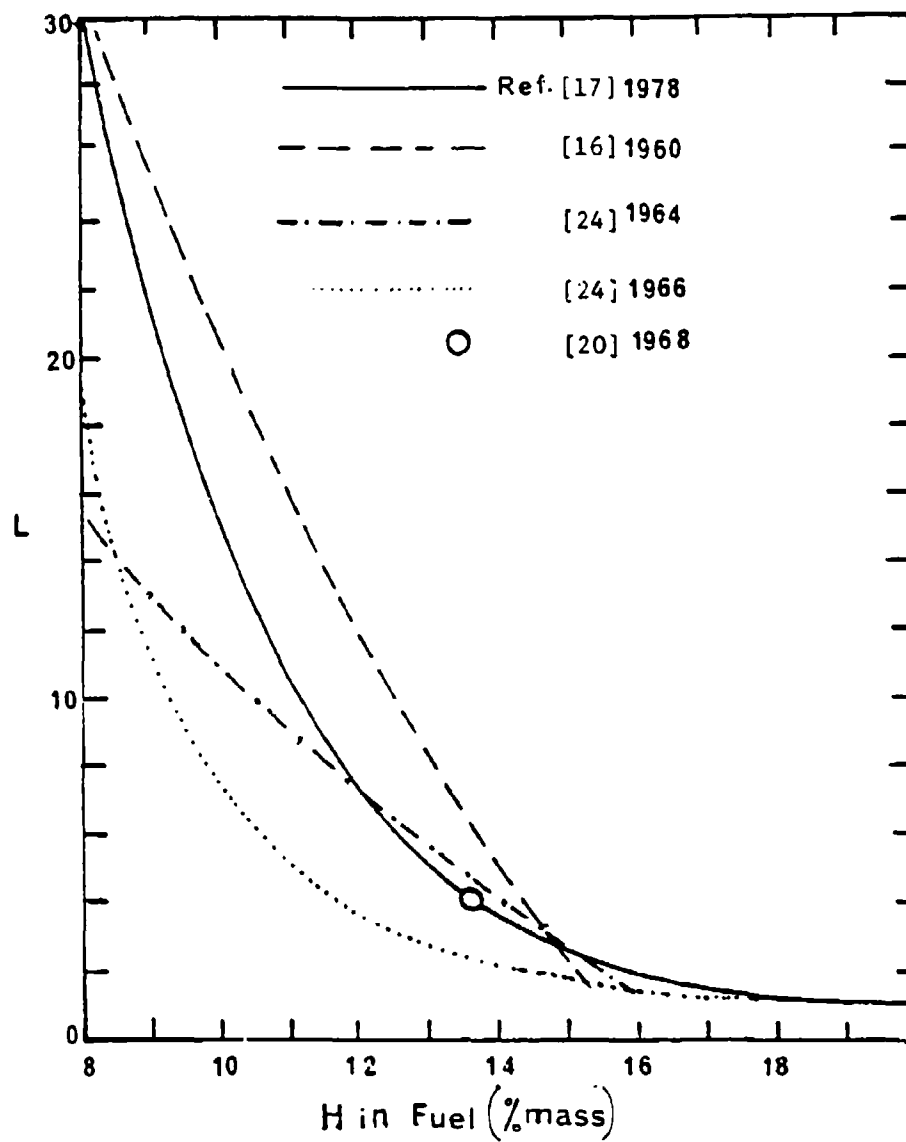


Fig.16 Dependence of luminosity upon fuel composition

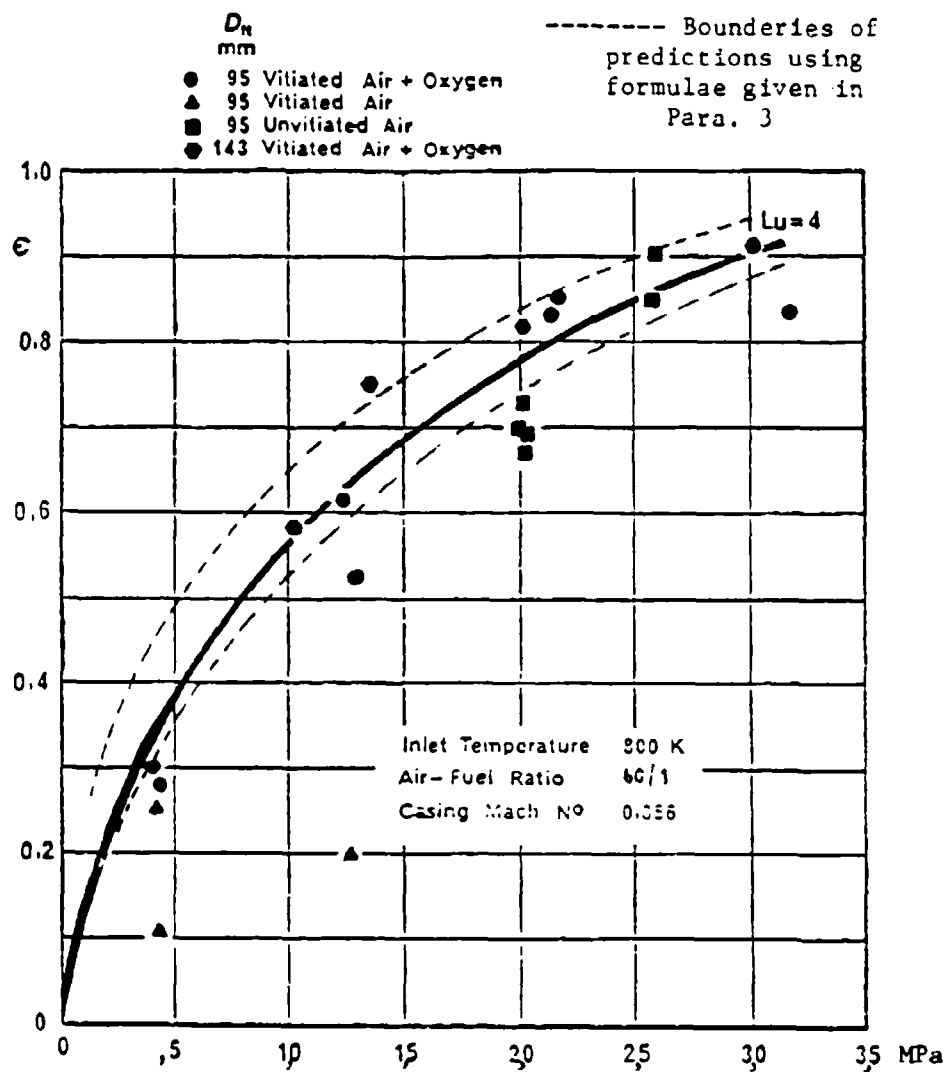


Fig.17 Variation of primary zone flame emissivity with pressure [20]

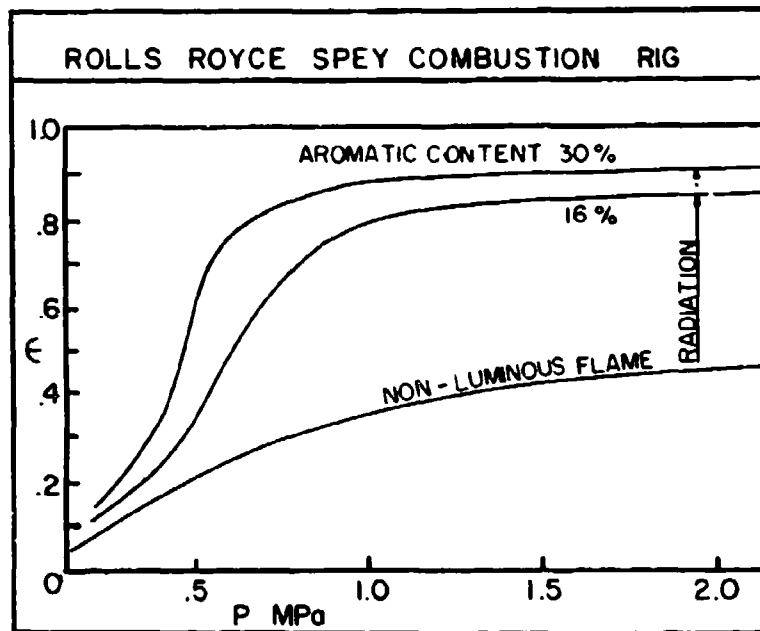


Fig.18 Variation of flame emissivity with fuel composition and pressure [26]

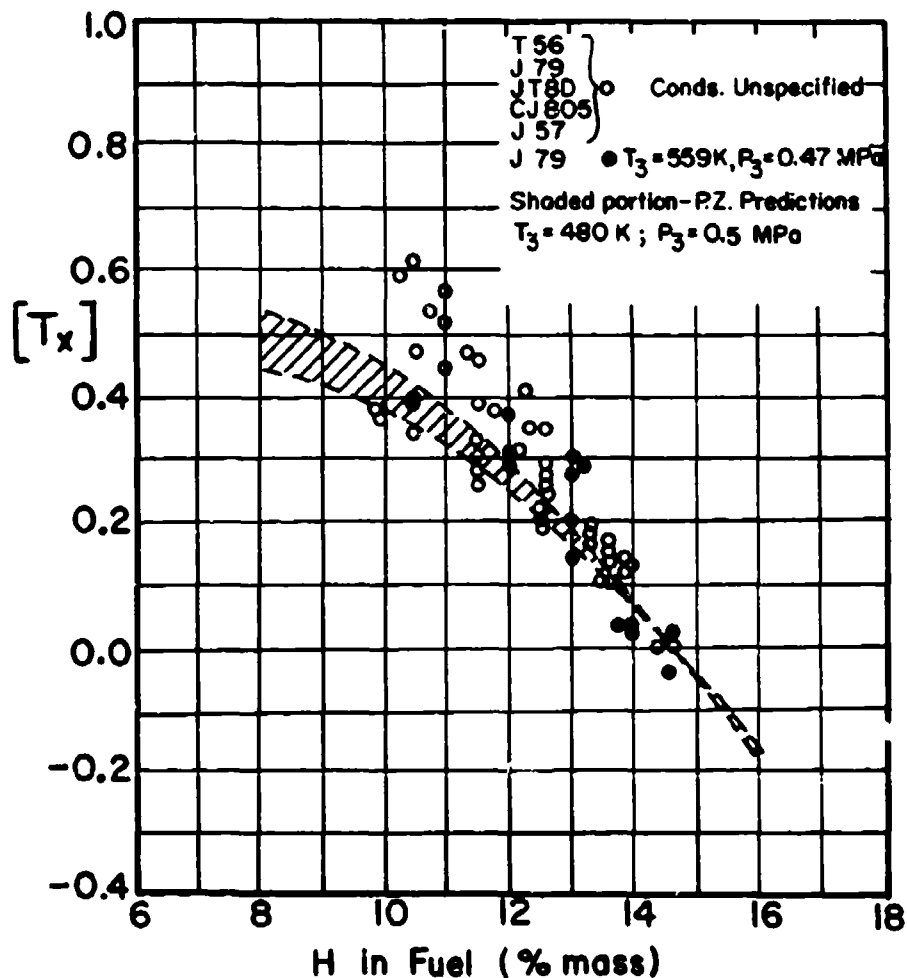


Fig.19 Effect of fuel composition upon combustor wall temperature [27]

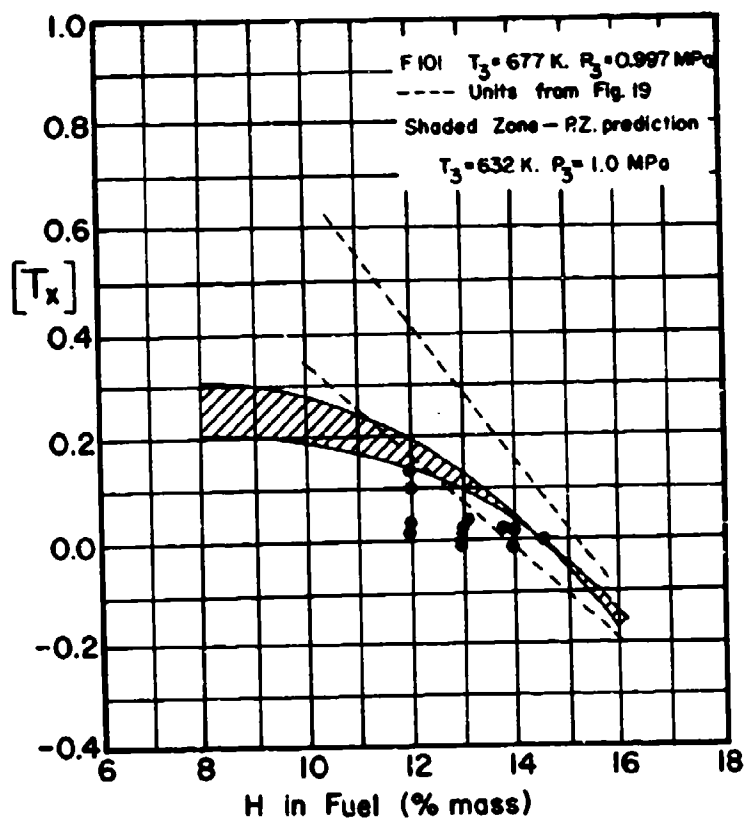


Fig.20 Effect of fuel composition upon combustor wall temperatures [28]

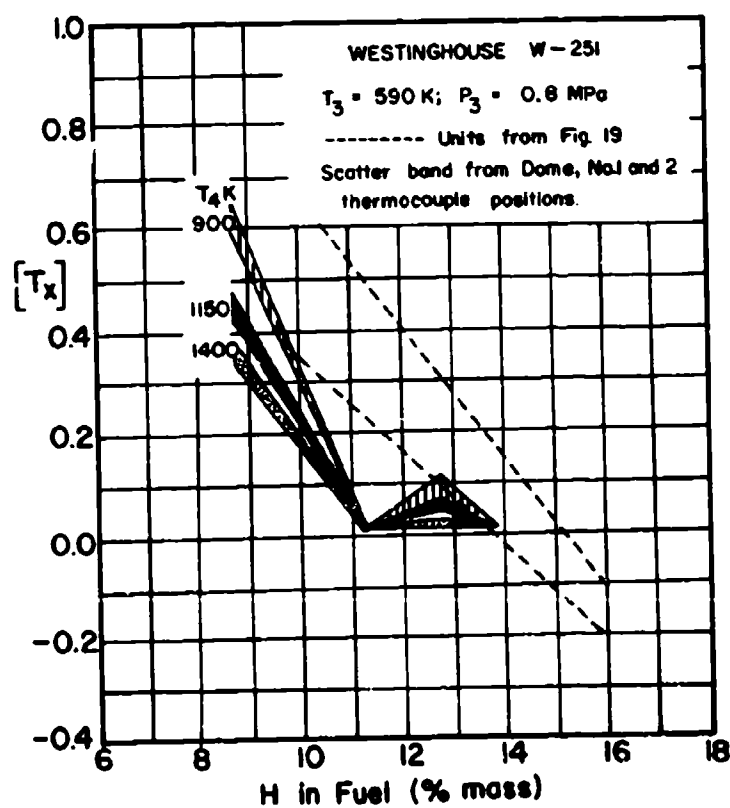


Fig.21 Effect of fuel composition upon combustor wall temperatures [29]

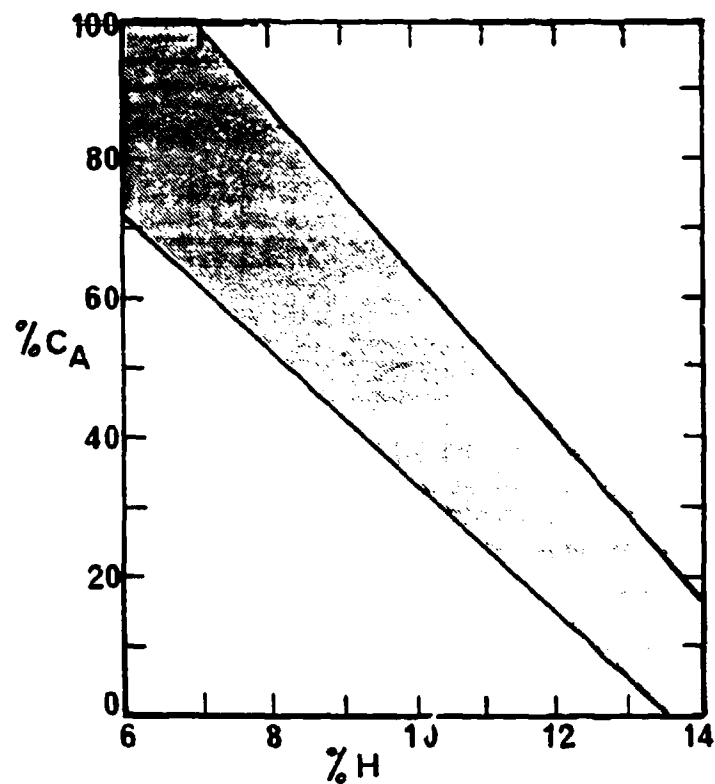


Fig. 22 Dependence of aromaticity upon fuel composition [31]

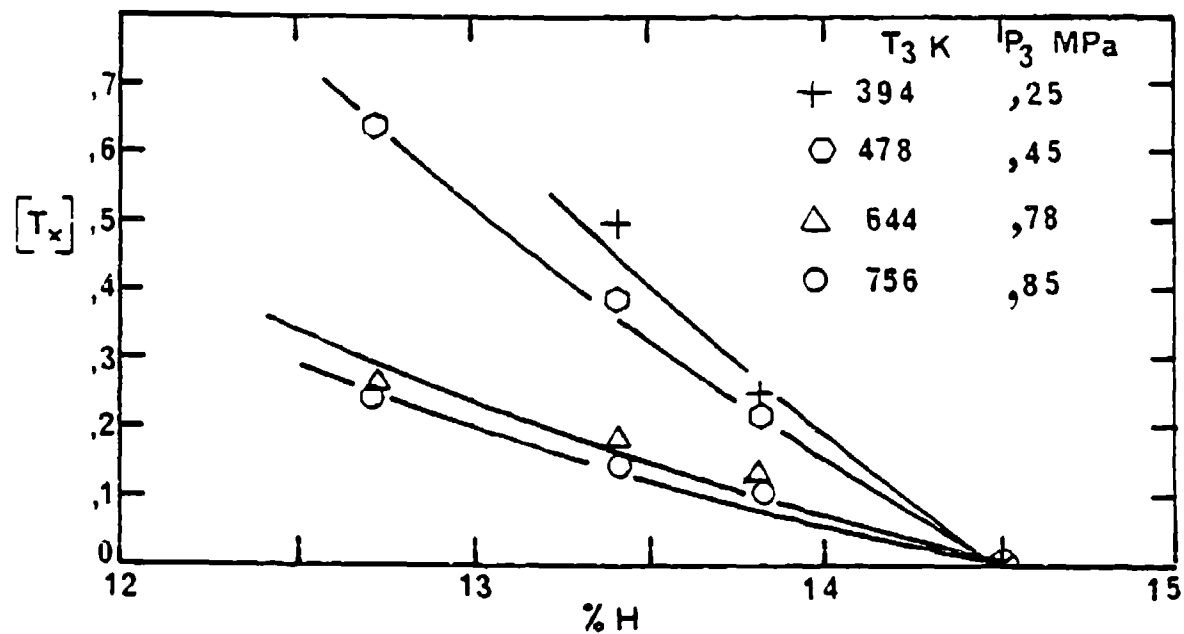


Fig. 23 Effect of fuel composition upon combustor wall temperatures – T₃₆ engine [23]

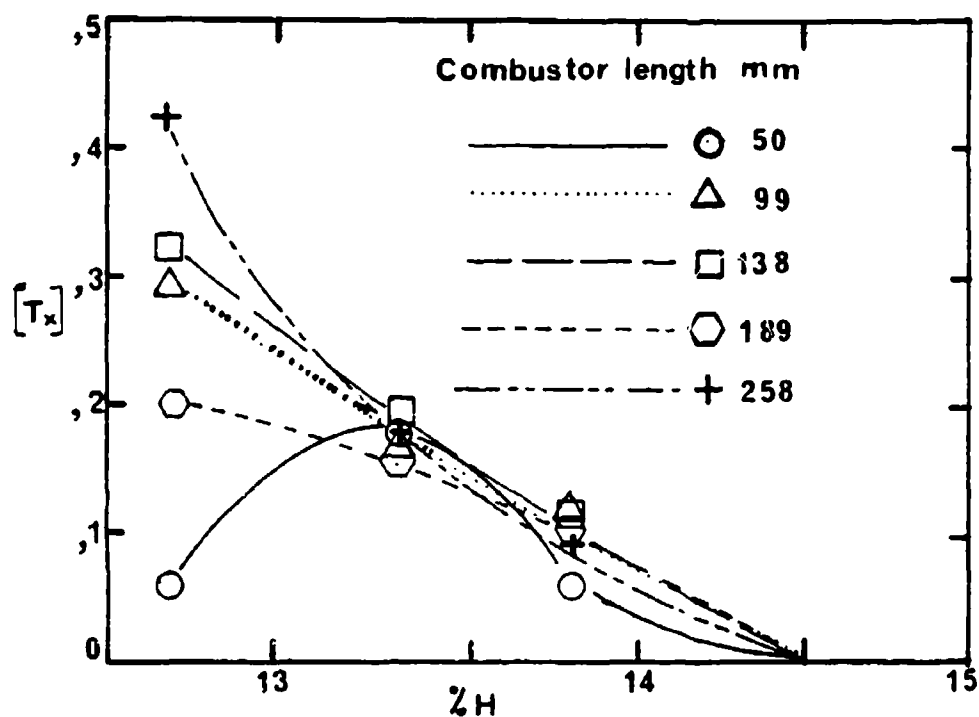


Fig. 24 Effect of fuel composition upon combustor wall temperatures - T_{56} engine [23]

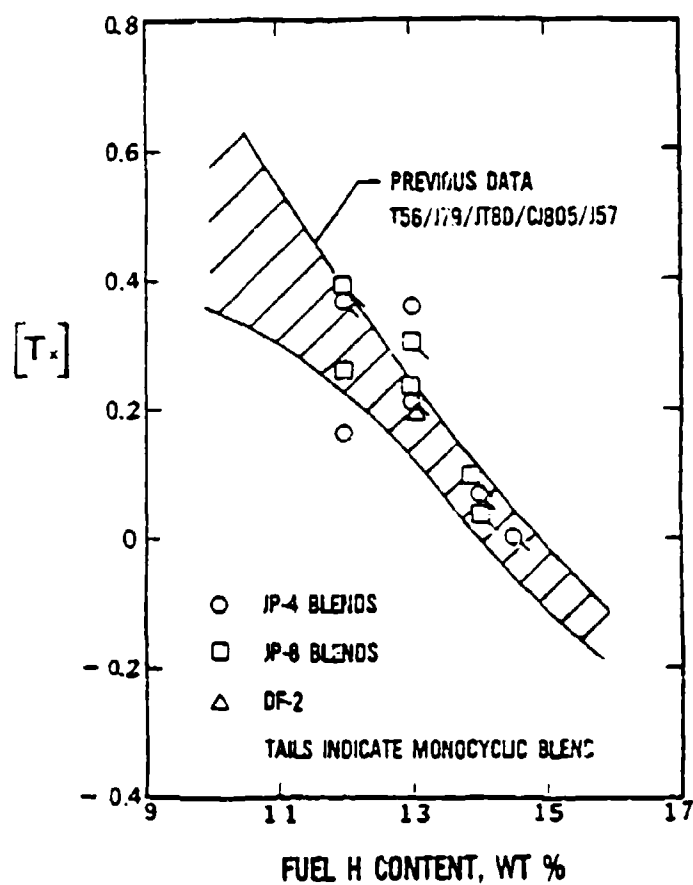


Fig. 25 Effect of fuel composition upon combustor wall temperatures [32]

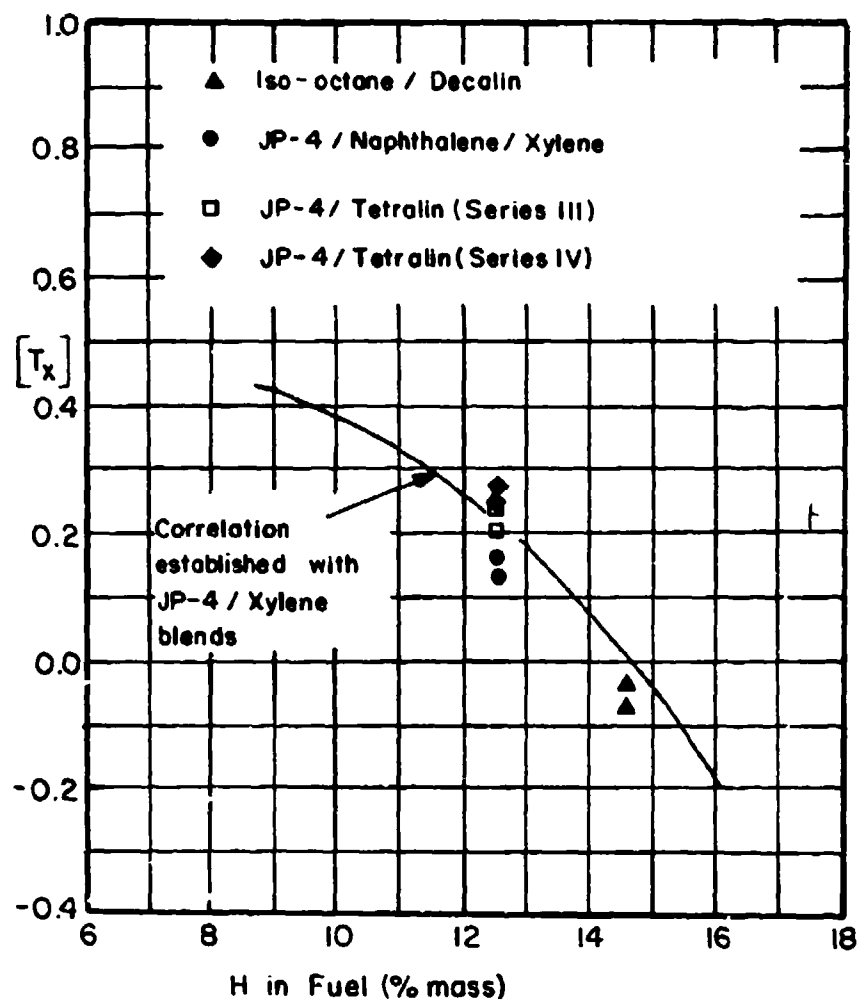


Fig. 26 Effect of fuel composition upon combustor wall temperatures [34]

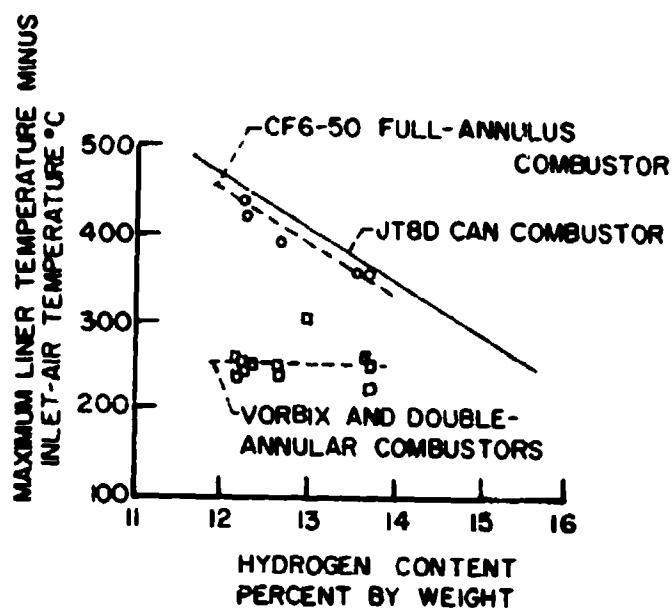


Fig. 27 Effect of fuel composition upon combustor wall temperatures [33]

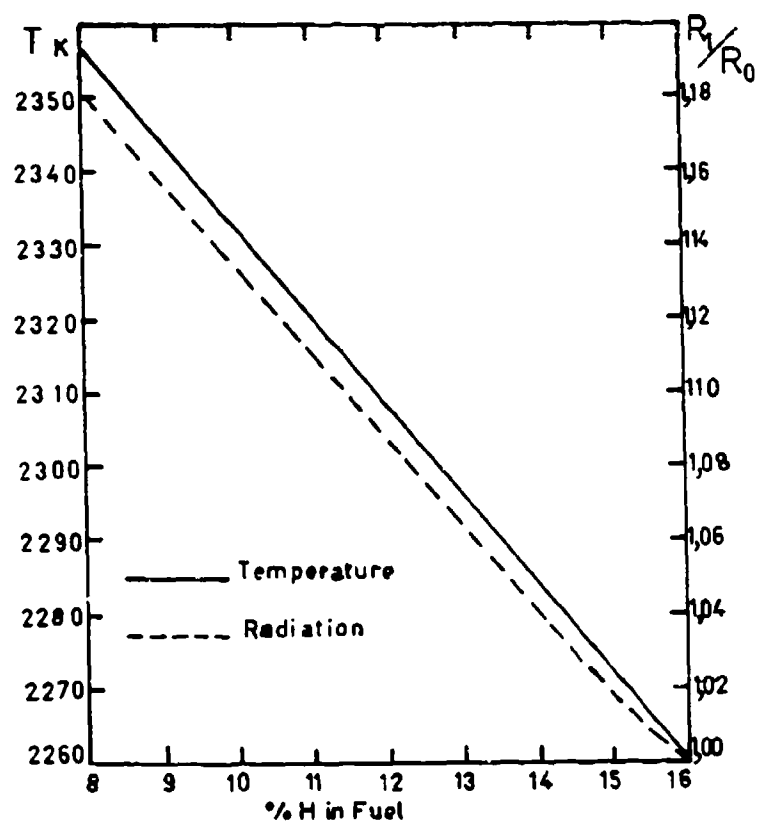


Fig. 28 Predicted effect of temperature increases due to fuel composition upon flame radiation

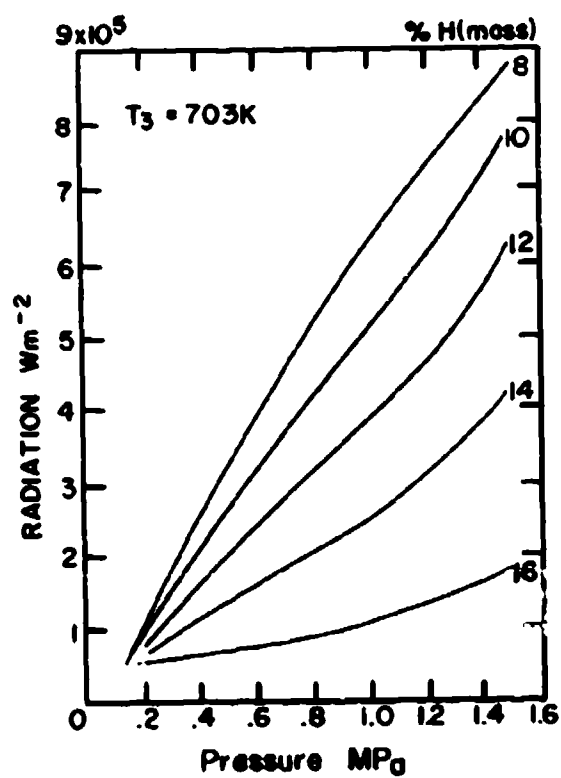


Fig. 29 Effect of fuel composition upon flame radiation at various pressures [35]

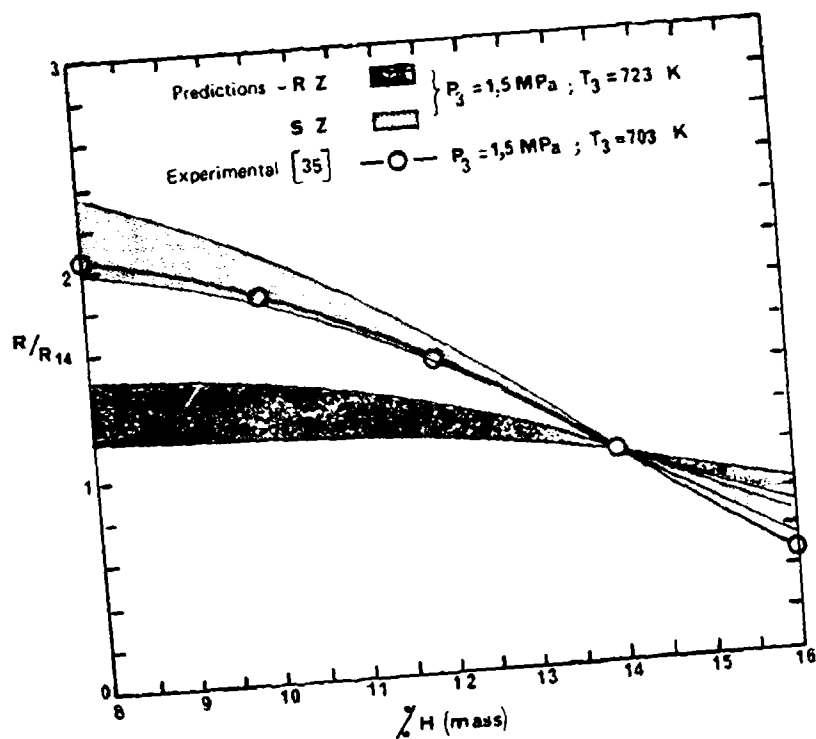


Fig.30 Effect of fuel composition upon radiation index [28]

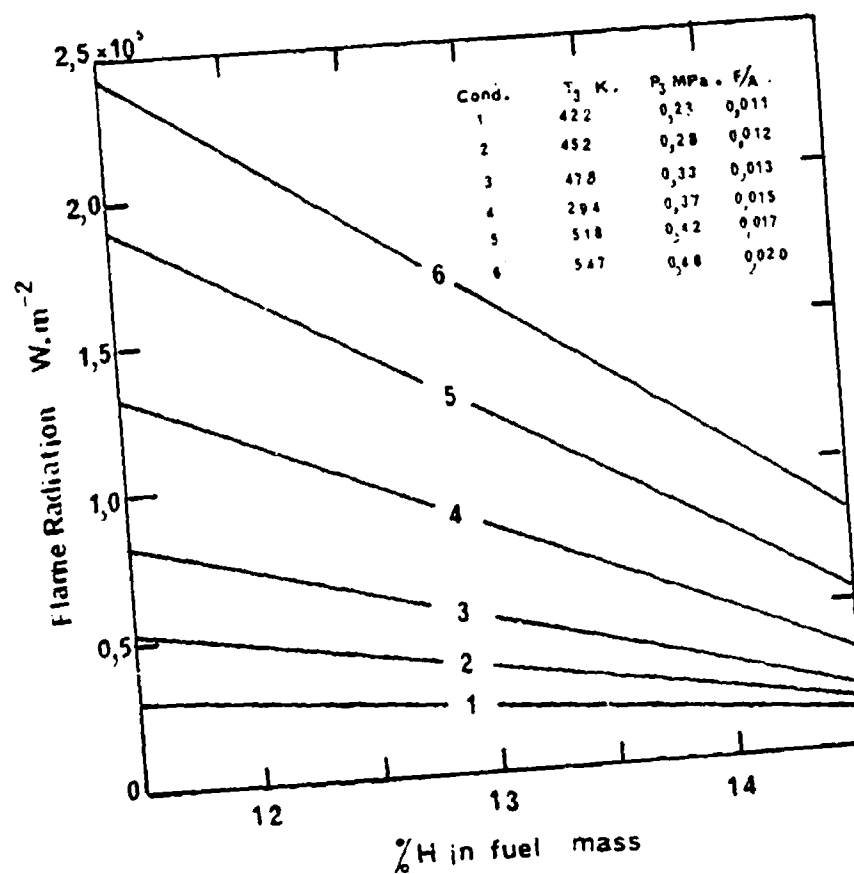


Fig.31 Effect of fuel composition upon flame radiation [37]

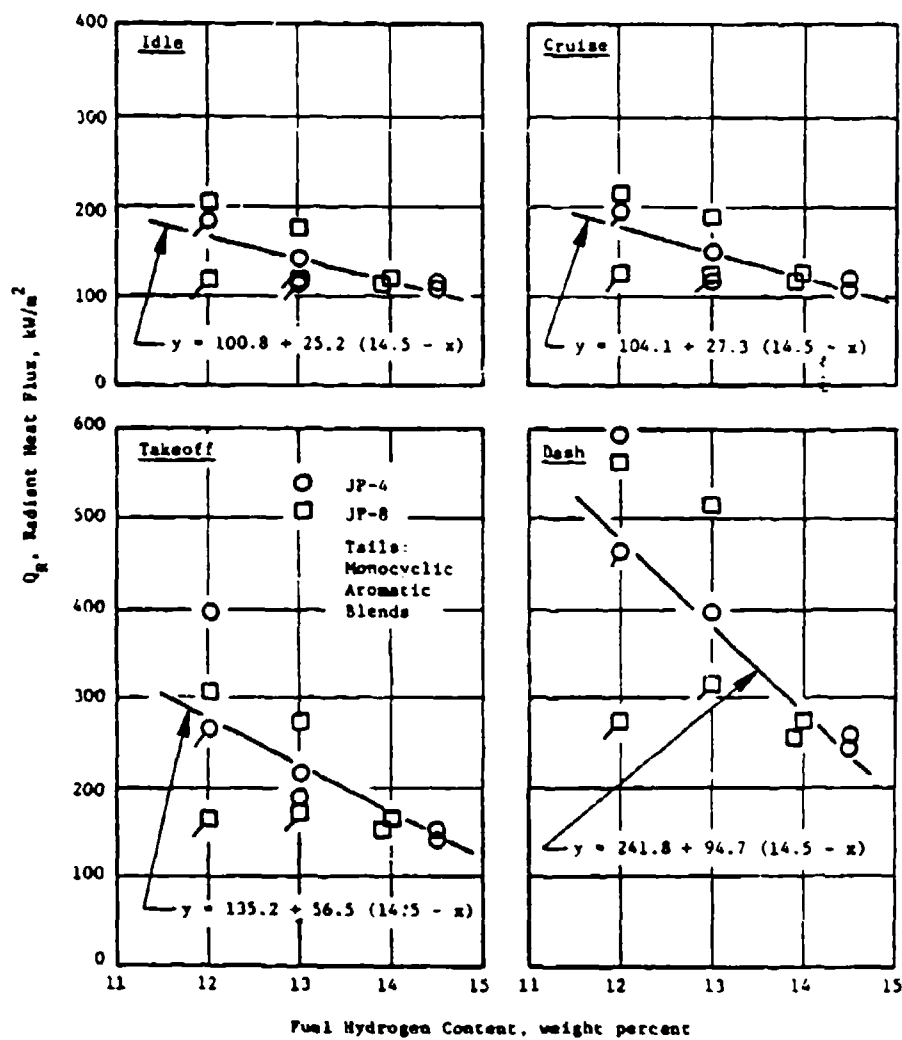


Fig.32 Effect of fuel hydrogen content on flame radiation

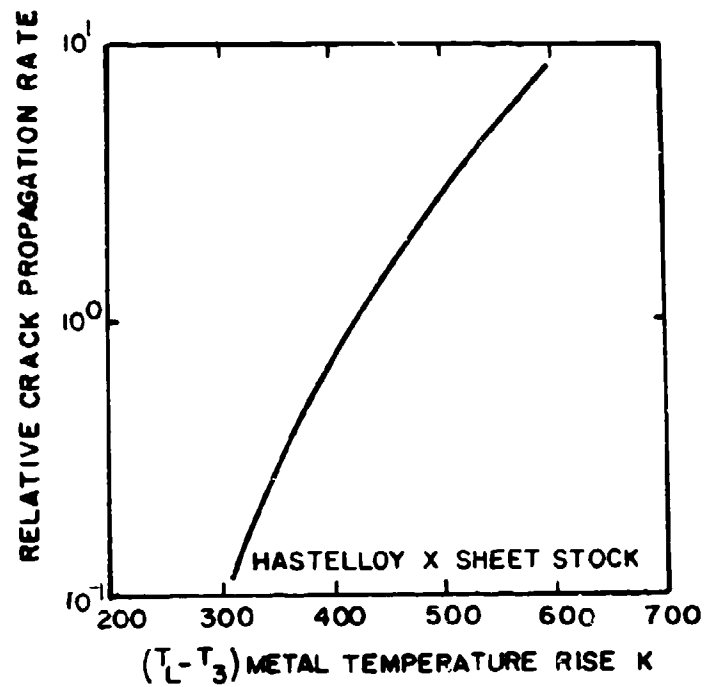


Fig.33 Effect of temperature gradient on J79 combustor lower crack propagation rate [38]

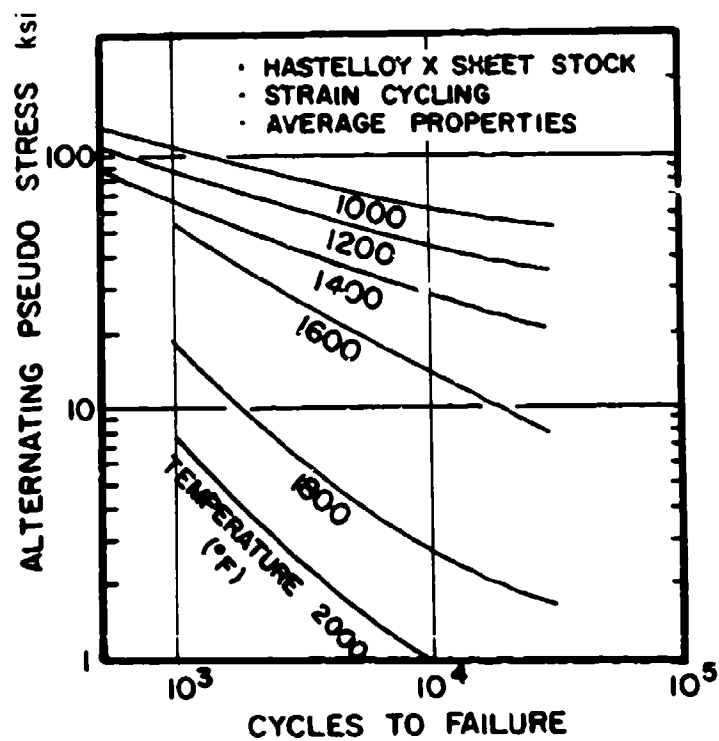


Fig.34 Material life fatigue diagram for F-101 combustor life analysis [38]

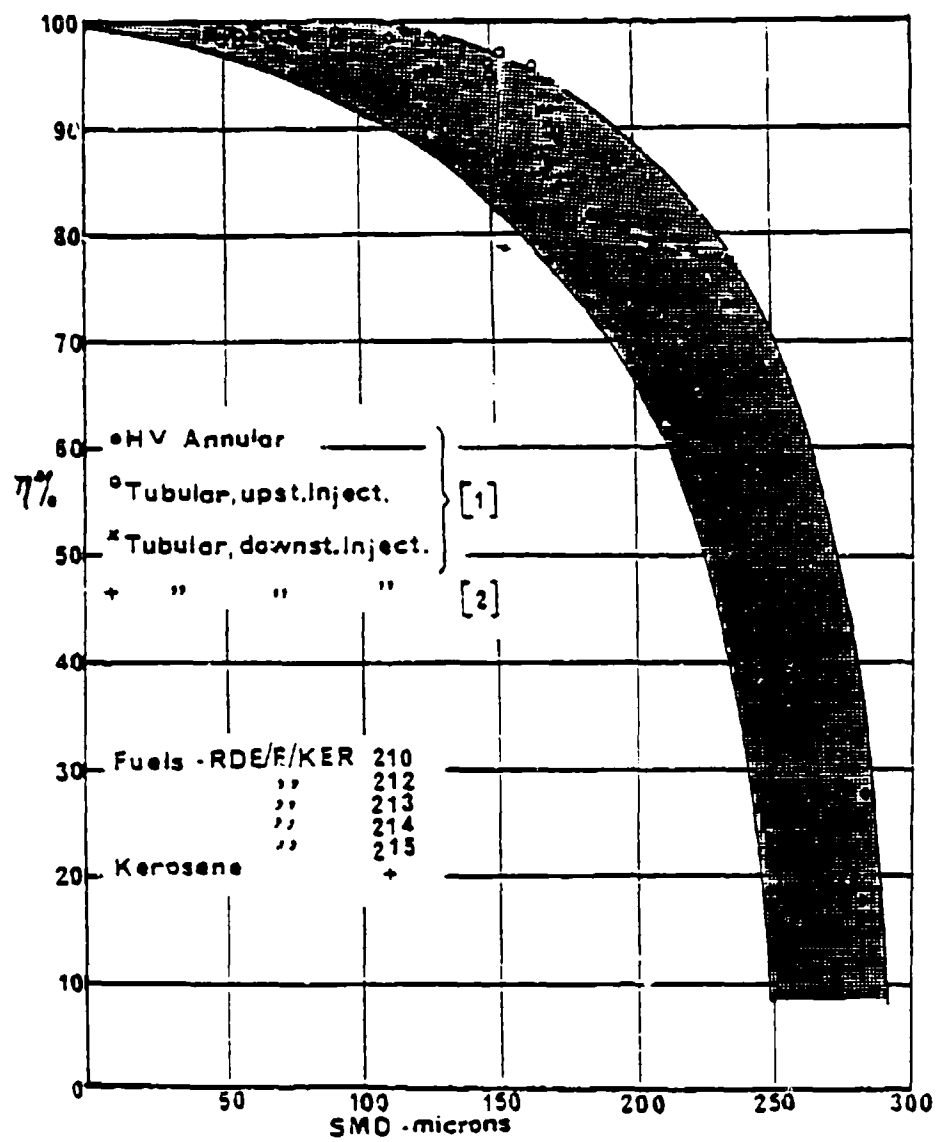


Fig.35 Dependence of combustion efficiency upon droplet diameter

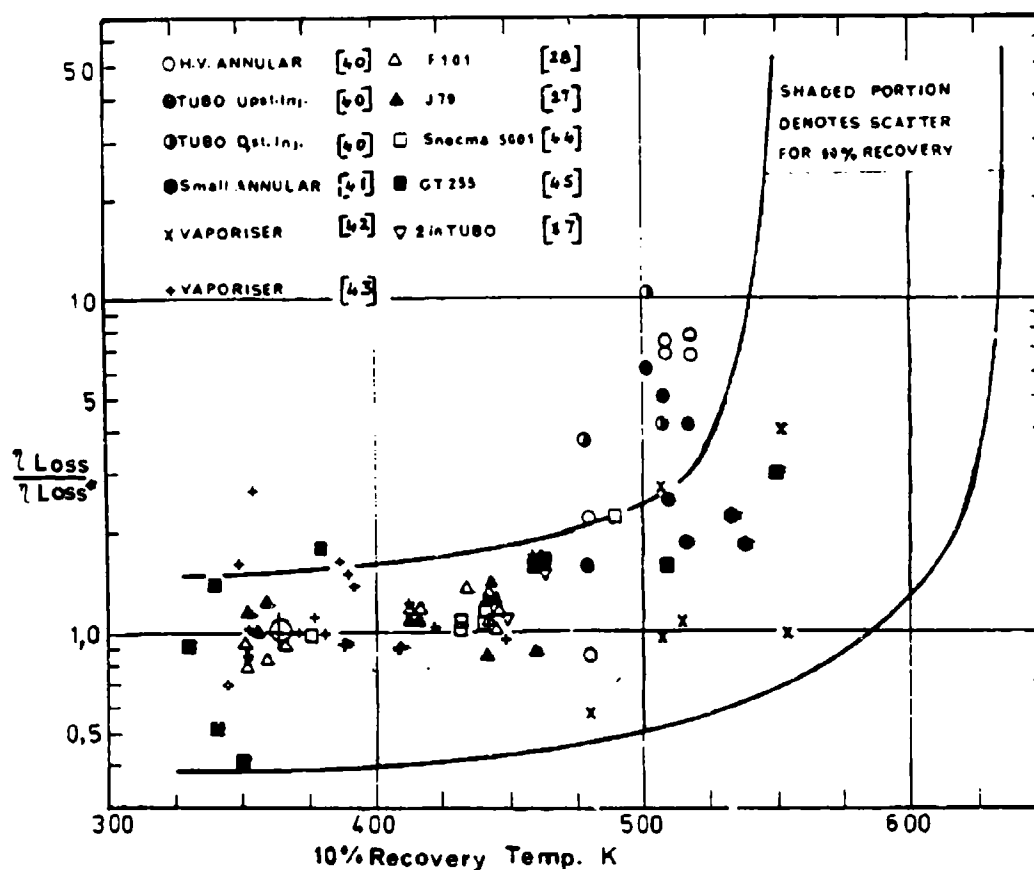


Fig.36 Effect of fuel volatility upon combustion efficiency
(Note - The shaded portion of the curve represents the scatter bands had the experimental points been plotted against the 90% recovery temperature. The points on the curve are the actual values plotted for the 10% RT. To avoid confusion the scatter band for these points has not been included.)

