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**SIMULATING INDIRECT THRUST MEASUREMENT
METHODS AS USED ON MODERN HIGH-BYPASS
TURBOFANS**

by

Jonathan D. Stevenson

B.Eng.(Mech.), Memorial University of Newfoundland (1990)

**A thesis submitted to the Faculty of Graduate Studies and Research in partial
fulfilment of the requirements for the degree of**

Master of Engineering

**Department of Mechanical and Aerospace Engineering
Ottawa-Carleton Institute for Mechanical and Aerospace Engineering (OCIMAE)
Carleton University
Ottawa, Ontario
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ISBN 0-315-84123-0

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Chairman,
Department of Mechanical and
Aerospace Engineering

Abstract

As yet, there is no known reliable method for directly measuring the thrust of a turbofan in flight. Manufacturers of civil turbofans use various indirect thrust measurements to indicate performance of an engine to the flight deck. Included among these are: Engine Pressure Ratio (EPR), Integrated Engine Pressure Ratio (IEPR), Fan Mechanical Speed as a percentage of its design speed (N_1), and various Turbine Gas Temperatures. Of key concern is whether these thrust indicators give an accurate account of the actual engine thrust. The accuracy of these methods, which are crucial at take-off, may be compromised by various types of common engine deterioration, to the point where a thrust indicator may give a false indication of the health and thrust of the engine.

A study was undertaken to determine the effect of advanced engine cycles on typical values of the most popular thrust setting parameters, using a generic computer model of a twin-spool high BPR turbofan. A preliminary investigation was conducted to determine the effects of various kinds of engine deterioration on the engine performance and the indirect thrust indicators.

To Michelle, for her support and patience

Acknowledgements

I wish to recognize the following for their assistance during this project:

Professor H.I.H. Saravanamuttoo, whose wisdom and guidance helped me throughout this research, keeping me from "straying off course" on a number of occasions.

My wife Michelle, who (along with Prof. Saravanamuttoo) helped proof-read the numerous manuscripts for this thesis.

Kevin Williams, who was extremely helpful in providing the computer source code for the twin-spool matching procedures for an RB211 industrial gas turbine simulator. This formed the basis of the off-design core simulation section of the turbofan simulator presented in this thesis.

The Natural Science and Engineering Research Council (NSERC), whose financial support helped make this research financially viable.

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Nomenclature

List of Symbols:

A	Exit area of nozzle
bl	HP Turbine cooling bleed (% core mass flow)
C	Air flow velocity
c_p	Specific heat (at constant pressure)
F, F_s	Thrust, specific thrust
$M, Mach$	Mach number
m	Mass flow
N_1, N_2	Mechanical LP and HP spool speeds
$(n-1)/n$	Polytropic index
nr	Corrected relative spool speed
p, T	Static pressure, temperature
p_a, T_a	Ambient pressure, temperature
Δp_h	Combustion pressure drop (% CDP)
p_N, T_N	Nozzle plane static pressure, temperature
p_o, T_o	Total pressure, temperature
Q	Non-dimensional or corrected mass flow parameter
δ	Corrected pressure ($p_o/1.01325$, p_o in Bars)
γ	Specific heat ratio
η_p	Polytropic efficiency
η	Isentropic efficiency
Θ	Corrected temperature ($T_o/288.16$, T_o in K)

Acronyms:

BPR	Bypass ratio
CDP	HP Compressor delivery total pressure
CDTO	Cold day take-off (ISA -15°C at SLS)

EGT	LP Turbine exhaust gas total temperature
EPR	Engine pressure ratio
FADEC	Full authority digital engine control
FF	Fuel flow
FPR	Fan pressure ratio
GG	Gas generator (core of turbofan)
HDTO	Hot day take-off (ISA +15°C at SLS)
HP, LP	High pressure, low pressure
HPC, HPT	High pressure compressor, turbine
IEPR	Integrated engine pressure ratio
IFSD	In flight shutdown
ISA	International standard atmosphere
ITT	Inter-turbine total temperature (gas generator EGT)
LPC, LPT	Low pressure compressor (Boosters), turbine
OPR	Overall pressure ratio
PR	Pressure ratio
RPR	Ram pressure ratio
SFC	Specific fuel consumption
SLS	Sea level static
TIT	HP turbine inlet total temperature
TOC	Top of climb
UHB	Ultra high bypass (turbofans)