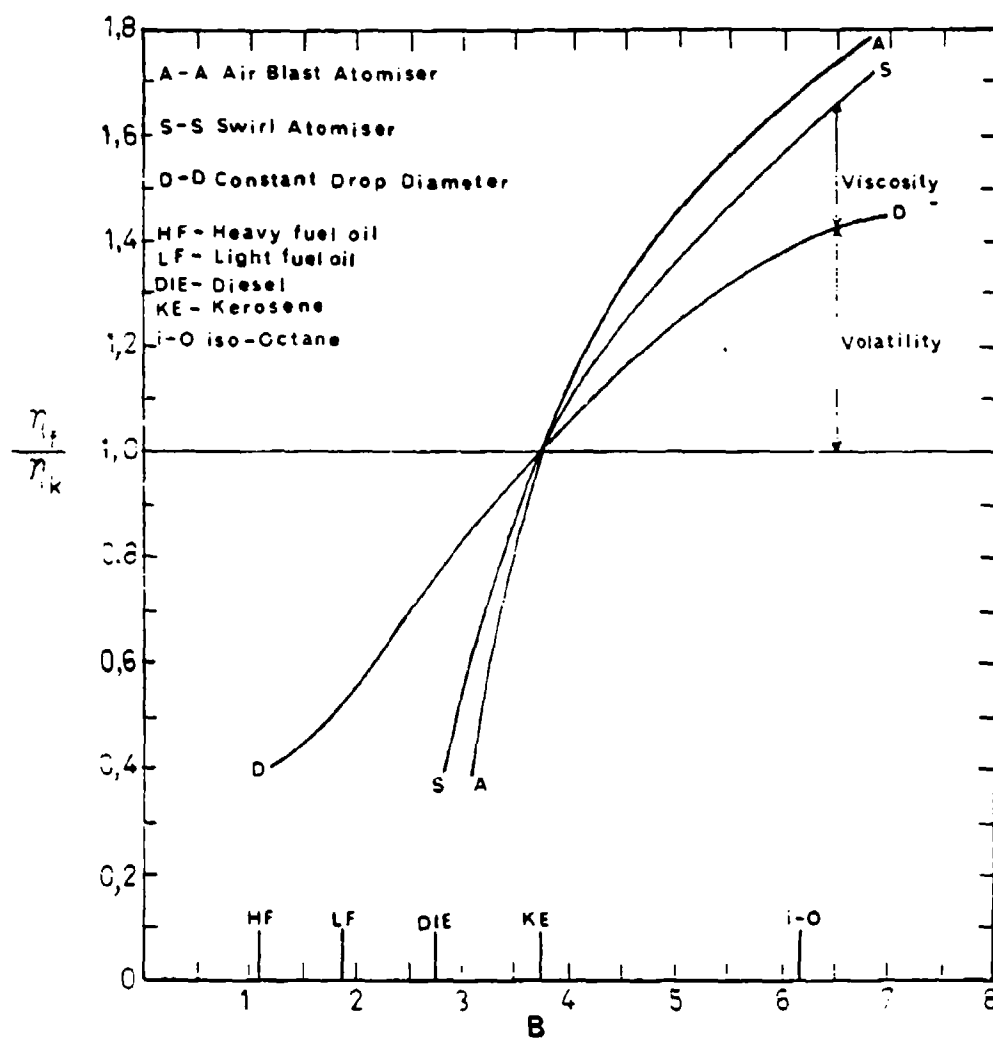


Fig 37 Effect of mass transfer number on combustion efficiency [46]

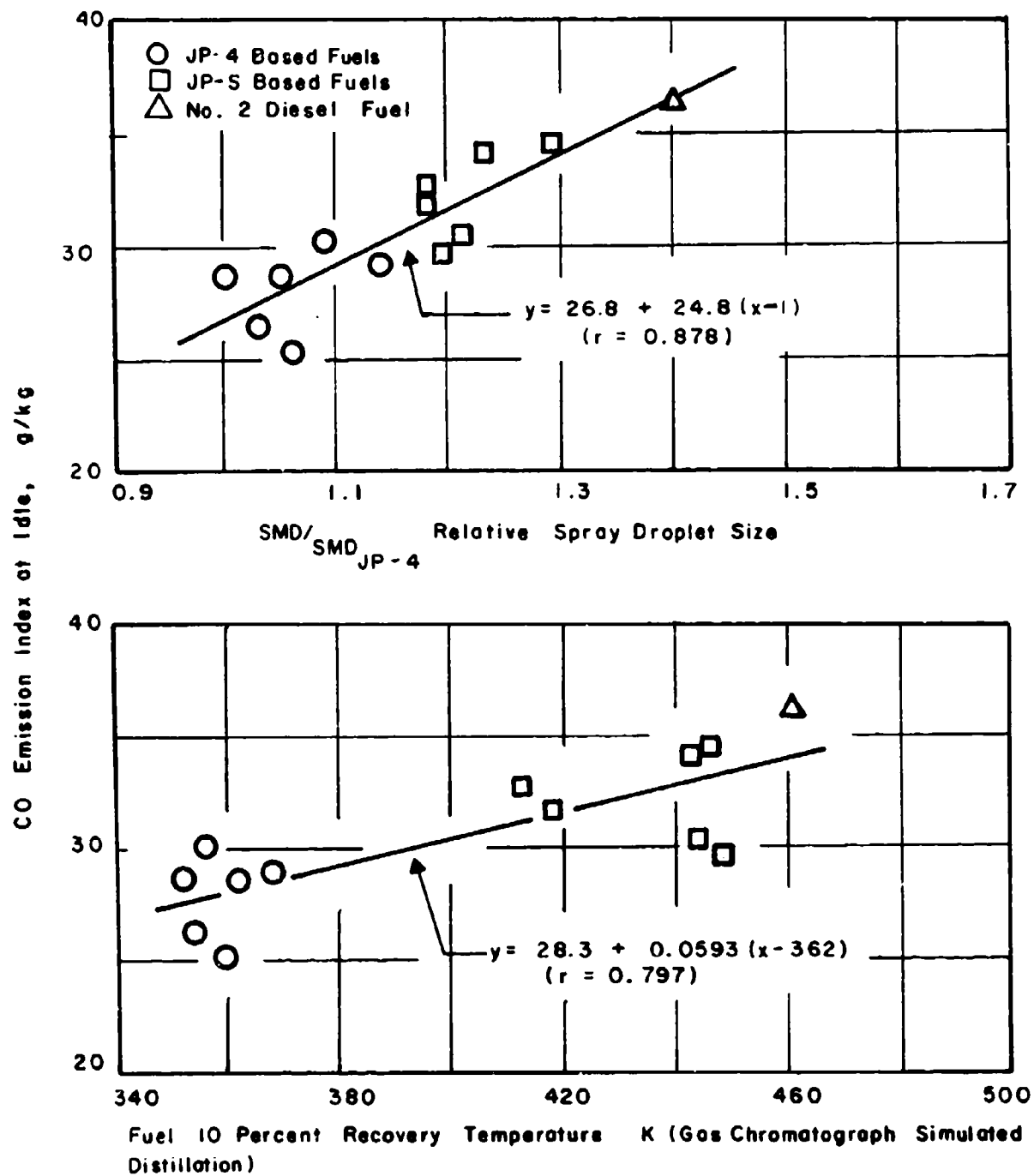


Fig.38 Effect of fuel atomization and volatility on idle CO emission levels.
 (F101 engine combustor) [28]

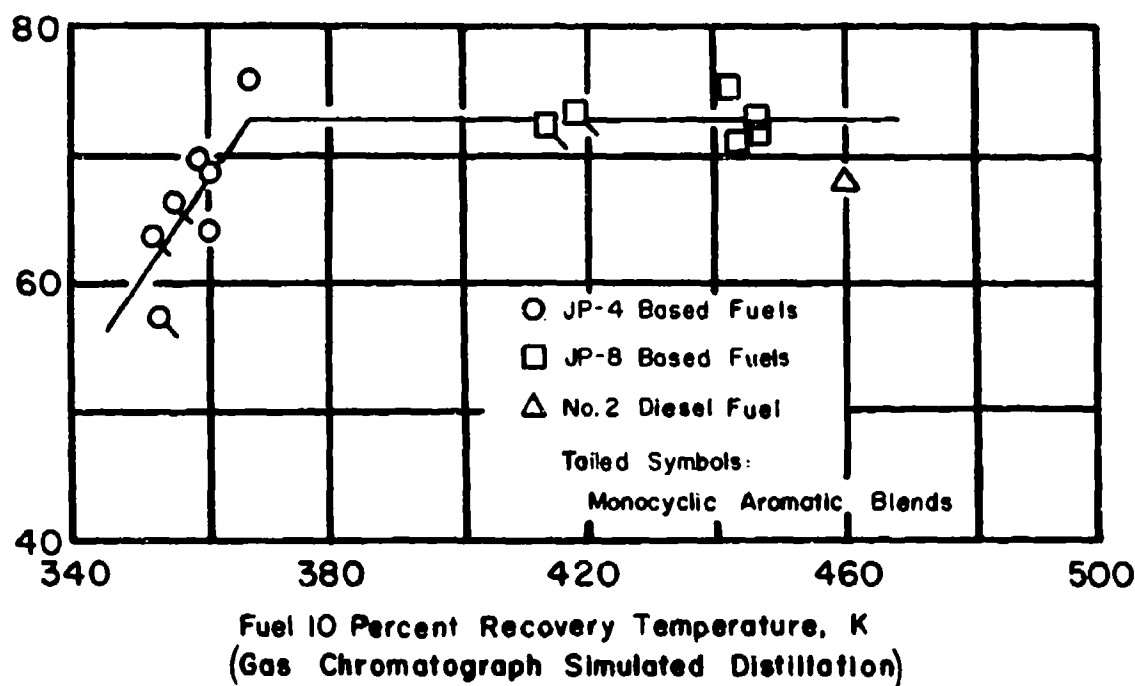
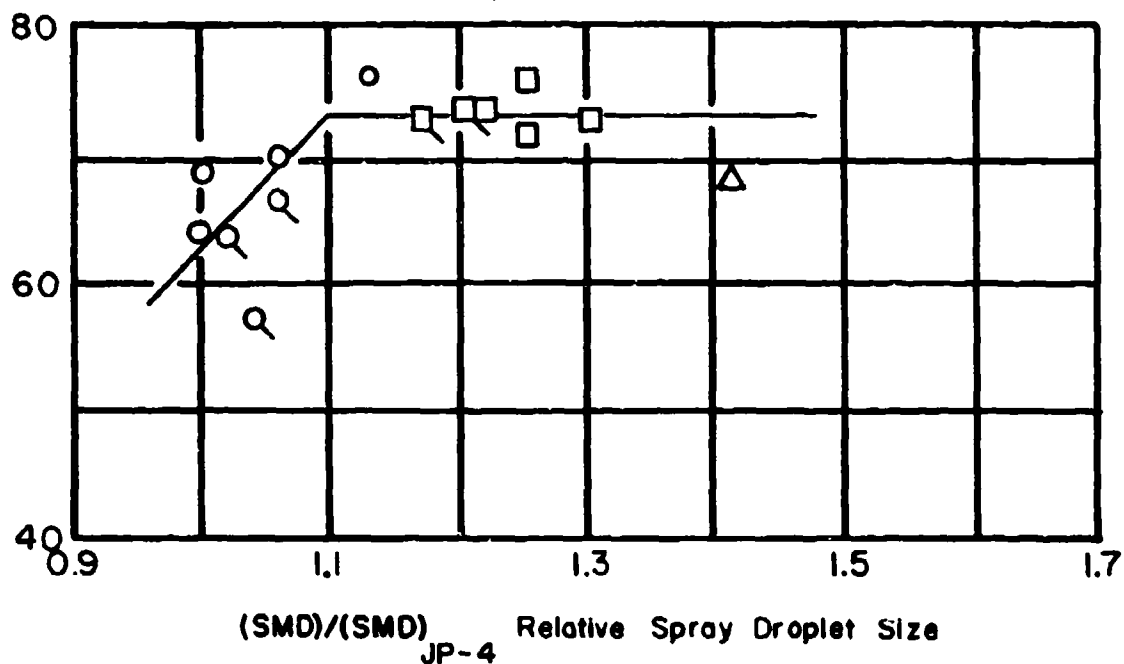


Fig. 39 Effect of fuel atomization and volatility on idle CO emission levels.
(J79 engine combustor) [27]

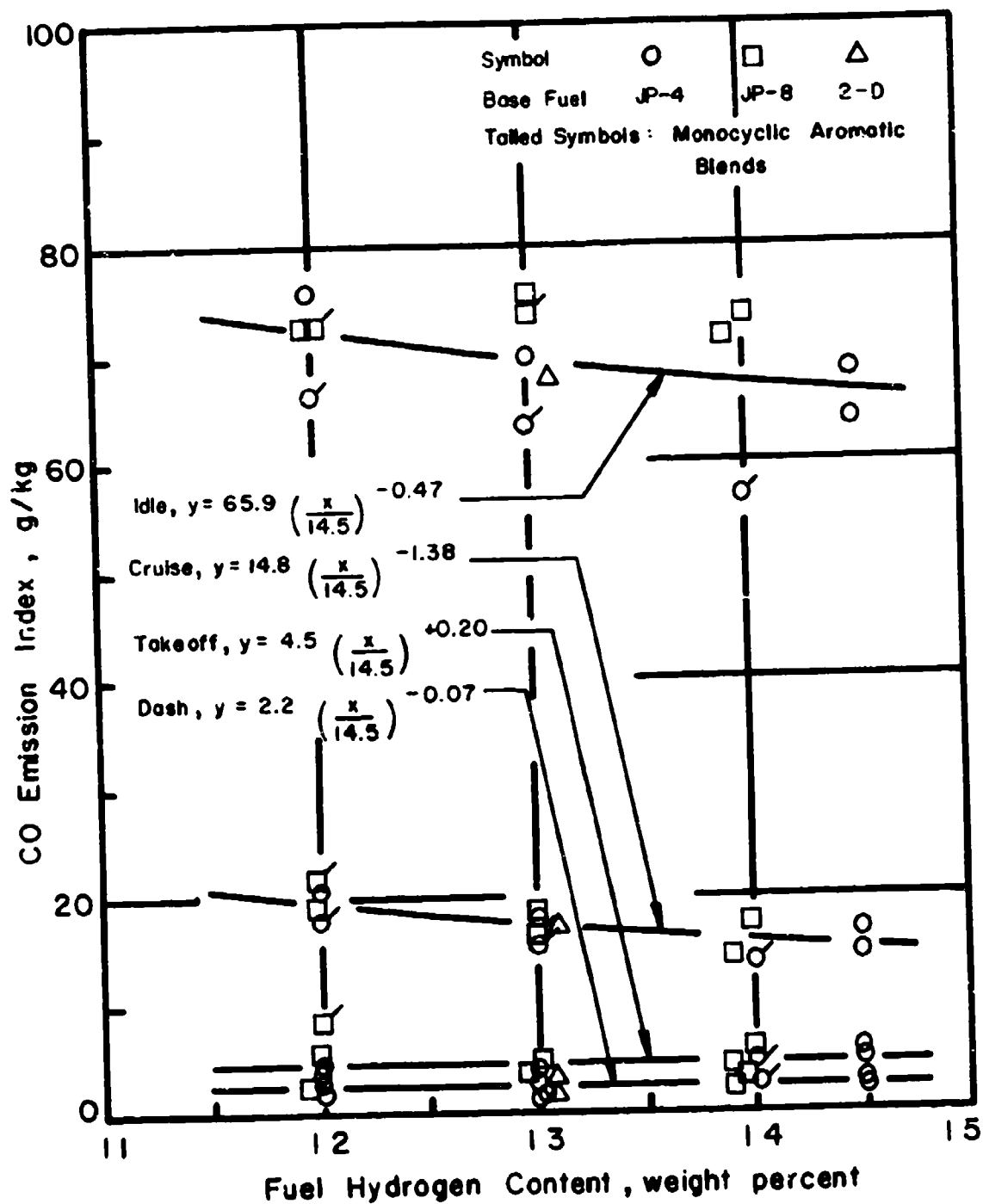


Fig.40 Effect of fuel hydrogen content on CO emission levels

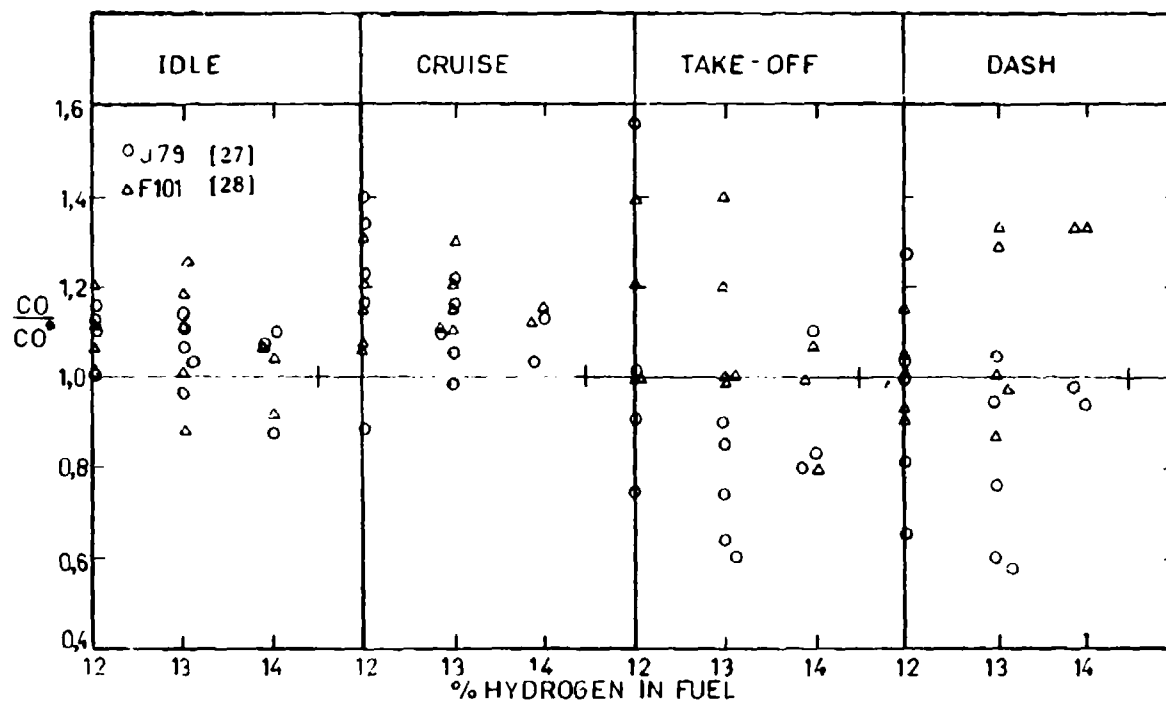


Fig.41 Effect of fuel composition upon carbon monoxide emissions

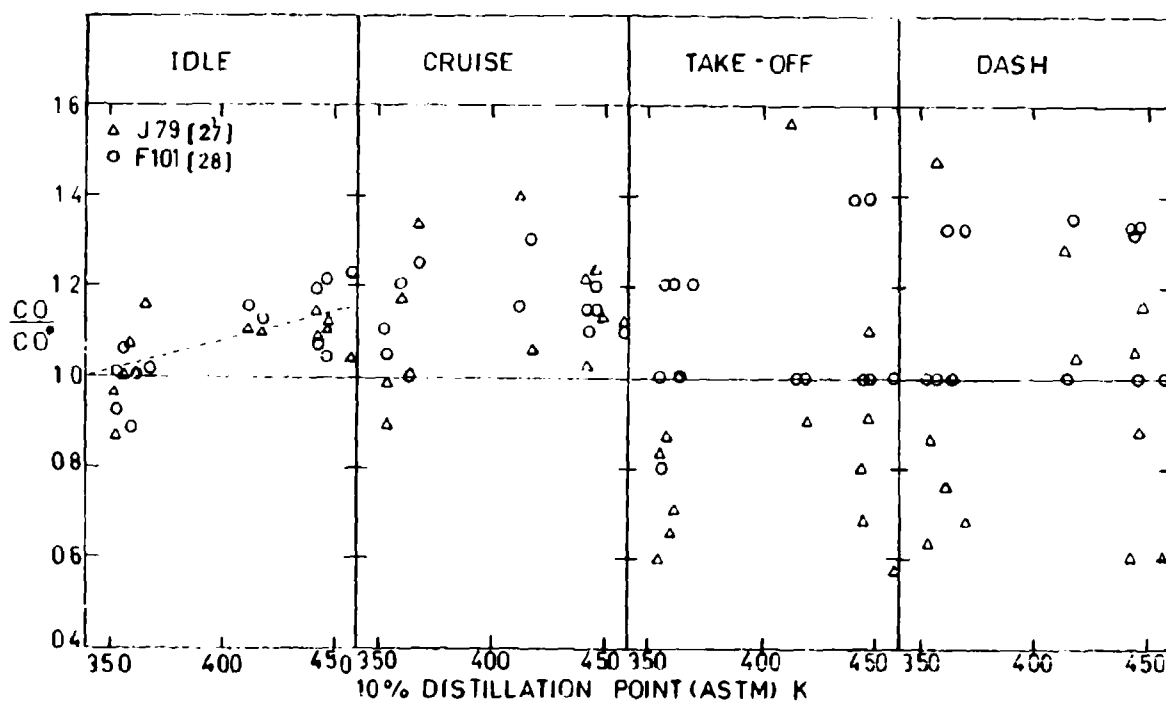


Fig.42 Effect of fuel volatility on carbon monoxide emissions

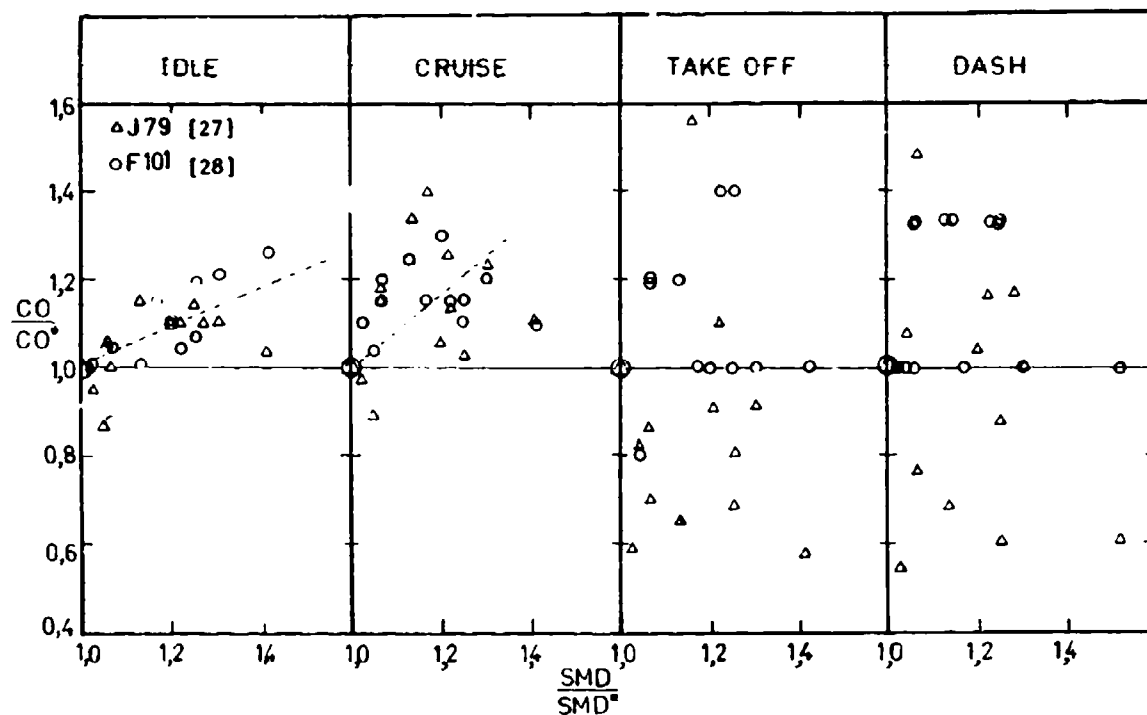


Fig.43 Effects of droplet size upon carbon monoxide emissions

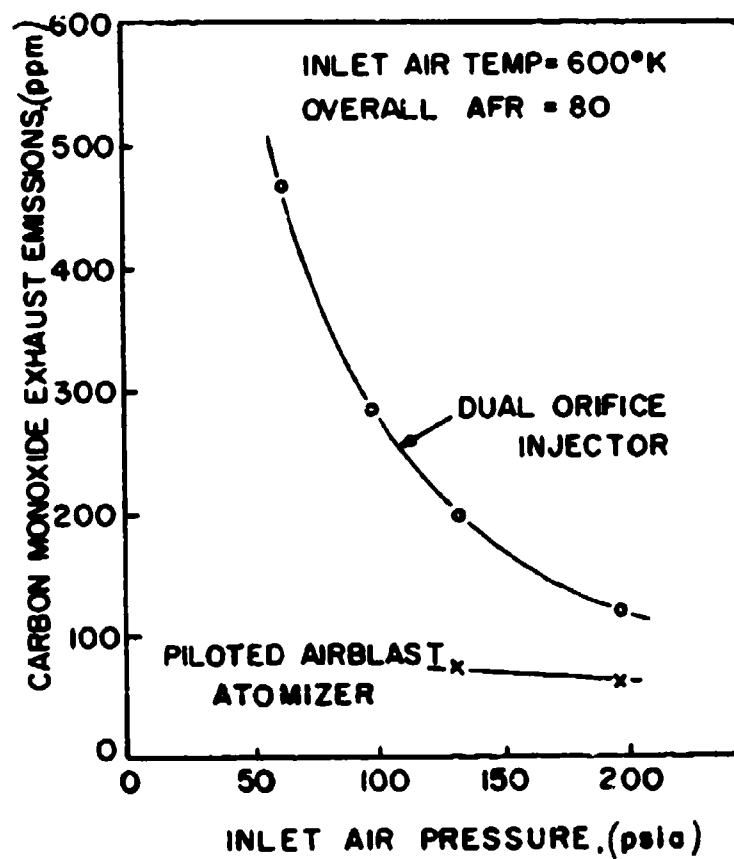
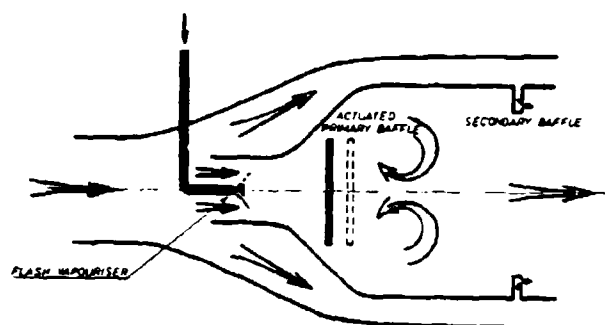
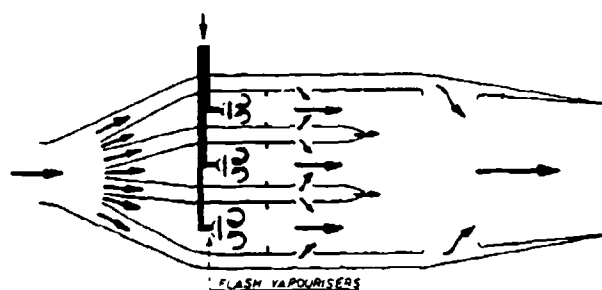


Fig.44 Effect of combustion pressure on CO exhaust emissions



(a) Schematic diagram of a variable area chamber



(b) Schematic representation of a banked annular chamber for low pollution

Figure 45

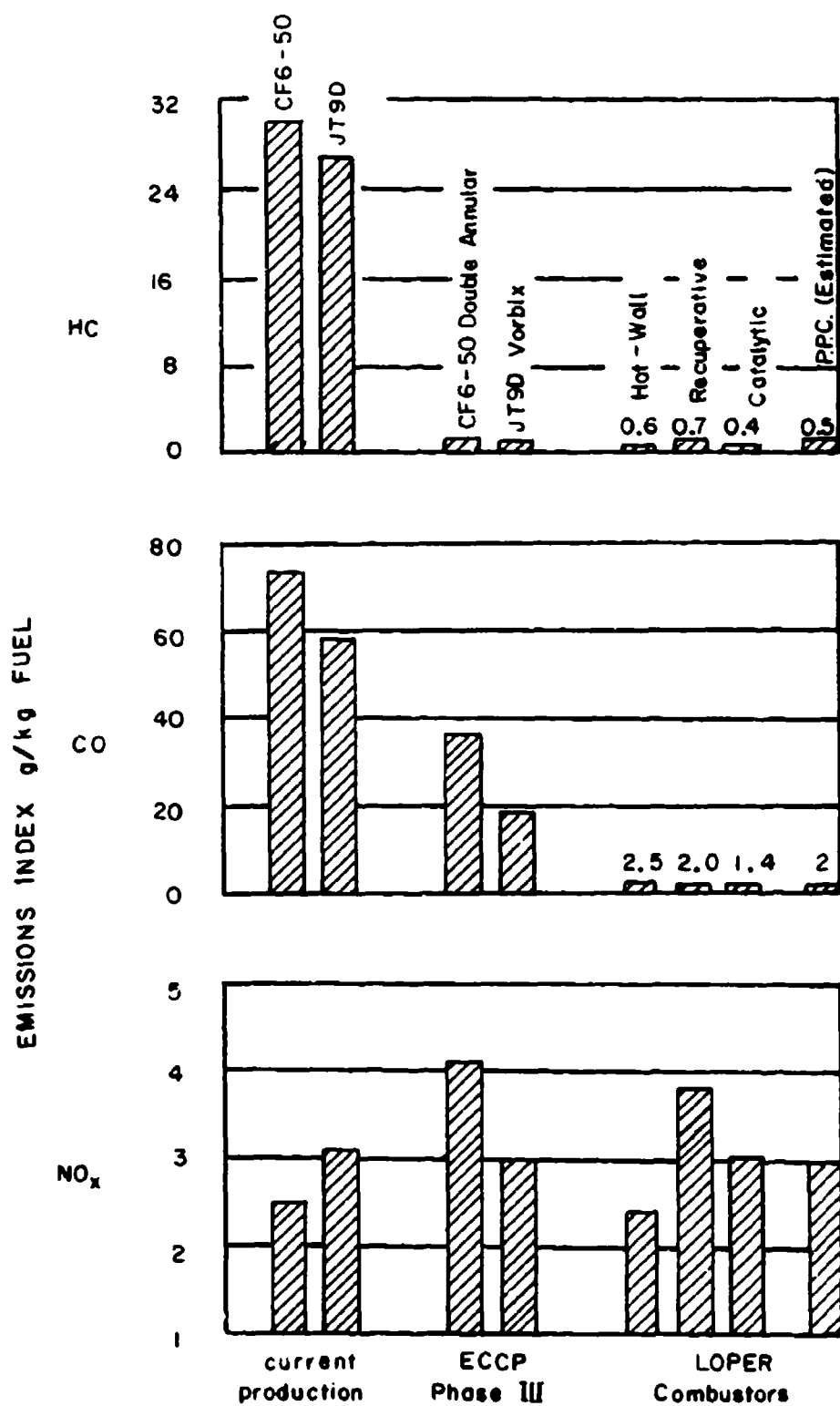


Fig.46 Comparison of current production, ECCP, and LOPER combustor idle emission levels. [After 57]

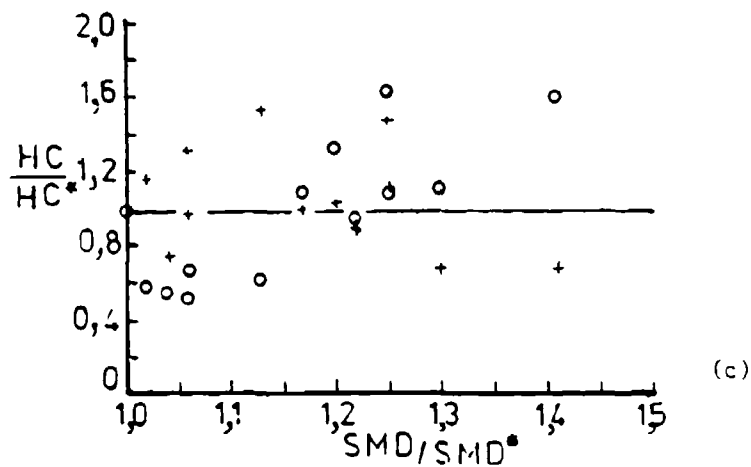
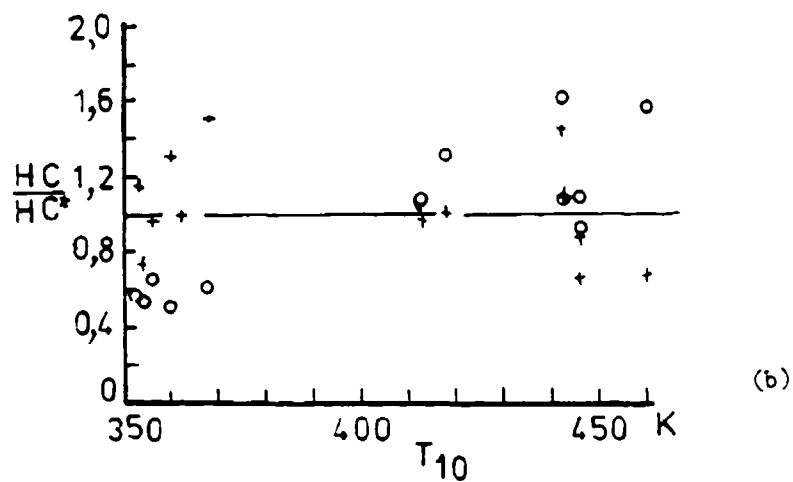
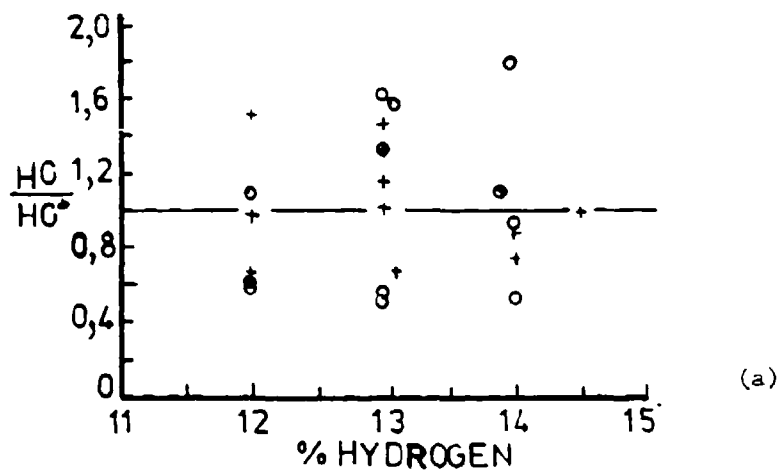
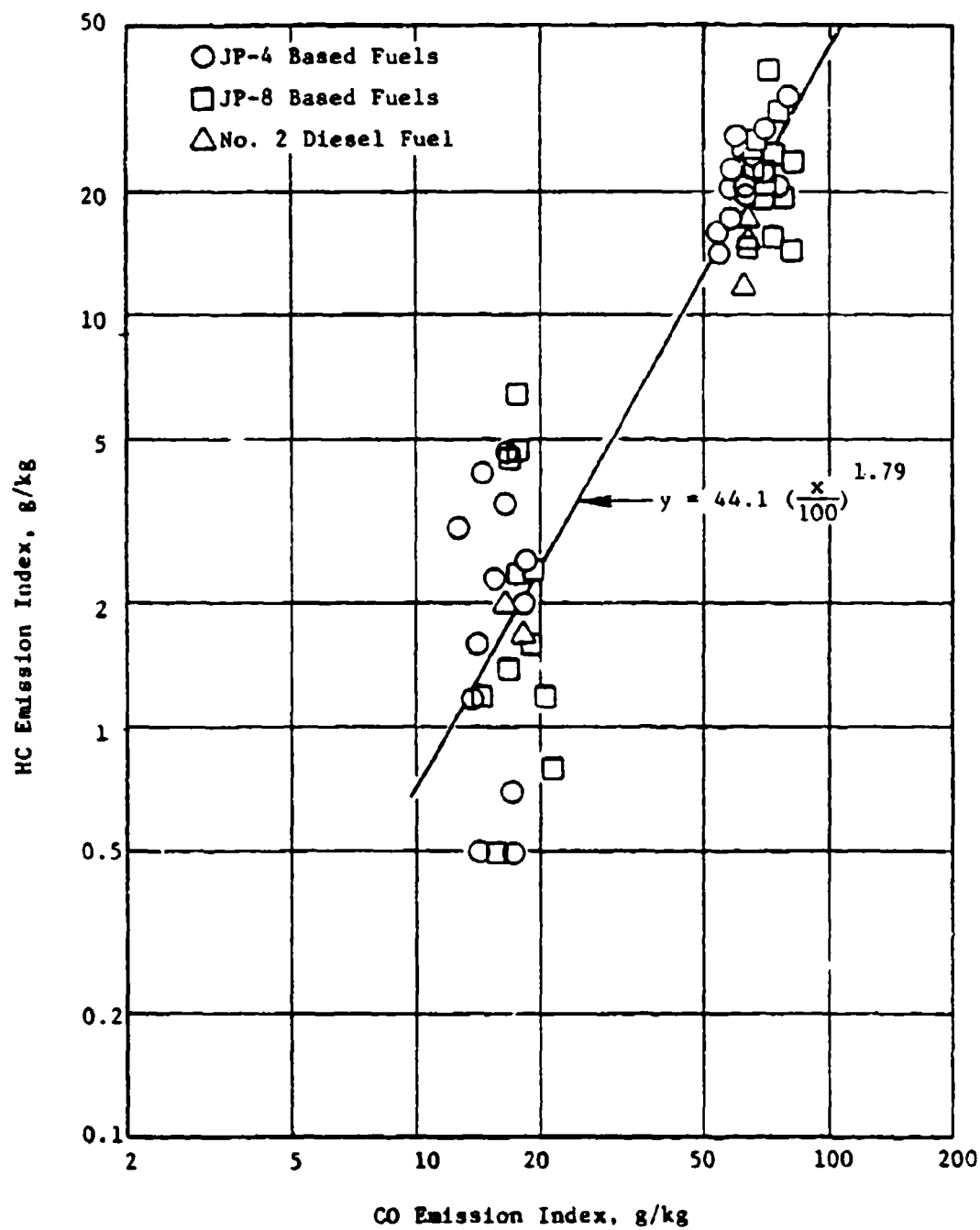


Fig.47 Exhaust hydrocarbons – effect of fuel properties at idling conditions.
Data from [27 and 28]



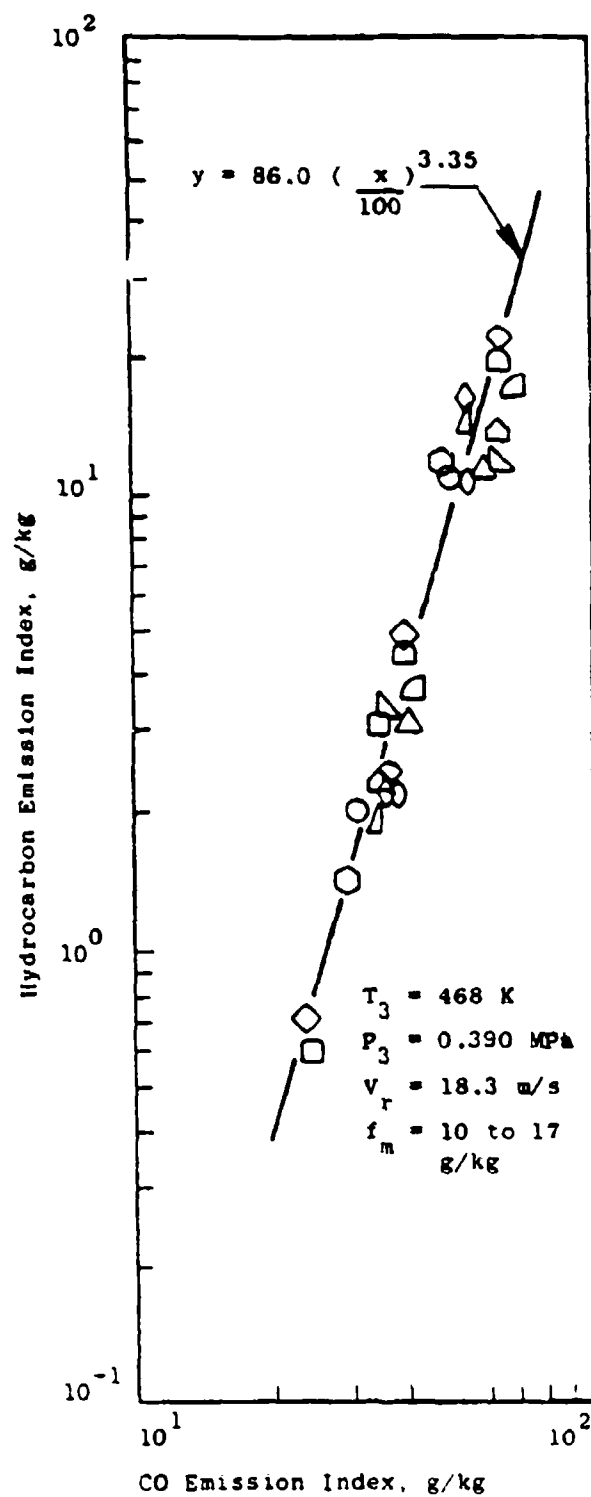
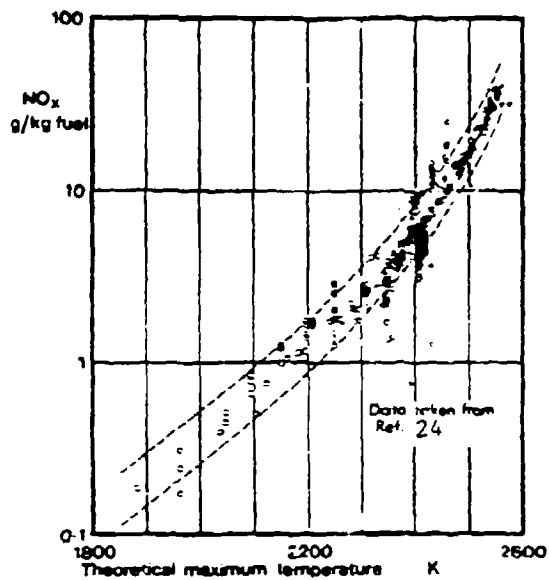
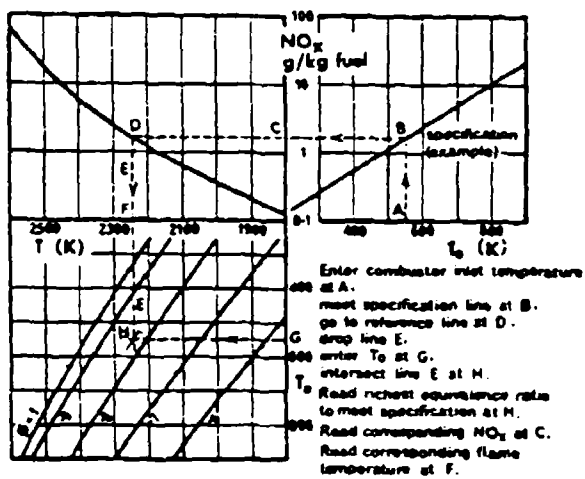


Fig.49 Variation of HC emission levels with CO emission levels at idle operating conditions.
F101 engine [28]



- A NO_x found in gas turbine exhausts, plotted against the theoretical maximum temperature attainable in the combustor.



- B Relationship of exhaust NO_x with equivalence ratio and combustor inlet temperature

Figure 50

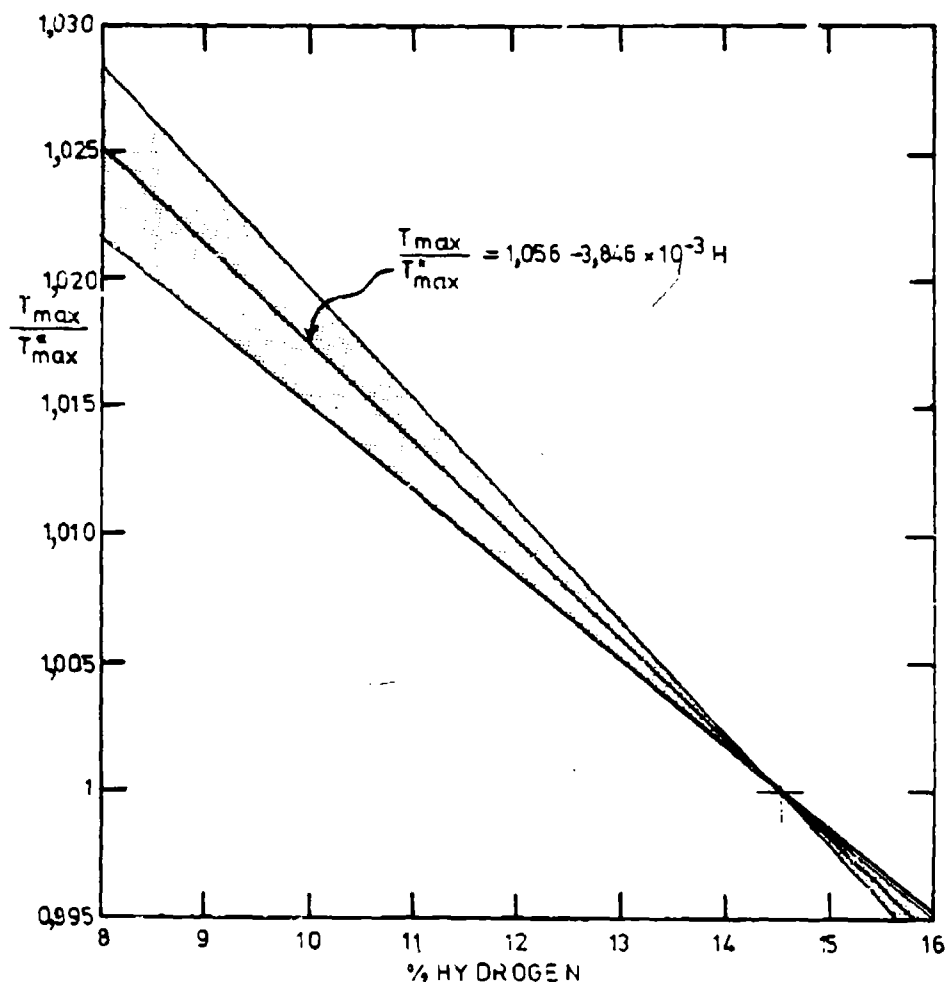


Fig.51 Effect of fuel hydrogen content on maximum flame temperature

NO_x Emission Index, g/kg
(Corrected to Standard Day T, P and h)

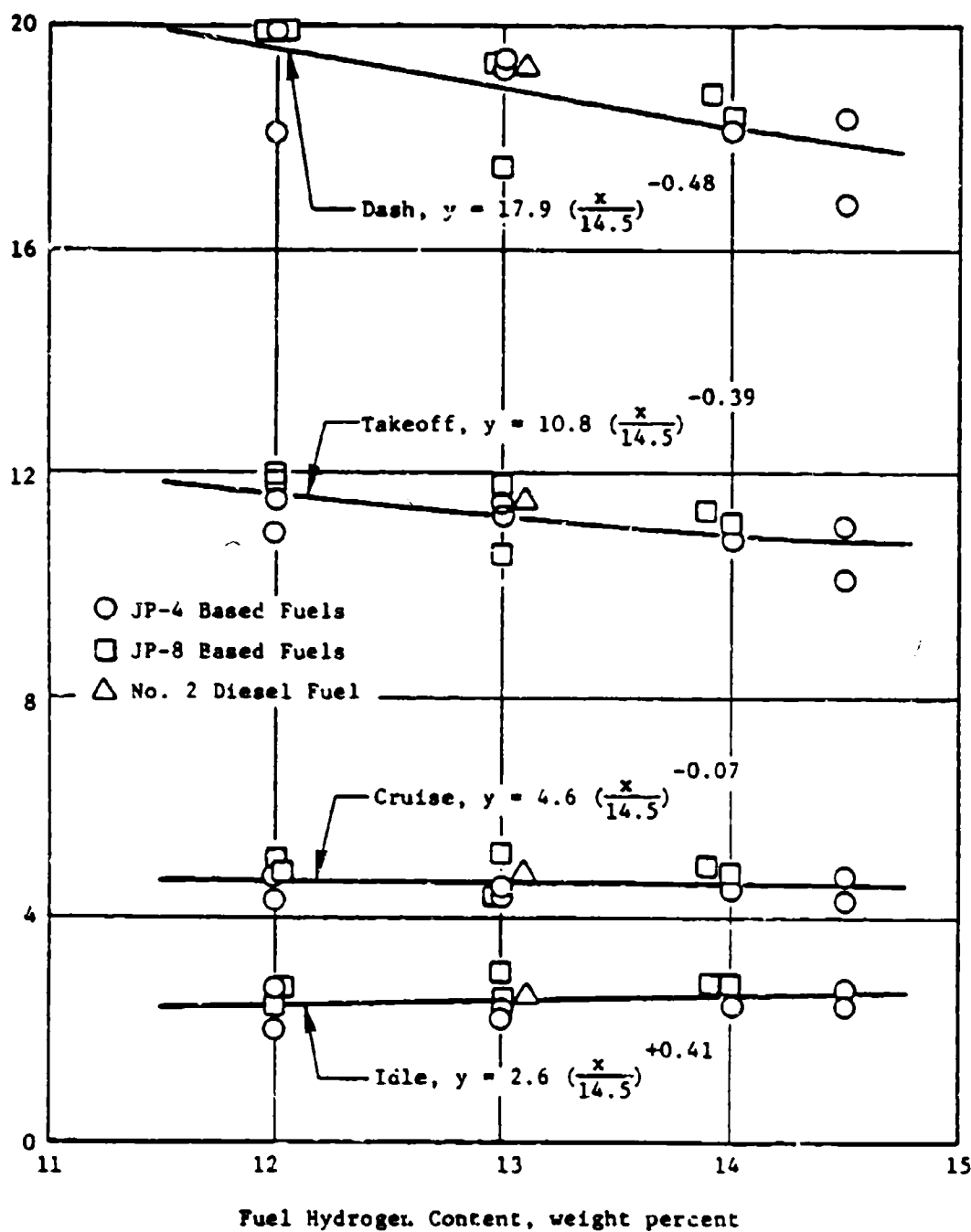


Fig.52 Effect of fuel hydrogen content on NO_x emission levels. (J-79) [27]

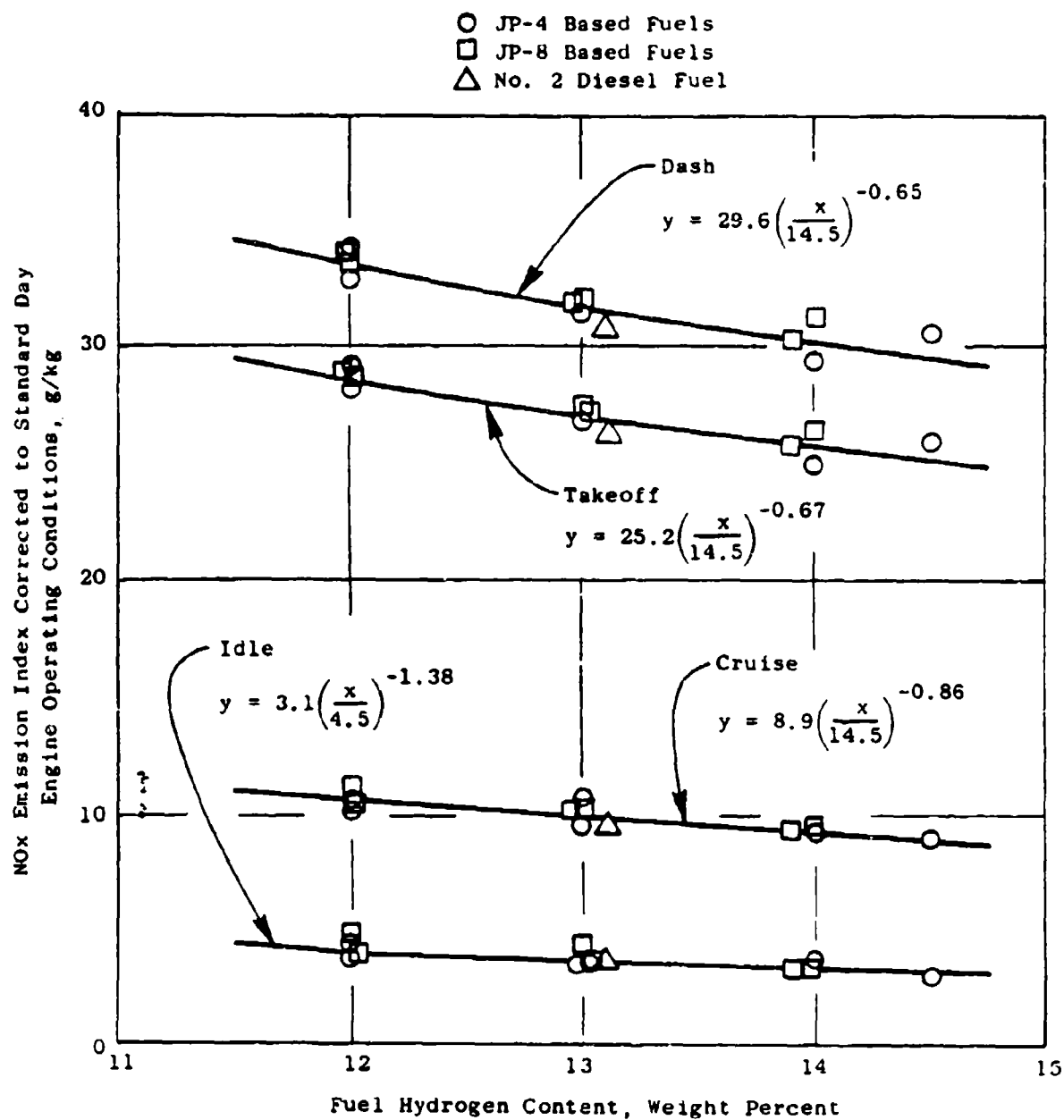


Fig.53 Effect of fuel hydrogen content on NO_x emission levels. (F101) [28]

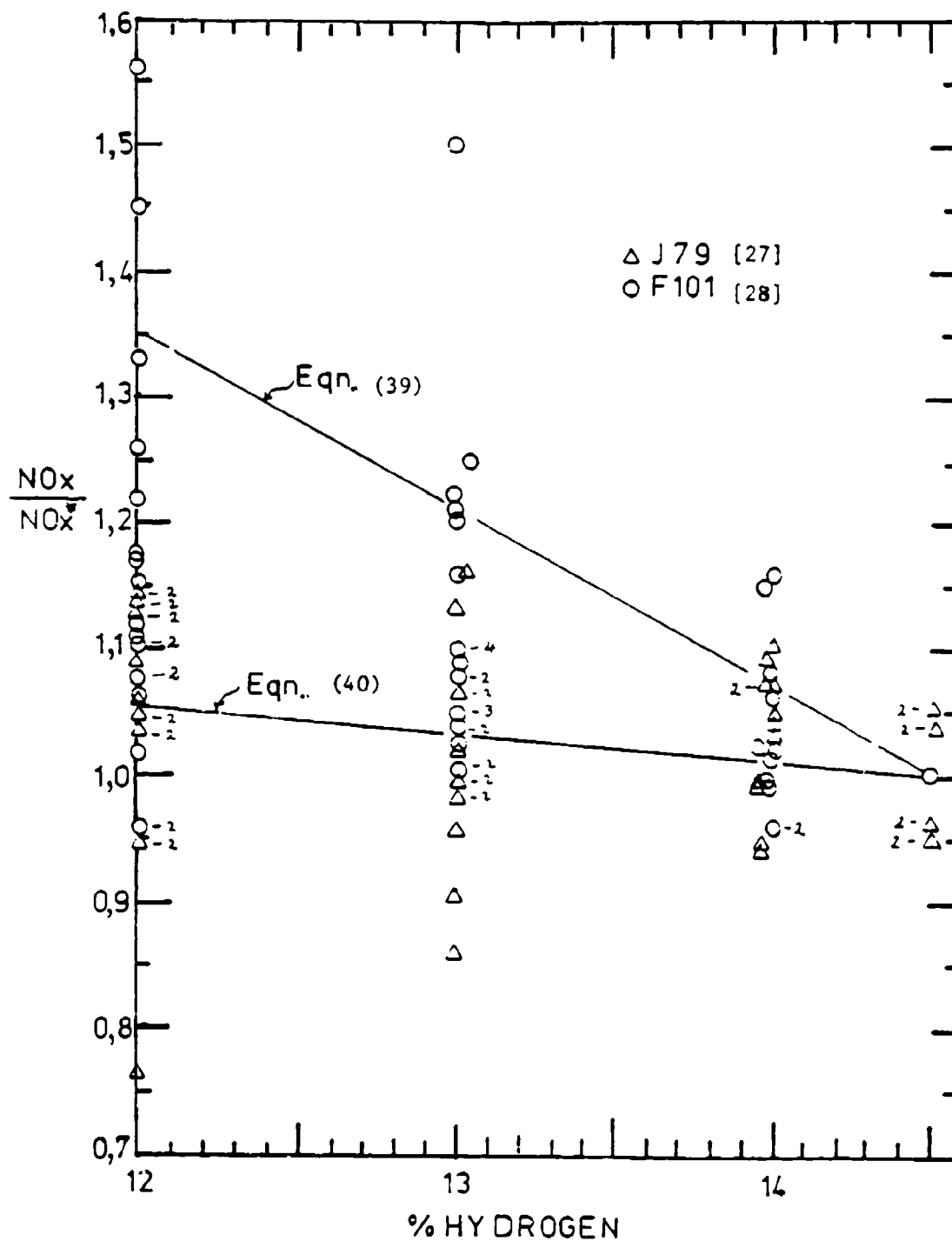


Fig.54 Effect of fuel hydrogen content on NO_x emission levels

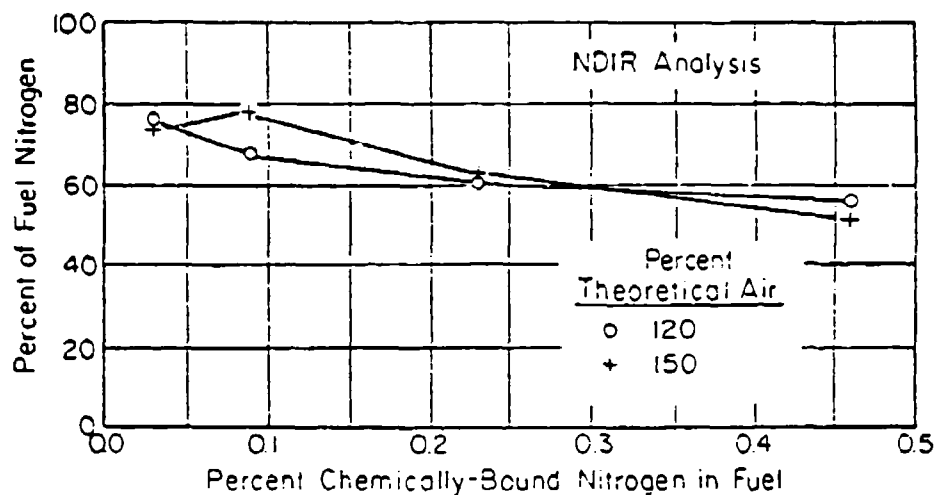


Fig.55 Percent of fuel nitrogen converted to NO as function of fuel nitrogen content -- NDIR data [68]

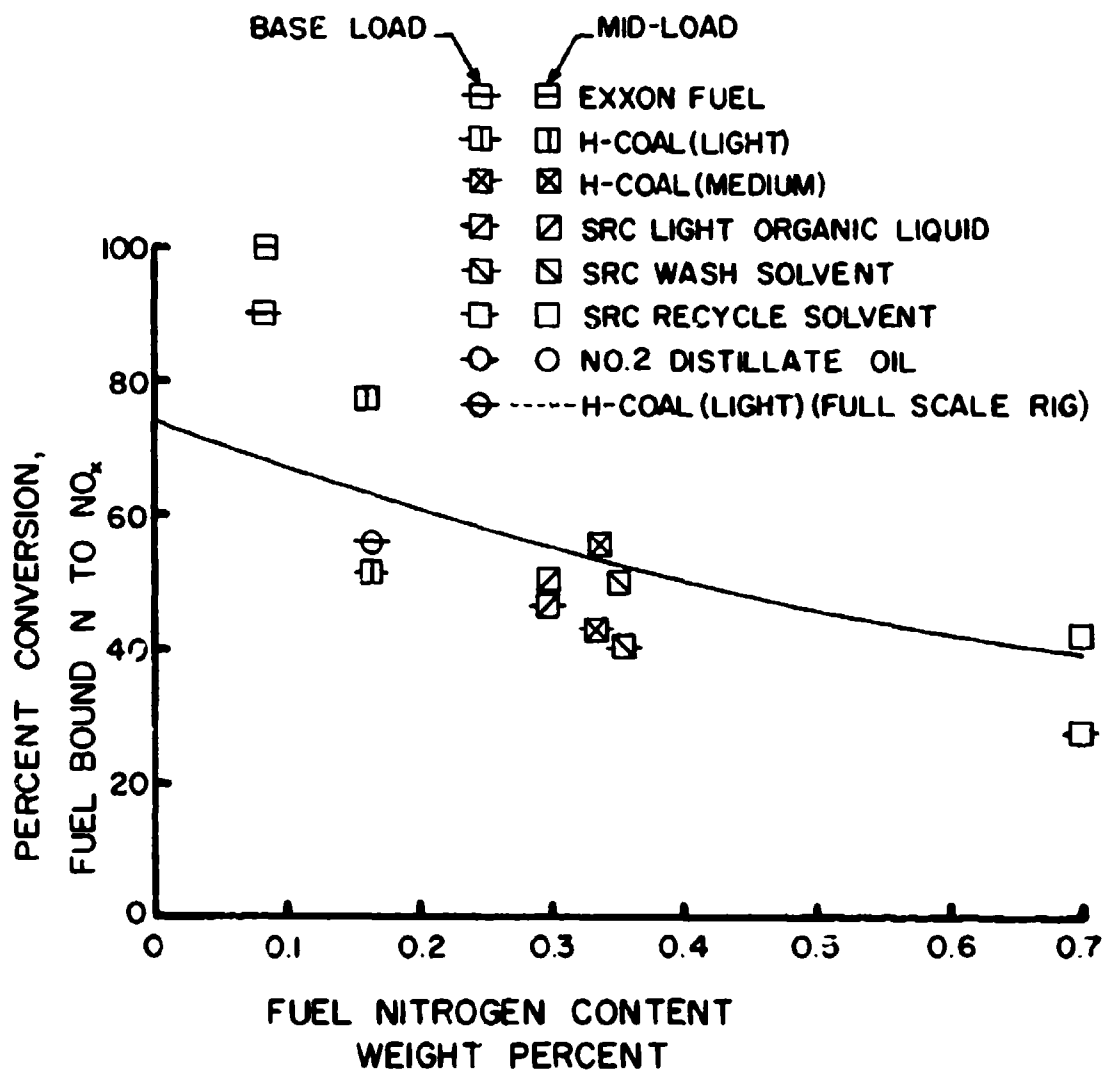


Fig.56 Conversion of fuel bound nitrogen to NO_x [69]

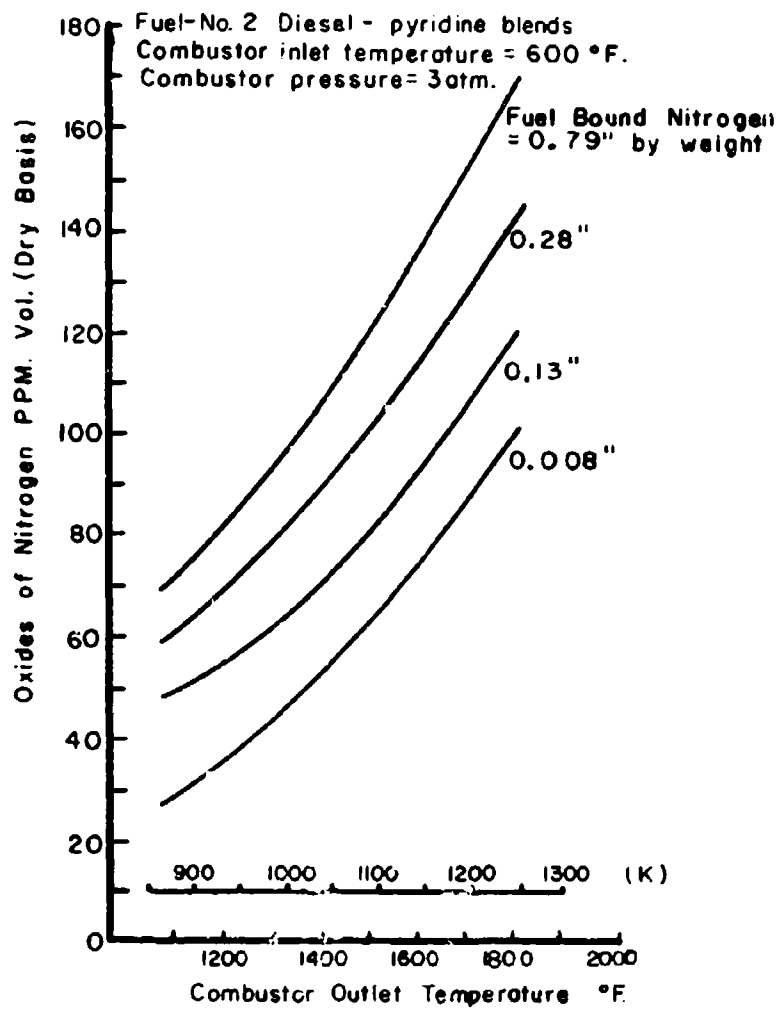
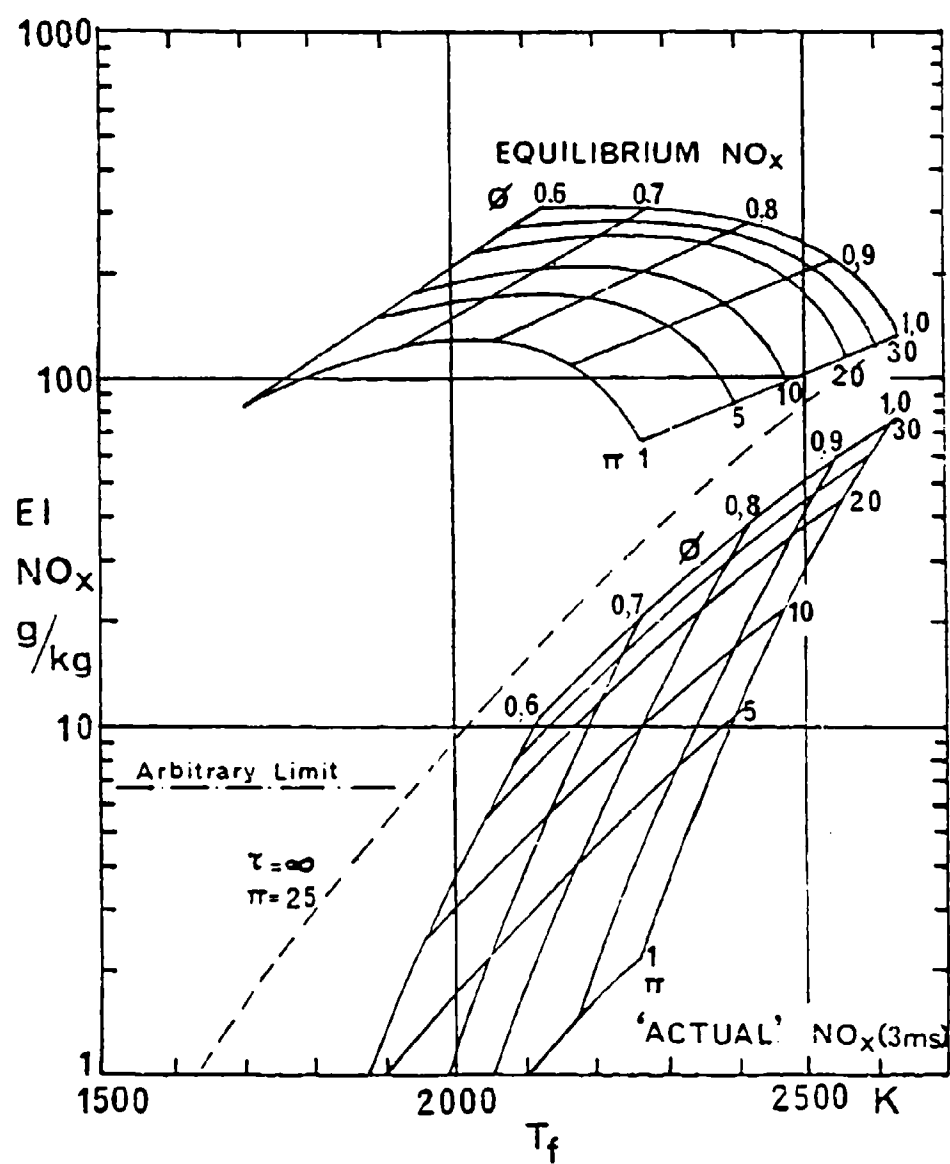


Fig. 57 Effect of fuel-bound nitrogen in emissions of NO_x from a half-scale industrial turbine combustor [70]

Fig.58 Equilibrium and "actual" NO_x

A2.3 COMBUSTOR SPRAY FORMATION, EVAPORATION, IGNITION AND BLOW-OFF

A2.3.1 Scope

The following chapter reviews the effects of variations in fuel viscosity, volatility, surface tension and liquid density on spray formation, evaporation, ignition and blow-off in combustors. Based on results available from literature and using theoretical models it is estimated which quantitative changes are to be expected. The numerical evaluations are based on the properties of today's aviation kerosine JET-A and of Diesel fuel No.2. Furthermore, it is discussed to what extent the fuel injection equipment has to be changed in order to maintain adequate mixture formation and ignition conditions in fuel flexible combustors, and how the changed atomization conditions affect engine operation.

A2.3.2 Spray Formation

A large part of jet engine research has been directed to the above subject in the past decades. However, for the assessment of the influence of fuel properties on atomization work carried out with respect to Diesel-engines and industrial burners is also of great value.

In technical combustion processes the atomization of liquid fuels is carried out in several ways. However, the principle on which these methods are based is nearly the same. A liquid fuel is converted into a thin layer flowing over a solid surface and then detached from it. After separation from the surface, the free sheet of liquid breaks up into ligaments and finally into droplets. The size and distribution of the so-formed droplets depends on the physical properties of the fuel: viscosity, surface tension and density. On the other hand, the atomizer type is of importance, for example, if pressure forces alone are the driving forces for the acceleration of the liquid, or if air is used to atomize the liquid. Fig.1 shows schematic drawings of some atomizer types which are of practical interest.

Since the atomization process is rather complicated, it is usual for engineering purposes to describe the quality of a droplet spray by means of empirical correlations which are derived from experiments. In these correlations, a characteristic droplet diameter, which is measured by a suitable method, is related to those parameters which influence atomization. Normally, the Sauter-mean-diameter (SMD) is used; it is the diameter of that droplet whose volume-to-surface-ratio equals that of the entire spray. In some publications, also a mass-mean-diameter (MMD) is used. The latter is the diameter of that droplet for which the integration of the fuel mass accumulated in all smaller droplets amounts to just 50% of the total mass of the spray.

The types of empirical correlation formulae vary with the individual methods of atomization. They describe the characteristics of a spray with a scatter of roughly $\pm 20 \mu\text{m}$, which due to the statistical nature of the process is inevitable. Basically, they are valid only for the range of experimental conditions from which they have been derived; extrapolation to other conditions may lead to larger errors. In these correlations, the influence of the individual parameters is given as a power law; however, one should bear in mind that the exponents are more or less the results of a mathematical effort to describe the experimental results properly. In the following, the types of the correlation formulae derived for particular kinds of atomizers are listed.

Pressure Swirl Atomizers

For these widely used atomizers, (see Fig.1a) either single- or dual-orifice nozzles, correlations of the following form are given [10, 11, 12, 14, 17]:

$$\text{SMD} = \text{const.} \frac{\nu_f^{0.16 \pm 0.3} \sigma_f^{0 \pm 0.6} \rho_f^{0.205 \pm 0.25} \dot{Q}_f^{0.205 \pm 0.25}}{\Delta P_f^{0.354 \pm 0.458}} \quad (1)$$

The powers of the parameters vary considerably. Some formulae do not contain the surface tension. Sometimes, the density of the fuel ρ_f and the volume flow $\dot{Q}_f$ are represented by the fuel mass flow $\dot{m}_f$, which obscures to some extent the effect of a change of fuel density. Some of the individual formulae found in the literature are given in the Appendix 2.2.

Air-Assist Atomizer

This atomizer type (see Fig.1c) is important for engine combustors. It represents a swirl atomizer, in which a small amount of air is flowing through a shroud surrounding the fuel orifices, thereby improving the quality of the spray. Application of correlations according to Equation (1) was not successful [11]. Instead, a relation developed by Fraser [13] for the break-up of liquid sheets into droplets seems to describe the process of air-assisted atomization adequately.

$$\text{SMD} = \text{const.} \cdot \nu^{0.1} \sqrt{\frac{\sigma_f \cdot s}{\rho_g V_e}} \quad (2)$$

where the thickness s of the fuel sheet is calculated according to Giffen and Muraszew [19]:

$$s = \frac{FN}{D_0 \sqrt{\rho_f} \cdot \cos \alpha/2} \quad (3)$$

The effective gas velocity V_e is assumed to be proportional to the shroud air discharge velocity according to

$$V_e = \text{const} \cdot \left(\frac{\dot{m}_a}{\dot{m}_f} \right)^{0.1} V_a \left[0.5 + \left(\frac{V_f}{V_a} \right)^2 - \left(\frac{V_f}{V_a} \right) \right] \quad (4)$$

Insertion of Equation (3) into (2) together with experimental results yields for the influence of the physical properties of the fuel:

$$\text{SMD} \sim \frac{\nu_f^{0.1} \sigma_f^{0.5}}{\rho_f^{0.15}} \quad (5)$$

Airblast Atomizers

With airblast atomization fuel is injected at relatively low velocity into a high-velocity airstream. This type of spray formation is of increasing importance for aero gas turbines because of the associated benefits with respect to reduction of exhaust emissions and smoke suppression. A well known airblast atomizer of that kind is the pintle-type prefilming atomizer (Fig. 1d) described by Lefebvre et al. [15]. Experimental investigations on the spray formation of airblast atomizers, resulting in correlations between SMD and the influence of the properties of fuel and atomizing air have been carried out by Fraser [13], Rizkalla and Lefebvre [15, 16], Jasuja [14]. Based on Fraser's experiences, all correlations contain two additive terms. This proved necessary if the influence of the individual parameters was to be taken into account adequately.

For prefilming airblast atomizers, a correlation derived by Lefebvre and coworkers [15, 16] is

$$\text{SMD} = A \frac{(\sigma_f \rho_f s)^{0.5}}{\rho_g V_g} \left(1 + \frac{\dot{m}_f}{\dot{m}_g} \right) + B \left(\frac{\eta_f^2}{\sigma_f \rho_g} \right)^{0.425} s^{0.575} \left(1 + \frac{\dot{m}_f}{\dot{m}_g} \right)^2 \quad (6)$$

The first term dominates for liquids of low viscosity, and the second shows less effect of air velocity for fluids which are more viscous. Since the thickness of the liquid sheet s could not be measured it was assumed to be inversely proportional to the diameter of the prefilmer, thus replacing s by $(1/D)$. In the above mentioned work the constants A and B are given for water and kerosine ($A = 6.5 \cdot 10^{-4}$; $B = 1.2 \cdot 10^{-4}$). Jasuja investigated the prefilming airblast atomizer for kerosine, gas oil, residual fuel oil and blends of the latter two [14]. He circumvents the problem of the unknown thickness of the fuel film by including it into the constants A and B , thereby sacrificing the non-dimensionality of the constants. Also, the influence of $(1 + \dot{m}_f/\dot{m}_g)$ is represented in a slightly different form. Jasuja's formula reads:

$$\text{SMD} = \left[1.10^{-3} \frac{(\sigma_f \rho_f)^{0.5}}{\rho_g V_g} + 0.6 \cdot 10^{-4} \left(\frac{\eta_f}{\sigma_f \rho_g} \right)^{0.425} \right] \left(1 + \frac{\dot{m}_f}{\dot{m}_g} \right)^{0.5} \quad (6a)$$

Plain Jet Airblast Atomizers

In this type (see Fig. 1e) a single fuel jet discharging from an orifice (i.e. a tube) is surrounded by a swirling airflow [11, 14]. For the heavier fuels tested by Jasuja, the following correlation is given [14].

$$\text{SMD} = A \cdot \left(\frac{\sigma_f}{\rho_f} \right)^{0.35} \frac{1}{\rho_g^{0.35} V_g} \left(1 + \frac{\dot{m}_f}{\dot{m}_g} \right)^{0.25} + B \eta_f \left(\frac{D}{\rho_f \sigma_f} \right)^{0.5} \left(1 + \frac{\dot{m}_f}{\dot{m}_g} \right) \quad (7)$$

where D is the diameter of the fuel orifice. The constants are $A = 0.19$ and $B = 0.127$.

Pneumatic Impact Atomizer

This type of atomizer has been studied by Hunter et al. [11]; in principle it comes close to "vaporizing" atomizers, as they are used in some engine combustors. In this type, fuel is injected into an airstream following through a venturi and impinges on a solid surface normal to the flow. The fuel film formed on that surface is atomized at the edge of the solid surface by the action of the shear forces of the airflow.

The correlation used by Hunter et al. [11] for the pneumatic impact atomizer is

$$\text{SMD} = A + B \frac{\sigma_f^{0.5} \rho_f^{0.21} \nu_f^{0.21}}{\rho_g^{0.3}} \left(1 + 0.065 \frac{\dot{m}_f}{\dot{m}_g} \right) f(s, V) \quad (8)$$

where $f(s, V)$ is a function of fuel sheet thickness s and of fuel and air velocities. The figures for A and B are $A = 1$ and $B = 20.036$.

A2.3.3 Influence of Fuel Properties on Droplet Size

Viscosity

The influence of increasing kinematic viscosity is, in general, to increase the mean droplet diameter. The rate at which the SMD is increased by increasing viscosity, however, depends on how the fuel sheet is disintegrated. For pressure atomizers the exponent of ν_f is between 0.1 and 0.3, with a tendency to 0.2–0.3 being the more probable values. Evaluating the tendency for the atomization of JET A1 ($\nu_f = 2$ cS) and a Diesel Fuel No.2 ($\nu_f = 3$ cS), by means of the same pressure swirl atomizer (according to Equation (A2)) yields for a fuel temperature of 293 K a ratio of the SMDs of

$$\frac{SMD_{DF}}{SMD_{JET A}} \approx 1.103 \div 1.157. \quad (9)$$

Actual measurements by Opdyke [17] for these two fuels resulted in

$$\frac{SMD_{DF}}{SMD_{JET A}} \approx 1.13. \quad (10)$$

Likewise, a change from JET A1 to Aviation Gasoline with a kinematic viscosity of 0.67 cSt at 293 K produced a reduction of the SMD by 22%.

If a research fuel were defined, which has a kinematic viscosity of 12 cSt at 250 K [4] (corresponding to ≈ 3 cS at 293 K), the atomization of this fuel in a pressure swirl atomizer would result in droplet sizes which are roughly 10% larger than with JET A1. If the corresponding deterioration of fuel preparation and combustion characteristics is to be avoided, the viscosity effect on droplet size can only be compensated for by an increase of the atomizer pressure difference Δp_f of approximately 30%.

The variation of the kinematic viscosity with temperature is shown in Fig.2, which is taken from [1]. Since this is an exponential influence of temperature, the more viscous fuel will exhibit the larger increase in mean droplet size. This effect is illustrated by Fig.3 in which the change of SMD for four different fuels (using Hunter's formula (A2) for pressure atomizers) is plotted if the temperature of the fuel decreases from 288 K to 253 K. The fuels are displayed according to their viscosity at 288 K. A broad specification fuel with a viscosity of slightly below 3 cSt at 288 K would thus exhibit an increase in SMD of approximately 35%. Fuel temperatures of approximately 250 K could be encountered during altitude flight. Therefore one has to conclude that with the envisaged broad specification fuels and using conventional pressure swirl atomizers, altitude flight problems would be heavily aggravated unless special precautions are taken.

In airblast atomization a similar influence of viscosity is observed. The viscosity enters in the second term of the empirical formula only. Numerical evaluation shows that this second term is relatively small compared to the first term; hence the influence of fuel viscosity on the airblast atomization process seems to be smaller than with pressure swirl atomization. Experimental results obtained by Lefebvre are shown in Fig.4 [9].