Subscripts:

a	Air
c	Cold (fan or bypass)
cc	Combustion chamber
fan	Fan
hpc, hpt	High pressure compressor, turbine
g	Gas

h	Hot (core)
i	Intake
j	jet
lpc, lpt	Low pressure compressor, turbine
mech, m	Mechanical
N	Nozzle plane

Chapter 1 Introduction

1.1 Objectives

The primary goal of this study was to analyze various indirect thrust measurement methods with the aim of determining the relative usefulness and reliability of some of the more popular methods on modern turbofans. This objective required fairly accurate simulation of internal engine conditions, especially the exhaust gas temperatures and pressures for the gas generator, and both nozzles of the turbofan. In addition, since the LP and HP spool speed relationships were required, accurate simulation of the twin-spool match was also necessary. This final requirement necessitated the use of some kind of component characteristics.

Thus, there was a need to develop a generic twin spool turbofan simulator which could accurately predict the overall performance of a typical modern high-bypass turbofan. This was complicated by the fact that most information required to accurately simulate a specific engine design is highly proprietary and available only to the manufacturer. However, it has been shown that fairly good simulation can be achieved using reasonable assumptions of component performance behaviour based on the current state-of-the-art and openly published engine data.

1.2 Indirect Thrust Indicators

Although jet engines have been used commercially for 40 years, there is still no direct method of measuring thrust in flight. Indirect methods of indicating thrust are used, based on pressure measurements or rotational speeds. The use of engine pressure ratio (EPR), which is defined as the ratio of total exhaust pressure over total inlet pressure, became dominant in early turbojet engines where there was a direct relationship between EPR and thrust. A problem arose with the introduction of high bypass turbofans, where a substantial portion of the total net thrust is provided by the cold or bypass

stream; EPR considered only the core engine pressure ratio and hence the thrust contribution from the hot stream. One alternative was to use integrated engine pressure ratio (IEPR) which is based on an area weighted average of the bypass and core nozzle pressure ratios. Another approach was to use fan rotational speed (N_1) to indicate thrust.

In addition to pressure ratios and spool speeds, modern high bypass engines must use temperature measurements to monitor and control engine performance. The key issue is that there is a thermal limit in all gas turbine engines. The most critical temperature is the Turbine Inlet Temperature (TIT) which in addition to determining the overall thermal efficiency of the engine, influences the maximum rotational speed that the engine can withstand safely. Higher TIT generally results in increased power in a given engine and higher specific thrust, and this has led to the need for gradual increases in TIT since the commercial jet engine was first introduced.

In a given engine, the maximum permissible TIT is influenced chiefly by the material limits and the desired overhaul life, especially in the HP Turbine rotor components. The gradual increases in TIT since the 1960s were made possible largely through improvements in turbine materials and the increased use of various turbine cooling techniques. The general trend for increased TIT and greater use of turbine cooling technologies can be seen in Figure 1.1. It has been estimated that the theoretical limit for cruise TIT is of the order of 2400 K, which will theoretically give a maximum overall efficiency of approximately 50% assuming the same type of fuel and reasonable values of OPR. The theoretical limit to TIT corresponds to the stoichiometric limit of the fuel. (Pickerell, 1984)

Figure 1.1 Turbine Inlet Temperature and Cooling Trends (Byworth, S., 1987)

It is important to realise that TIT is rarely measured due to the difficulties with measuring such high temperatures. Instead, most high bypass engines have temperature probes located between the HP Turbine exhaust and the LP Turbine inlet which measure the Inter-Turbine Temperature (ITT). Some engine manufacturers call this quantity the Exhaust Gas Temperature, but Gas Generator EGT would be more precise. For the remainder of this thesis ITT will be used to describe this quantity, while EGT will be used to denote the LP Turbine Exhaust Gas Temperature (EGT). It can be shown that the relative value of TIT can be inferred fairly accurately from ITT.

Another indirect way of estimating the performance is by monitoring the Fuel Flow (FF). This is of primary concern to commercial airliner operators in that FF dictates the range and relative economy in operating an aircraft with a specific engine. In modern engines, such as those equipped with FADEC (Full Authority Digital Engine Control), FF is automatically controlled by an on-board computer system such that a pilot selects a specific throttle setting corresponding to take-off, climb or cruise thrust. The automatic control system then adjusts fuel flow to prevent engine overheat and to satisfy a desired fuel schedule. An example is the need for minimum fuel burn during climb, which is particularly important for short range flights when the climb to cruise altitude may burn a significant portion of fuel. On long haul flights, minimum fuel flow at cruise is more critical for good range.