A numerical comparison for JET A fuel and Diesel fuel No.2 at 288 K and 253 K temperature has been made for an air density $\rho_g = 1.35$ kg/m³, an air velocity of 100 m/s and an air-fuel ratio of 4:1, using Equation (6) with constants given by Jasuja, and the given data on viscosity and surface tension, specified above, showing the following results. Using DF2 instead of JET A in the same airblast atomizer yields an SMD-ratio:

$$\frac{SMD_{DF}}{SMD_{JET A}} \approx 1.06 \quad \text{for} \quad T_f = 288 \text{ K} \quad (11)$$

and

$$\frac{SMD_{DF}}{SMD_{JET A}} \approx 1.11 \quad \text{for} \quad T_f = 253 \text{ K}. \quad (12)$$

Using DF2 at 288 and 253 K and the same conditions as above,

$$\frac{SMD_{253K}}{SMD_{288K}} \approx 1.15 \quad (13)$$

and the same for JET A

$$\frac{SMD_{253K}}{SMD_{288K}} \approx 1.10. \quad (14)$$

This numerical comparison shows that with airblast atomization the use of a more viscous fuel results in a more moderate increase in mean droplet size, which is somewhat larger at the lower temperatures. If for a particular fuel the temperature and hence the viscosity is changed, this results in somewhat larger increases in drop size, amounting to 15% for DF2 and 10% for JET A, for temperatures of 288 K and 253 K. For these particular conditions average drop sizes are on the order of 40 μm , according to the correlations given above.

Thus, it can be concluded that with airblast atomization the influence of viscosity on spray formation is somewhat smaller than with pressure swirl atomization. Since, however, evaporation characteristics strongly depend on spray characteristics, the influence of viscosity has to be taken into account regardless of the type of atomizer used. In particular, this is important for altitude relight. Airblast atomizers should be better suited for use with more viscous, higher boiling broad specification fuels, since they seem to be able to produce smaller SMDs than pressure swirl atomizers.

A similar result has been achieved by Lefebvre, Mellor and Peters [21], who calculated the ratio of average drop size (SMD) for a range of different jet fuels manufactured from shale oils, tar sands and coal syncrudes and aviation kerosine. The jet fuels made from syncrudes had, on the average, much higher viscosities than aviation kerosine. Consequently, the SMD for conventional pressure swirl atomization increased by up to a factor of 2.2. With airblast atomizers the SMD ratio compared with kerosine is markedly lower. This shows again that airblast atomizers are better suited to atomize the higher boiling, more viscous fuels.

Surface Tension

According to the correlations for SMD given above the surface tension should have an effect on average drop size. However, there are only a few papers on spray formation in which actual figures for the surface tension of the fuels used are given, e.g. [14 and 17]. Such values for the surface tension of several hydrocarbon fuels are plotted against their temperatures in Fig. 5. It can be seen that the absolute values of the most important fuels lie in a rather narrow range, at 310 K between 0.022 and 0.025 N/m. However, there is a pronounced increase of surface tension if the liquid temperature is decreased.

As a consequence of the narrow range of the absolute values of the surface tension, one can conclude that besides the influence of viscosity and density, no major change of the average drop size in a spray should be expected if the fuel type is changed. If the available correlations predict the effects of surface tension on spray formation correctly, then surface tension alone would cause an increase of SMD by 6–8% if a JP4 fuel is replaced by DF2. This should be valid as long as the temperature does not change. This limited influence on drop size of the surface tension is also illustrated by Fig. 6, which originates from experiments of Lefebvre [9], carried out with airblast atomizers.

The influence of surface tension on average drop size should however be larger if the fuel temperature is changed. For a decrease of the fuel temperature by 100 K (which could be easily the case, when restarting an engine after inflight shutdown), the available correlation predicts an increase of the average drop size by approximately 26–32% (estimated for JP4 fuel). Since the temperature dependence of the surface tension is nearly the same for all fuels one could expect a similar behaviour for a future broad specification fuel.

However there is some doubt whether in all experiments reported in the literature the effect of surface tension has been considered adequately. For example, Hunter's formula for the Sauter mean diameter of a spray generated by pressure swirl nozzles [11], does not take into account the surface tension explicitly. If the effect of viscosity were studied experimentally by changing the temperature of the fuel, this would mean that surface tension is changed, too; hence this latter effect on average drop size may eventually be hidden in the effect of viscosity.

For an assessment of the combined effect of surface tension and viscosity one could assume the worst case, which is that both effects are superimposed. For a possible broad specification fuel with its somewhat larger viscosity a decrease of fuel temperature by 100 K would correspond to an increase of the Sauter mean diameter by a factor of 2–2.1, using Hunter's formula for a swirl nozzle. This represents a considerable deterioration in spray quality, which in case of altitude relight can cause severe difficulties with respect to ignition characteristics.

In order to find out whether this combined effect of viscosity and surface tension is actually observed, new experiments with different grades of fuels seem appropriate, in which surface tension should be varied independent of viscosity, for example by use of suitable additives which influence surface tension only.

Liquid Density

The effect of an increase in liquid density is thought to consist of a somewhat retarded break-up of the liquid sheet, where it is subject to lower relative velocities of fuel and air [9]. This should increase the average drop size, (see Equations (1) and (8)). On the other hand, it is reported that, in some cases increased liquid density results in a reduced thickness of the liquid film before atomization, which then results in smaller droplet sizes. This latter seems to be true for air-assist atomizers and plain jet atomizers (see Equations (5) and (7)) for which

$$\text{SMD} \sim \frac{1}{\rho_f^{0.15+0.5}} \quad (15)$$

For other types of fuel injectors the correlation formulae predict

$$\text{SMD} \sim \frac{1}{\rho_f^{0.105 + 0.25}} \quad (16)$$

In total, however, the influence of liquid density on spray quality is rather small. A change of ρ_f from 0.8 to 0.85 would yield a corresponding increase of droplet size of approximately 1–3%.

Droplet Size Distribution

Based on the work of Simmons [48, 49], it appears that the volume fraction and drop size distributions for most well developed atomizers are known once the Sauter (or mass) median diameter is measured. That means, drop size distributions from a given nozzle design are not affected by changes of fuel viscosity. This conclusion results from both optical and wax droplet tests performed over a wide fluid viscosity range (1.2 to 19 centistokes) with over one hundred differing injector designs, including swirl and dual-orifice pressure and airblast and air-assist nozzles. As with most studies to date, the experiments were limited to injection into ambient atmospheric air. One research need is to establish if similar results obtain under gas turbine combustor working conditions.

A2.3.4 Influence of Fuel Properties on Evaporation

Combustion processes can be characterized by considering time scales for the most important processes. For the combustion of liquid fuel sprays the important processes are mixture formation by fuel atomization and evaporation, turbulent mixing and chemical reaction. In the present case, it is the characteristic time of droplet evaporation which is of interest. For example, Mellor [25] has shown that this time is very useful in correlating emissions data obtained from combustor investigations. Another example of the relative importance of the evaporation time is reported by El Wakil et al. [22], who showed that in Diesel engines the so-called physical part of the total ignition lag which is caused by atomization and evaporation of liquid fuel droplets, can amount to 50% or more of the total ignition lag.

In most practical liquid fuel sprays the number of droplets and their separation distance can be such that the evaporating droplets influence each other; they can evaporate as a droplet cloud. However, in order to facilitate an assessment of the influence of the physical fuel properties on evaporation times it is assumed that it would be sufficient to consider single droplet evaporation only. Normally, liquid fuel is injected at a lower temperature than prevailing in the combustor. Therefore the total evaporation time can be divided into a heating-up time and an evaporation time. During the first time period, the droplet temperature rises until a condition is reached where the total amount of heat supplied to the droplet by convection is entirely used for the evaporation; under this condition, the temperature of the liquid remains constant (wet-bulb temperature). For a pure substance this temperature is lower than the boiling temperature at the given pressure. In case of a diffusion flame connected with the droplet, the radiative heat transfer to the droplet can raise its temperature above the wet-bulb value.

Heating Time of Droplets

If evaporation is neglected during that period, a simple heat balance for a single droplet yields for the temperature rise with time

$$\frac{dT}{dt} = 6 \cdot \frac{k}{\rho_f \cdot C_{pg}} \frac{Nu}{d^2} (T_g - T) \quad (17)$$

Integration with respect to time yields

$$t = \ln \left(\frac{T_g - T_{f,0}}{T_g - T_f} \right) \frac{\rho_f d^2 C_{pg}}{6 k Nu} \quad (18)$$

where for heat transfer under forced convection conditions

$$Nu = 2 + 0.6 Re^{1/2} \cdot Sc^{1/3} \quad (19)$$

or if $Sc \approx 0.77$ is assumed:

$$Nu = 2(1 + 0.266 Re^{1/2}) \quad (20)$$

Equation (18) shows that the heating-time of a drop increases with the square of the drop diameter. Using the same atomizer conditions, fuels having viscosities higher than that of JET A generate sprays with a higher SMD. The preceding chapter showed that differences in the order of 10 to 15% may be expected. Therefore, the heating time of more viscous fuels would be 20 to 30% higher than that of JET A. Moreover, for a given gas temperature T_g and initial fuel temperature $T_{f,0}$, an increase in 10% evaporated-temperature results in an additional increase of the heating time. Thus raising this temperature from an assumed value of 200°C to 225°C can prolong the heating-time by another 15%. How much this change affects the total evaporation time depends in part on the droplet size distribution, the large droplets suffering more than the smaller ones.

It should however be noted that this volatility effect diminishes the higher the gas temperature in the combustor, e.g. under working conditions. Therefore, the effect of the increased heating time becomes more serious when the combustor inlet temperature is lowered, for example under high altitude conditions. It will be heavily felt during relight conditions, where gas temperature in the combustor is close to ambient temperature.

Evaporation Time

It is well established that, after having reached a constant temperature, droplets in a stagnant environment evaporate according to the d^2 -law:

$$d^2 = d_0^2 - \beta t \quad (21)$$

β being the evaporation constant. Evaluation of the heat and mass balance of quasi-steady-state evaporation of a single droplet under conditions of forced convection shows that

$$\beta = \frac{8k}{\mu_f C_{pg}} \ln(1+B) (1 + 0.276 Re^{1/2} \cdot Sc^{1/3}) \quad (22)$$

where B represents the mass transfer number

$$B = \frac{C_{pg}(T_g - T_f)}{L} \quad (23)$$

Together with the liquid density B reflects the influence of the physical properties of the fuel. T_f represents the temperature of the evaporating droplet. L consists of the enthalpy increase of the liquid droplet plus the heat of vaporization.

Spalding [18, 60] has examined the influence of fuel properties on the value of B , assuming that T_f , the liquid temperature at the droplet surface, approximates the boiling temperature of the liquid, and including in the denominator also the amount of heat necessary to heat up the droplet to the temperature T_f . His results for an evaporating and burning droplet, based on a fuel injection temperature of 288 K, are shown in Fig. 7. The approximate boiling ranges of the fuels JP4, JET A and DF2 have been superimposed showing the order of magnitude of the changes of the transfer number B if one goes to higher boiling fuels.

Characteristic Evaporation Time

From the d^2 -law for evaporation of single droplets a characteristic evaporation time can be deduced as follows [25, 21]:

$$\tau_{eb} = \frac{d_0^2}{\beta} \quad (24)$$

If for a given spray the Sauter mean diameter (SMD) is introduced as initial droplet diameter d_0 , it is possible to estimate the combined effects of injector characteristics, fuel viscosity and volatility.

$$\tau_{eb} = \frac{SMD^2}{\beta} \quad (25)$$

A relevant study on the effect of the fuel properties has been performed by Lefebvre, Mellor and Peters [21] for a spray formed by a simplex pressure atomizer. The combustor conditions are given in [21], and fuel viscosity and 10%-evaporated temperature have been varied. The results are given in Fig. 8, which is taken from [21]. It shows lines of constant evaporation times, which for a given fuel viscosity, indicate the large influence on the evaporation time of the 10%-evaporated temperature of the fuel. The respective τ_{eb} values for 4 available hydrocarbon fuels are also entered showing that the use of Diesel-type fuels in gas turbine combustors instead of JET A increases the average evaporation time by more than a factor of 2, if no special precautions are taken to adapt the atomizer.

In a theoretical study concerning the physical ignition lag under Diesel engine conditions [26] a similar relationship has been derived, the characteristic time being proportional to $d^{1.77}$, where the proportionality factor depends on temperature.

The above is valid if the droplets in the spray were all of the same size. In practice, the influence of the fuel properties on evaporation time is more critical since in a real spray the size distribution of the droplets has to be taken into account. Calculations performed by Boysen and Swithenbank [45], and Chidananda, Kayser and Eickhoff [27] show that the small droplets evaporate first, resulting in an increase of SMD during the evaporation period. The increase in SMD depends on the droplet size distribution; it is the smaller, the narrower the range of droplet sizes in the spray is, or, in view of the results of Simmons [48, 49], the smaller the initial SMD.

A2.3.5 Control of Evaporation Times by Adaption of Atomizing Conditions and Equipment

As outlined above, the atomization quality and the evaporation characteristic deteriorate if a higher boiling fuel is used with existing atomizing equipment. They can be restored if the parameters governing the atomization and evaporation are adapted accordingly. The necessary change of these parameters is discussed in the following.

Similarity of the evaporation process of fuels with differing properties demands that the characteristic evaporation times are kept constant. According to Equation (25), this means that

$$\tau_{eb} = \frac{SMD^2}{\beta} = \text{const.}$$

Therefore, if a less volatile fuel is to be used in a gas turbine combustor instead of JET A, the Sauter mean diameter of the fuel spray has to be decreased accordingly.

The dependence of the characteristic evaporation time τ_{eb} on the droplet diameter can be estimated considering limiting cases. If there is a strong convective effect, e.g. $Re_d^{1/2} \gg 1$; then ($Re_d \sim SMD$):

$$\tau_{eb} \sim \frac{SMD^2}{1/\rho_f \ln(1+B) SMD^{1/2}} \sim \frac{SMD^{3/2}}{1/\rho_f \ln(1+B)} \quad (26a)$$

If the droplet relative velocity approaches zero (stagnant conditions),

$$\tau_{eb} \sim \frac{SMD^2}{1/\rho_f \ln(1+B)} \quad (26b)$$

The actual changes of atomizer operation parameters which are necessary in order to keep constant the evaporation time, can be evaluated by inserting into the above equation the empirical correlations for the individual atomizers. The physical properties of a possible broad specification fuel most probably lie between those for the current JET A fuel and Diesel fuel No.2. It is therefore appropriate to use JET A as a reference and to estimate upper limit values by using the properties of Diesel fuel No.2. Based on these assumptions, Equation (26a) yields

$$\frac{SMD_{DF}}{SMD_{JET A}} \sim \left(\frac{\rho_{JET A} \cdot \ln(1+B_{DF})}{\rho_{DF} \cdot \ln(1+B_{JET A})} \right)^{2/3}$$

From Equation (26b)

$$\frac{SMD_{DF}}{SMD_{JET A}} \sim \left(\frac{\rho_{JET A} \ln(1+B_{DF})}{\rho_{DF} \ln(1+B_{JET A})} \right)^{1/2}$$

The evaluation of the mass transfer number B , according to Equation (23), was based on the 10%-evaporated temperature as suggested in [21], furthermore, the heat of vaporization was calculated according to Maxwell [58], the temperature of the surrounding gas was assumed to be 1173 K. Taking approximately constant fuel densities, 0.85 kg/dm³ for DF2 and 0.81 for JET A, the ratio of the Sauter mean diameters necessary to obtain constant evaporation times, becomes for the two limiting cases:

$$\frac{SMD_{DF}}{SMD_{JET A}} \approx 0.80 \div 0.84$$

This relation can be evaluated using the empirical SMD-correlations for the specific atomizer types. In the following this will be done using the lower value for strong convective effects, since it is that case according to which the spray characteristics have to be selected.

Pressure Swirl Atomizers

For pressure swirl atomizers, the pressure drop across the nozzle fixes the droplet size once the fuel and the fuel flow or the flow number of the nozzle is specified. Using Equation A2 of the Appendix, the change in ΔP_f necessary for maintaining a specified evaporation time is given by

$$\frac{SMD_{DF}}{SMD_{JET A}} = 0.80 = \left(\frac{\nu_{DF}}{\nu_{JET A}} \right)^{0.3} \left(\frac{\rho_{DF}}{\rho_{JET A}} \right)^{0.205} \left(\frac{\Delta P_{fDF}}{\Delta P_{fJET A}} \right)^{-0.354}$$

or

$$\frac{\Delta P_{fDF}}{\Delta P_{fJET A}} = 1.87 \left(\frac{\nu_{DF}}{\nu_{JET A}} \right)^{0.847} \left(\frac{\rho_{DF}}{\rho_{JET A}} \right)^{0.579}$$

Assuming an average operating fuel temperature of 353 K the respective viscosities are $\nu_{DF} \approx 1.25 \text{ cSt}$ and $\nu_{JET A} \approx 0.76 \text{ cSt}$, this yields

$$\frac{\Delta P_{rDF}}{\Delta P_{rJET A}} \approx 2.80.$$

Using the temperature dependence of the kinematic viscosity given by Fig. 2, the same estimation yields for a cold start temperature of 263 K

$$\frac{\Delta P_{rDF}}{\Delta P_{rJET A}} \approx 3.88.$$

This means that with Diesel fuel No. 2 the maximum fuel pressure has to be increased by approximately 180% at normal operating conditions. If one assumes that the viscosity of a possible broad specification fuel will lie in the middle between DF2 and JET A, an increase of the maximum fuel pressure on the order of 80-90% will still be necessary. As a consequence, the power input into the fuel pressure pump will increase considerably, as well as the weight of the HP-fuel piping.

An increase of the pressure difference across the nozzle at constant fuel flow means also that the flow number of the fuel nozzle must be smaller than that for JET A. Hence, a change of the fuel type would be accompanied by a change of the nozzle size. Viewing the future fuel situation realistically it seems very probable that an engine has to be operated with several fuel types; that is, multifuel capability must be provided. Therefore, if the combustion should not deteriorate, nozzles will have to be used which allow changes of fuel flow without a major change of the atomization quality.

This requirement can be satisfied with the well known spill nozzle or similar types. Albeit there are only few data on the quality of sprays produced by spill nozzles it is known that the Sauter mean diameter is described by a formula similar to those for conventional swirl nozzles. Furthermore, the Sauter mean diameter has been found to decrease if fuel is spilled back from a spill nozzle. This indicates that a matching of drop sizes to changing fuel properties should be possible with this kind of atomizer.

It must be added that fuel droplets generated by pressure atomizers enter the combustion zone with considerable velocities relative to the surrounding gas flow. Fuel distribution is, therefore, influenced by the interaction of droplet size, evaporation rate and droplet drag. If evaporation times are to be matched to the fuel properties via droplet size the penetration distance of the fuel droplets, and hence fuel distribution will change. An estimation of this effect for JET A - droplets of 70 μm initial diameter and Diesel No. 2 - droplets of 57 μm initial diameter shows that, assuming an initial relative velocity of 50 m/s, (gas conditions 4 bar, 1100 K) the penetration distance of the smaller Diesel - No. 2 - droplet is 10% less than that of the kerosene droplet. This change can probably be tolerated; however, it cannot be excluded that difficulties occur under special circumstances (e.g. higher fuel-air ratios, low temperatures, etc.).

Air-Assist Nozzles

According to Equations (2) to (5) above, the main parameters which influence atomization with these nozzles are viscosity, surface-tension, fuel density and the velocity of the atomizing air. The latter depends on the pressure drop across the atomizer. The empirical relationship to be introduced into Equation (29) is then:

$$SMD \sim \frac{\nu_f^{0.1} \sigma_f^{0.5}}{\rho_f^{0.15} \Delta P_N^{0.5}} \quad (27)$$

Constant evaporation time with a higher boiling fuel therefore demands an increase of the atomizer pressure drop ΔP_N according to:

$$\frac{\Delta P_{NDF}}{\Delta P_{NJETA}} \approx 1.56 \left(\frac{\nu_{DF}}{\nu_{JET A}} \right)^{0.2} \frac{\sigma_{DF}}{\sigma_{JET A}} \left(\frac{\rho_{JET A}}{\rho_{DF}} \right)^{0.3}$$

For an operating temperature of 353 K:

$$\frac{\Delta P_{NDF}}{\Delta P_{NJETA}} \approx 1.82$$

and for 263 K:

$$\frac{\Delta P_{NDF}}{\Delta P_{NJETA}} \approx 1.94.$$

The numerical comparison shows that, as an upper limit, the pressure drop across the atomizer has to be increased by 80 to 90%. If a broad specification fuel with properties between those of JET A and DF2 is used, the increase of the pressure drop still amounts to roughly 40-45%.

The basic form of the spray correlation formula, Equation (2), suggests that the thickness of the fuel sheet, s , could be used for matching droplet sizes to a particular fuel. One possibility for achieving this could be the use of spill-type nozzles. At present, it seems as if the open literature does not provide information about air-assist spill-type nozzles. Therefore, it is recommended that research is carried out in order to examine the characteristics of this atomizer design.

Airblast Atomizers

In this case an estimation of the necessary change of atomizing conditions suffers from the more complicated form of the empirical relationship, Equations (6) and (7). A rough check is possible, assuming that the contribution of the second term (which amounts up to approximately 10%) can be neglected. In that case the empirical relationship to be introduced into Equation (26) is

$$SMD \sim \frac{(\rho_f \sigma_f)^{0.5}}{(\Delta P_{FT})^{0.5}} \left(1 + \frac{\dot{m}_f}{\dot{m}_g} \right) \quad (28)$$

As for air-assist nozzles, the atomizing air velocity was assumed proportional to the pressure drop across the flame tube ΔP_{FT} . The increase of this pressure drop necessary for maintaining a constant evaporation time is then:

$$\frac{\Delta P_{FT DF}}{\Delta P_{FT JET A}} \approx 1.56 \frac{(\rho_f \sigma_f)_{DF}}{(\rho_f \sigma_f)_{JET A}} \frac{(1 + \dot{m}_f/\dot{m}_g)_{DF}}{(1 + \dot{m}_f/\dot{m}_g)_{JET A}}$$

With the assumption that an increased air velocity increases the air/fuel ratio $\dot{m}_g/\dot{m}_f$ correspondingly, the estimation yields for an operating temperature of 353 K

$$\frac{\Delta P_{FT DF}}{\Delta P_{FT JET A}} \approx 1.60 (1.67)$$

and for an operating temperature of 263 K

$$\frac{\Delta P_{FT DF}}{\Delta P_{FT JET A}} \approx 1.55 (1.61)$$

The figures in brackets result, if the second term of Equation (6) is taken into account by a suitable numerical factor. The estimation shows that in order to retain the same evaporation characteristics, the pressure drop across the flame tube has to be increased by approximately 50%. In case of a realistic fuel with properties between those of JET A and DF2 one must still reckon with an increase of the pressure drop by some 25 to 30%. This, of course, has a deleterious effect on the specific fuel consumption of the engine.

In principle, another possibility for decreasing the Sauter mean diameter is the change of the fuel sheet thickness s (Eq. (6)). An experimental investigation of Rizk and Lefebvre [59] shows that the Sauter mean diameter increases proportional to s . However, it seems rather difficult to make use of this relationship. Specific investigations are necessary to explore this possibility, by making use of a spill-type fuel supply to the prefilming device.

Considering the multi-fuel capability of an engine, there does not seem to exist a simple way of adjusting the pressure loss of the flame tube dome to the specific atomization characteristics of the fuel. A variable geometry device in the air flow path around the fuel nozzle would be necessary, the safe operation of which in the vicinity of a high temperature region would cause difficulties.

Again, it seems necessary that research work be carried out with the aim of adapting airblast atomizers, either prefilming to plain jet atomizers, to a certain range of fuel qualities without disturbing the evaporation characteristics of the resulting sprays.

The above estimation leaves several open questions. Since the properties of a future broad specification fuel will lie between those of JET A and DF2, the necessary adjustments of the atomization conditions will be as dramatic as outlined above. This raises a first question, namely to what extent a deterioration of the spray and mixture formation can be tolerated without influencing the combustion behaviour too much. The answer can be given by relevant combustor experiments only. Secondly, multi-fuel capability of fuel injectors will be asked for in the near future. Therefore, appropriate injector designs must be developed and investigated under realistic conditions. Spill-type nozzles for fuel supply systems are good candidates and should therefore be investigated together with assistance of atomizing air. Attention must be paid also to the question whether airblast atomizers with multifuel capability can be developed. Thus, a wide field of activity is left to creative engineers.

A2.3.6 Influence of Fuel Properties on Ignition

The ignition of a liquid fuel spray in a gas turbine combustor is a very complicated process. It cannot be investigated easily since it is difficult to measure in the immediate ignition region without disturbing it. Due to the general measuring difficulties most ignition studies describe the ignition characteristics in terms of some suitable global parameters, like ignition energy, overall fuel-air ratio, time to ignition, flow velocity, etc.

From basic knowledge it follows that ignition of a combustible mixture takes place in the gas phase; hence, a liquid fuel has to be evaporated prior to ignition. On the other hand, ignition depends on the gas phase fuel-air ratio in the ignition region, and on the amount of energy supplied to that region by means of a suitable igniter device. The gas phase fuel-air ratio in the ignition region is the result of the evaporation process in which the volatility of the fuel on one hand and the spray characteristics on the other hand are the dominating factors. Therefore, the influence of the fuel properties on ignition has to be discussed in terms of volatility and spray characteristics.

Survey of Existing Data

The volatility of a practical fuel, consisting of a large number of compounds, can be characterized by the boiling curve. Another characterizing quantity is the Reid vapour pressure, which is the vapour pressure measured at 311 K, when only a small fraction of the fuel is vaporized. Reid vapour pressure for aviation gasoline is 0.42 bar, and 0.175 bar for JP4 fuel. For JET A and other higher boiling fuels Reid vapour pressure is too small to be measured accurately. The volatilities of those fuels are often characterized by the flash point [1, 2].

However, the latter two quantities are not very well suited to characterize the ignition behaviour of different grades of fuels. This was demonstrated by Foster and Straight [34], who carried out ignition experiments in a combustor under sea level and altitude conditions. They used 6 different fuels of the JP3-class, with initial boiling points between 310 K and 473 K and with Reid vapour pressures of 0.49, 0.21 and 0.07 bar. The spark energies necessary for ignition of the various fuels at different temperatures could not be correlated in terms of Reid vapour pressure; instead, the ASTM-15%-evaporated temperature could be used for that purpose, both at sea level and altitude conditions.

Previous experiments by Rayle and Douglass [32] concerning wide cut and narrow-cut jet engine fuels produced similar results however indicating that ASTM-10 %-evaporated temperature would be best suited for characterizing the ignition behaviour of the fuels.

Replotting the results of Foster and Straight, as was done by Lefebvre, Mellor and Peters [21], shows that the 10%-evaporated temperature indeed yields the better correlation with ignition energy, Fig.9. As can be seen from this figure, the data for JP4 fall below the correlating curves. Lefebvre et al. attribute this fact to the lower viscosity of JP4 (0.67 cS at 293 K), whereas the viscosities of the other fuels used were between 1 and 1.3 cS. Thus, viscosity should play a role in ignition behaviour, influencing the mean droplet size of the fuel spray. In the preceding chapter it was shown that the evaporation time, necessary for the formation of an ignitable mixture, depends on the square of the Sauter mean diameter (SMD). Thus, the less volatile fuels which in a given time form a less fuel-rich mixture need the larger spark energies for their ignition.

Extensive investigations of the influence of fuel properties on ignition have been carried out by Moses [30] and Moses and Nægeli [31], using a T63 combustor. Two series of experiments have been performed; in the first JP4, JET A and DF2 have been investigated, as well as blends of the latter two with 10 and 20% pentane in order to achieve vapour pressure comparable to that of JP4 [30]. The second series of experiments investigated JET A, JP5, synthetic JP5 made from oil shale, tar sands and coal, Diesel fuel DF2, marine Diesel DFM1 and 2, and leaded gasoline, as well as blends of DFM with JP5 and leaded gasoline [31]. However, since the two series of experiments were carried out at different air mass flows a direct comparison of all fuels and blends is not possible. In order to characterize the ignition behaviour the overall fuel-air ratio was measured at the onset of combustion. It was arbitrarily defined that ignition had to take place within 30 seconds; times longer than 30 seconds were defined as no ignition.

The results of the first test series [30] showed that JP4 offered the lowest fuel-air ratio at the lean ignition limit, and hence is most easily ignited. With JET A ignition became more difficult shifting the lean ignition limit from a f.a.r. of 0.25 to 0.7. Under the air-loading conditions chosen, DF2 could not be ignited at all. A blend of JET A with 10% pentane showed improved ignition behaviour; however, despite the vapour pressure matching that of JP4, the lean ignition limit of the blend was found to be at a higher f.a.r. than JP4. Tests with DF2 plus 10 and 20% pentane showed similar results. From this it was concluded that vapour pressure is not sufficient to characterize the ignitability of a fuel.

The data have also been examined in the light of the influence on ignition limits of droplet size. As, however, the droplet size increases only by up to 30%, when JET A and DF2 are used instead of JP4, it was felt that the measured spread in ignition characteristics could not be explained in terms of droplet sizes. Fig.10 shows the same ignition characteristics in terms of fuel-air-ratio at the lean ignition limit versus 25%-evaporated temperature. It is taken from Lefebvre et al. [21] who replotted Moses' results. The 25%-evaporated temperature was chosen in order to include also the blended fuels.

With the JP5-based experiments [31] which were performed at a lower air loading, it was found that all JP5 fuels showed no differences in lean ignition limit, regardless of the nature of crude the fuels were made from. Again, the measured fuel-air ratio at the lean ignition limit had to be increased with decreasing volatility of the fuel. Leaded gasoline showed the lowest f.a.r. of 0.02, followed by DFM2 blended with 30% gasoline. The mixtures of JP5 with 10, 20 and 40% DFM1 were found to ignite around a f.a.r. of 0.4 - 0.45, whereas the two marine Diesel fuels required the highest f.a.r. of approximately 0.55. Moses et al. explain these results in terms of "front-end-volatility" and viscosity (via droplet size) of the fuel. All experiments of Moses et al. have been performed at ambient pressure conditions or slightly above. Whether the results are valid also for high-altitude conditions has yet to be shown, although Ballal and Lefebvre [37] have addressed this area.

Körber [35] has carried out experiments on ignition of fuel sprays injected into a recirculation zone generated by a disc flame holder subjected to an air stream. For the correlation of ignition characteristics he used the maximum air velocity to which the flame holder was exposed. At this condition the fuel-lean and the fuel-rich branches of the ignition limits merge. The investigation comprised JP4, JP1, JP5, two research fuels with boiling ranges between 471 and 502, and 463 and 521 K, as well as several pure hydrocarbons. According to his results the ignitability decreases with increasing 10% evaporated temperature. Fuel preparation and hence ignitability improves at lower pressures, because the ambient pressure approaches the vapour pressure.

However, if at the lower pressure of 267 mbar, the temperature of the air flow was increased (500 K), the ignition behaviour is reversed, that is: a lower 10%-temperature yields worse ignitability. Körber explains this result by assuming that the ignitability is largely determined by the lower-boiling fractions of the fuel. If however the pressure is decreased and the temperature increased, evaporation may be accelerated to such an extent that adequate mixing is prevented and an over-rich mixture is formed. Körber found similar trends if he used the vapour pressure measured at 400 K and the flash-point as correlating parameters. He found no pronounced differences in the ignitability between pure aromatic and paraffinic compounds. However, in the cases of cyclic hydrocarbons (1-methyl naphthalin, tetralin and decalin), ignition characteristics improved with decreasing number of double bonds.

As already mentioned, ignition is also influenced by the spray characteristics, i.e. average droplet size and distribution. The effect of droplet size on ignition has been investigated by Rao and Lefebvre [38] concentrating on the influence of spray quality on minimum ignition energies using kerosine as a fuel. They found that fuel drop size is very important for ignition in flowing gases. Fig.11 shows the relevant results indicating, that if SMDs increase from approximately 60 μm to 85 μm the average fuel-air equivalence ratio has to be increased from $\phi = 0.56$ to approximately 0.95. Bearing in mind that the use of a higher-boiling alternative fuel would result in a larger average drop size, this would mean that with these fuels the local fuel-air ratio in the ignition region must be chosen much richer than with kerosine. Rao and Lefebvre also showed that improvements in spray quality had a large positive effect on the minimum ignition energy, sprays with smaller SMDs exhibiting the lower ignition energies. The data obtained indicate that evaporation is most critical for ignition, at least at atmospheric pressure and temperature. Therefore, if ignition is to be improved, it is recommendable to promote evaporation by decreasing the average drop sizes. This should be the more valid the lower the volatility of a fuel.

Sangiovanni and Kesten [29] report on studies concerning the influence on ignition of droplet interaction. They produced sequences of monodispersed droplets, in which the distance between two droplets following each other could be changed. It was found that for very small distances, below 5 μm , interactions are observed which lead to worsened ignitability. Such effects are reported to be more pronounced with small droplets, low temperatures and for low-volatility fuels.

Spadacchini [40] examined the available literature on auto-ignition of liquid fuel sprays in high-temperature air-streams, a study which was undertaken to get data for premixing-prevaporizing combustors. In part, he states that the degree of vaporization achieved prior to the onset of auto-ignition may have a significant influence on the magnitude of delay times. He therefore concludes that the overall fuel-air ratio may not be sufficient to correlate ignition data.

Correlations Between Fuel Properties and Ignition

Ballal and Lefebvre [36, 37] have investigated the influence of different fuel grades on minimum ignition energy. Based on experimental work they state that the ignition of kerosine sprays is evaporation-controlled. They relate ignition energy and fuel properties by the following equation:

$$E_{\min} \sim \left(\frac{\text{SMD}^{1.4}}{1/\rho_f \ln(1+B)} \right)^3 \sim \tau_{\text{eb}}^3 \quad (27)$$

which means that $E_{\min}$ is roughly proportional to the cube of the characteristic evaporation time τ_{eb} , (see Equation (26a)).

The effect of fuel properties on ignition energy requirements is shown in Fig.12, taken from [36]. Incorporating a suitable droplet size correlation for a swirl atomizer, e.g. Equation (A2) of the Appendix, yields the upper curve in Fig.12, showing that the ignition energy requirement increases very rapidly when higher boiling fuels are used. If the Sauter mean diameter is kept constant by adjusting the atomizer conditions the effect of fuel viscosity (as well as the

small effect of density and surface tension) and forced convection on ignition energy -- isolated from the effect of fuel volatility.

If with broad specification fuels, the Sauter mean diameter is adjusted such that a constant evaporation time is maintained, the ignition energy requirement should be the same as with kerosine. However, it remains to be shown whether current ignition devices are capable of igniting this type of fuels safely, in particular on high-altitude cold conditions. In principle, ignition devices capable of deploying higher ignition energies should be used with higher-boiling fuels. Ignition torches seem to be interesting candidates, especially if they use small amounts of oxygen for the promotion of ignition. It would be worthwhile to do research work into that direction.

Mellor [50] has recently described the evolution of a characteristic time model, originally suggested by the gas turbine combustor results of [25], for emissions, efficiency, spark ignition, and lean flame stabilization. Quantitative semi-empirical correlations are obtained in simplified combustor geometries, and, in the case of gaseous emissions, are then extended to practical turbine hardware [50]. Considerable progress has now been made to the expansion of the spark ignition and lean blow-off models to engines [51, 52], and a brief review will be given here. Both models describe the relevant limit in terms of combustor geometry and inlet conditions, fuel and injector properties, and in the case of ignition, igniter properties.

Conceptually the two models are identical except that the energy source is the spark plug for ignition and the primary zone recirculating flow for lean blow-off. There are small differences in the details of the formulations at the present writing. For the spray flame sufficient residence time must be provided in the vicinity of the energy source to both vaporize and ignite the fuel, or, in terms of the characteristic times

$$\tau_{sl} \sim \tau_{hc} + k \cdot \tau_{eb} \quad (29)$$

where τ_{sl} is the residence time, τ_{hc} is the ignition delay time for the fuel vapor, and, as before, τ_{eb} is the evaporation time for a droplet of the Sauter mean diameter. The constant k is selected empirically because all characteristic times are order of magnitude estimates.

For spark ignition in combustors the characteristic times are evaluated as follows:

$$\tau_{sl} = d/V_{ref} \quad (30)$$

$$d = \left[E / \frac{\pi}{6} \rho_g C_{pg} \Delta T_{\phi=1} \right]^{1/3} \quad (31)$$

$$V_{ref} = \dot{m}_a R T_{in} / P \cdot M \frac{\pi}{4} d_{comb}^2 \quad (32)$$

Here d is the diameter of the spherical volume heated by the spark plug of rated energy E , the stoichiometric temperature rise $\Delta T_{\phi=1}$, ρ_g , C_{pg} and M are respectively the density, specific heat at constant pressure, and molecular weight of air; $\dot{m}_a$, T_{in} , and P are the air flow rate and combustor inlet temperature and pressure; R is the universal gas constant; and d_{comb} is the maximum combustor diameter.

The kinetic time for ignition is taken as

$$\tau_{hc} = 10^{-5} \exp(26100/RT_{\phi=1})/\rho_g \quad (33)$$

where τ_{hc} is in msec, ρ_g in kg/m^3 and the activation energy is in cal/mole. The value of the latter was suggested by Fenn [53] based on his work with C_3H_8 and C_3H_{12} . $T_{\phi=1}$ is the adiabatic stoichiometric flame temperature, the highest in a spray turbulent diffusion flame, where therefore the ignition delay time is a minimum.

Finally, the droplet evaporation time is given by Equation (25), where SMD is evaluated following Jasuja [14], Equation (A5), for the Avco-Lycoming AGT-1500 (Schmidt [54]; Marchionna and Opdyke [55]) and from

$$\text{SMD} = 140.3 \cdot \nu_f^{0.18} \dot{m}_f^{0.215} / \Delta P_f^{0.442} \quad (34)$$

cited by Moses [30] for the Allison T63 [30, 31]. The evaporation coefficient β is given by Equation (22) with a multiplicative factor of $(0.185 \text{ Re}^{0.6})$ suggested by Kanury [56] to account for forced convection. Re is based on reference velocity (Eq. (32)) and Sauter mean diameter (Eq. (A5) or [34]). All ambient gas properties in the characteristic times for spark ignition are evaluated at 1300 K, and following Foster and Straight [34] and Lefebvre et al [21] the transfer number B is computed for the 10% boiling point of the fuel.

The correlation of combustor data is shown in Fig. 13 in terms of these characteristic times. Data are presented for four AGT-1500 combustor geometries, two igniter locations, and four fuels designated in the figure, and for the T63 combustor operating with thirteen differing fuels. A linear fit is obtained with $k = 0.021$ and slope of 1.8. As noted in the figure, the y-intercept is near zero. Ignition can be accomplished above the least squares fit line, but the limit is

approached by decreasing E or increasing V_{ref} (decreasing τ_{sl}), by decreasing T_{in} or ρ_g (increasing τ_{hc}), or by increasing SMD or decreasing B (increasing τ_{eb}). The two data to the left of the fit at $\tau_{sl} = 1.17$ msec are for gasoline and 30% gasoline with Diesel fuel at very low fuel flows (and viscosities), where the validity of Equation (34) is questionable. The results are extremely sensitive to the estimated SMD [51].

The results shown in Fig.13 are encouraging but must be applied to additional engine data. They suggest a universal spark ignition limit curve can be obtained through the characteristic time model in terms of combustor inlet conditions (τ_{sl} and τ_{hc}), igniter design (τ_{sl}), and fuel and injector properties (τ_{eb} and τ_{hc}).

Note that the model does not involve the primary zone equivalence ratio, probably because the spark plug is wetted by the fuel spray prior to ignition. Thus for a given fuel and engine starting condition, the limit gives the trade-off between igniter energy and SMD, or atomization, for successful ignition. The increasing ease of ignition as primary zone equivalence ratio is increased is in fact attributed to better atomization (decreasing τ_{eb}) or lower reference velocity (increasing τ_{sl}) [51].

A2.3.7 Influence of Fuel Properties on Blow-off

Presently, only limited data are available on the influence of fuel properties on the stability limits of combustors. Moses [30] and Moses and Nægeli [31] included in their work tests on the lean blow-off limits of their T63 combustor, using the fuels already described in the previous section. The measurements were carried out under 75% of full power conditions.

In the first investigation concerning JP4, JET A, DF2 and blends with 10 and 20% pentane, the lean blow-off limits shifted slightly to higher fuel-air ratios if the volatility of the fuel was decreased. However, the addition of 10 and 20% pentane to the JET A and DF2 fuels showed no noticeable effect. Fig.14 gives the results of [30].

In [31] Moses and Nægeli carried out similar experiments, however at different combustor loadings ranging from full load to idle. Again, only those fuels which were physically different, influenced the lean blow-off limit noticeably. As Fig.15 shows, the blow-off limit widens when the volatility is increased. Thus, gasoline shows the widest blow-off limit, where as with the less volatile high-boiling marine Diesel fuel blow-off occurs at markedly higher fuel-air ratios. It is also shown that this effect is more pronounced at the lower power settings. This behaviour is attributed to the lower combustor inlet temperatures at idle conditions which result in a slower vaporization of the higher boiling fuels. At full load the higher inlet temperatures should cause the less volatile fuels to vaporize faster while having little effect on highly volatile fuels. Blending marine Diesel with gasoline widened the stability limits of DFM whereas a blend of JP5 and DFM showed no degradation of the stability limits.

Leonard and Mellor [52] have correlated these data, as well as data from Avco-Lycoming [54, 55], with a characteristic time model for lean blow off [46, 47]. These parameters are redefined as follows:

$$\tau_{sl} = \tau_{sl,co} = l_{co}/V_{ref} \quad (35)$$

$\tau_{sl,co}$ is the CO oxidation fluid quenching time (Mellor [57]) generally determined by the position of the primary air penetration holes measured from the tip of the fuel injector to the hole centreline

$$l_{co} = (l_{pri}^{-1} + d_{comb}^{-1})^{-1} \quad (36)$$

and V_{ref} is given by Equation (32).

$$\tau_{hc} = 10^{-4} \exp(2100/RT_{\phi=1})/\phi_{pz} \quad (37)$$

with symbols and units as before and ϕ_{pz} the primary zone equivalence ratio.

The convective correction of Equation (19) (divided by two) is used to model forced convection; for the AGT-1500 Equation (A2) is the source of estimates for SMD, whereas Equation (34) serves for the T63.

Equation (29) is modified as follows: due to the temperature gradient across the shear layer surrounding the recirculation zone, the flow will be accelerated:

$$\tau_{sl,co} \rightarrow T_{in}/T_{\phi=1} \cdot \tau_{sl,co} \quad (38)$$

or, rewriting Equation (29):

$$\tau_{sl,co} = \tau'_{hc} + k\tau'_{eb} \quad (39)$$

where the primes indicate inclusion of the temperature ratio ($T_{\phi=1}/T_{in}$) in the times on the right.

Ambient condition properties for lean blow-off are evaluated for air at 1000 K, the transfer number at the fuel's 50% boiling point, and a relative velocity between fuel droplets and air is taken as 50 m/s.c. As in spark ignition, Maxwell's methods [58] are used to estimate fuel properties at elevated pressures and temperatures.

The correlation is shown in Fig. 16 for six AGT-1500 geometries operating with four different fuels, and representative data from Moses [30] and Nægeli [31]. The darkened symbols to the right of the correlation are data obtained with No.6 oil, which may have penetrated to the liner walls. Again the fit is linear, with a near-zero y-intercept; further, the fit of engine combustor data shown in Fig. 16 is almost the same as that obtained in simplified burners [46, 47], lending further credence to the model. Additional engine combustors should be tested against the correlation to test its universality.

Ingebo and Norgren [42] investigated the effect of JET A and Diesel fuel DF2 on the blow-off limits of an experimental combustor, equipped with air atomizing splash-groove nozzles. Blow-off was achieved by lowering the combustor inlet total pressure. It was found that the minimum total pressure at which a stable flame could be maintained was nearly the same for the two fuels. This behaviour is attributed to the atomizing qualities of the air-atomizing nozzle used.

Plee, Schmidt and Mellor [44] investigated the blow-off characteristics of premixing/prevaporizing combustors, using JET A and propane as fuels. They found that the more volatile propane gave the wider lean blow-off-limits compared to JET A, because JET A is thought not to vaporize completely in the available residence time. With JET A they observed also a secondary influence of fuel properties on the blow-off limits insofar as increasing average drop size of the fuel spray led to narrower burning range of the flame. This indicates that there might be conditions within the operating envelope of a combustor, where evaporation is a controlling process at the limits of flame stability. Hence, fuel viscosity and volatility could also influence the stability limits. It should however be noted that the reported results may not be generally valid, since they are obtained in rather specialized premixing/prevaporizing combustion systems. Plee and Mellor [46] and Leonard and Mellor [47] discuss these and related results further.

A2.3.8 Summary

With regards to atomization and evaporation, both physical fuel and injector properties influence the quality of the fuel spray. Lefebvre et al. [21] have recommended one method of characterizing this quality by evaluating the fuel droplet evaporation time as the ratio of Sauter mean diameter squared to an evaporation coefficient modified for forced convection. The former depends primarily on injector design and fuel viscosity, and the latter on volatility. Since drop size distribution is a function only of SMD, such a simple parameter seems adequate.

Advanced injectors utilizing air blast or air assist provide better atomization than pressure atomizers. One pressing research need is to extend atomization studies to the higher ambient temperatures and pressures usually encountered in aircraft engines, as well as to altitude flight conditions.

Based on constant droplet evaporation time, higher-boiling fuels would require fuel sprays with smaller Sauter mean diameters, if combustor performance should not deteriorate. The possible future requirements of multifuel capability would demand injector designs which, for a given fuel and fuel flow, allow a reduction of the Sauter mean diameter of the spray.

Ignition and blowout are mechanistically similar: the gaseous fuel-air ratio in the spark gap or adjacent to the flame stabilizing recirculation zone is the important fuel related parameter. Thus short droplet lifetimes improve ease of ignition and flameholding. As fuel specifications are relaxed, again if injector type is chosen to hold drop evaporation time constant, no degradation in ignition or lean blowout limit is expected. Changes in fuel chemistry are thought to be of only secondary importance, but this insensitivity should be verified for fuels from sources other than petroleum.

In a previous section the energies required to hold droplet evaporation time constant were calculated, in terms of increased injector pressure drops, if DF 2 is substituted for JET A. Even for air assist nozzles this increase was prohibitive (order of 50%). Thus for future fuels two options are available: either more sophisticated fuel injectors (spillover air blast or standard air assist) or igniters with larger energy delivery must be used.

However, it should be remembered that the primary requirement for air assist occurs only during engine starting, either on the ground or in the air after a flameout. The assist air, perhaps connected to an air blast nozzle, could be provided by external means on the ground, and by air (or O₂) storage of several compressed gas bottles in the air. The ultimate selection of either enhanced igniter or enhanced injector would depend on the weight penalties or space requirements associated with each.

A2.3.9 Nomenclature

A_{ref}	(m ²)	combustor reference area
A		constant
B		constant
B		mass transfer number

c_p	(kJ/kg K)	specific heat at constant pressure
$D_{o, D}$	(m)	diameter of the fuel nozzle or pintle of an airblast atomizer
D_{ref}	(m)	reference diameter of flame tube
d_{comb}	(m)	maximum combustor diameter
d	(m)	droplet diameter
d	(m)	diameter of spherical volume heated by the spark
E_{min}	(mJ)	minimum ignition energy
H_v	(kJ/kg)	latent heat of evaporation
k	(W/m K s)	heat conductivity
k	-	constant
l_{co}	(m)	characteristic length for CO production
l_{pri}	(m)	characteristic length of primary zone
$\dot{m}$	(kg/h)	mass flow
Δp_f	(bar)	pressure drop across the atomizer
Δp_{FT}	(bar)	pressure drop across flame tube
$\dot{Q}$	(dm ³ /h)	volume flow
s	(m)	liquid fuel sheet thickness
T	(K)	temperature
t	(s)	time
V	(m/s)	velocity
V_{ref}	(m/s)	combustor reference velocity
V_e	(m/s)	effective velocity
α	(deg)	spray cone angle
β	(m ² /s)	evaporation constant
ϕ		equivalence ratio
η_f	(N s/m ²)	absolute viscosity
ρ_f	(kg/dm ³)	density
ρ_g	(kg/m ³)	
$\theta = \frac{A_{ref} D_{ref}^{0.75} p_3^{1.75} e^{T_3/300}}{\dot{m}_a}$		air loading parameter
ν	(cS)	kinematic viscosity
σ_f	(N/m)	surface tension
τ_{eb}	(ms)	characteristic evaporation time
τ_{sl}	(ms)	residence time
τ_{hc}	(ms)	ignition delay time for fuel vapor

Other Symbols or Abbreviations

F.N.	atomizer flow number
f.a.r.	fuel-air-ratio
Nu	Nusselt number
Re	Reynolds number
Sc	Schmidt number
SMD	Sauter mean diameter

Indices referring to:

o	initial state
f	fuel
l	liquid
g	gas
a	air
k	kerosine
3 or in	combustor inlet conditions

A2.3.10 References

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A2.3.11 Appendix to A2.3

Some empirical correlations relating spray quality (Sauter mean diameter SMD) of pressure swirl nozzles with fuel properties and atomizing conditions:

Stewart [10]:

$$\text{SMD} \sim \frac{\nu_f^{0.2} \sigma_f^{0.05} \nu_g^{0.25} \dot{m}_f^{0.25}}{\Delta P_f^{0.4}} \quad (\text{A1})$$

Hunter et al. [11]:

$$\text{SMD} = 91.5 \frac{\nu_f^{0.3} \rho_f^{0.205} Q_f^{0.205}}{\Delta P_f^{0.354}} \quad (\text{A2})$$

Whitelaw et al. [12]:

$$\text{SMD} = 75 \frac{\dot{m}_f^{0.25}}{\Delta P_f^{0.4}} \quad (\text{A3})$$

Fraser [13]:

$$\text{SMD} = 76.9 \frac{\nu_f^{0.215} \dot{m}_f^{0.209}}{\Delta P_f^{0.458}} \quad (\text{A4})$$

Jasuja [14]:

$$\text{SMD} = 563.7 \frac{\nu_f^{0.16} \sigma_f^{0.6} m_f^{0.22}}{\Delta P_f^{0.43}} \quad (\text{A5})$$

units used:

$$\begin{array}{llll} \text{SMD} & \mu\text{m}: & \rho_f & \text{kg/dm}^3; \quad \Delta p_f \quad \text{bar} \\ \nu_f & \text{cS}: & Q_f & \text{dm}^3/\text{h} \\ \sigma_f & \text{N/m}: & m_f & \text{kg/h} \end{array}$$

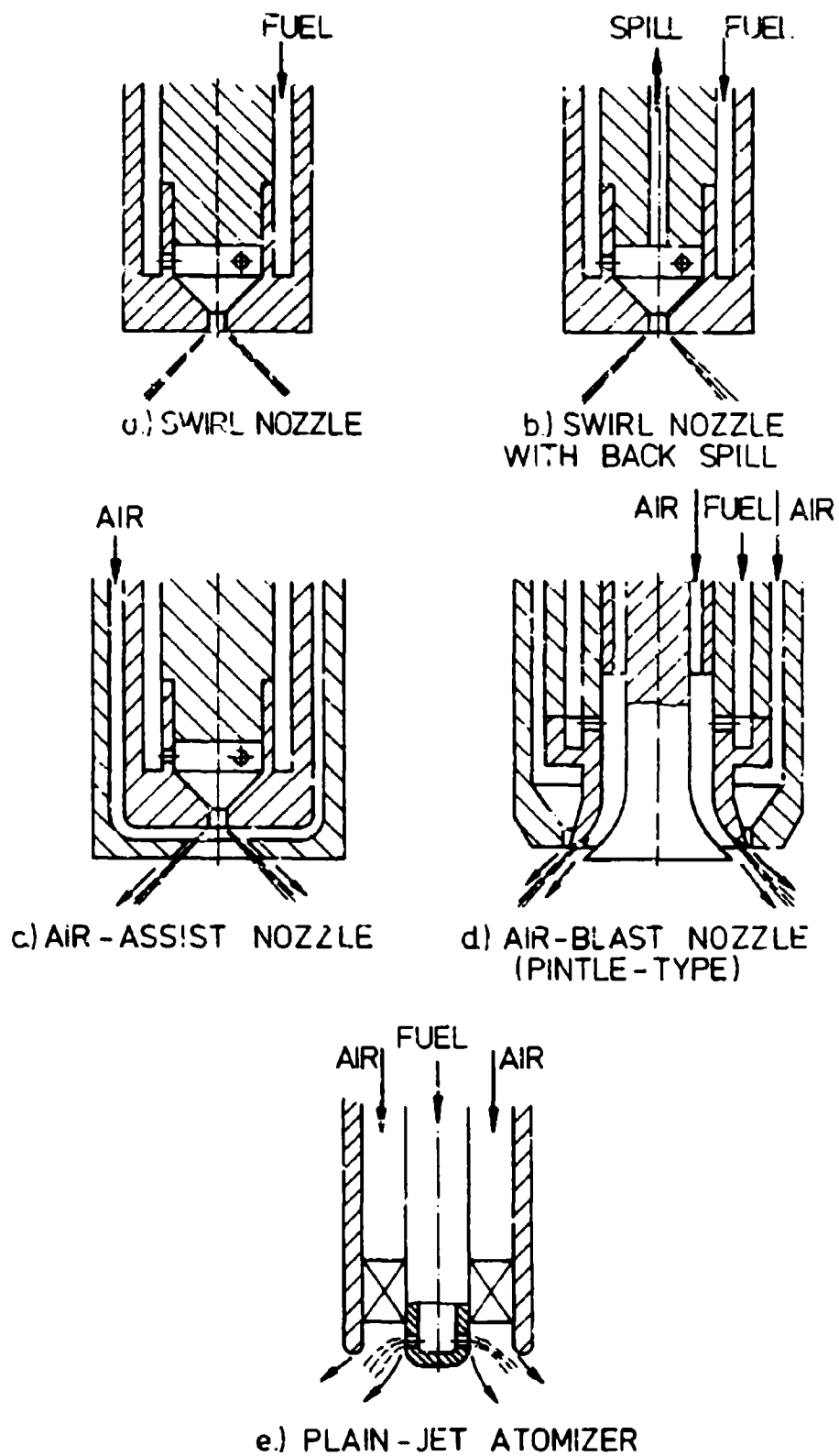


Fig.1 Schematic designs of different atomizer types

- 1 AVIATION GASOLINE
- 2 JP 4
- 3 KEROSENES
- 4 DIESEL FUEL

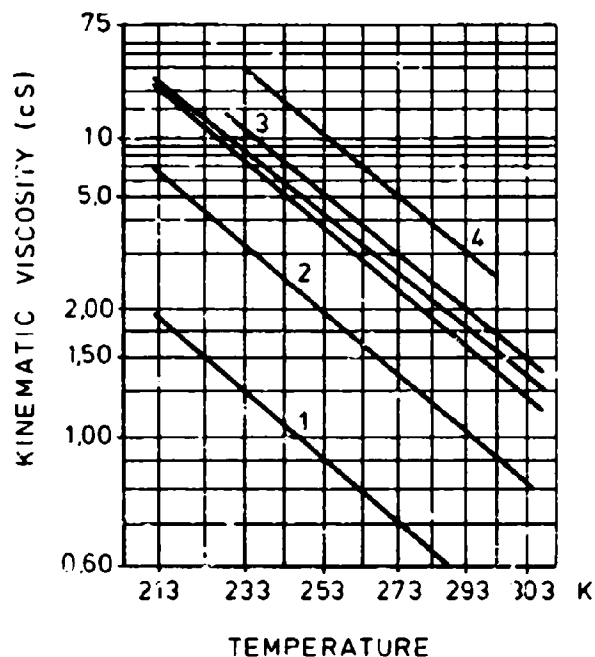


Fig.2 Viscosities of different fuels and their variation with fuel temperature [1]

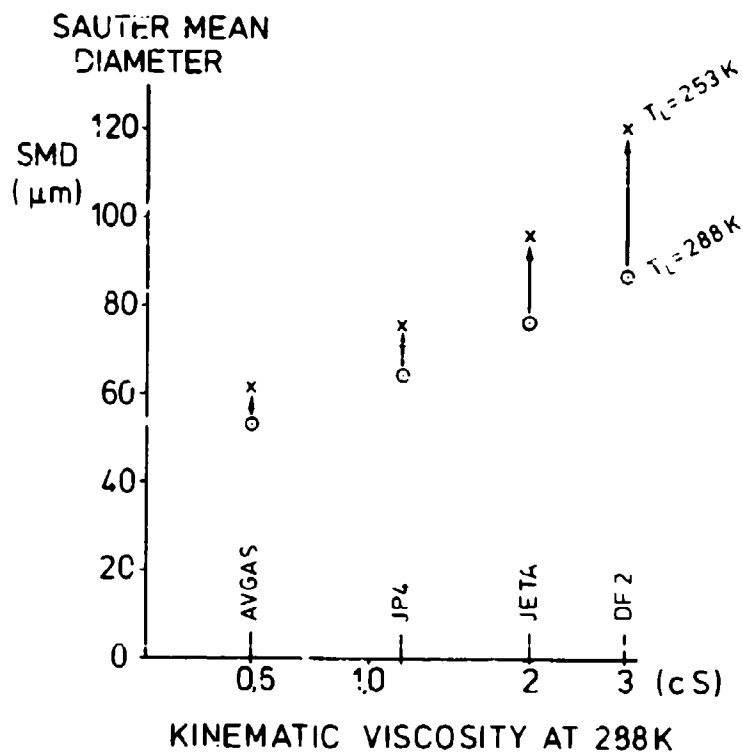


Fig.3 Influence of decreasing temperature on the Sauter mean diameter of fuel sprays generated by pressure swirl atomizers

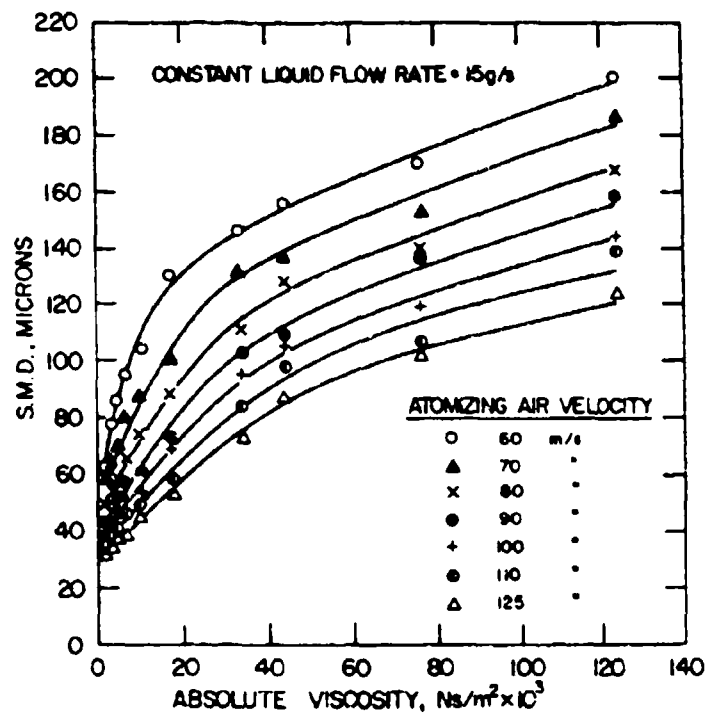


Fig.4 Influence of viscosity on the Sauter mean diameter of fuel sprays generated by air blast atomizers after Lefebvre [9]

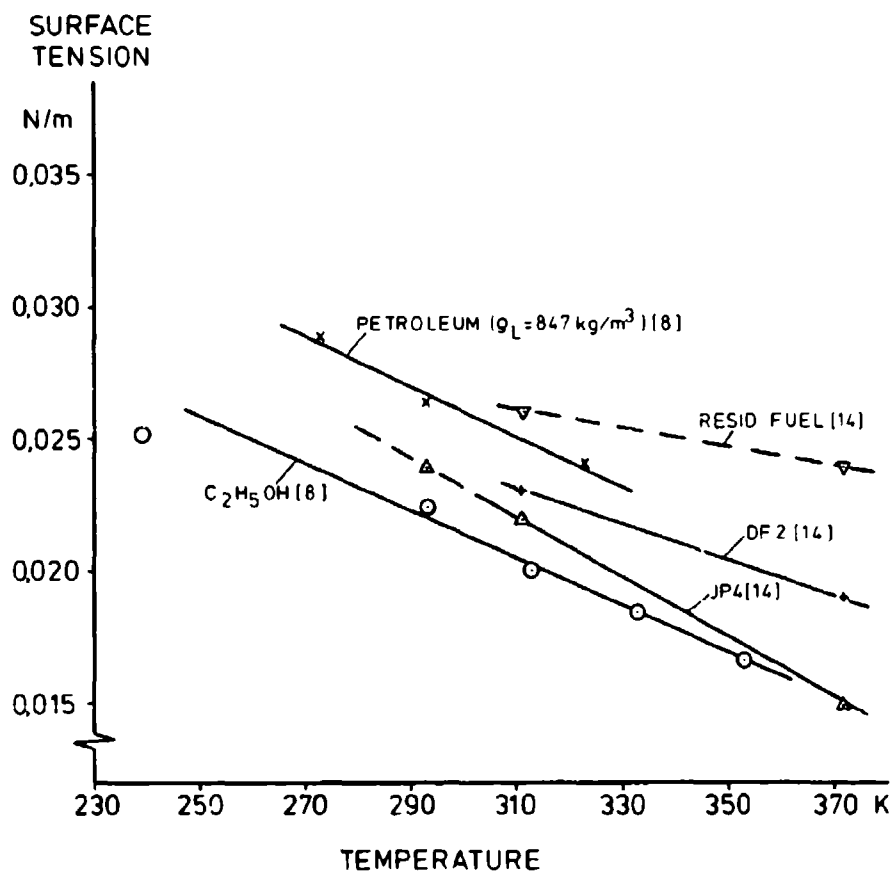


Fig.5 Surface tension: of different hydrocarbon fuels and ethyl alcohol and their variation with fuel temperature [8, 14]

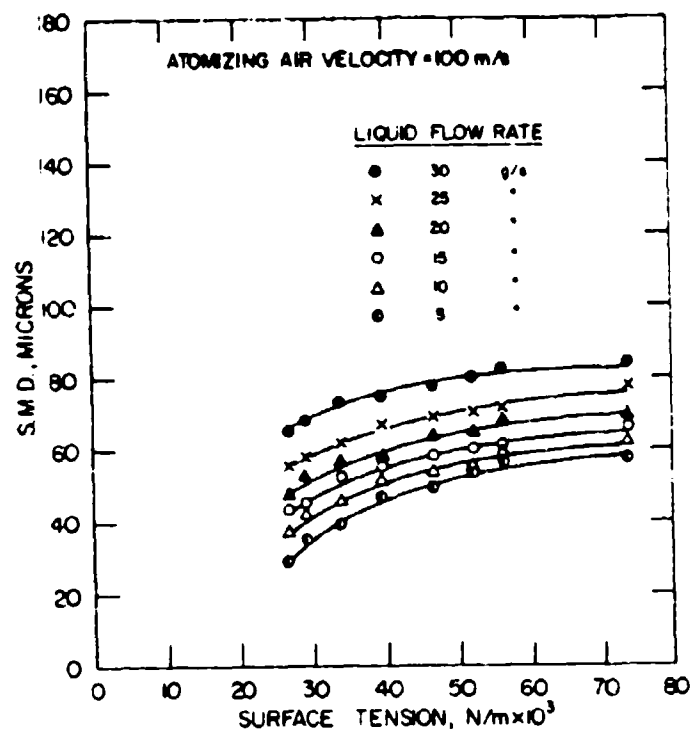


Fig.6 Influence of surface tension on the Sauter Mean Diameter of liquid sprays generated by airblast atomizers after Lefebvre [9]

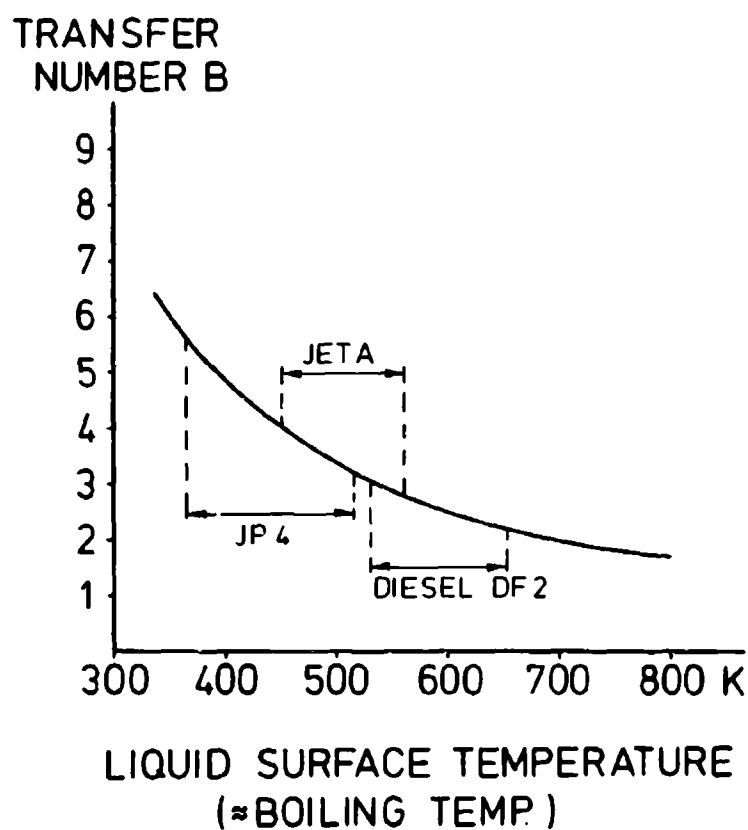


Fig.7 Relationship between mass transfer number B and the liquid surface temperature of an evaporating and burning fuel droplet [18]

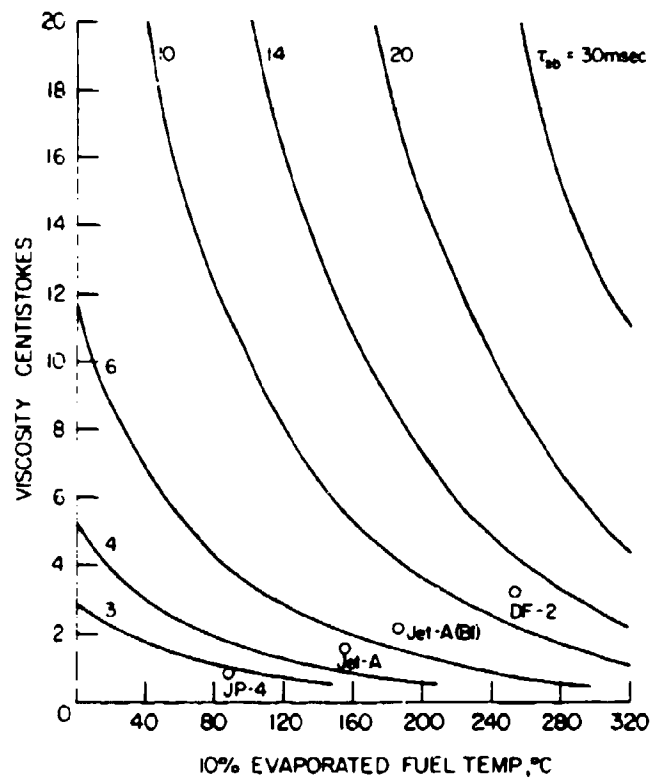


Fig.8 Influence of volatility and viscosity on the life time τ_{eb} of evaporating droplet sprays [21]

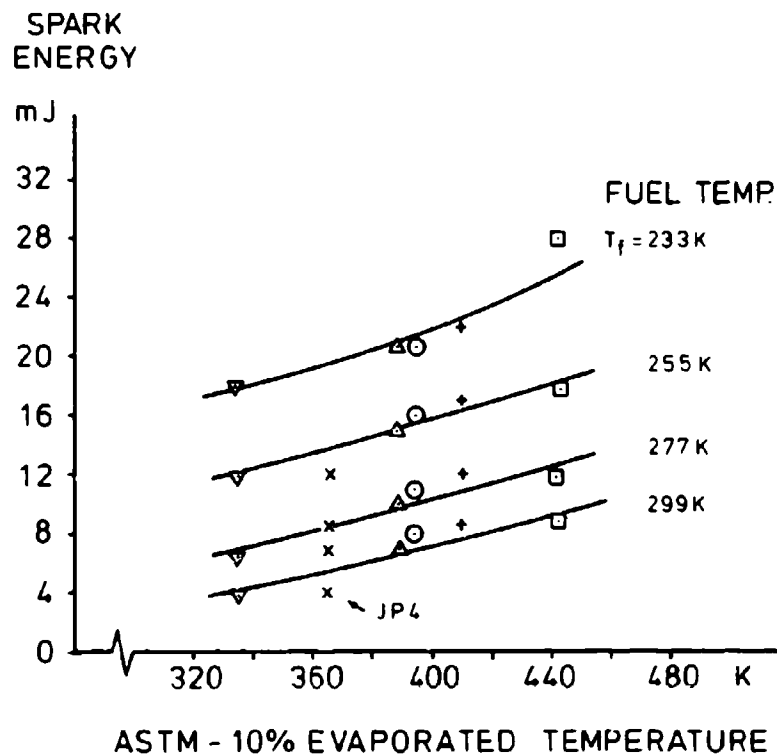


Fig.9 Relationship between the volatility of hydrocarbon-fuels and spark energy necessary for ignition of fuel sprays [34]
Fuels: NACA research fuels, based on JP3 specifications with different Reid vapour pressure, 1, 3 and 7 psi

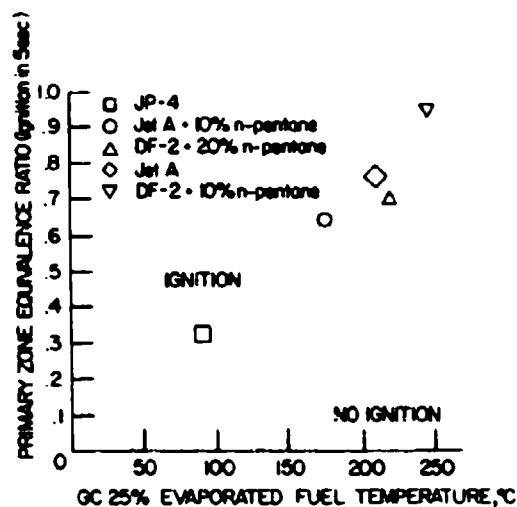


Fig.10 Influence of the volatility of hydrocarbon fuels and blends with pentane on the fuel-air-ratio necessary for ignition [30, 21].

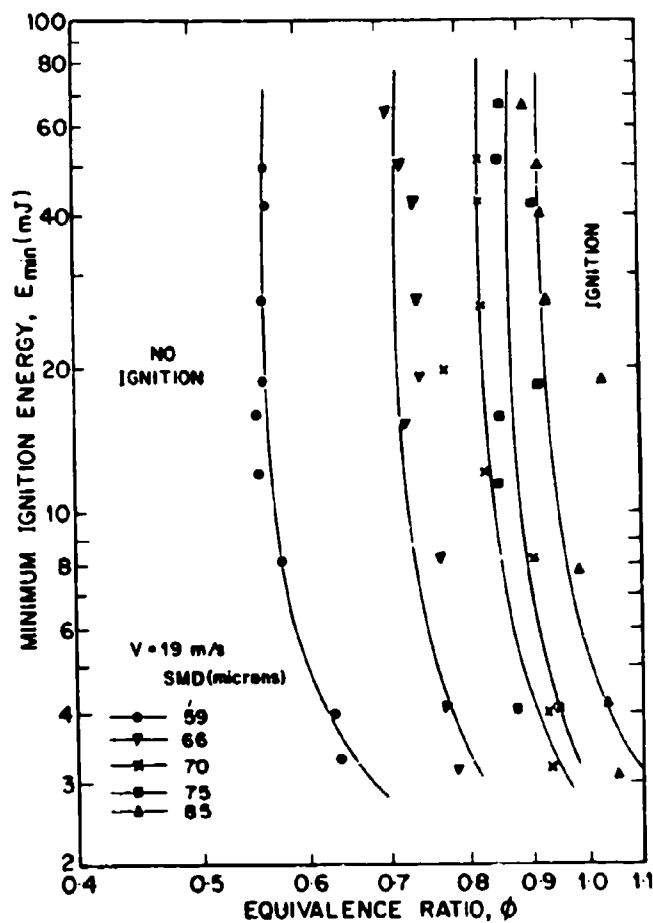


Fig.11 Influence of the fuel spray quality (i.e. Sauter Mean Diameter) on ignition characteristics [38]. Fuel: kerosine

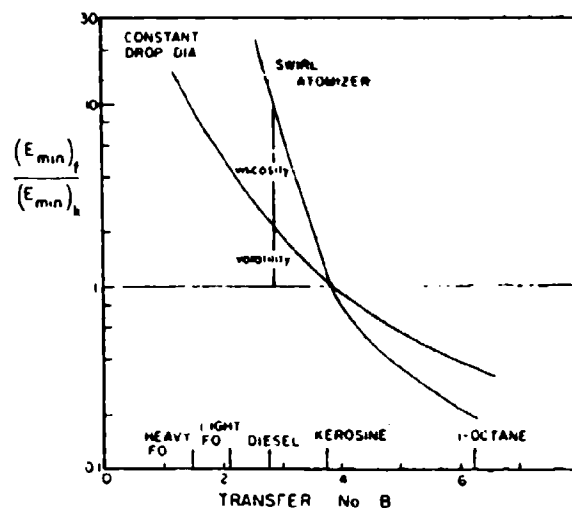


Fig.12 Influence of fuel volatility and viscosity on ignition energy requirements for liquid fuel sprays [36]

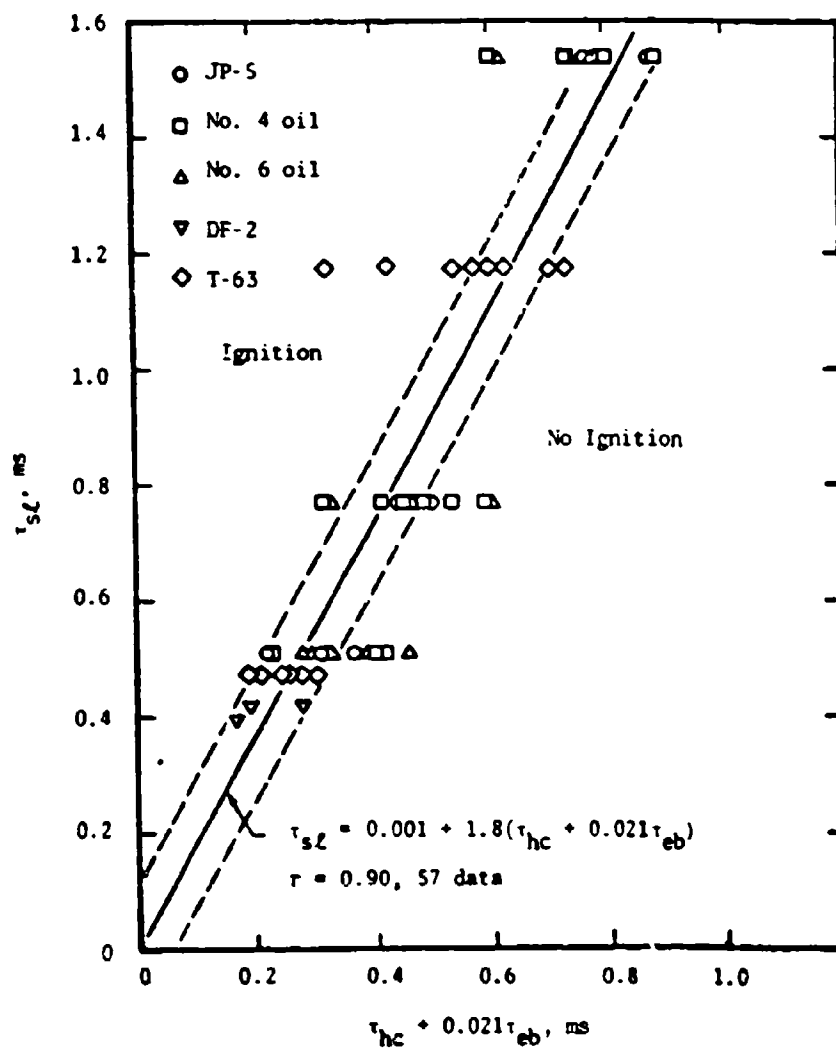


Fig.13 Model correlation of AGT-1500 [54, 55] and T63 [30, 31] ignition data [51]

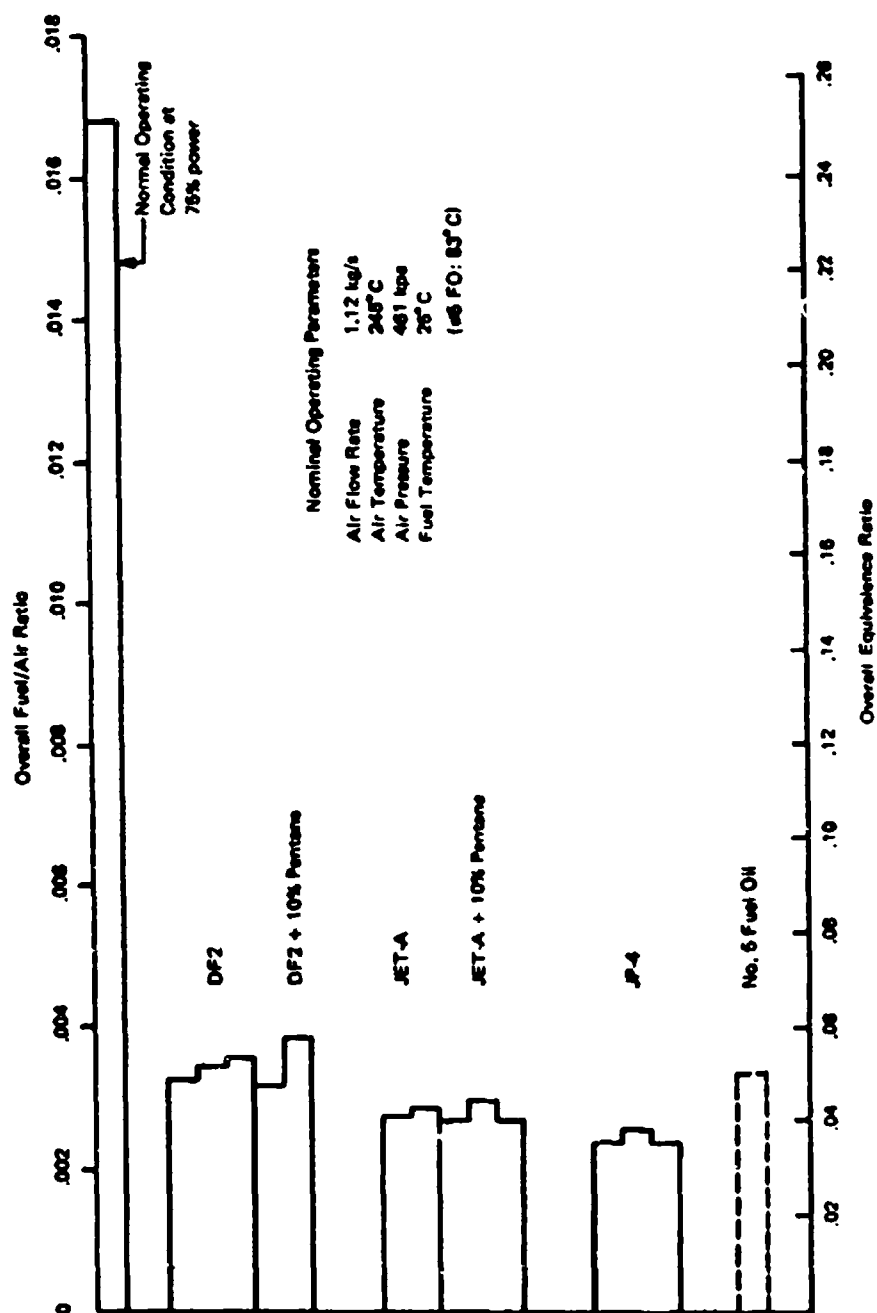


Fig.14 Influence of fuel properties on the lean blow-off-limit of a T63-combustor [30].
Combustor air loading corresponding to 75% full load condition

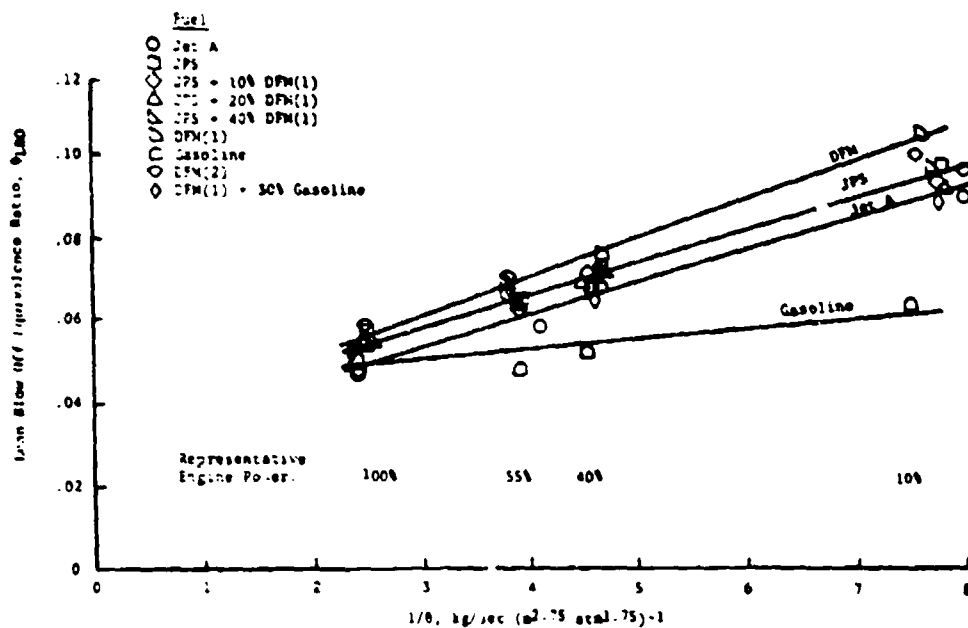


Fig.15 Influence of fuel properties on the lean blow-off-limit of a T63-combustor at various combustor loadings [31]

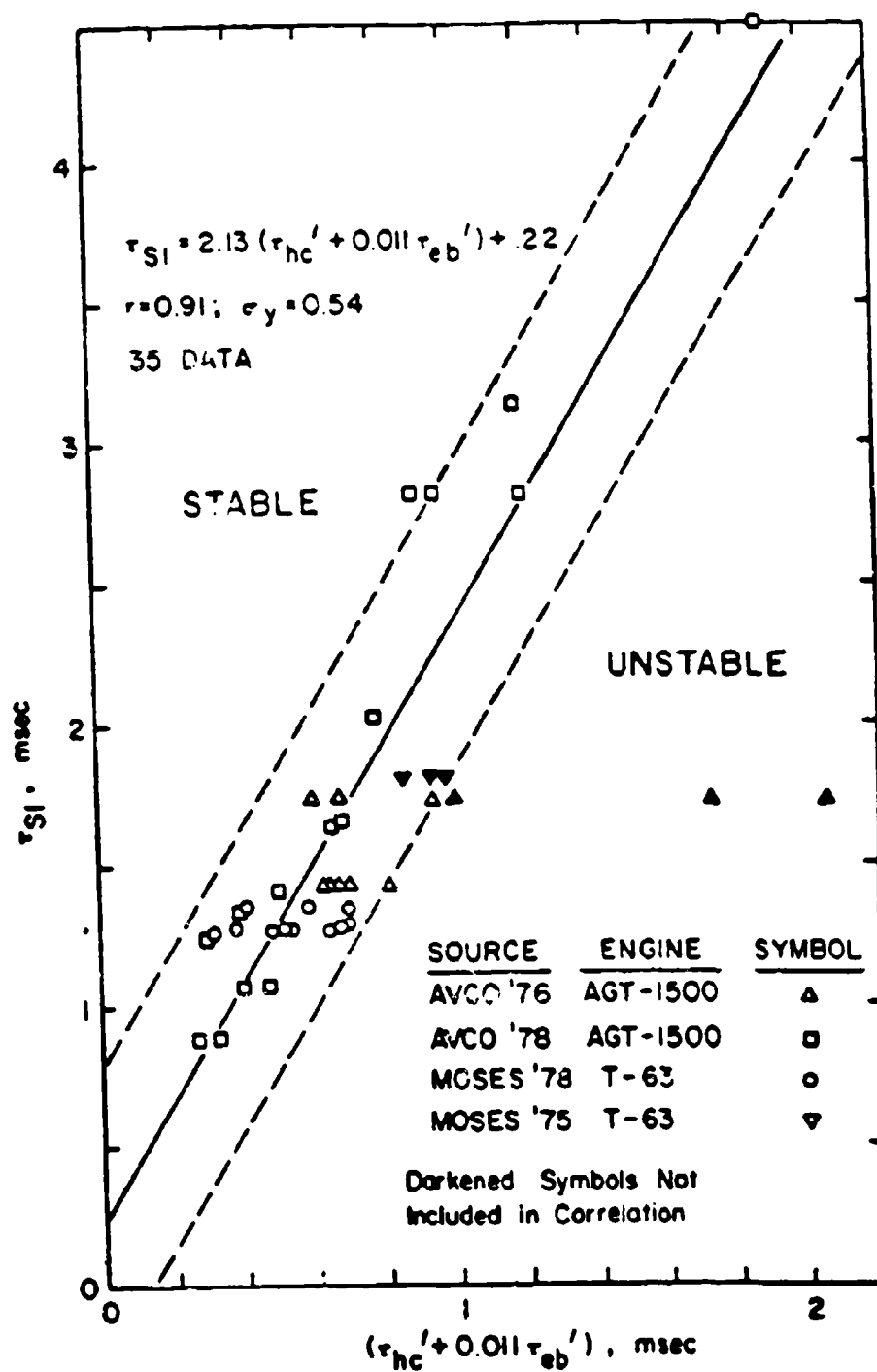


Fig. 16 Model correlation of blow-off data [30, 31, 54, 55]. Fuels include No.4, No.6, JP-5 and DF-2 for the AGT-1500, and DFM, JP-5 and gas for the T-63 [52]

A2.4 FUEL THERMAL STABILITY

A2.4.1 Scope

When an aviation fuel is thermally stressed, or subjected to long term storage, degradation can occur resulting in a variety of end products. Some of these degradation products are insoluble and can either adhere to surfaces or remain suspended in the fuel. In addition to causing aircraft fueling difficulties the suspended insolubles within the aircraft and engine can also restrict orifices and filters whilst their adherence to surfaces (deposition) can result in restriction of movement ultimately leading to fuel system malfunction and/or operating difficulties.