1.3 Origin of EPR and IEPR

The use of Engine Pressure Ratio (EPR) as an indirect thrust measurement originates from the use of such ratios in pure turbojet engines. EPR can be shown to be directly proportional to net thrust in the case of a simple, fixed nozzle turbojet in the following manner:

For a choked nozzle,

$$F = m C_N + A (p_N - p_a), \text{ and } \frac{p_N}{p_o} = \left(\frac{2}{\gamma+1} \right)^{\frac{\gamma}{\gamma-1}} \quad (1.1)$$

$$\text{Also, } m = \rho_N A C_N; \quad \rho_N = \frac{p_N}{R T_N}; \quad C_N^2 = 2 C_p (T_o - T_N) \quad (1.2)$$

$$\text{Hence, } F = \rho_N A C_N^2 + A (p_N - p_a) \quad (1.3)$$

$$= \frac{p_N}{R T_N} A \cdot 2 C_p (T_o - T_N) + A (p_N - p_a) \quad (1.4)$$

$$= 2 \frac{\gamma}{\gamma-1} A \left(\frac{p_N}{p_o} \right) \left(\frac{p_o}{p_a} \right) p_a \left(\frac{T_o}{T_N} - 1 \right) + A p_a \left(\frac{p_N}{p_o} \cdot \frac{p_o}{p_a} - 1 \right) \quad (1.5)$$

Substituting the value of $\left(\frac{p_N}{p_o} \right)$:

$$\therefore \frac{F}{A p_a} = \left[2 \frac{\gamma}{\gamma-1} \cdot \left(\frac{2}{\gamma+1} \right)^{\frac{\gamma}{\gamma-1}} \left(\frac{p_o}{p_a} \right) \left(\frac{\gamma+1}{2} - 1 \right) \right] + \left[\left(\frac{2}{\gamma+1} \right)^{\frac{\gamma}{\gamma-1}} \frac{p_o}{p_a} - 1 \right] \quad (1.6)$$

This gives,

$$\frac{F}{A p_a} = \frac{p_o}{p_a} \left\{ \left(\frac{2}{\gamma+1} \right)^{\frac{\gamma}{\gamma-1}} (\gamma+1) \right\} - 1 \quad (1.7)$$

It follows that

$$\frac{F}{A p_a} = K \left[\frac{p_o}{p_a} \right] - 1 \quad \text{where } K = f(\gamma) \quad (1.8)$$

This can be extended to work with EPR instead, and noting that

$$\frac{P_{o7}}{P_a} = \frac{P_{o7}}{P_{o1}} \cdot \frac{P_{o1}}{P_a} \quad \text{i.e. nozzle PR} = \text{EPR} \times \text{RPR} \quad (1.9)$$

Then,

$$\boxed{\frac{\left(\frac{F}{A P_a} + 1 \right)}{\text{RPR}} = K(\text{EPR})}$$

(1.10)

Where $K = 1.2594$ for $\gamma = 1.333$

A similar derivation can be determined for the case of an unchoked nozzle; however, in most cases the hot nozzle is likely to be choked over the useful operational range of a jet engine. Thus, for a fixed nozzle turbojet, the thrust can readily be found from the EPR.

In the case of a turbofan, the EPR will depend on both the bypass ratio and fan pressure ratio since both of these have an effect on the total pressure of the LP Turbine exhaust. Because of this there is no longer a direct relationship between total net thrust and EPR. Also, in high bypass engines a majority of the net thrust is generated by the cold stream, but the cold nozzle conditions are not measured in any way. The concept of IEPR, pioneered by Rolls-Royce in the late 1960s, attempts to include the substantial thrust contribution from the fan or bypass nozzle through an area-weighted average defined as follows:

$$\text{IEPR} = \frac{P_{o2} A_c + P_{o7} A_h}{P_{o1} (A_c + A_h)} \quad (1.11)$$

This requires extensive pressure measurements in the fan duct, involving the use of sophisticated pressure probe "racks" and data acquisition systems. However, IEPR does appear to be a more logical way to indicate the thrust of a high bypass turbofan.