The tendency of aviation fuels to produce such deposits as a result of exposure to high temperatures is recognized and in an effort to ensure some form of quality control, tests are currently listed in aviation fuel specifications under "Thermal Stability".

The thermal stability of aviation fuels is influenced by a variety of factors other than time at a temperature, namely fuel quality, composition and catalytic influences.

Investigators and service experience have shown that aviation kerosines can produce thermal degradation products at temperature regimes as low as 100°C [1, 2, 3]. Further, deposits can exist in a variety of types: sticky gums, hard strongly adhering lacquers and fine cokes, often of a friable nature [4].

This Appendix endeavours to summarise the major aspects of fuel thermal stability, namely:

- Chemistry of Deposit Formation
- Evaluation of Thermal Stability
- Aircraft/Engine Problems and Future Trends

Attention has been drawn, wherever necessary, to emphasise the potential lowering of fuel quality and the ever increasing demands made on fuel in its role as a heat sink for the aircraft and engine.

Aero engine manufacturers regard the property of fuel thermal instability as one of the most important fuel related problems they will have to overcome.

Two major avenues of investigation are available:-

- (1) Research to improve fuels by refinery techniques or additives.
- (2) Improved engine or aircraft design to "live with", or prevent, deposit formation.

A2.4.2 The Chemistry of Deposit Formation

The chemistry of fuel degradation is intrinsically complex, with the interaction between fuel and oxygen the primary process involved. The subject of fuel thermal degradation, with all its ramifications, has been admirably reported on numerous occasions [1, 5, 6]. This section endeavours to present some of the major conclusions in brief terms, with the subject having been categorised into the following topics:-

- General Chemistry
- Mechanisms of Deposit Formation
- Effect of Metals
- Effect of Additives
- Effect of Deoxygenation
- Future Fuels

General Chemistry

- Whilst removal of oxygen markedly improves fuel stability with the majority of fuels investigated, several thermal degradation processes can occur without the presence of oxygen (see later paragraph "Effect of Deoxygenation"). However, its severity greatly increases and manifests itself at lower temperatures if the fuel is saturated with air [7]. The reactions between fuel components and oxygen are complex, involving free radical mechanisms and is termed Autoxidation [6, 8, 9, 10]. The rate at which fuel oxidizes is an exponential function of the increase in fuel temperature.
- Deposit formation can occur within the engine fuel system at bulk fuel temperatures as low as 373 K (100°C) e.g. in heat exchange systems and fuel washed bearings.
- The rate at which deposits form is temperature dependent and follows an Arrhenius relationship with an activation energy of about 10 kcal/mol. Fig.1 from Reference [6] plots metal surface temperature versus coking rate and shows that at higher flow rates (i.e. lower bulk fuel temperatures) deposit formation rate is lower.
- Deposits discovered in aircraft fuel systems vary from soft gums to hard lacquers and brittle cokes. They invariably contain large amounts of oxygen and are deficient in hydrogen.

Reactive compounds in fuel include hydrocarbons such as diolefins and aromatic olefins plus heteroatomic compounds which contain elements other than carbon and hydrogen such as oxygen, sulphur and nitrogen. Components of these types are involved in fuel decomposition and deposit formation [1, 5, 6, 11, 12].

- In all instances the deposits contain significant amounts of heteroatomic compounds. The concentration of nitrogen and sulphur in deposits exceeds the level in fuel by factors of 100 to 100,000 [13]. Further, low values of H/C ratios in many instances suggest a highly aromatic nature [5, 14].
- The 'route' from initial oxidation (soluble reaction products) to the formation of insoluble material remains unclear. A suggested route is by the formation of insoluble material through hydroperoxide thermolysis. The decomposition of hydroperoxides to radical fragments that propagate the oxidation chain reaction is accompanied by or followed by deposit formation [15].
- Removal of oxygen can usually improve fuel thermal stability i.e. decrease the rate and amounts of deposit formed. Fig.2 illustrates that deoxygenated fuels exhibit a much lower rate of deposit formation [7]. The transition from oxidative to pyrolytic reactions at about 500°C is clearly illustrated.
- Studies regarding the morphology of deposits reveal a general structural arrangement; an agglomeration of very small microspherical particles [4, 14].
- Fuel pressure is of significance as it determines whether liquid or vapour phases predominate at certain temperatures. Evidence supports the conclusion that deposits are often more severe when a vapour phase exists [5, 12, 16].
- Certain metals, such as copper and silver can catalyse the fuel decomposition reactions thereby more easily promoting fuel instability.
- Long term storage of certain fuels even at ambient temperatures can result in fuel degradation (storage instability).

Mechanisms of Deposit Formation

There are a wide variety of crude oil types and refinery techniques which result in the presence of a multitude of compounds, hence the diverse nature of aviation turbine fuel characteristics. Further, there are variable thermal stresses imposed on fuel both in storage and within the fuel system of an aircraft/engine. As a result the problem of determining the exact nature of the deposit mechanism(s) is extremely difficult to define. However, with many years of investigation a general theory as to what may occur has been derived. Fig.3 illustrates the series of steps which start with fuel oxidation and lead to gums and cokes. The main factors are:

- Initially, reaction rates and products formed during the free radical chain reactions are interdependent on temperature, oxygen concentration, hydrocarbon structure and the presence of catalysts or free radical initiators.
- The ease of oxidation does not parallel and is not necessarily a guide to the deposit formation rate.
- Soluble hydroperoxides are frequently produced as one of the initial oxidation products during fuel degradation. They can reach limiting concentrations, ultimately decomposing to form radicals which can participate further in chain propagating reactions, along with other products.
- The major hydroperoxide decomposition products will be alcohols, ketones, aldehydes, esters, acids and hydroxy-ketones depending on oxygen availability and initial fuel composition [1, 5, 6, 17]; The disappearance of hydroperoxides is followed by the development of deposits as illustrated in Fig.4 [5] which plots product concentrations and metal tube deposit rating against metal temperatures in a thermal stability apparatus.
- Compounds containing heteroatoms are generally more easily oxidized than most other hydrocarbons found in jet fuel.
- The subsequent steps from oxidation products to the formation of deposits are not defined. However, several hypothesis have been suggested for the evolution of insoluble products [1, 6, 18] viz.
 - (a) polymerisation (may be limited to dimerisation)
 - (b) increased polarity; reaction products become increasingly polar as oxidation progresses (supported by high concentrations of O, N and S in deposits)
 - (c) rearrangement and condensation reactions: - studies of heating oil sediment show that thiols present can act as catalysts for oxidation, and after oxidation to sulphonic acids, catalyse rearrangements and condensations etc. Esters were major intermediates formed by reactions of hydroperoxides and aldehydes [19].

The method of formation of actual deposits still remains unclear. It is postulated that insoluble products can agglomerate and/or coagulate to form micelles several hundred Angstroms in size [1, 4, 5, 6]. These micelles are believed to accumulate or impinge on surfaces, and depending on the environmental conditions will produce gums, lacquers or cokes.

- A second deposit forming regime that does not depend on oxygenated species exists at high temperatures (>450°C). Deposits form as hydrocarbons pyrolyze to form reactive olefinic fragments.

Effects of Metals

- All fuels contain a variety of dissolved metals at very low concentrations. They can originate from several possible sources: refinery, storage, aircraft and engine fuel systems and cross contamination by other fuels
 - The majority of studies have involved the addition of soluble metallic compounds to fuel and measuring their effect on fuel thermal stability by conventional test methods [20, 21].
- The most virulent metal, with respect to its effect on lowering fuel thermal stability, is copper. Other harmful metals are iron, zinc, beryllium, cadmium, silver and cobalt [21, 22].
- The magnitude of the effect of metal presence can vary with fuel type, oxygen concentration, water content, additives present and temperature. The role of metals is in the catalysis of oxidation reactions. Surfaces such as stainless steel or titanium appear to be more reactive for deposit formation than non-reactive aluminum. Whether metals must dissolve in fuel or surface reactions alone play the catalytic role is uncertain.

Effects of Additives

- The thermal stability of some fuels can be substantially improved by the addition of acceptable concentrations of certain additives. Additives used for the specific task of "improving" fuel thermal stability can act in several ways, by inhibiting:
 - oxidation precursors via antioxidants e.g. hindered phenols
 - catalysis by metal deactivators/passivators e.g. N,N-disalicylidine-1,2-propane diamine
 - deposition via dispersants.
- The majority of investigations show that antioxidants are more effective in increasing jet fuel thermal stability threshold temperatures when used in higher than maximum allowable concentrations. Hindered phenolic antioxidants were more effective on prolonged storage fuels [23]. Petroleum derived fuels contain some natural antioxidants that inhibit oxidation precursors. The hydrogen treatment used to desulphurize fuel usually removes these natural inhibitors.
- Metal deactivators were effective in reducing the catalytic effect of dissolved metal thus raising the threshold limits for concentration of various dissolved metals. At 15 times normal concentration they eliminate the degradation of jet fuel during prolonged storage [24].
 - Currently specified metal passivators form metal complexes when metals are soluble in the fuel, and passivate metal surfaces to prevent dissolution [25, 26, 27].
 - Dispersants function by colloiddally suspending insolubles in fuel, the advantage being that they act irrespective of reactions leading to the production of solids. However, it must be noted that fuel-water separation characteristics are deleteriously affected [24, 28, 29].
 - Miscellaneous fuel additives, such as static dissipators, icing inhibitors, corrosion inhibitors, lubricity aids, etc. can influence fuel thermal stability, depending once again on the type of fuel used and the conditions experienced. Some of the corrosion inhibitors available contain phosphorus, and have been shown to lower thermal stability thresholds [30, 31].

Effect of Deoxygenation

Fuel at atmospheric pressure and room temperature will contain 40-80 ppm of molecular oxygen, which is one of the participants in the reaction mechanisms of fuel thermal degradation. Removal of oxygen markedly improves fuel thermal stability in the majority of fuels investigated and decreases the amount of deposits produced in certain aircraft simulator experiments [32, 33].

Several thermal degradation processes occur without the requirement for oxygen and these do still result in fuel degradation and deposit formation in an oxygen free environment. However deposit-forming pyrolysis reactions not involving oxygen occur at higher temperatures. As in all aspects of the chemistry of thermal stability, inevitably, fuel composition will influence the eventual outcome of lowering oxygen levels [7, 34, 35].

Studies have been made on the effect of fuel composition and environments on deoxygenated systems; results suggest that deposits are formed by chemical reactions and physical processes different from those in oxygenated systems. Dissolved metals were generally less deleterious with respect to deposit formation in deoxygenated fuels.

The effects of adding specific sulphur compounds are complicated. Generally, the addition of polysulphide, disulphide and thiols all result in an increased rate of deposition; alternatively condensed thiophenes do not, and in some cases even act as inhibitors [7, 34, 35].

Nitrogen compounds were studied e.g. pyrroles, pyridines, amines, piperidines, amides and decahydroquinoline, and were found not to promote deposit formation, the opposite effect to when fuel is aerated (oxygen present) [1, 35].

Oxygen containing compounds (e.g. hydroperoxides, carboxylic acids, phenols, furans, alcohols, ketones and esters) show a variety of effects, with peroxidic compounds shown to be highly deleterious, regardless of structure, even when added at quite low concentrations [1, 35].

Hydrocarbon types have been investigated (i.e. paraffins, naphthenes, aromatics, olefins, etc.). The only group which affected fuel thermal stability was olefins, again with variable magnitude depending on structure. The most deleterious unsaturates with respect to deposit formation included the acetylenes, di-olefins and mono-olefinic aromatics with conjugated side chains [1].

Synergistic effects can occur between certain compounds in fuel resulting in increased deposit formation or inhibition [1].

Future Fuels – Chemical Considerations

Future fuels either as 'broad' specification types or fuels derived from coal, shale oil and/or tar sands etc. could have significant compositional differences when compared with the currently available fuels. The introduction of any of these fuels, either as a supplement to existing jet fuels or neat, could generally result in an increase in heteroatomic compounds present with the resultant lowering of fuel thermal stability [36, 37]. These compounds are active participants in fuel degradation reactions (see Section 2.4.2).

Relaxation of existing fuel specifications can help alleviate the problems of steeply rising costs and diminishing supplies by reducing refinery costs and the use of previously unacceptable 'out of specification' aviation fuels. Further the current practice of cracking the heavy crude fraction to produce gasoline, diesel and heating fuels will be used more extensively to produce more distillates and less gasoline and fuel oil. The use of such distillates including higher boiling components for incorporation into aviation fuels could aggravate thermal stability problems. At a USA symposium, predictions were made for the properties associated with 'broad' specification fuels. The consensus of opinion has resulted in guidelines for an Experimental Referee Fuel (ERBS), to be used in correlation testing and R&D programmes [38, 39]. It was similarly agreed that thermal stability threshold temperatures would be lowered.

There have been several investigations into the development of 'high stability jet fuels', primarily designed for military applications, i.e. Mach 2–5 aircraft types. Basically, selected jet fuels are used, 'stripped' of certain dissolved metals, fuel compounds actively associated with fuel degradation and certain anti-oxidant type additives have been administered [40, 41, 42].

As discussed earlier even minor modifications in fuel composition can alter the thermal stability characteristics and hence lead to changes in its tendency to produce deposits. The magnitude of such compositional changes will be variable and will depend on the extent of refining the base stock undergoes. However, any refinery treatment beyond current practices could result in cost increases and so compromises between fuel quality and economics will be sought. Furthermore a closer definition of the engine fuel stability requirements is needed. It is predicted that even with 'typical' refinery processing, alternative fuels will contain generally higher concentrations of heteroatomic compounds, particularly fuel-bound nitrogen, and possible dissolved metals (it seems that nitrogen compounds are much harder to remove than trace impurities). The thermal stability of fuel has been shown in Fig.5 to be related to the nitrogen content of fuels.

Storage Effects

Some aviation fuels are often stored for long periods of time, e.g. in strategic reserves primarily for military purposes, and therefore must be resistant to degradation. During long term storage fuels can degrade, producing sediments and/or suspended matter which can cause aircraft and/or storage site problems. For example failure to start engines, loss of power in flight and reduced refuelling rates as a result of filter blockage. Further, chemical degradation products can remain in the fuel and lead to problems in the aircraft fuel system. For example, 'peroxides' formed in fuel can severely attack some fuel system elastomers, causing them to harden, become brittle and crack. This type of elastomer failure can result in system malfunction or complete failure to operate.

Several studies [32, 43, 44] have been undertaken to evaluate storage characteristics of various 'future' fuels, with the main conclusions reached as follows: –

- (a) many of the future fuels will most probably contain significantly higher levels of 'fuel-bound' nitrogen compounds, recognized as active participants in fuel degradation. Hence, depending on the degree of refining carried out many such fuels could have poor storage qualities.
- (b) Removal of nitrogen compounds from a previously poor fuel, can improve its storage characteristics, however, the process of removing such compounds is difficult and costly.
- (c) Not all nitrogen compounds are deleterious.
- (d) Interaction of some nitrogenous compounds with carboxylic acids can be particularly potent in promoting deposition tendencies.

A2.4.3 Evaluation of Fuel Thermal Stability

Laboratory tests to measure deposit forming tendencies of fuel have covered the extremes from simple glassware tests to large scale simulator rigs that "mock up" engine fuel systems [1]. In the following sections various tests that examine aspects of deposit formation in fuels are discussed:

- small laboratory tests that sense chemical or physical effects
- bench tests that simulate part of an engine fuel system
- large scale rig tests that use actual engine fuel system hardware
- combustor type rigs based on operating engines.

While each of these tests for thermal stability exposes fuel to temperatures typical of engine fuel systems it is recognised that some fuels held at ordinary storage temperature can form deposits with time. This aspect of fuel stability is labelled "storage stability" even though the chemistry of deposit formation is closely related to the chemistry involved in the higher temperature deposit-forming reactions in engines.

Bulk Storage Stability

Fuels are expected to remain stable for many years in storage in different climates under varying temperature conditions. It is necessary, therefore, to ascertain the storage capabilities of fuels, particularly new "synthetic" fuel derived from coal. Storage tests necessitate the use of accelerated aging techniques using elevated temperatures for a finite period of time in relatively small containers. The validity of these results applied to bulk storage in large vessels is questionable. For this reason, unlike thermal stability, there is no specification test for storage stability in current or future turbine fuel specifications. An accelerated ageing test (ASTM D873/IP138) run for 16 hours in an oxygen bomb at 100°C has been used to predict the storage stability of aviation fuels. It was found that fuels which passed the JFTOT at 260°C always passed the accelerated ageing test so the latter was dropped from specifications as being redundant. Whilst certain other distillate fuels do have "storage tests" specified, current views are that no single test, and in some cases mixed tests, can be guaranteed to predict the safe life of fuel in store. With the exception of "long term barrel storage" type testing most tests consist in the main of maintaining fuel at elevated temperatures for a relatively short period of time with the fuel quality being monitored throughout the test duration by means of assessing colour, formation of insolubles, gum and JFTOT (Coker) ratings [45]. Some experimental work has been undertaken to determine the effect of trace impurities such as nitrogen compounds on fuel storage capabilities (see Section 2.3.2) [44, 46, 47].

Most synthetic kerosines demonstrate thermal stability problems during experimental ageing tests, resulting in increased deposit formation rate together with the lowering of JFTOT break-point temperatures. Further there is often a rise in the existent gum determined after storage, as compared with the 'as received' fuel [12, 44, 46, 73].

Small Laboratory Tests that Sense Chemical or Physical Effects

These tests depend on heating a relatively small volume of fuel under set conditions for a set time and measuring the fuel degradation by some form of light transmission technique. Examples are the Phillips 5 ml bomb [48] and the Thomson Flask Test. These are effective screening tests but their repeatability is poor.

Another small fuel volume unit is the 'Minex' which uses a capillary tube heat exchanger and measures the difference in heat transfer coefficient over a range of operating conditions [49, 50].

Dynamic Bench Tests

ASTM-CRC Coker

For quality control purposes, aviation turbine fuel specifications now include a measurement of thermal stability within the specific requirements for the fuels. The two current tests are the ASTM-CRC Fuel Coker and the Jet Fuel Thermal Oxidation Tester (JFTOT). The Coker is the original method. It involves passing fuel, at predetermined flow rates, over a preheated tube and through a heated, sintered 20 µm filter [1, 51, 52]. The evaluation is by visual appearance and the colour of deposits on the preheater tube, as well as the pressure differential across the filter. Normal specification conditions are as follows:

Preheater exit temperature	= 422 K (149°C)
Pressure	= 1170 KPa (150 psig)
Filter temperature	= 477 K (204°C)
Fuel flow rate	= 4.5 l/hr
Test duration	= 5 hrs
Fuel test volume	= 22.7 litres

The long standing use of the ASTM-CRC Coker for determining fuel thermal stability has not been universally accepted as a specification method, e.g. it is no longer featured in the UK aviation turbine fuel specification. There is a relatively large sample size requirement of 22.7 litres (5 gallons). In addition problems have been encountered with pump control and pump wear. The control of heat input at the preheater tube is via fuel temperature although the fuel is exposed to higher temperatures at the tube wall.

Wide differences exist between the operating conditions used in the Coker and those found in the actual aircraft engine fuel system. For instance, the test duration, for practical purposes is minute, the flow rate very low, and only a single heat input is provided. This method is a compromise between a quick and practical method of maintaining quality assurance with respect to thermal stability and reproducing an actual engine condition (simulation).

There has been a consistent problem in evaluating the tube deposits themselves. The deposit rating has relatively poor precision and accuracy, primarily due to the fact that colouration is "assessed" rather than making a qualitative measurement of thickness or amount; the latter would be preferred by engine manufacturers.

Two modified versions of the ASTM-CRC Fuel Coker have been used, a high temperature unit called the Research Coker for testing advanced military fuels and a Gas Drive Unit [53] to eliminate the pump problems and reduce the sample volume requirements. For research purposes, new techniques for quantitative deposit measurements have been developed.

Jet Fuel Thermal Oxidation Tester (JFTOT)

The JFTOT (ASTM D3241/IP323) is a relatively new method currently specified in aviation turbine fuel specifications. A schematic diagram of the JFTOT apparatus is shown in Fig.6. After aeration, prefiltered fuel is passed over a heated polished aluminium tube and through a nominal 17 μm filter [1, 5, 54, 55]. As specified, evaluation is by an assessment of the colour of the tube deposits formed and filter blockage measured by pressure drop.

Normal specification operating conditions are as follows:

Pressure	= 3448 KPa (500 psig)
Duration	= 9000 secs (150 minutes)
Fuel Test volume	= 0.6 litre
Flow rate	= 0.24 l/hr

For specification purposes the JFTOT is run at a test temperature of 533 K (260°C) to achieve a "Pass" rating as defined by rating the tube for deposits and measuring the pressure drop across the filter. To assess the limit of thermal stability of a fuel the test may be run at two or more temperatures and from this the temperature at which the fuel just passes the test may be determined. This is called the "break-point" temperature for fuel thermal stability.

Certain difficulties inherent in the coker method also apply to the JFTOT namely the discrepancy between test and aircraft operating conditions and the problem of evaluation of tube deposits. On the other hand, the JFTOT shows advantages over the Coker method in the requirement of a much smaller sample size, high pressure operation permitting high fuel temperature without vaporization and control of the tester by regulation of the preheater tube temperature corresponding to the maximum temperature exposure of the fuel.

Extensive research and development are being undertaken to improve the technique and use it as a research tool. Incorporation of a heated fuel reservoir provides an opportunity for evaluating bulk fuel temperature effects. From results available so far, it would appear that the mini-heated reservoir type JFTOT could prove to be a better quality control device for fuel thermal stability, certainly so for future fuels. New and existing techniques for tube deposit assessment have been developed and updated [56], to determine the weight of carbon in the deposits via pyrolysis [13, 34] and β -ray backscatter [57] measuring the weight and thickness of tube deposit respectively. Work on the pyrolysis technique for the quantitative measure of carbon deposits resulting from fuel degradation is still being actively pursued since this method shows promise. Further it is being used with success in conjunction with some current "simulator" burner fuel manifold -- fuel degradation test rig work [58].

Microphotographs of deposits formed in the JFTOT apparatus are shown in Fig.7. It is noteworthy that the character of the deposits changes significantly from spherical agglomerates at the lowest temperature to melted aggregates and then to coke-like particles at higher temperatures.

The JFTOT apparatus has also been used in investigating the effects of various metals and metal alloys on fuel instability leading to deposit formation [59, 60].

Future fuel types, i.e., 'broadbase specifications', synthetic fuels from coal, shale oil, tar sands, etc. have been evaluated using the JFTOT [38, 60]. Basically, break-points, tube ratings and ΔP indicate a general lowering of fuel quality with respect to thermal stability. However, the results are also dependent on the degree of refining for each individual fuel.

Small-Scale Flow Testers

While acceptable for quality control purposes, the Coker and JFTOT are unsuitable for quantitative assessments of deposit buildup. Research testers have been developed to expose the fuel to flat, heated surfaces rather than tubes, thus producing more uniform surface deposits that can be measured. In a test apparatus at the United Technologies Research Center [61], fuel is pumped through a rectangular channel with several removable heated plates. Measurement of the deposit thickness formed on the plates yields useful correlation of fuel degradation as influenced by concentration of

specified heteroatoms and hydrocarbon constituents. A more refined apparatus capable of operation on a wide range of fuel flows and surface temperatures is currently being updated at the NASA Lewis Research Center [62].

Large Scale Rig Tests

As already stated, when using small scale test devices there remains the problems of relating results to actual engine performance. Test simulators have evolved in order to minimise such problems. Simulators are all primarily concerned with fuel degradation within the engine fuel system either duplicating some specific unit or attempting to demonstrate an operational problem. Hopefully they will provide a link between results obtained using the small/medium scale laboratory devices and the aircraft engine. The majority of test simulators both past and present have generally been designed for specific aircraft, components or environments.

There are, however, several difficulties which arise from operating test simulators such as:--

- high fuel consumption
- long test duration
- fuel quality changes due to long test duration
- the limited nature of some data obtained due to the degree of specificity of the particular simulator.

General Electric Co. GE4 Fuel System Simulator

This simulator [47] was designed and operated to subject certain fuel system components to the extremes of fuel system conditions envisaged during a supersonic flight of the Boeing SST. From the results obtained predictions were made of component life and correlations made with the Minex fuel/oil heat exchanger device [49, 50].

Shell/Thornton Half-Engine-Scale Fuel System Rig

This simulator [3, 13, 63] was designed for predicting the fuel requirements for the supersonic 'Concorde'. The rig was used in a wide ranging programme, covering many aspects of fuel thermal stability, viz:

- (a) correlation of rig to the CRC coker results
- (b) the effect of fuel pump materials of construction, fuel recirculation and reduced tank pressure -- none of these factors were reported to have any significant effect.
- (c) utilisation of a fuel of known, poor thermal stability -- results indicated that heat exchanger and atomisers were sensitive to degradation products. The nature of deposits could vary greatly and importantly deposits were formed in heat exchangers at fuel temperatures as low as 100°C.
- (d) the effect of fuel additives namely corrosion inhibitors, static dissipator additives and fuel soluble icing inhibitors on thermal stability was investigated -- some adverse effects were noted with HITEC E515 (corrosion inhibitor/lubricity aid).
- (e) the effect of 'Merox' refinery treated fuel was investigated -- initially, high deposit rates were encountered which increased on addition of HITEC E515. However, further repeat tests did not corroborate the initial findings.

No problems due to fuel degradation, have been reported from 'Concorde' operations so far.

Advanced Aircraft Fuel System Simulator (USAF)

This rig [64] was designed for the investigation of advanced hydrocarbon fuel performance, with respect to thermal stability, at high Mach number flight conditions.

Results from experiments indicated:--

- (i) large amounts of deposits in wing tanks at 260°C due to oxidation reactions in the liquid phase, created by vapour condensation on the cooled internal boost pump.
- (ii) deaeration of fuel markedly reduced deposit rates in steady state manifold tests on Jet A fuel in the Simulator [65] as shown in Fig.8.
- (iii) increased aromatic content of fuel up to 25% (by xylene doping) did not increase deposition [66].
- (iv) a future fuel type (shale-oil derivative) was evaluated -- no significant differences in results noted when compared to normal F-40 fuels [66, 67].

Combustor Sector Testing

Several engine combustor systems have been rig tested to evaluate fuel property effects on performance and durability. A wide range of fuel types/composition have been used to simulate the possible use of broadened specification and synthetic fuels in the future.

General Electric Company -- F101 Engine Combustion System

The F101 augmented turbofan engine main combustion system was investigated [68] at various engine conditions with respect to performance, emissions and durability, including nozzle fouling. Test fuels covered a range of hydrogen contents (12.0 - 14.5 wt %), aromatic types (monocyclic and bicyclic), initial boiling points 12°C - 100°C, final boiling points 279°C - 406°C and viscosity 0.83 - 3.24 mm²/sec at 27°C.

With reference to nozzle fouling the results indicated that

- (i) extended cyclic testing revealed significant effects of fuel type and temperature on nozzle life.
- (ii) the results correlated with laboratory JFTOT thermal stability ratings of the fuels based on tube deposits alone (Fig.9).

General Electric Company -- J79 Engine Combustion System

Testing at engine conditions was undertaken on the J79-17A turbojet engine combustion system [69] similar to that performed on the G.E. F101 Engine Combustion system with identical test fuels.

Results for nozzle fouling tests revealed there was no significant fuel or temperature effect; however, test duration was abnormally low (5 hrs).

Detroit Diesel Allison -- Current High Pressure Ratio, Can-Type Turbine Combustion System

Engine testing of the TF41 high pressure ratio, cannular, turbofan combustion system [70] was undertaken using a variety of fuel types/compositions. A special 1 hour fouling/deposition test at modified sea level take off conditions was run on each fuel; very little fouling or carbon deposition occurred.

General Electric Company -- J79 Smokeless Combustor

Engine testing of the J79-17C turbojet engine combustion system [71] was undertaken similar to those described previously for the GE F101 Engine Combustion System; results were similar.

A2.4.4 Aircraft/Engine Problems and Future Trends

Historically, problems attributed to poor fuel thermal stability date back to the mid 1950s. Voluminous evidence [1] points to the formation of insoluble fuel degradation products, e.g. gums, hard lacquers and friable cokes as one of the major causes of aircraft/engine fuel related problems.

The formation of insoluble fuel degradation products occurs chiefly as a result of oxidation reactions influenced by heat stressing the fuel for significant periods of time.

Heat stresses arise from the increasing use of aviation fuel as the primary aircraft/engine "heat sink" together with the trend towards higher engine environmental temperatures (associated with higher compressor exit temperatures). The magnitude of such stresses is governed by a variety of considerations [1, 2, 5, 6, 72, 73, 74, 75]:

- aircraft/engine performance
- operational role (i.e. supersonic, subsonic, high or low altitude)
- cooling of engine lubrication oil and ancillary aircraft equipment
- engine environmental temperatures

Aircraft Considerations

During supersonic flight, significant aerodynamic heating of the aircraft occurs, particularly in specific military applications requiring low altitude high speed flight [72]. A proportion of this heat energy is transferred to the fuel, the amount being influenced by the design and construction of the wing and tanks [6, 74]. SST simulators have shown that prolonged supersonic flight at Mach numbers above 2.7 will produce insoluble deposits from residual fuel liquid and vapours in wing tanks [74]. Whilst aerodynamic heating is not a problem with subsonic aircraft both types can experience fuel thermal instability due to engine factors.

Engine Considerations

In the engine fuel system, fuel heating occurs via pump pressurisation, flow control mechanisms, high environmental engine temperatures and the fuel role as coolant in heat transfer systems. Fig.10 indicates the fuel temperature at the nozzle under typical flight conditions. In each of the fuel system components the residence time is usually sufficient to initiate oxidation reactions leading to deposit accumulation with unstable fuels. There is now a wealth of evidence which has gone some way to elucidate and quantify some of the major engine parameters which influence the mode, magnitude and rate of fuel thermal degradation and deposit formation [1, 2, 3, 5, 6, 17, 61, 75, 76]:

- Metal-fuel contact temperatures
- Engine environmental air temperatures
- Fuel flow (laminar, turbulent, residence time, velocity)
- Fuel pressure (presence of vapour)
- Surface to volume ratio
- Materials of construction in contact with fuel
- Surface finish
- Mechanical stress
- Bulk fuel temperature

Insoluble fuel degradation products can affect engine/aircraft operation either by depositing in areas of maximum temperature input or as suspended and "breakaway" solids arrested in areas removed from the sites of formation. Several features of the fuel system are prone to deposit problems, viz [2, 72, 75, 76, 77, 78]

- moving parts of close tolerance
- intermittently moving parts, e.g. fuel shut off cocks
- calibrated mechanisms
- orifices and narrow bores
- filters
- bearing assemblies
- 'catalytically' active areas
- heat transfer points
- centrifugal collecting areas
- fuel manifolds, fuel injectors, nozzles and flow equalizers

Fig. 11 depicts a fuel injection nozzle exhibiting deposits from fuel degradation. No problems have been reported with afterburner nozzles which have large orifices and are therefore not so susceptible to blockage by deposits. Deposits can affect engines directly by the malfunction of flow equalizers, fuel control systems, and loss of efficiency of heat exchangers or indirectly by the premature deterioration of 'hot section' engine parts as a result of maldistributed combustor exit temperature profiles caused by deposits in the fuel nozzle producing an uneven spray pattern. Fuel spray maldistribution can have an adverse effect on engine starting and relight, temperature scatter in the turbine section, smoke emission and erosion of "hot end" parts; the latter being the result of increased smoke and/or spalling of carbon deposits from the combustor.

Engine Development

The evolutionary trend of engines manufactured for commercial aircraft reveals that the quest for higher performance and superior fuel efficiency is resulting in ever increasing fuel, oil and environmental engine temperatures. For example, compressor discharge temperatures have risen from about 280°C in the late 50s to approximately 500°C at present. Future high efficiency engines and high compression ratio core engines will have maximum compressor discharge temperatures above 600°C.

To achieve greater engine cycle efficiencies and reduce specific fuel consumption with both current and future fuels engine developments are demanding increased pressure ratios and more sophisticated fuel systems, including fuel injection equipment. Higher engine pressures will inevitably raise fuel temperatures, thus rendering such fuel systems more susceptible to fuel thermal instability problems.

Thermal stressing and long term storage of certain fuels could well lead to the production of fuel insoluble compounds, particularly so with fuels which are predicted for the future, ex coal, shale, tar sands and "cracked" heavy hydrocarbons as personified by the AGARD and ERBS type reference fuels. Higher pressure ratio engines and supersonic aircraft will exacerbate this situation through their inherently higher thermal loadings on the fuel systems of engines and aircraft. The presence and deposition of these insolubles within the fuel system, particularly the fuel injectors may lead to fuel spray maldistribution problems which, could, directly or indirectly have an adverse effect on engine starting and relighting, temperature scatter in the turbine section, smoke emission - engine signature, and erosion of "hot end" parts, the result of increased smoke and/or spalling of carbon deposits from the combustor. Adherence of fuel degradation products to moving parts can also result in fuel system component malfunction and failure. Deposition within heat exchangers can result in reduced cooling capabilities which could raise engine oil temperatures with potential adverse effects on engine lubrication.

The overall impact of fuel thermal instability on engine efficiency, performance and durability is likely to require considerable design and development work to maintain current levels.

A quantitative definition of factors influencing thermal stability is vital, including both aircraft and engine data with AGARD research type fuels. Of equal importance is the requirement for laboratory specification-type methods for evaluating fuel thermal stability in line with engine experience (which may require a "rig" to act as an intermediate step between the engine and laboratory testing).

With strategic fuel stocks in mind, storage stability could be an acute problem; so far as could be ascertained, no single existing test method is able to predict storage life with any degree of accuracy. Studies in this field will be necessary.

A2.4.5 Conclusions

- (1) Jet fuel instability is an important property with a potentially powerful impact on storage life and on engine/ aircraft readiness, maintenance and efficiency.
- (2) Key engine areas potentially at risk as a result of fuel degradation are starting and relight, engine performance, fuel consumption, maintenance and emissions.
- (3) Heat stressing of aircraft fuels at temperatures as low as 100°C can cause fuel degradation with the resultant formation of fuel insoluble compounds; similar reactions can occur when certain fuels are stored for long duration at ambient temperatures.
- (4) While the mechanisms of deposit formation are not thoroughly understood, oxygen and fuel composition, particularly hetero-compounds and metals are known to play major roles.
- (5) If the current diverging trends of increasing engine and aircraft temperature and reducing fuel stability continue, especially with the introduction of alternative fuels similar to AGARD Research Fuel, the engine deposit problem will get worse.

A2.4.6 Recommendations

- (1) It is recommended that fuel system design be directed toward minimizing heat input to the fuel from components to lower their vulnerability to deposits.
- (2) Reliable engine/laboratory interface test data are needed to provide better design data and a more realistic fuel quality control specification requirement.
- (3) New laboratory methods should be identified and defined to evaluate fuel thermal stability in line with engine prediction.
- (4) More research relating fuel composition to deposits should be undertaken, particularly for fuels from alternative sources such as shale, tar sands, coal liquids, etc.
- (5) The role of metals, foreign matter and different additives in catalyzing or influencing deposit forming reactions should be investigated, including the effect of metal surface finish.
- (6) Research on additives and refinery processing techniques to improve fuel stability should be intensified.
- (7) More full scale engine tests of future fuels should be conducted.
- (8) A test for predicting storage stability should be developed.

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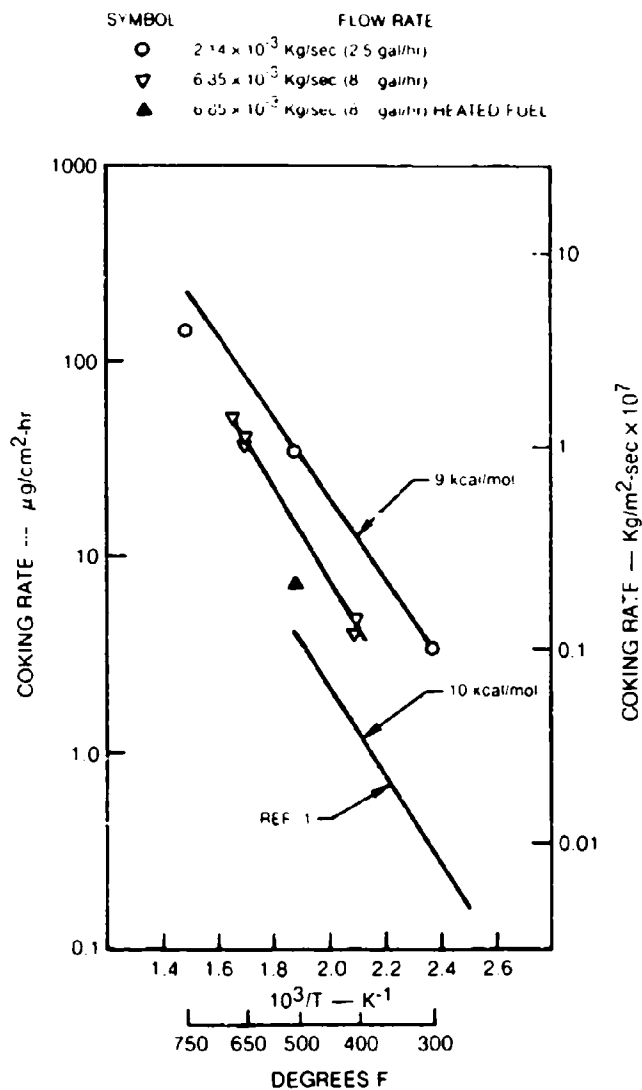