1.4 Scope of Study

This study was limited to the simulation of high bypass turbofan engines as are commonly used in modern civil transport aircraft. The configuration assumed was a twin-spool type with separate nozzles. This excludes the simulation of low bypass military type turbofans which almost always incorporate some kind of mixer and afterburner assembly. There are a few high bypass engines which use a mixed nozzle, but in general such a configuration is rarely seen, as the slight thrust or SFC advantage from using such a scheme is limited to a very restricted range of fan and LP Turbine exit conditions.

Figure 1.2 Turbofan Schematic

Figure 1.2 illustrates the basic twin-spool configuration that was used in this study. The high bypass Fan is driven by the LP Turbine, while the HP Compressor is driven by the HP Turbine on another shaft. Note the station numbering used in Figure 1.2, which includes the presence of a portion of the low pressure compression system (Fan+LPC) in the core flow. These so-called "booster" stages are common in modern turbofans. While the inclusion of these booster stages has almost no effect on the on-design performance (assuming similar efficiencies in the LPC and HPC), they do have an effect on the LP spool power balance and consequently affect the twin spool matching calculation at off-design operating points.

Since the requirement of this study was to establish generic engine performance, the accurate modelling of a specific engine was not of paramount importance except for verification purposes. In particular, the modelling of three-spool turbofans such as the RB211 was not considered. Other details such as variable geometry stators were ignored, and a generalized HPT cooling method was assumed even though specific engine designs have different turbine cooling details. A reasonable cooling system, which is described in detail in Chapters 3 and 4, was modelled by a certain fixed percentage of core air being bled off at the HP Compressor exit location, bypassing the combustion chamber, and being introduced back at the HPT stators and rotors. This cooling air was assumed to be split more or less equally between the HPT stator and rotor stages. This is roughly equivalent to current twin-spool turbofan designs. (Collins and Hill, 1991)

The off-design operation of the generic turbofan was restricted to high rotational speeds, as the assumption of LP Turbine choking (see Chapter 4) would not be valid at very low speed settings such as idle or descent power. Restricting the simulation to high speeds is necessary given the restricted speed range on available component characteristic maps. Such a restriction is not considered to be a major problem, since the issues of adequate thrust and good efficiency are usually only critical at take-off, climb and cruise operating conditions.

Chapter 2 Choice of Design Point

2.1 Definitions of Turbofan Design Points

Modern high-bypass turbofans are designed around several flight conditions or design points, and not just the Sea Level Static (SLS) condition as was perhaps the common design point in early engines when adequate take-off thrust was the primary concern. The most common flight conditions for which modern designs are optimized are (Philpot, 1992):

1) Sea Level Static (SLS) Takeoff: Usually at ISA +15°C, to allow a flat rated take-off thrust up to a hot ambient temperature condition (Hot Day Take-off, or HDTO). Most modern engines automatically control the take-off fuel flow to prevent the TIT from exceeding a specified safety limit. In practical terms, the Inter-turbine Temperature (ITT) is used as the limiter due to difficulties with directly measuring the high TITs common in modern turbofans. The HDTO condition is important, limiting the maximum allowable TIT and hence influencing the ultimate thermodynamic cycle choice. Note that the ITT is sometimes called the gas generator Exhaust Gas Temperature (EGT) by many engine manufacturers. In this thesis, EGT will be used to denote the LP Turbine exhaust temperature.

2) Cruise: A majority of the fuel burned during a typical civil airliner flight will be during cruise, especially for long range. It is essential to have an excellent Specific Fuel Consumption (SFC) while allowing high cruising speed (i.e. maximizing C/L). The choice of engine cycle conditions (FPR, BPR, OPR, and cruise TIT) is optimized around this goal. Aircraft may have different optimum cruise conditions owing to differing flight characteristics, but the most common standard cruise conditions used are Mach 0.8 or 0.85 and 35000 ft altitude. The Mach 0.8 cruise condition has been used in this study.

3) Top-of-Climb (TOC): This condition is also called Maximum Climb, and for high bypass engines it is the point of maximum pressure ratio within the engine. For this reason, the TOC condition sets the maximum Overall Pressure Ratio, and also the fan conditions (FPR, corrected mass flow Q_1 , and corrected LP spool speed). This point is commonly used as the Aerodynamic Design Point of the engine. (Philpot, 1992)

2.2 Matching Different Design Points

For fixed geometry nozzles, which will be the case with modern high-bypass turbofans, the choice of design point has a marked effect on off-design performance. This is especially true when the TOC and SLS conditions are compared, since both nozzles (Fan and Core) will be choked at the TOC, but will normally be unchoked at SLS. The off-design calculation method based on twin-spool matching is highly dependent on an accurate estimate of the fixed nozzle areas, as well as accurate determinations of the design point aerodynamic characteristics of the Fan, Compressor and Turbines.