Fig.1 Arrhenius plot of coking rate in Jet A

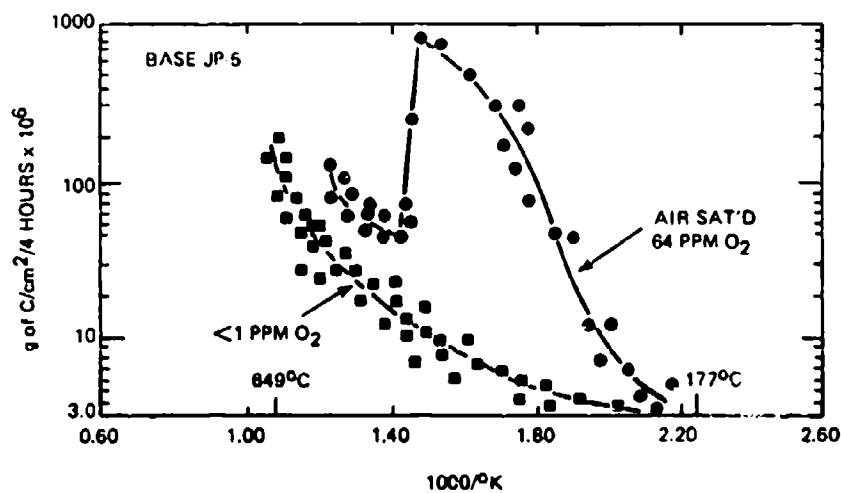


Fig.2 Deoxygenation can markedly decrease deposit formation

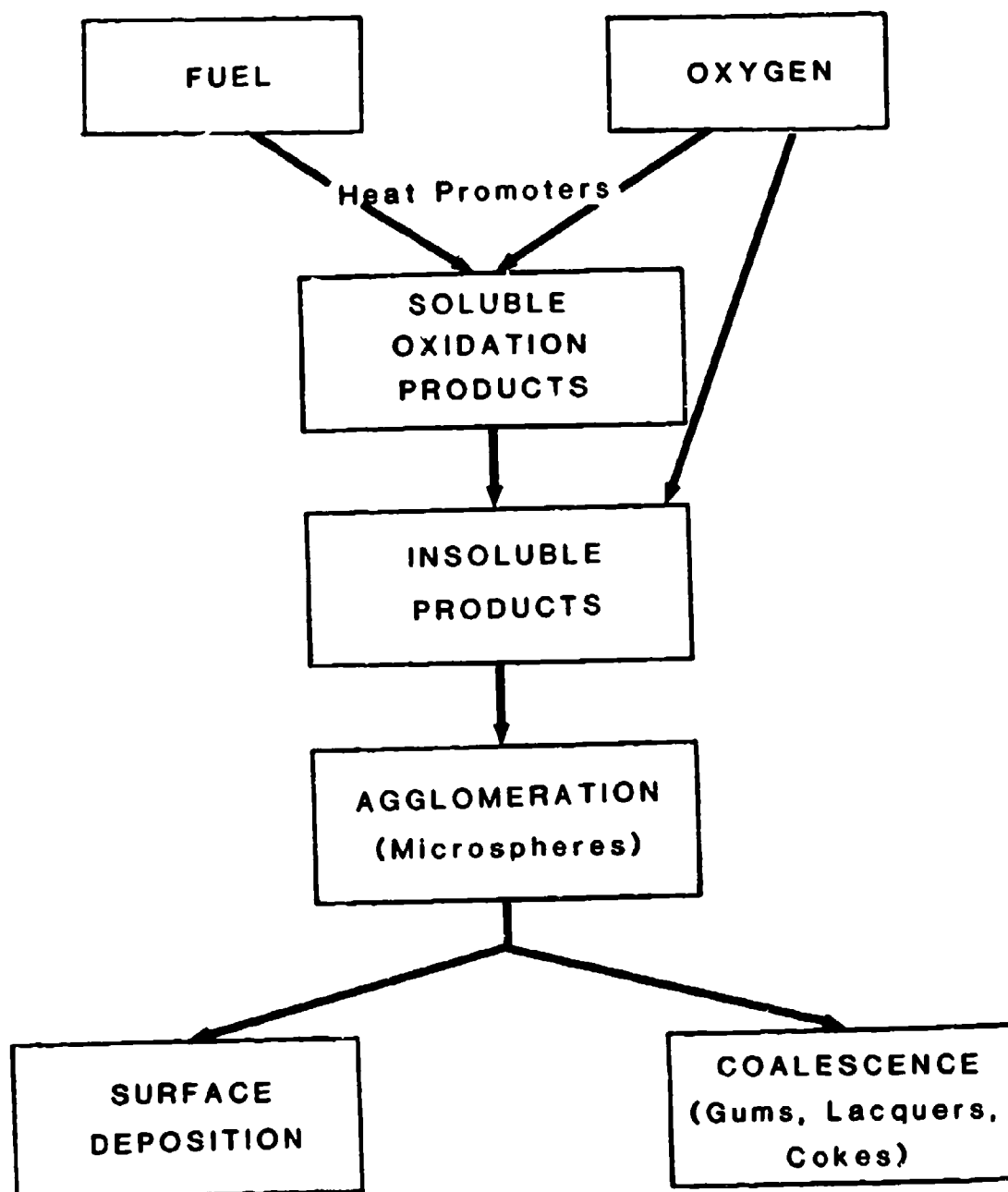


Fig.3 Mechanism of deposit formation

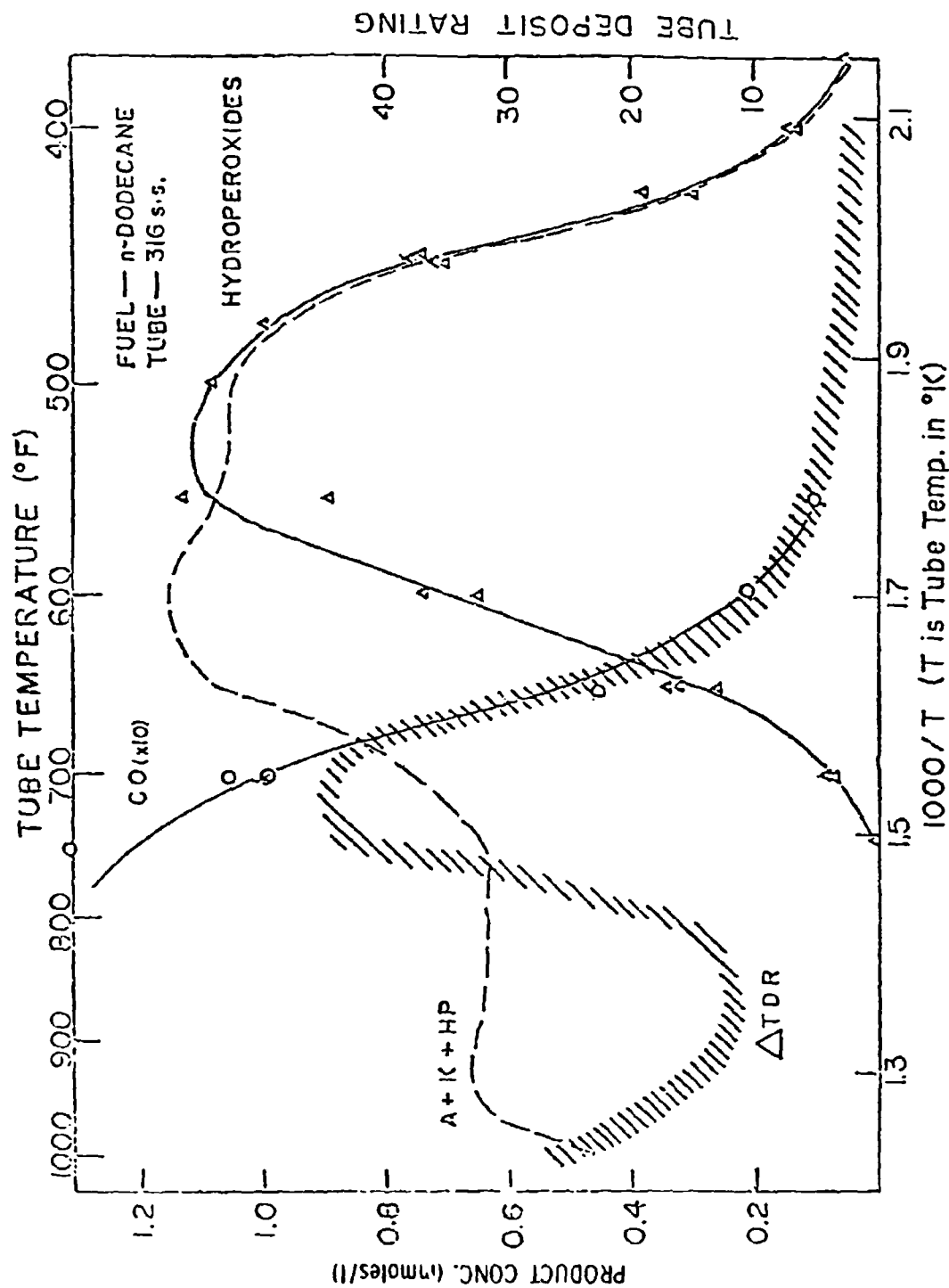


Fig.4 Reaction products and deposits from thermal stability testing

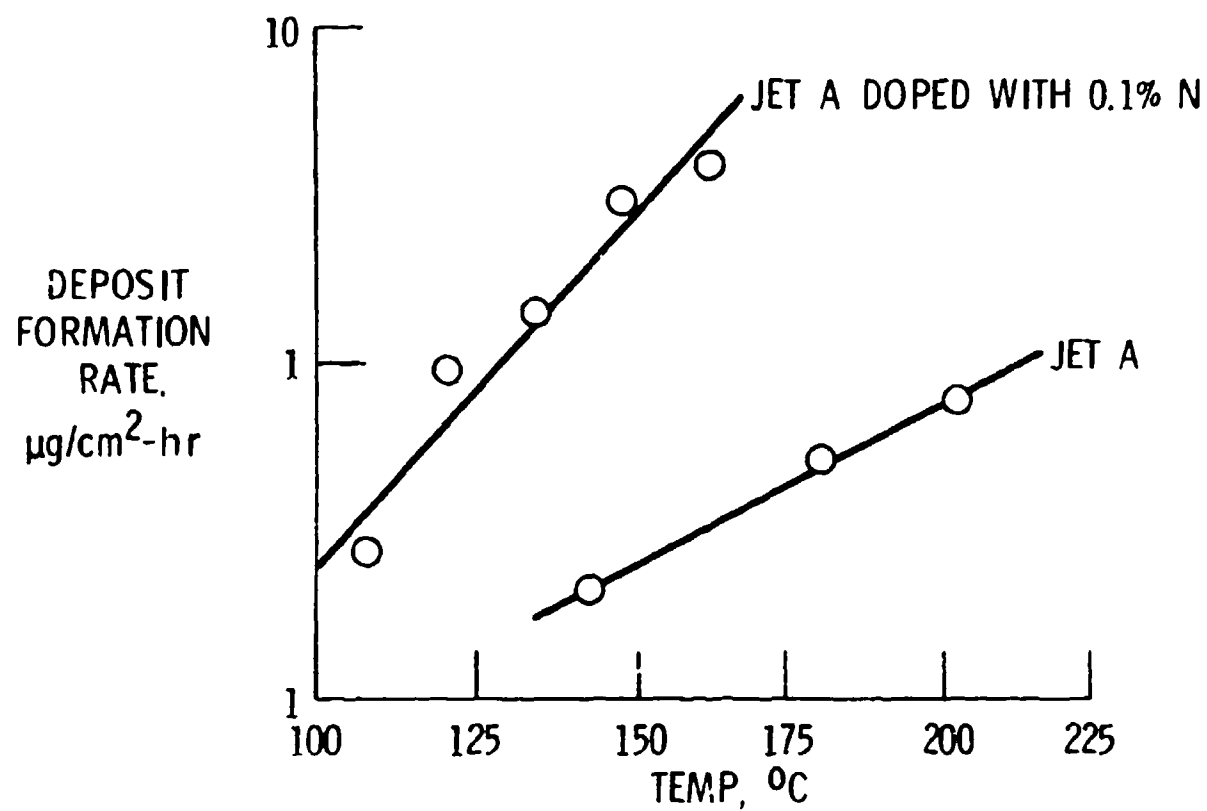


Fig.5 Effect of fuel nitrogen content on thermal stability

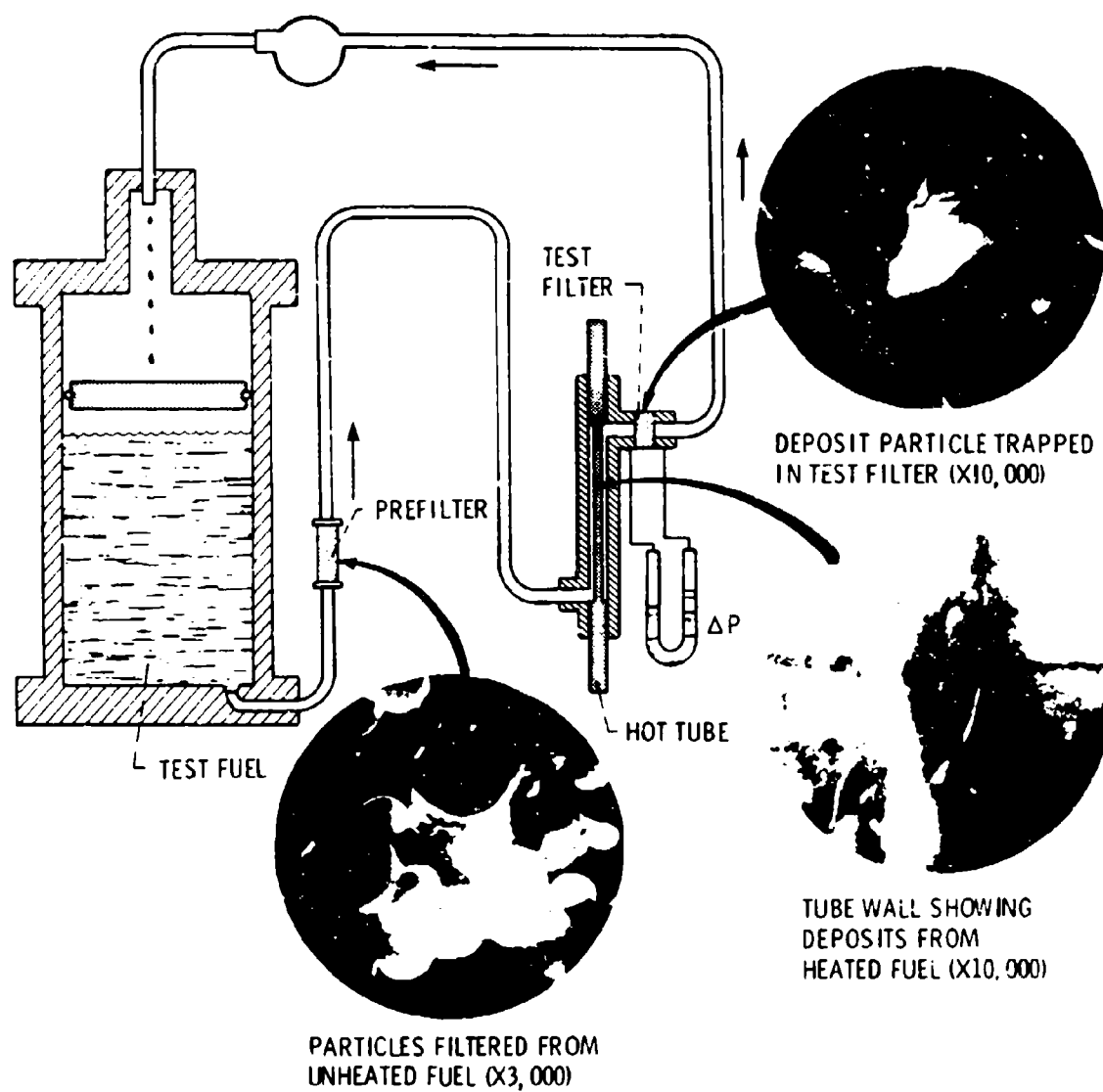
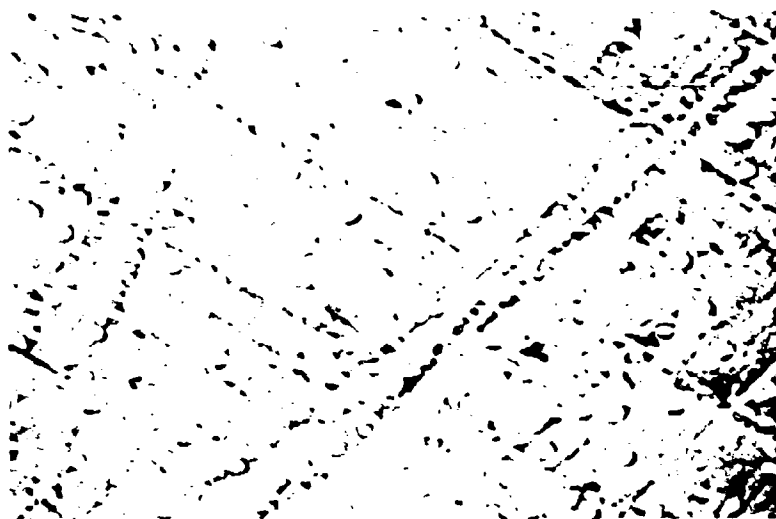


Fig.6 Jet fuel thermal oxidation tester (JFTOT) schematic



280 °C



300 °C



340 °C

Figure 7

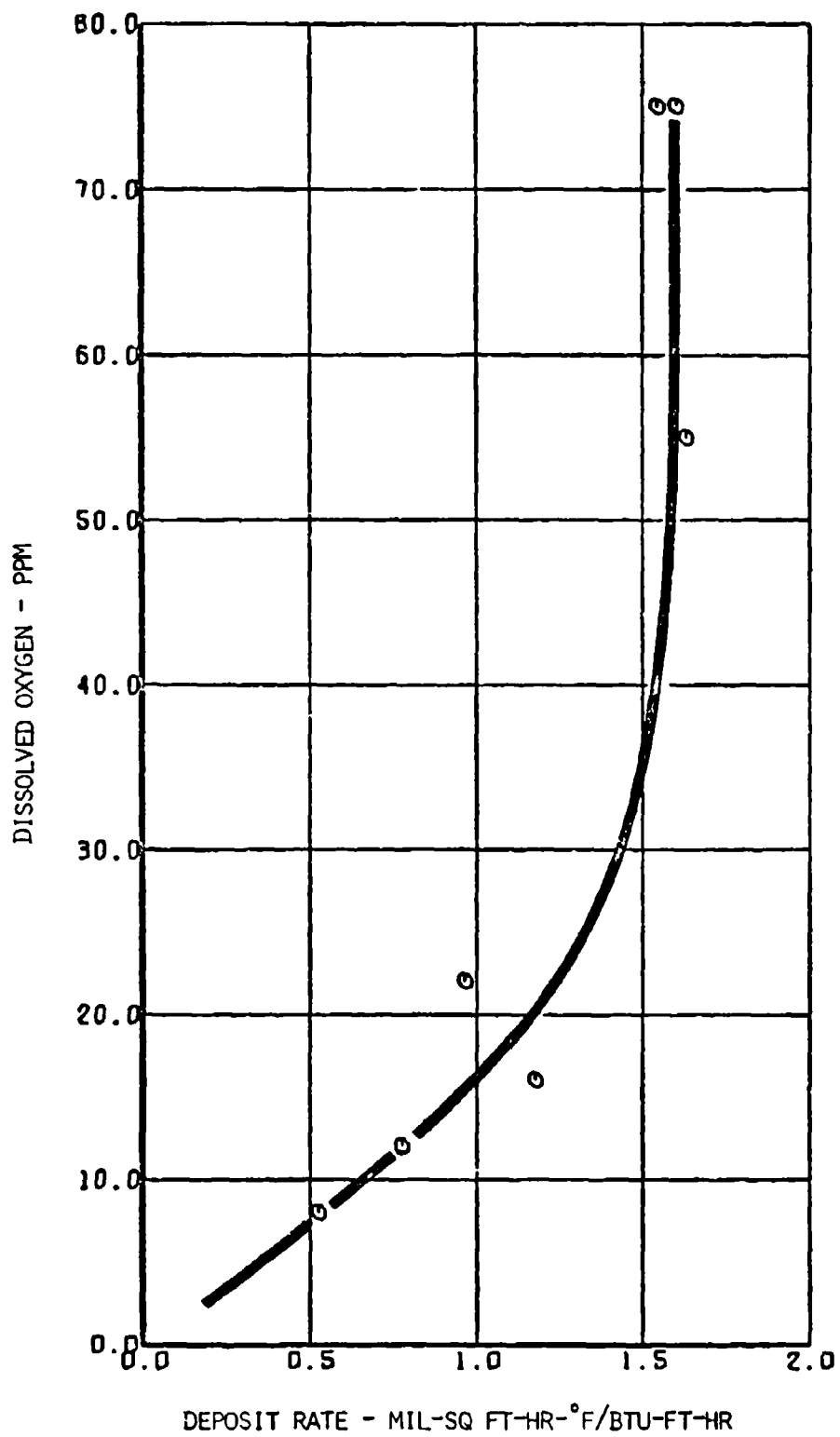
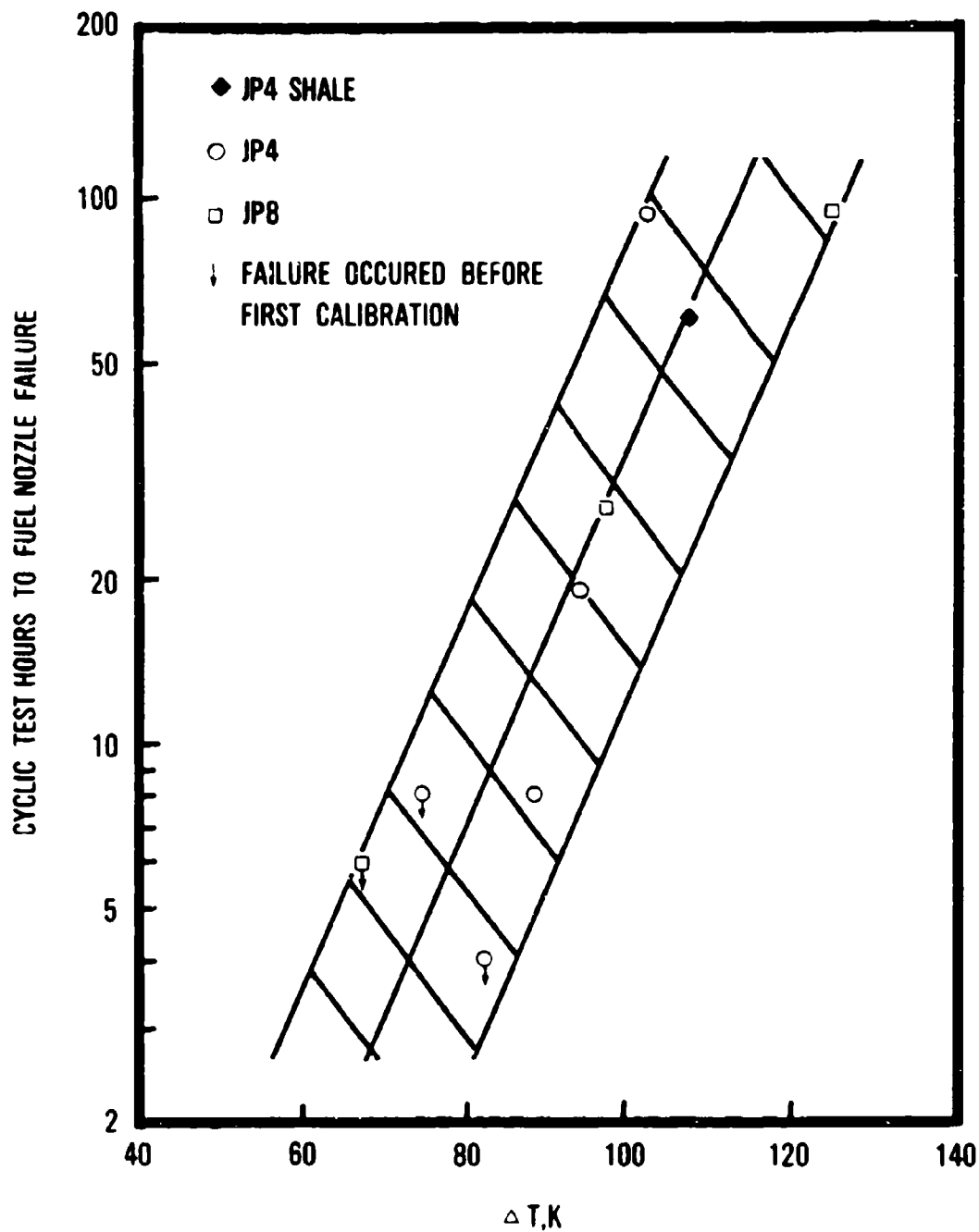


Fig.8 Effect of oxygen in the simulator



(FUEL BREAKPOINT TEMPERATURE LESS FUEL SYSTEM TEMPERATURE)

Fig.9 Fuel thermal stability/F101 fuel nozzle life

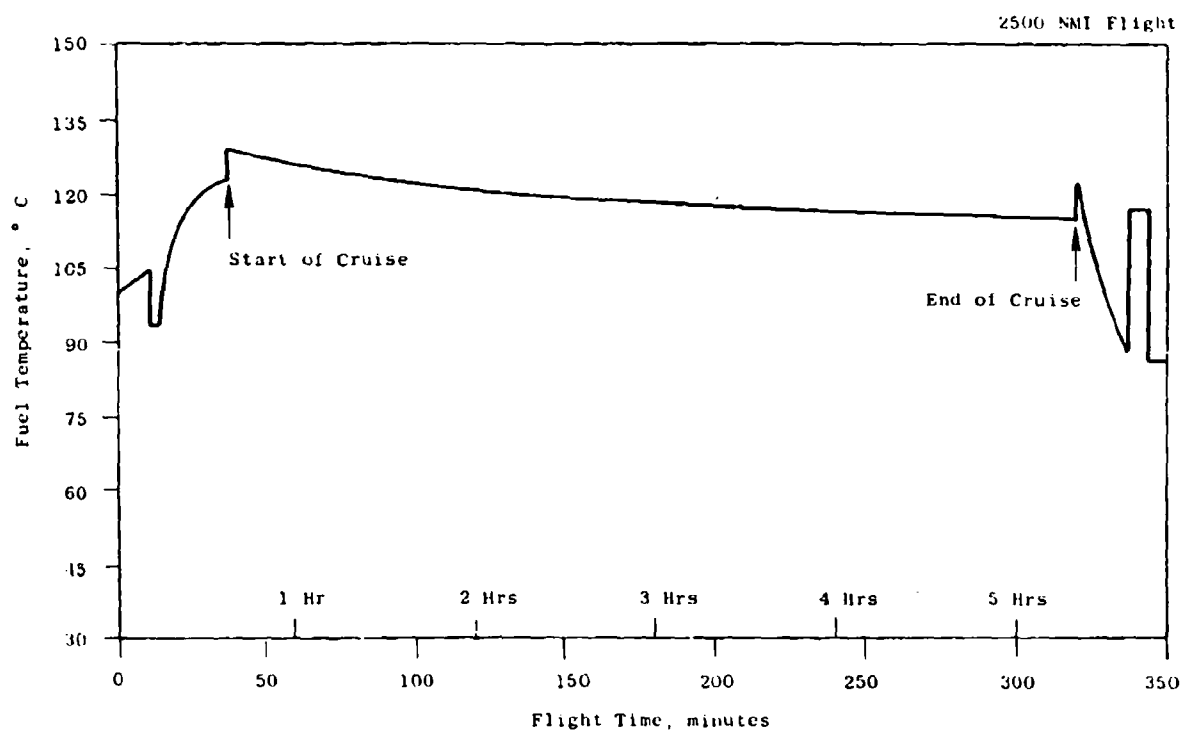


Fig.10 Fuel temperature into nozzle with ECS bleed air WHRS
(ECS -- Environmental Central System, WHRS -- Waste Heat Recovery System)



Fig.11 Nozzle fouled with fuel deposits

A2.5 FUEL FLOW CHARACTERISTICS

A2.5.1 Scope

This section assesses the potential impact of aviation turbine fuel property variations on the fuel flow characteristics within aircraft fuel systems. A literature survey was conducted to identify significant hydrocarbon fuel property variables and their effects on the performance, operating envelope, durability, maintainability, reliability and safety of aircraft fuel systems. The study is mainly focused on problems related to fuel system flow, pumpability and materials compatibility. This study incorporates significant portions of recent NASA survey papers by Friedman [1]* and Reck [2]. Considerable background perspective on the subject of aircraft fuels and fuel property effects on fuel systems may be obtained from Barnett and Hibbard [3], Smith [4], and Coordinating Research Council [5]. Fuel properties of most concern to the aircraft fuel system are identified and the significance of changes in these properties are examined. The status of recent fuel system research activities is reviewed and technical areas requiring more data on fuel property characteristics and effects are identified together with recommendations for needed research.

A2.5.2 Fuel Properties of Significance to Aircraft Fuel Systems

The fuel properties that have the most significant effect on flow, pumpability and material compatibility within the aircraft fuel system are fluidity, chemical composition, volatility and contaminants. The fluidity properties, which include freezing point and viscosity, influence the flow behavior and pumpability of fuel at low temperatures. The compatibility of fuels with polymeric materials within the fuel system including elastomers and seals may be affected by chemical constituents such as aromatics and sulfur. Corrosion of metals and alloys within the fuel system may result from the presence of sulfur compounds in the fuel. Fuel volatility, as characterized by flash point and vapour pressure, influences aircraft safety. Contaminants which may affect fuel system cleanliness include water, dirt, and surfactants.

The properties of the proposed "AGARD Research Fuel" (ARF) are presented in Table I of the Executive Summary for comparison to the requirements for JP-8 (NATO F-34) and JP-4 (NATO F-40). The ARF properties designated as R (Report) values are dependent on the other limiting properties. The limiting range for hydrogen content corresponds to an approximate range of aromatics content of 24–32 volume percent. The maximum freezing point for ARF is -30°C compared to a value of -50°C for JP-8. Limits for total sulfur, flash point, and the various tests for the control of corrosion or contaminants are maintained comparable to current aviation kerosine, JP-8. The maximum viscosity at -20°C for ARF is 12 cS, compared to a value of 8 cS for JP-8.

Fluidity

Freezing point – Limits placed on freezing point as well as flash point have a significant control over the maximum yield of jet fuel available from the crude as illustrated in Fig. 1 [6]. The maximum theoretical yield of US Navy jet fuel JP-5 produced from North Slope Alaskan Crude increases markedly with increasing freeze point and/or decreasing flash point.

The characterization of freezing point for aviation turbine fuels is reviewed by Friedman [1]. Because aviation turbine fuels are complex mixtures of many compounds, freezing or crystallization does not occur at a definite temperature, but a phase change is exhibited over a range of temperatures. The higher molecular weight, straight-chain or normal paraffins (and certain symmetrical molecule hydrocarbons) crystallize first in the form of waxes. As temperatures are lowered a waxy matrix builds up, trapping other liquid constituents of the fuel until a nearly solid structure is formed [7]. Several freezing point specification tests have been devised, based on the temperature at which wax crystals first appear, which in effect defines a conservative, maximum freezing temperature. These tests include wax appearance, cloud point, and wax disappearance methods (Table I). Wax appearance is the observation of the temperature of the first appearance of the crystals in a chilled, stirred fuel sample. Cloud point is the temperature at which a solid suspension or "cloud" is observed near the bottom of an unstirred sample. Wax disappearance temperature is the temperature observed at the disappearance of crystals in a stirred fuel sample removed from a freezing bath and warmed after the first crystals are formed. In contrast, a minimum, solid phase, "freezing" temperature may be defined by the pour point, which is the lowest temperature at which the surface of a chilled fuel sample will move when turned vertically. Table I also lists approximate values of precision and accuracy for these test methods. For each specification, Table I lists estimates for: (1) repeatability, the mean deviation between duplicate measurements by the same observer; (2) reproducibility, the mean deviation between comparison measurements by different observers, and (3) bias, the mean deviation of the specification with respect to wax disappearance, specification D-2386-67, which is adopted as the present standard for aviation turbine fuels because of improved precision. The specifications (except for D-1477-65, which is now obsolete) are described in detail in the latest edition of the American Society for Testing and Materials [8]. Repeatability, reproducibility, and bias estimates are those published in this reference or reported at a December 1976 low-temperature symposium of the ASTM.

Freezing point measurements as a function of boiling point are illustrated in Fig. 2. The circular data points are freezing points of narrow boiling range (8 to 14°C) fractions of a diesel-type fuel, measured at the NASA Lewis Research Center. The difference between wax appearance and wax disappearance temperatures is represented by the shaded band, which ranges from 0 to 4°C , being greatest at the lowest temperatures where the absolute accuracy of the measurements is lowest. (For acceptance testing, specification D-2386-67 requires that the difference between wax appearance

and disappearance be no greater than 3°C or the test must be repeated.) Data for wax disappearance tests of two other fuels are also shown in Fig.2: a conventional petroleum-derived aviation turbine fuel, and a similar fuel derived from shale oil [9]. The three fuels shown in Fig.2 represent a wide variation of composition. Freezing points vary as much as 15°C at the same boiling temperature. In general, above a boiling temperature of 230°C, the data show that freezing point increases with boiling temperature as expected, with an approximate slope of 0.6 degree per degree. Below 230°C, the freezing point-boiling temperature relationship is not consistent; and over some temperature intervals, freezing point is seen to decrease with increasing boiling temperature.

Aviation turbine fuels are of course petroleum fractions with a range of boiling temperatures rather than a single boiling point. The highest boiling material is richest in the higher-molecular weight fractions, which tend to have higher freezing points, especially for the normal paraffins. Hence the maximum (final) boiling temperature is generally far more influential in characterizing the freezing point than the minimum (initial) or average boiling temperatures.

The effect of the chemical composition on the freezing point of jet fuel is reviewed by Affens [10]. The freezing point of pure hydrocarbons in a given homologous series generally increases with increasing carbon number or molecular weight. The freezing point of the various isomers of a given hydrocarbon may vary significantly depending on molecular structure. The isomers with the more compact, symmetrical, or spherical structure tend to have a higher freezing point. The freezing point of multicomponent fuel mixtures is affected by an additional variable, the intermolecular forces between different types of molecules. The freezing point of a multicomponent solution becomes a function of the solubility of one or more compounds in each other. The freezing point of the solution tends to increase as the concentration of the higher freezing point components increases. For an ideal binary solution, an Arrhenius type plot of the log of concentration for the least soluble component against the reciprocal of the solution's "freezing point" may be constructed. Multi-component fuels, however, are not ideal solutions and may depart from the linearity of an Arrhenius type plot.

Several studies, reported in the literature, have attempted to correlate freezing point with the concentration of the higher n-paraffin compounds in jet fuel as determined by gas chromatography. Petrovic and Vitorovic [11] related freezing point to the total content of the three highest members of the n-paraffin series in the fuel. Hazlett and Hall [12] correlated the freezing point of various jet fuels derived from petroleum, tar sands, oil shale, and coal against the concentration of n-hexadecane in the fuel.

The n-hexadecane concentration is shown plotted against the reciprocal of freezing point in Fig.3 for several research fuels. Data obtained by Antoine [13] from research fuel samples derived from shale oil and coal syncrudes are compared with the data of Hazlett and Hall [12]. The line through the circular data points is the plot of the mole per cent n-hexadecane [C_{16}] against freezing point in a commercial kerosine-type solvent (isopar kerosine), and the square data points represent JP-5 samples derived from oil shale, tar sands, and coal [12]. The triangular data points for experimental jet fuel blends [13] exhibit a similar trend but are displaced to lower freezing points. Attempts to relate the freezing point of jet fuels with either final boiling point or concentration of higher n-paraffins compounds in the fuel appear to be successful only in predicting general trends. There is a need for research that would provide a better understanding of the factors affecting the freezing point of multicomponent hydrocarbons and improved quantitative techniques for predicting the freezing point for fuel blends.

Viscosity – Viscosity-temperature relationships for typical petroleum derived fuels are presented in Barnett and Hibbard [3]. These data provide the linear relationship shown for generalized fuel in Fig.4, plotted on special logarithmic ASTM viscosity-temperature charts. Fuels behave as Newtonian fluids down to about their freezing point. As the temperature is reduced below the freezing point, the linear relationships for Fig.4 no longer apply, and the fuel begins to behave as a non-Newtonian fluid with viscosity a function of shear stress as well as temperature [4].

Composition

The chemical constituents present in jet fuel that may affect the integrity of fuel system materials include but may not necessarily be limited to aromatics and sulfur content. A correlation of aromatics content with hydrogen content for 50 kerosine, JP-4, and diesel type fuels is shown in Fig.5 [14]. These experimental data exhibit an approximate inverse relationship of aromatics content with hydrogen content. The linear relationship shown is only approximate because variations in hydrogen content may occur for variations in molecular structure of the aromatic compounds present in fuels. The mean value for aromatics content corresponding to 13 mass percent hydrogen is about 25 percent by volume. The sulfur content of jet fuels is generally much lower than the limits shown in Table I, and there is no evidence that would indicate a significant increase in the future despite the trend towards higher sulfur crudes.

Volatility

Flash point – As discussed in a previous section, an additional jet fuel property which could significantly increase potential jet fuel yield if it were changed is the flash point temperature. A reduction in flash point would permit inclusion of additional lighter fractions in jet fuel and would tend to mitigate some problems such as higher freezing point and viscosity resulting from the addition of higher-boiling fractions. This change has been considered by the ASTM and was the subject of a symposium held by the ASTM [15].

Vapor pressure – Methods for estimating vapor pressure of petroleum-derived fuels are described by Barnett and Hibbard [3]. A linear relationship between vapor pressure and temperature over a short range of temperatures is obtained by plotting log pressure against reciprocal temperature. Examples of these generalized vapor pressure relationships for several aviation fuels are shown in Fig.6, taken from the early work cited [3].

Contaminants

The solubility of water in aviation turbine fuel is typically about 0.01 weight percent at 30°C [4]. Water solubility decreases exponentially as the temperature of the fuel is reduced. The degree of water saturation in the fuel is influenced by the relative humidity of the air in contact with the fuel. The water content of the fuel quickly comes to equilibrium with the air in contact with the fuel; thus during climb, much of the water may be released from the aircraft fuel tanks by venting. The remaining water in the tank is of concern because of potential filter icing. Filter icing is generally prevented by techniques such as fuel line heating upstream of the low pressure filter or the use of anti-icing additives. The solubility of water in fuel tends to increase with increasing aromatics content, which may exacerbate the filter icing problem.

Free water and dirt, which may be inadvertently introduced into the fuel during delivery and storage, are carefully removed at the airport prior to aircraft fueling by filtration and water removal units called filter/separators. Surface active additives (surfactants) present in the fuel are categorized as contaminants since they reduce the effectiveness of filter/separator units in removing free water and dirt. Trace quantities of surfactants are normally present in the fuel after refining. Additional surfactants may be inadvertently introduced during delivery and storage. The ease with which fuel will release free water when passed through a filter/separator unit is rated by an ASTM test method that measures water separation characteristics of fuel [8]. Since many fuel additives may behave as surfactants, the potential increased use of additives for controlling the characteristics of broadened property fuels may introduce additional filter/separator problems.

A2.5.3 Aircraft Fuel System Sensitivity to Fuel Property Variations

The discussion in this section has been limited to those fuel property effects which are expected to have a major impact on fuel system design. Most of this section is concerned with low-temperature pumpability. In addition, this section contains limited discussion on fuel lubricity, polymeric material compatibility, and aircraft safety. The reader is referred to Smith [4] for a detailed discussion of fuel system corrosion and cleanliness problems related to contaminants such as water, dirt, and surfactants. Fuel evaporation loss is not discussed because it is not considered to be a significant problem for the potential range of vapor pressures associated with the AGARD Research Fuel.

Low-Temperature Pumpability

Effect of fuel freezing point – Fuel stored in aircraft integral wing tanks can reach very low temperatures during long range, high-altitude flights as a result of heat transfer to the surrounding environment. The heat flux, largely convective, is dependent on the total air temperature, which in turn varies with the static air temperature and Mach number. Fig.7 illustrates the variation of fuel temperature in a typical commercial aircraft on a long-range mission representative of an extreme winter condition. The information in the figure was obtained from a Boeing Company computer program which has been developed for the prediction of in-flight fuel temperatures [16]. The total air temperature schedule shown in Fig.7 [17] is based on a flight profile with cruise at a Mach number of 0.84 and a minimum altitude static temperature of -72°C. Note that the total air temperature affecting the wing skin temperature gets no colder than -56°C due to aerodynamic heating. Calculations for initial fuel temperatures of 12° and -21° are shown. After about six hours of flight time, there is no longer an influence of initial fuel temperature, and the fuel temperature eventually approaches the total air temperature. Fuel temperature variations predicted by the model have been compared to measured fuel temperatures, and the trends shown in Fig.7 have been verified.

Fig.8 shows accumulated probabilities for inflight minimum fuel temperatures experienced with Boeing 707 and 747 aircraft flying polar route missions. The data were obtained from about 1100 missions, each greater than 7400 kilometers [18]. The probability that the temperature of fuel in the tank will fall below a given minimum value is plotted against minimum fuel temperature. The differences in temperature between the two types of aircraft are mainly attributed to differences in flight Mach number or flight path. Inflight problems with freezing fuel have not been documented. Situations have occurred, however, where gage-warnings of low fuel temperature have prompted flight crews to accelerate to a higher Mach number, or to divert to warmer, low altitude air masses. Such diversions are costly in terms of operation at less than optimum altitude-speed combinations or increased path length. A margin of 3° to 5° above the specification freezing point is required as a minimum fuel temperature during flight.

The specification test used for aviation fuels is based on observations of the temperature at the disappearance of crystals in a stirred fuel sample removed from a freezing bath and warmed after first crystals are formed. This procedure defines a conservative melting point, independent of supercooling. Other tests have been defined to measure low temperature fluidity of fuels, some of which attempt to relate low temperature behavior to a desired operating characteristic [1]. These tests include measurement of viscosity-temperature relations, filter or pumping pressure drop, and flow rate through tubes or capillaries under pressure or vacuum. Most of these methods are devised for testing diesel or fuel oils for winter service, but they may have eventual application to jet fuels [19-20].

At least two methods have particular interest for studies of low-temperature performance of aviation turbine fuels. A liquid-solid apparatus of Dimitroff et al. [7] used a 100 kPa (one atm) pressure differential to force liquid through a filter in a cold, constant-temperature bath for two hours. The difference in fuel volume between the original sample and the liquid recovered through the filter is the fraction crystallized. A cold flow test developed by Shell Research, Ltd., measured the gravity flow discharge of chilled fuel between two chambers separated by a valve [21-22]. The solid fuel retained in the upper chamber is recorded as a percent holdup. For both these performance tests, measurements can be taken at several temperatures to yield a plot of fraction of fuel frozen or held up as a function of temperature.

Large-scale flow tests simulating actual service conditions for aviation fuels were also conducted by Shell Research [22] in an apparatus consisting of rectangular or contoured aircraft fuel tanks mounted in a cold room. Fuel in the tanks was cooled for 32 to 72 hours to a uniform test temperature and then pumped out of the tanks to an external receiver. The fraction of fuel remaining in the tank that cannot be pumped was called the holdup, and several tests established a relation of holdup against temperature, analogous to the aforementioned smaller-scale test technique.

Example of results of performance tests, determining fraction of frozen fuel as a function of temperature, are shown in Fig. 9. Tests of three fuels by the Shell cold tank method (data from Ford and Robertson [23] and additional data from Dr Peter Ford) and of a fourth fuel by the liquid-solid separator [7] are illustrated. Fuel A, a jet fuel with a high normal paraffin content, has a narrow range of freezing behavior, about 1.5°C from liquid to completely solid. In contrast, fuel C with a low n-paraffin content, shows a wide range of freezing behavior; in fact, it is limited to only 10 percent solid by the -60°C low temperature capability of the apparatus. Fuel D is a wide-boiling-range oil, covering jet and Diesel specifications, also with a low n-paraffin content. The freezing behavior of this fuel was measured by the liquid-solid separator, yet the slope of the freezing curve for this fuel in Fig. 9 agrees well with that of fuel C measured by holdup tests. Finally, curve B illustrates results for an intermediate n-paraffin content fuel, with a range of freezing behavior covering about 8°. Laboratory studies of freezing behavior of mixtures of known composition confirm that normal paraffins crystallize most readily, and accordingly, the high n-paraffin fuel will form a complete crystalline matrix over a short temperature span. The low n-paraffin fuels, in contrast, exhibit a much slower buildup of the solid matrix extending over a large range of temperatures. The zero holdup temperatures measured by the small scale cold flow tests [23] agree with the corresponding temperature measured in the full scale cold tank tests. On the other hand, the specification wax disappearance freezing point does not necessarily correlate with the zero cold tank holdup, nor does pour point correlate with the 100 percent holdup. These discrepancies are especially evident for higher boiling fuels above the jet fuel range.

In order to better identify the low temperature performance of fuels in an aircraft system, a full-scale apparatus designed to represent a section of an airplane wing tank was used [24]. The apparatus, shown in Fig. 10, included the capability to cool the upper and lower tank surfaces to simulate the wing surface temperature history typical of a given flight profile. Instruments measured temperature gradients and other parameters. At an appropriate point during the test, fuel withdrawal was initiated, and the mass fraction of fuel remaining in the tank as unpumpable solids was reported as the percent holdup. Fig. 11 is a photograph of the interior of the simulator tank after completion of a test in which the fuel was cooled to an average temperature of -26°C, two degrees above the measured freezing point of the test fuel. The fuel slush covering the bottom of the tank represents a holdup of 8.8 percent.

The fuel tank simulator has been used to investigate the performance of a series of test fuels having a range of freezing points from -28° to -52°C. Each fuel was derived from known petroleum or shale oil sources to examine the effect of hydrocarbon type composition on performance. Analysis of the data suggested two regimes of behavior: a low holdup regime below 10 percent, in which the minimum average temperature is well above the fuel freezing point, and a high holdup regime, greater than 10 percent, in which the minimum average fuel temperature is at, or below, the fuel freezing point. The low holdup regime is of most practical interest since it represents a situation in which the bulk fuel temperature would not indicate any frozen fuel, yet due to the large temperature gradient near the lower surface, fuel temperatures just above the lower skin may be below the freezing point. As shown in Fig. 12, the holdup data for the low holdup regime correlated with a fuel temperature 0.6 centimeters above the lower tank surface. The curves in Fig. 12 indicate no recognizable effect of fuel source on fuel holdup, but suggest a strong correlation of the fuel temperature corresponding to a holdup of one percent or less and the fuel freezing point. Thus, fuel freezing point temperature may in fact be a good indicator of incipient fuel freezing if the fuel temperature is measured at an appropriate position near the lower tank surface. These results are reported in detail by Friedman and Stockemer [24].

Technology for higher freezing point fuels - For short-range flights, ground preheating of fuel can indeed keep the fuel warmer throughout the flight (Fig. 7). Unfortunately, for the long-range flights where fuel temperatures are most critical, initial fuel temperatures have little or no influence in flight (Fig. 7). Ground preheating of fuel prior to long-range flights may be helpful for storage and ground handling of very high-freezing-point fuels at extreme winter conditions [17].

It was previously pointed out that the freezing process of fuels generally involves a buildup of a waxy or crystalline matrix, which eventually traps even liquid constituents as gels [7, 25]. Certain polymeric compounds in small concentrations are effective in delaying the matrix buildup by dispersing the initially formed solid particles. These additives are pour point depressants, which improve the low-temperature flow properties of the fuel without affecting the crystal formation as observed by freezing or cloud point tests. The mechanism of this dispersion probably involves several physical actions altering crystal size, shape, and surface attractive forces [26]. The flow-improving additives are often

blended into lubricating or fuel oils for winter service, but they have not been explored as a means for modifying the low-temperature properties of jet fuels. The additives may be of interest, however, in the production of future broadened-property jet fuels because the higher boiling fractions blended into these fuels may be quite sensitive to additives [27-28]. In Fig. 13, an example of this behavior is shown. These data are taken from Shell cold tank tests [23], and the percent of frozen fuel held in the tank is plotted as a function of temperature. The base curve, shown as a solid line, is the freezing behavior of fuel B in Fig. 9. The freezing results of a wider-boiling-range fuel are shown by the broken curve in Fig. 13. This fuel was a mixture of the base jet fuel plus 20 percent of a heavier, higher-final-boiling point fraction from the same source material. The low temperature performance of the wider-boiling blend is poorer than that of the base fuel as evidenced by greater holdup, or less flowability, at the same temperature. The second broken curve shows the performance of the same blend with the addition of 300 ppm of a flow improver. In this case, the holdup of the wide-boiling fuel has improved to the extent that it surpasses that of the base fuel.

Several potential fuel system technology approaches to using higher freezing point fuels have been analytically evaluated for NASA by the Boeing Commercial Airplane Company [17]. These approaches included the use of insulation to reduce heat transfer from the aircraft fuel and the addition of heat to the fuel in the tank during flight to prevent fuel freezing. The studies incorporating heat addition considered waste heat recovery systems as well as auxiliary heat generation systems. The sketch in Fig. 14 indicates the various approaches which were examined.