A brief study was conducted to study the possibility of "matching" the three design points discussed earlier in the same engine. The objective was to model a known engine that was very similar to the generic turbofan design that will be used later in this study. The primary focus was to match known TOC performance with known SLS (take-off) performance of the same engine, and thus determine the design point engine parameters at each discrete design point.

A simplified method was used which utilized the on-design calculation subroutines to specify the first on-design point (usually the TOC) in terms of BPR, OPR, TIT, FPR and cycle efficiencies. The specific thrust and actual thrust (from a known engine) were used to calculate the required mass flow and then the cold and hot nozzle areas. These latter two were expressed as a fixed ratio A_c/A_h . Then, the second design point was defined, as were a second set of cycle parameters except for the BPR. An iterative approach was conducted (with the second set of TIT, OPR and FPR fixed) by varying

BPR until the same A_0/A_n ratio was obtained. Then, the actual mass flow and thrust obtained at this second design point was calculated by scaling the per unit mass flow areas by the nozzle areas found at the first design point.

It was noted that during the BPR iteration, some combinations of TOC and SLS cycles were invalid, resulting in an unsuccessful search for a BPR which gave the correct A_0/A_n ratio. Also, for some combinations of TIT, FPR and OPR at sea level conditions, it was possible for there to be more than one solution and hence two valid BPRs. In this case, the BPR match which yielded the closest mass flow and SLS thrust (as compared with known engine data) was taken as the solution.¹ The important results from the analysis are summarized in Table 2.1.

The TOC-SLS matching process was conducted for two discrete take-off conditions, ISA SLS and ISA+15°C. It was noted that for reasonable BPR matches, the TIT for the ISA+15°C case had to be increased from the equivalent ISA SLS value. This is reasonable when compared with known quoted hot day TITs for an actual engine. The corresponding FPRs for successful matches on hot days had to be lower. In general, high TOC TITs made it difficult (or impossible) for reasonable cycle matches at SLS conditions.

Obviously, there is great flexibility in selecting TOC cycle conditions that will yield reasonable SLS engine conditions, as evident from Table 2.1. However, the solutions closest to actual engine data are fairly difficult to obtain, requiring extensive iterations to "fine-tune" the match until a satisfactory solution is found. The highlighted (*) values denote the solutions that were found which matched available engine data and also approximate the generic engine performance modelled during this study. This similarity to actual off-design performance is notable since the effect of different mechanical spool speeds at the different design points was ignored.

¹. During this analysis, a TOC thrust of 28.6 kN and a SLS Take-off thrust of 133.5 kN (with 427.6 kg/s mass flow) were the target engine values. These correspond to data for the CFM56-5B1 engine which is very similar to the hypothetical advanced cycle engine used in this study. The CFM56-5B1 has a BPR of 5.5 at SLS conditions and a TIT of approximately 1610 K at HDTO, 1530 K at standard SLS conditions. (CFM56 Family Engine Brochure, 1990)

Table 2.1 TOC and SLS Engine Cycle Matches

Engine Cycle Parameters for:								
	TOC	ISA SLS			HDTO (ISA +15 C)			
BPR	5.5	3.75	5.58	6.25	4.4	5.3	5.5	5.7
FPR	1.65	1.58	1.61	1.58	1.55	1.55	1.54	1.54
OPR	35	32.6	32.6	32.6	33	33	33	33
TIT(K)	1400	1572	1572	1572	1597	1597	1597	1597
m_a (kg/s)	175	459	434	420	425	410	406	400
Fnet(kN)	28.6	165	137	126	142	130	125	123
BPR *	5.5 *	3.2	5.5 *	5.6	4.8	5.3	5.5 *	5.7
FPR	1.70	1.58	1.61	1.61	1.55	1.55	1.54	1.54
OPR	35	32	32.6	32	32	32	33	33
TIT(K)	1400	1550	1550	1550	1550	1565	1597	1597
m_a(kg/s)	176.5	400.5	429	429.5	409.5	402.9	406	400
Fnet(kN)	28.6	120	132.6	133.1	127.6	122.5	130	125
BPR	5.5	4.6	4.8	5.9	4.8	4.8	5.4	5.5
FPR	1.65	1.53	1.54	1.53	1.47	1.5	1.47	1.5
OPR	35	33	31	32	32	32	32	33
TIT(K)	1450	1550	1550	1550	1550	1590	1550	1590
m_a (kg/s)	168.8	408.3	408	393	375.3	385	368.1	376.9
Fnet(kN)	28.6	132.4	131.8	119	115.2	123	108.4	113.9
BPR	6.0	5.25	5.5	5.6	4.75	5.25	5.6	
FPR	1.70	1.62	1.61	1.6	1.56	1.55	1.54	
OPR	35	32	32	32	33	33	33	
TIT(K)	1425	1550	1550	1550	1575	1575	1575	
m_a (kg/s)	178.2	440	437	434	425	412	406	
Fnet(kN)	28.6	140	135	136.9	138	127	123	

2.3 Comparison of Different Choices of Primary Design Point

A brief analysis was conducted to show the sensitivity of the overall performance of a bypass engine to the choice of design point. The nature of the three commonly quoted "design points" and a simplified method for matching such design points has been described. It is believed that many modern designs use the TOC as the aerodynamic design point and this is important to consider since at this location both nozzles are likely

to be choked. The areas that would therefore be calculated will be quite different from the sea level static or take-off case when one or both of the nozzles are likely to be unchoked.

Using a known engine², four designs were created corresponding to ISA SLS take-off, HDTO, TOC and Cruise designs. These designs were matched to known engine performance (Net thrust) and cycle parameters at these different conditions. In most cases a number of iterations were needed to match the engine cycle with known design parameters such as TIT and BPR. Table 2.2 details the nozzle areas found for each of the different designs and Table 2.3 shows the variation of cycles for these designs.

Table 2.2 Comparison of Nozzle Areas for Different Designs					
Engine Design	Known Data	ISA SLS	HDTO	TOC	Cruise
Bypass (m_2)	0.999	0.979	1.011	1.001	1.049
Core (m_2)	0.310	0.331	0.376	0.312	0.246

Table 2.3 Comparison of Engine Cycle Parameters and Performance				
Design Type:	ISA SLS	HDTO	TOC	Cruise
BPR	6.0	6.0	5.2	6.0
OPR	28.5	28.5	31.3	26.0
FPR	1.56	1.55	1.75	1.50
TIT (K)	1485	1536.7	1375	1300
m_a (Kg/s)	386.8	386.8	177	159
Net Thrust (kN)	111.3	111.4	25	22.3

². In this case, fairly complete information was available for a related engine, the CFM56-5A1 engine. Accurate values for the fan and core nozzle areas were obtained. (D.Hovey, Air Canada Maintenance, Montreal, August 1992)

It is clear from Table 2.2 that using the TOC flight condition to calculate required nozzle areas provides the most accurate prediction. The performance of these designs over the same set of flight conditions corresponding to a typical climb profile for a modern airliner was simulated using the twin-spool turbofan simulation program detailed in Chapter 5. A number of iterations were needed to obtain the most reasonable climb performance profiles for most of the designs, particularly for finding the most appropriate design spool mechanical speeds for the Fan and HP Spool. The mechanical design speeds will not necessarily be 100%, especially at the TOC and cruise flight conditions.

Figures 2.1 and 2.2 show the variation of Net Thrust and EPR respectively. It is notable that the two SLS designs both appear to accurately predict take-off thrust while they tend to under-predict net thrust at TOC and cruise conditions. Also, whereas these designs give reasonable values for EPR at take-off, during climb it can be seen that the EPR drops to a very low value, even dropping below 1.0. The primary cause seems to be that the core nozzle in both designs has been sized for unchoked conditions and is thus too large. The cruise and TOC designs appear to predict both the take-off and high altitude thrusts more reasonably. The same is true for EPR, though in the case of the cruise design (which has an abnormally large fan nozzle, and small core nozzle) the EPR predicted appears to be rather high overall. The TOC design seems to provide both an accurate prediction of net thrust during climb, and a reasonable prediction for EPR variation. It also has the most accurate combination of nozzle areas, with a maximum error of less than 1% for both nozzles. The TOC condition will be used during the remainder of this study as the main design point for fixing nozzle areas and the design point parameters for the aerodynamic components (Fan, LPC, HPC and turbines).