The calculated effect on fuel temperature of two levels of heat addition to the fuel is indicated in Fig. 15 for a typical long-range, wide-bodied aircraft. A heat addition rate of 62 kilowatts per tank would be required to maintain the fuel temperature above -29°C , while an input of 108 kilowatts per tank would maintain the fuel temperature above -18°C . Additional calculations indicated that the use of wing tank insulation (without heat addition) could reduce initial cooldown rates but would have very little effect on the minimum fuel temperature for long duration flights. The use of insulation in conjunction with fuel tank heating could reduce heat input requirements for higher-freezing-point fuels, but performance penalties associated with increased weight and increased drag or decreased fuel tank volume must also be considered.

After detailed consideration of the advantages and disadvantages of the heat sources identified in Fig. 14, two approaches were selected for further study, the lubricating oil heat exchanger and the engine-drive electrical heater [29]. The lubricating oil heat exchanger system shown schematically in Fig. 16 represents a minor modification to the fuel system that could be implemented with relatively low risk and cost. This system uses heat rejected by the engine lubricating oil as a heat source and involves the addition of a second oil-to-fuel heat exchanger. When the fuel heating system is not in operation, the fuel control relieves excess fuel flow back to the engine fuel pump. When fuel heating is needed, a three-way recirculation valve diverts the excess flow through the second heat exchanger and back to the fuel tanks. The maximum amount of heat available is dependent on engine operating condition but is relatively constant throughout the cruise portion of the flight. The maximum heat available will also vary with different engines, and future high performance engines may have less heat rejection from the engine oil than current engines. The calculated fuel temperature for a long-range extreme winter flight using this system is shown in Fig. 17. In the example shown, the fuel heating rate averages 50 kilowatts per engine-tank combination increasing the minimum fuel temperature to -31°C . The advantages of this system are that it has little or no effect on the engine or fuel system operation, and that weight, performance and cost penalties are small. The primary disadvantage, however, is the limited heat available from this system corresponding to a small increase in fuel freezing point limit.

The second approach to fuel heating which has been studied uses an electrical system to divert a small portion of the engine energy to fuel heating. A schematic of this approach is shown in Fig. 18. Suitable engine-driven generators have been developed for auxiliary power generation on some current military aircraft. To eliminate the hazards of directly heating fuel with electric heaters, a heat transfer medium such as an ethylene-glycol solution would be used. The transfer fluid reservoirs and heaters would be centrally located and the fluid would be pumped out to heat exchangers in the fuel tanks. The primary advantage of this system is that it can provide practically any amount of heating power desired, thus accommodating a substantial increase in fuel freezing point. Also, the system could be easily adapted for ground power heating. Disadvantages include the relatively major fuel system modifications needed to incorporate the system and the small but appreciable aircraft performance penalty due to the added weight and engine power drain of the system.

Research and technology status — Design studies of both the lubricating-oil heat exchanger system and the electrical heating system have indicated that these concepts represent two feasible approaches to heating system technology. However, many additional data will be required before systems such as these can be realistically considered for future aircraft designs. Experimental studies currently in progress using a fuel heating system simulator will provide some of the needed information [30]. The simulator, shown in Fig. 19, adds a fuel heat exchanger and recirculation loop to the fuel tank simulator described earlier (Fig. 10) and can simulate the heat input characteristics of either of the two heating system concepts. The current studies will provide data on heat transfer rates, fuel tank behavior with heat input, and system dynamics, which will aid in modeling the system performance. This information will be sufficient for engineering evaluations of the systems, but demonstrations of the concepts in full-scale hardware will be required before actual development of this technology is likely to occur.

Several additional issues such as ground handling of higher freezing point fuels and the effects of these fuels on turbine-powered general aviation aircraft must also be examined and resolved. Special provisions for storage and distribution systems as well as aircraft parking may be required at airports in northern latitudes if jet fuel freezing points change significantly.

Fuel Lubricity

Fuel lubricity is currently not controlled by any ASTM specification or test method. Infrequent problems related to fuel pump or fuel control durability that have occurred over the last twenty years have generally been traced to fuels that have been subjected to a high degree of hydroprocessing. Severe hydroprocessing may remove the small concentrations of polar compounds present in the fuel that provide good lubricity characteristics. Lubricity may generally be restored to a highly refined fuel either by blending of less refined component streams or the addition of surface active additives. Considerable research has been conducted to obtain a better understanding of the nature of fuel lubricity and to evolve laboratory test methods for evaluating lubricity [31-34]. One of the laboratory techniques evolved to measure fuel lubricity is referred to as the "Ball-On-Cylinder" (BOC) Lubricity Tester [32]. This apparatus rates the relative lubricity of aviation turbine fuels by measuring the wear scar which is generated on a stationary loaded ball when rubbing against a fuel-wetted rotating cylinder. The size of the wear scar is a measure of fuel lubricity. The wear scar diameter (WSD) is larger for fuels with poorer lubricity. This laboratory test serves as a means for predicting the likelihood of future friction or wear problems, and the need for corrective measures such as the use of fuel additives.

Polymeric Materials Compatibility

Increased aromatic content of future jet fuels may present a problem with regard to compatibility with polymeric materials used as gaskets, sealants, adhesives and coatings in aircraft fuel systems. Elastomers typically swell when exposed to aromatics and standard design procedures take this into account. However, some data have been acquired [35] which indicate a significant loss in elasticity after exposure to a fuel with higher aromatics content. Fig. 20 compares results for an elastomer exposed to Jet A with 20 percent aromatics and two higher aromatics content blends. It should be noted that these aging tests were conducted at an elevated temperature of 150°C; the loss in elasticity was less severe at lower temperatures. Another study [36] involved the examination of a wide variety of non-metallic aircraft materials. With some exceptions, most materials were found to be reasonably tolerant of a JP-4 base fuel with aromatics levels up to 35 volume percent and sulfur levels up to 1 weight percent.

Both of the cited studies found an indication of potential problems with some materials. Followup studies should investigate these specific cases more thoroughly and examine as many classes of materials as possible. In addition to aging tests with specific fuels, cyclic tests where aromatics concentrations are varied are needed. Compatibility problems with some materials may be identified, but many elastomer compounds can be tailored to specific fuel properties so that undesirable effects would be minimized for a given fuel in a given application.

Aircraft Safety

A thorough discussion on the effect of fuel property variations, such as volatility, on aircraft safety is beyond the scope of this paper. An assessment of the relationship of fuel volatility including flash point with fire safety, particularly under postcrash conditions is presented by Carhart et al. [37]; and Dukek and Strauss [15]. The probability of a post crash fire using wide-cut Jet B is concluded to be somewhat greater than for kerosine-based Jet A, based on the evaluation of accident data and fuel characterization comparisons [37]. The advantage regarding potential jet fuel yield of reducing flash point was discussed in a previous section. A kerosine fuel with a flash point of 6° to 11°C lower than Jet A would generally be expected to behave somewhat similar to Jet A under postcrash conditions; but differences in flammability characteristics would be expected at fuel temperatures near the flash point [37].

Potential fires in turbine aircraft crashes where fuel is released from ruptured wing and fuselage tanks in the form of a fine mist may be suppressed by the addition of antimisting additives [38]. The addition of as little as 0.3 percent of an antimisting additive, which is a high molecular weight polymer, to Jet A fuel has been demonstrated to inhibit flame propagation in simulated crash tests. Jet A containing an antimisting additive tends to resist misting and atomization from wind shear and impact forces and instead tends to form globules which suppress ignition and flame propagation. Unfortunately AMK (antimisting kerosine) exhibits undesirable characteristics in the fuel system and combustor. The non-Newtonian flow behavior of AMK may result in blockages in filters and small passages in the fuel system and poor atomization in the combustor, unless the AMK is restored to the flow characteristics of neat Jet A by means of a mechanical shear degrader [39].

Fiorentino et al. [39] investigated the effect of AMK on the performance of fuel system components and the combustor of a JT8D engine. This study concluded that while the use of AMK might be technically feasible, many problems remain to be solved to ensure the complete safety and reliability of the aircraft and engine systems. Further research and technology is required in the following areas: (1) development of a practical cost and energy effective, flight-worthy mechanical shearing device, (2) an improved understanding of AMK flow characteristics and compatibility or interaction with both approved additives and contaminants such as water, (3) low-temperature fuel systems flow performance evaluations and (4) component endurance evaluations on filters, pumps, controls, valves, heat exchangers, combustors, and turbines.

A2.5.4 Aircraft Fuel System Research and Technology Needs

Research and technology studies investigating the impact of fuel property variations on aircraft fuel systems were reviewed in the previous section. Although most of the emphasis was placed on the effects of fuel property variations on

low-temperature pumpability, lubricity, material compatibility and aircraft safety, other considerations such as system cleanliness and the effectiveness of fuel additives are equally important. Several recent conferences have addressed the need for fuel system research and technology [30, 40-41]. Several recommendations for freezing-point research from a 1977 workshop [40] have been the subject of completed and continuing studies described in the previous section. The temperature environment in long-range commercial flights has been analyzed by heat-transfer calculations [17] and by statistical compilation of airline data [17]. A design analysis examining the feasibility of aircraft fuel tank heating was reported by Pasion [29]. Experimental studies have been conducted in scale-model wing tanks to examine the behavior of fuels near the freezing point [24]. Results of these studies have established relationships for fuel pumpability with freezing point, other fuel properties and the external tank conditions. Further experiments are being conducted that incorporate the recirculation of heated fuel [30].

Experimental studies of low-temperature fuel system behavior should continue in small-scale, and eventually full-scale, installations. Further design and component test studies of fuel system modifications to take advantage of fuels with a range of freezing points are needed, including investigations of fuel heating systems and wing tank insulation. Additives such as pour point depressants could be explored to determine the feasibility and limits of two-phase flow for fuels near the freezing point. In-flight temperature measurements in aircraft fuel tanks are needed to verify low-temperature pumpability data obtained from scale-model wing tank simulators. The development of a practical instrument to measure fuel freezing point that is suitable for rapid and portable measurements at the airport might provide a means for establishing reliable operating margins for fuels with a wide range of freezing points. Fundamental research is required to develop correlations relating freezing point and viscosity to the chemical composition of non-ideal multi-component hydrocarbon fuel mixtures.

Improved laboratory test methods are needed to evaluate fuel lubricity. A more definitive set of data is required to document the effect of potential fuel property variations on the various materials within the aircraft fuel system. Water solubility is a minor characteristic, but potential fuel compositional shifts may increase the solubility and cause cleanliness problems. In addition to investigating water solubility, the effect of potential surface active additives on the performance of airport filter/separator units designed to remove water from the fuel should be evaluated. More data are required to assess the feasibility of using anti-misting fuel additives and to evaluate the impact of fuel property variations on the effectiveness of anti-misting fuels. Finally, systems analyses are required to assess the effects of fuel property variations on both commercial and military aircraft fuel system designs and operating characteristics in order to identify technical and economic tradeoffs.

A2.5.5 References

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TABLE I

ASTM Low Temperature Fuel Test Methods

Test No.	Title	Type	Repeatability ^a ±°C	Repeatability ^b ±°C	Bias ^c °C
D-2386-67	Freezing point of aviation fuels	Wax disappearance	0.7	2.6	-
D-3117-72	Wax appearance point of distillate fuels	Wax appearance	0.8	2.2	-2
D-1477-65	Freezing point of aviation fuels (obsolete)	Wax appearance	0.8	3.1	-2
D-2500-66	Cloud point of petroleum oils	Wax appearance	2	4	-3
D97-66	Pour point of petroleum oils	Wax matrix	3	6	-8 ^d

^aDifference between repeat measurements by same observer [8].

^bDifference between measurements by different observers [8].

^cApproximate difference of method minus D-2386-67 as reference (as reported by W.G.Dukek at the ASTM Symposium of Jet Fuel Low Temperature Requirements, December 1976).

^dExtreme bias values may range between 0° and -17° or greater [7, 20].

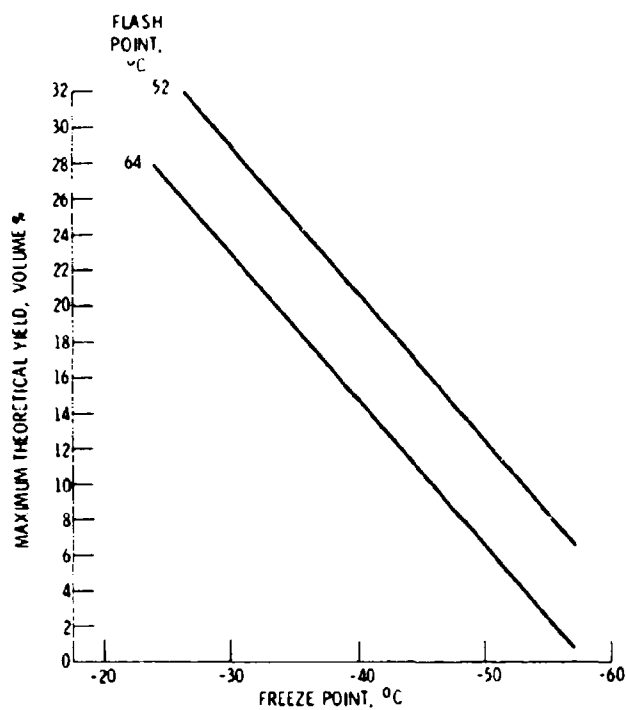


Fig. 1 Effect of relaxation of freeze/flash points on maximum theoretical yield of JP-5, North Slope Alaskan crude [6]

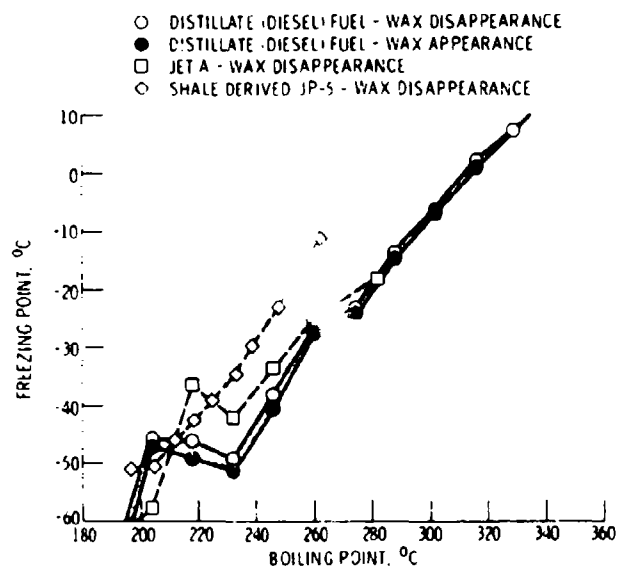


Fig. 2 Freezing points of narrow boiling range samples of fuels

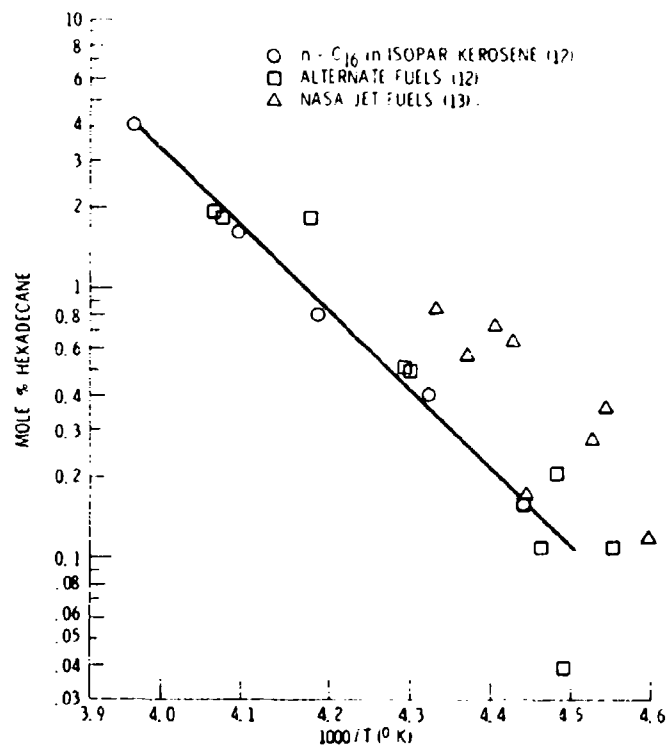


Fig.3 Freezing point of various research fuels as a function of n-hexadecane concentration

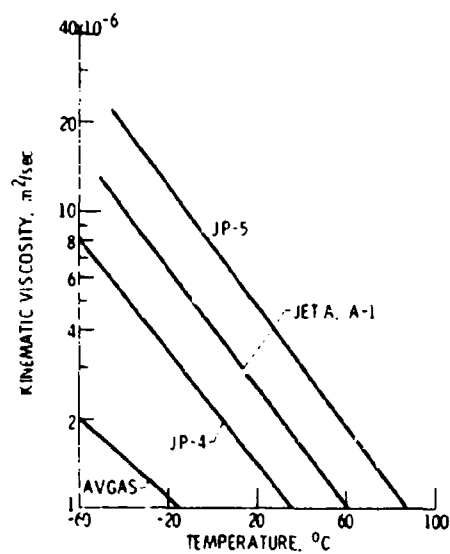


Fig.4 Effect of temperature on fuel viscosity [3]

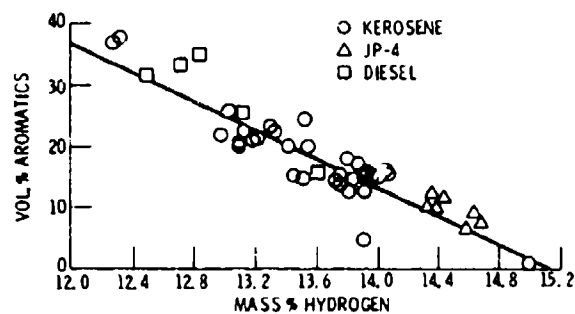


Fig.5 Correlation of aromatics and hydrogen content of 50 fuels [14]

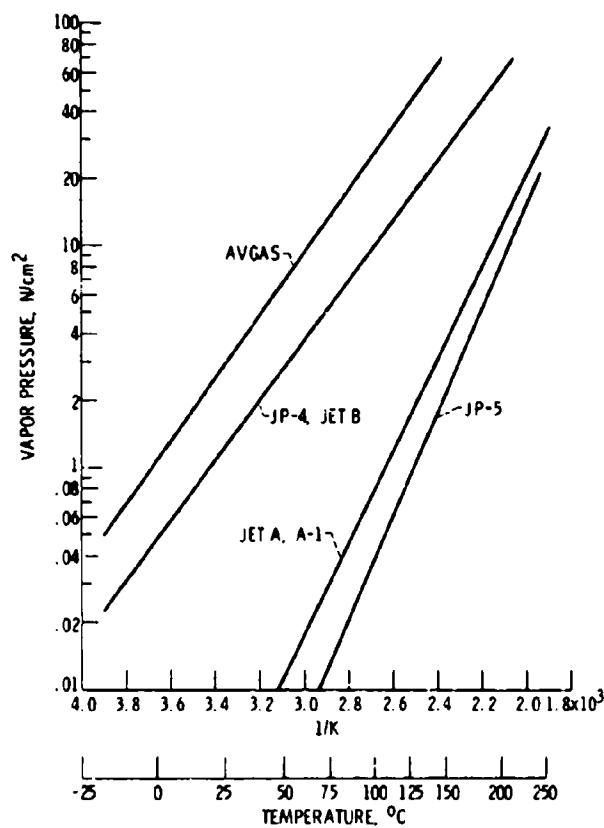


Fig.6 Effect of temperature on true vapour pressure of aviation fuels [3]

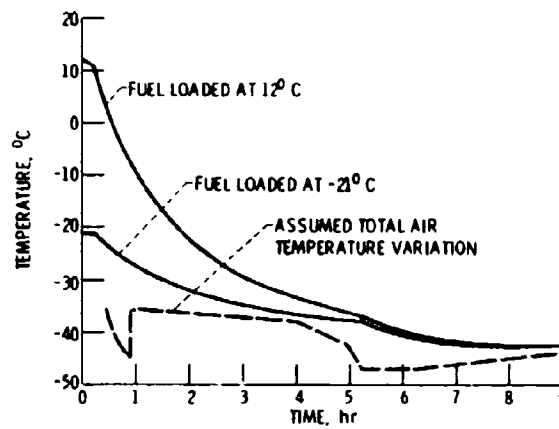


Fig.7 Predicted fuel temperature for a long-range (9100 km) commercial aircraft mission, based on a minimum static air temperature of -72°C

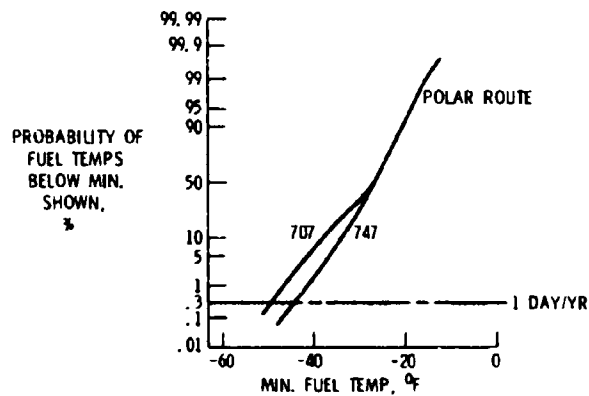


Fig.8 Summary of in-flight minimum fuel temperature measurements

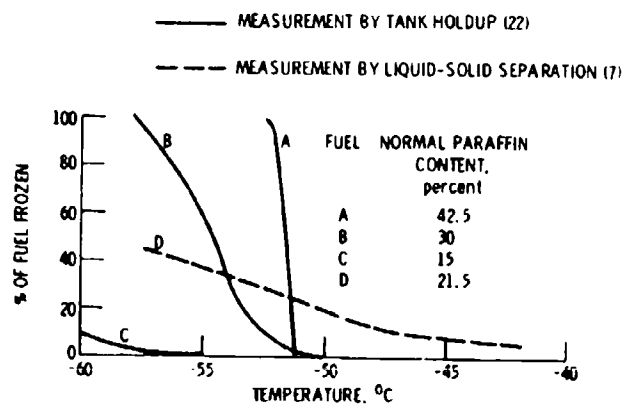
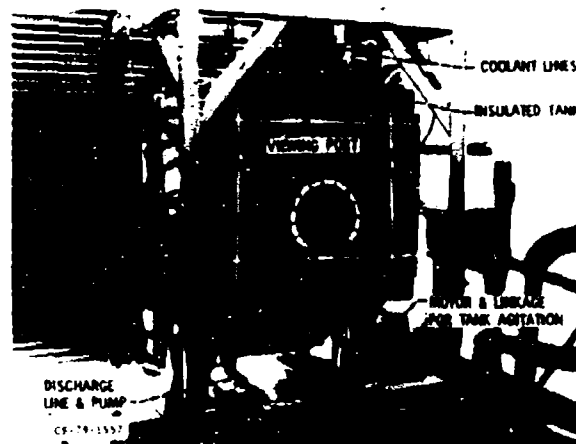
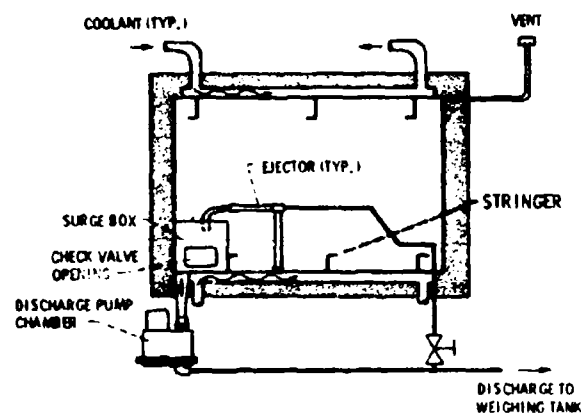


Fig.9 Fuel freezing behavior



(a) PHOTO OF APPARATUS.



(b) CROSS-SECTION OF APPARATUS.

Fig. 10 Apparatus used to simulate low temperature environment of aircraft fuel tank



Fig. 11 Interior view of fuel tank simulator apparatus after completion of test with approximately 8.8 percent fuel holdup

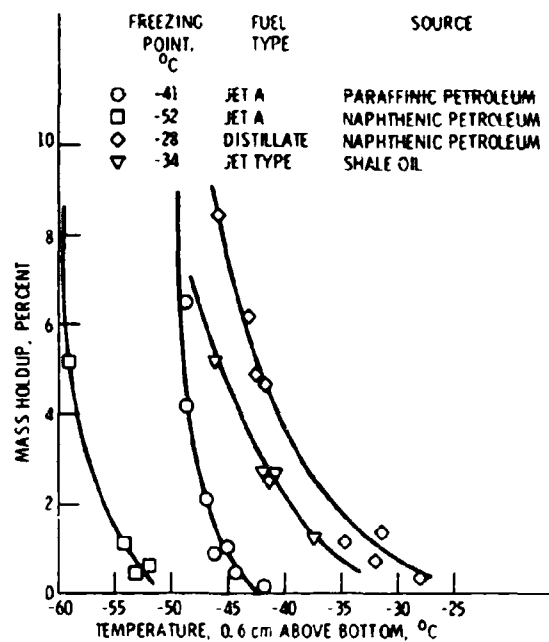


Fig. 12 Results of fuel tank simulator tests with low holdup conditions

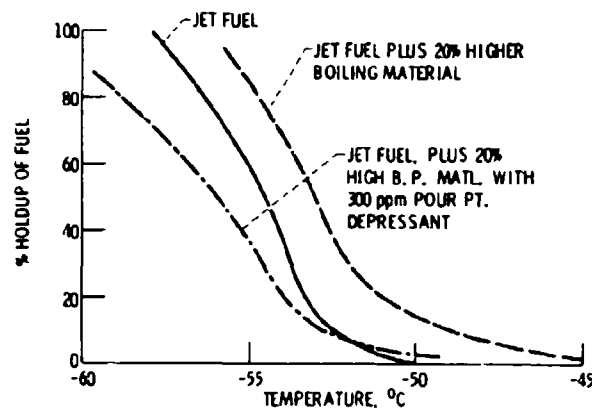


Fig. 13 Fuel freezing behavior curves, comparing high-boiling-point fuel blends with and without additive

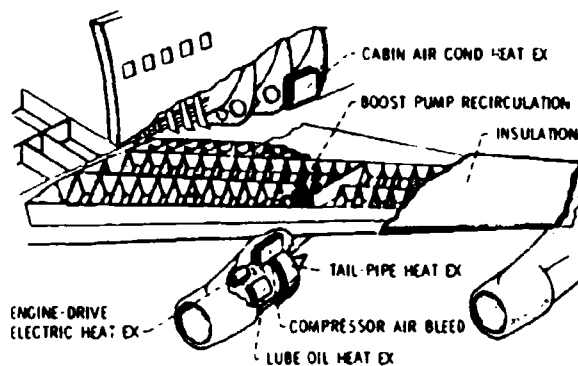


Fig. 14 Potential fuel tank heating sources

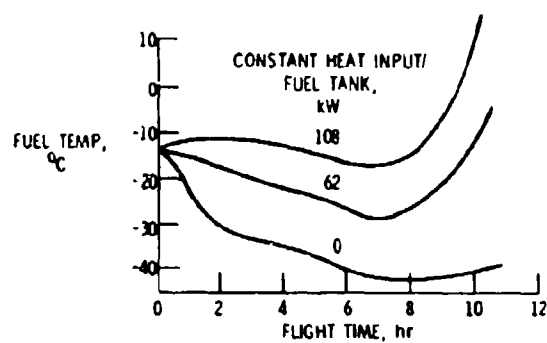


Fig. 15 Fuel-tank temperatures for a 9100-kilometer flight with heating

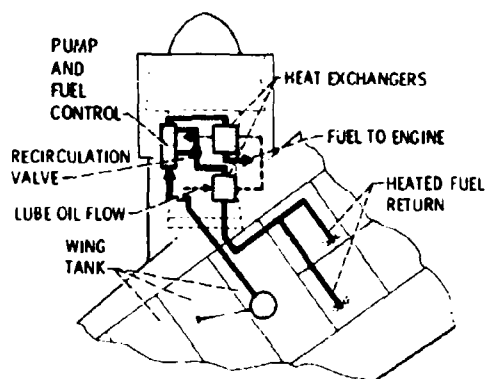


Fig. 16 Schematic of a lubricating-oil heat exchanger fuel heating system

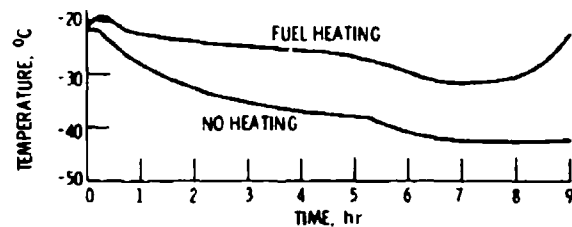


Fig. 17 Predicted fuel temperature for a 9100-km commercial aircraft mission, with fuel heated by lubricating oil heat rejection

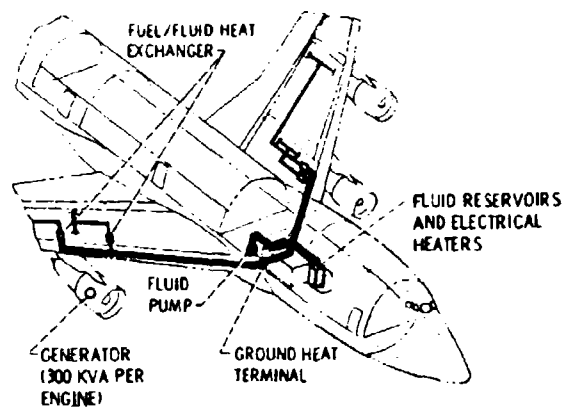


Fig. 18 Schematic of electrical fuel heating system on a wide-bodied jet aircraft

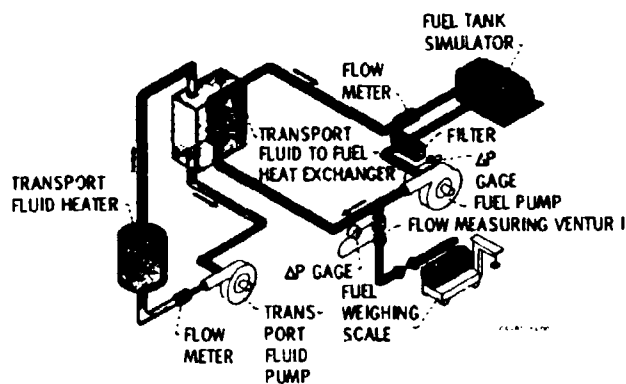


Fig. 19 Sketch of fuel heating system simulator

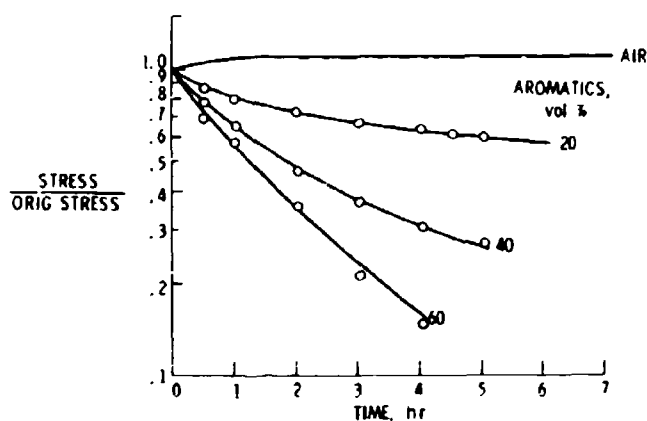


Fig. 20 Stress relaxation of butadiene-acrylonitrile rubber exposed to Jet A with varying aromatics content at 150°C

APPENDIX 3**AGARD RESEARCH FUEL PROPERTIES**

Aviation fuels in the year 2000 are predicted in most NATO countries to be different in various ways from present specification fuels. There is therefore a need today for the prediction of the properties and for pre-production of quantities of such fuels. Those research fuels are required for research and engine development so that the research work can be aimed realistically at coping with potential problems in the handling and combustion of aviation turbine fuels of the future. The definition of such research fuels is not a simple task for several reasons. Firstly, we do not have sufficient reliable data on how present engines can handle, on a continuous basis, fuels which have properties at or beyond the present specification limits, particularly in the combustion area. Secondly, we have only limited data on properties which can be expected in fuels in the year 2000 because of the possible variations in refinery feedstocks — heavier conventional crudes, materials derived from tar sands, shale and coal — and processing. It is possible with present technology to produce a satisfactory fuel (meeting present specifications) from any of these sources, if they are subject to severe enough hydrogenation or other processes. The difficulty arises because of the cost of hydrogenation based on plant capital cost, the cost of producing hydrogen, increased refinery fuel requirements and a reduction in yield. The feeling in many NATO countries is that it will not be economically desirable to maintain present specifications if we consider projected commercial and military fuel demands. Hence there is a need for the definition of research fuels to focus research and development studies on probable future qualities.

Since the NATO Air Forces and the majority of civilian requirements both tend towards the use of F-34 (kerosine type) fuels, the Task Group decided that the Research Fuel(s) should be of this type.

After considerable discussion the Task Group recommended the following limits as the most reasonable predictions based on present knowledge to provide an envelope within which research fuels should be produced:

- (1) *Combustion Properties*: It was agreed that it was most meaningful to state combustion properties in terms of hydrogen content rather than aromatics, because the test method is more precise and there is probably a better correlation with combustion properties. The limits agreed were $13.2 \pm 0.2\%$ wt hydrogen. For US Jet A fuels this corresponds to roughly 24–32% aromatics.
- (2) *Thermal Stability*: A minimum JFTOT temperature of 230°C was agreed. Fuel bound nitrogen content, which is characteristic of some alternative sources, is also a factor which would be important in relation to its influence on stability and exhaust emissions.
- (3) *Storage Stability*. This property is recognized as being of major importance for military fuels and, in present specifications, is adequately regulated by the thermal stability (JFTOT) requirement of 260°C . However since this requirement has been relaxed to 230°C this may no longer give satisfactory storage stability, and research work is required to demonstrate if the 230°C JFTOT limit will give the required storage stability. If not, then further investigation is required to produce a satisfactory test method and limit to ensure adequate regulation of this property.
- (4) *Distillation*: Since a research fuel should be a genuine kerosine fraction, the distillation should give at least 10% evaporated at 205°C with the final boiling point regulated by the freeze point.
- (5) *Freeze Point*: To enable studies of aircraft fuel system handling capabilities the maximum freeze point was set at -33°C .
- (6) *Viscosity*: Since engine manufacturers will not guarantee engine starts with fuel viscosities above 12 cS this limit was set at 12 cS at -20°C .
- (7) *Flash Point*: To preserve the safety and vulnerability aspects of F-34 type fuels it was agreed to retain this limit at 38°C .
- (8) *Sulfur, mercaptan sulfur*: These were discussed but no limits were proposed since setting limits might inhibit the production of suitable test fuels.

These values for Research Fuel(s) provide a framework (Table 1) within which blends can be made from components likely to be incorporated into fuels in the year 2000. These blends should then be fully tested for key characteristics to be compared with conventional specification requirements. The component will vary from country to country depending on future developments and most likely will include shale-derived materials in the US, tar sands products in Canada, and coal derived liquids in many parts of the world.

The Task Group strongly recommends that any fuel to be used either in combustion research, thermal and storage stability investigations or in engine performance and development studies should lie in the Research Fuel framework. It is also essential that the analysis detailed below should be carried out on every fuel so used to enable adequate correlations to be established between fuel properties and engine performance:

Flash point $^{\circ}\text{C}$	ASTM D56 or D3243
Freezing point $^{\circ}\text{C}$	ASTM D2386
Kinematic viscosity at -20°C	ASTM D445
Distillation:	
IPB $^{\circ}\text{C}$	
10% recovered, $^{\circ}\text{C}$	

50% recovered, °C	
90% recovered, °C	
FBP °C	
Residue, % vol.	
Loss, % vol.	
Density, kg/l at 15°C	ASTM D1298
Hydrogen content, % mass	ASTM D3701
Aromatics, % vol.	ASTM D1319
Net heat of combustion	ASTM D1405 or D2382
Luminometer number	ASTM D1740
Smoke point	ASTM D1322
Naphthalenes, % vol.	ASTM D1840
Sulfur, % mass	ASTM D2622 or equivalent
Mercaptan sulfur, % mass	ASTM D1219 or D3227
Corrosion, copper strip, 2 hr at 100°C	ASTM D130
Corrosion, silver strip	IP227
Water separation index, modified	ASTM D2550, D3602 or D3948
Total acidity	ASTM D974
Thermal stability (determine actual break-point temp.)	ASTM D3241
Analysis of hydrocarbon types – paraffins, naphthenes and aromatics including 1-ring 2-ring and 3-ring structures	GC/LC/MS or other suitable methods
Analysis for nitrogen, oxygen and trace metals	Suitable analytical methods

Note that it is important to use the methods quoted or their exact equivalents in other countries since differences in test methods will only serve to complicate any possible correlations.

TABLE I
Properties of AGARD Aviation Turbine Fuels

Type	Wide-Cut		Kerosine		
NATO Code	F-40		F-34, F-35		Proposed
	Spec Limit	Typical Values	Spec Limit	Typical Values	Test Fuel ARF (1)
Composition					
Acidity, total (mg KOH/g), max.	0.015	0.006	0.015	0.004	(0.015)
Aromatics (% vol), max.	25.0	11.4	25.0	17.2	R
Olefins (% vol), max.	5.0	0.8	5.0	1.5	R
Sulfur, total (% mass), max.	0.40	0.042	0.30	0.048	(0.30)
Sulfur, mercaptan (% mass), max.	0.001	0.0005	0.001	0.0003	R
Hydrogen content (% mass), min.	13.6	14.4	13.5	13.8	13.2 ± 0.2
Volatility					
Distillation, IBP, °C	R	59	R	166	—
10% recovered at °C, max.	R	94	205	183	205
End point °C, max.	270	237	300	253	—
Flash point, °C, min.	—	—	38	52	(38)
Density at 15°C (Kg/L), min.	0.751	.763	.755	.804	R
Density at 15°C, max.	0.802		.840		
Fluidity					
Freezing point, °C, max.	−58	−62	−50	−54	−30
Viscosity at −20°C, cs, max.	—	—	8.0	4.3	12.0
Combustion					
Specific energy, net (KJ/g), min.	42.8	45.5	42.8	43.2	R
Smoke point, mm, min.	20.0	27.6	19.0	23.9	R
Naphthalenes (% vol)	—	—	—	1.86	R
Corrosion					
Copper strip, 2 hr at 100°C, max.	1b	1a	1b	1a	(1b)
Thermal Stability					
Maximum tube temp., °C	260	>260	260	>260	230
Contaminants					
Existent gum (mg/100 ml), max.	7.0	0.8	7.0	0.9	(7.0)
Water reaction interface, max.	1b	1b	1b	1b	(1b)
Water separation index (mod.) with/ without corrosion inhibitor, min.	70/85	90	70/85	94	(70/85)
Conductivity					
(pS/m), min.	200	—	200	—	(200)
max.	600		600		(600)

(1) ARF represents AGARD Research Fuel with properties defined by Propulsion and Energetics Panel Working Group 13. Values marked R are considered to be limited by other specification requirements and should be reported. Values in parenthesis are carried over from NATO F-34 to maintain SO₂ emissions, corrosion or contaminants at present levels.

(2) JFTOT at specified max. tube temperature requires a maximum pressure differential of 25 mm Hg and a maximum Tube Deposit Rating, Spun, of 17 (or less than Code 3 Visual Rating).

DISTILLATION RANGE OF MAJOR FUEL PRODUCTS

(Typical Initial and Final Boiling Points)

ARF = AGARD RESEARCH FUEL

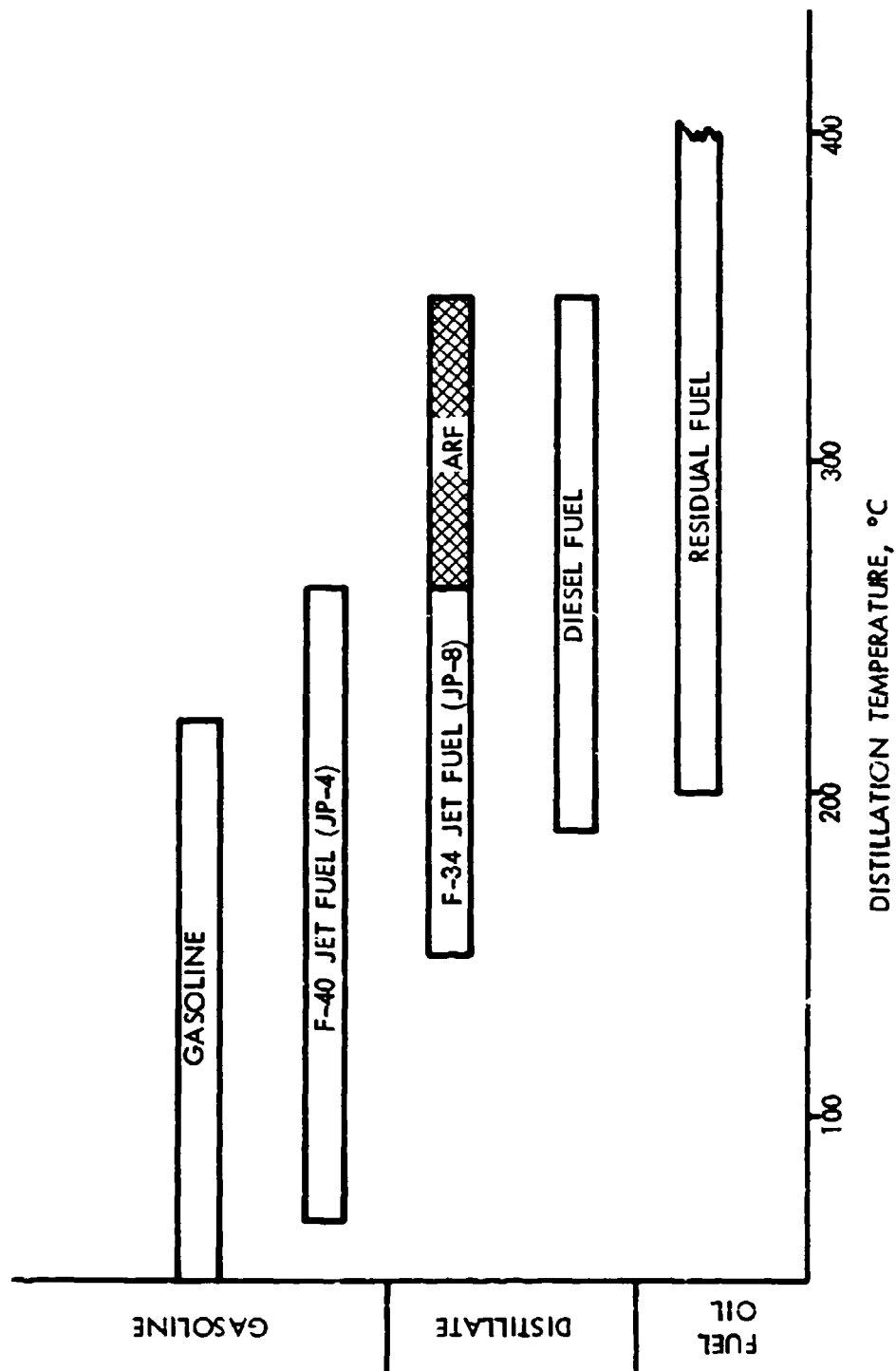


Figure 1

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