Figure 2.1 Variation of Net Thrusts during Climb for Different Cycle Designs

Figure 2.2 Variation of EPR during Climb for Different Advanced Cycle Designs

Chapter 3 On-Design Simulation Methods

3.1 Need for Accurate Design Point Performance

Before the performance of a turbofan can be predicted over its operational range, an accurate determination of its design point (hereafter called on-design) performance must be accomplished. In practise, the overall design point performance will be known, along with some of the corresponding design point parameters such as BPR and OPR. Unfortunately, the more obscure parameters such as TTT, FPR and component efficiencies are seldom available. Therefore, it is usually necessary to iterate, using reasonable assumptions for the unknown quantities, to achieve the same performance as is known. Of course, this procedure would not be necessary to simulate a hypothetical design.

Of major importance, the off-design calculation procedure which will be described later required certain on-design performance parameters at a fairly detailed level. These included the design values of PR, non-dimensional mass flow and efficiency for the aerodynamic components (fan, compressors and turbines), to allow off-design simulation based on scaled component characteristics. The twin-spool matching procedure made extensive use of the fan and core nozzle exit areas; indeed the latter area was used during the off-design matching calculation to check the error in successive match trials. Obviously, incorrect nozzle areas would not yield the correct off-design performance trends.

3.2 Basic Assumptions

The on-design calculation procedure followed in this study used several simplifying assumptions. The first assumption concerns gas properties. Throughout the simulation constant gas properties were assumed for the air before the combustion chamber, and for the expansion gases in the hot end (turbines and core nozzle). These fixed gas properties are shown in Table 3.1.

Table 3.1 Constant Gas Properties Assumed

	Compressor Air	Hot Gases
γ (specific heat ratio)	1.400	1.333
c_{pa} (specific heat at constant pressure, kJ/kg)	1.005	1.149
Gas Constant (kJ/kg*K)	0.287	0.287

The turbofan modelled was assumed to use standard hydrocarbon jet fuel with standard properties as detailed in (Cockshutt and Chappell, 1974) hereafter referred to as the Cockshutt-Chapell Tables. The addition of this fuel in the combustion chamber was assumed to have a negligible effect on the net mass flow through the turbines.

Another assumption was that the bypass and core nozzles are separate, such that no mixing of the streams occurs. This simplifies both the on-design and off-design calculations. The use of mixed exhaust nozzles, while very common in low-bypass engines (older turbofans and modern military engines), is rare in high bypass civil engines due to the restricted range of cycle conditions when they are beneficial. As implied by the off-design performance calculations, both nozzles are assumed to be converging nozzles of fixed geometry.

Other assumptions will be noted in the next section which outlines the calculation procedures followed for the on-design performance calculations.

3.3 Calculation Methods

The on-design calculation procedure followed a slightly modified version of the turbofan design point calculation methods as outlined in the text Gas Turbine Theory 3rd Edition (Cohen, Rogers and Saravanamuttoo, 1987). The fundamental thermodynamic principle used was that the compression and expansion processes were polytropic. The details of the solution methods used follow. Note that the station numbering used

throughout this discussion is as shown in Figure 1.2.

First, on-design parameters values are specified for the design point flight condition desired, including:

- (a) Cycle parameters (FPR, PR_{lpc} , OPR, TIT, BPR and m_d)
- (b) Component efficiencies (Polytropic)
- (c) Mechanical efficiencies, cooling bleed and CC pressure drop
- (d) Ambient conditions and flight Mach number, and design values for the mechanical speeds of the LP and HP spools.

Internal Pressures and Temperatures for Specified Cycle

The compression and expansion processes are assumed to be polytropic processes, hence the values of the polytropic indexes $(n-1)/n$ are calculated as:

$$\text{for compressors,} \quad \frac{n-1}{n} = \frac{1}{\eta_{mc}} \left(\frac{\gamma_a - 1}{\gamma_a} \right) \quad (3.1)$$

$$\text{for turbines,} \quad \frac{n-1}{n} = \eta_{mt} \left(\frac{\gamma_g - 1}{\gamma_g} \right) \quad (3.2)$$

Where η_{mc} is the design point polytropic efficiency of the fan, LPC or HPC as appropriate and η_{mt} is the design point polytropic efficiency of the appropriate turbine.

Using the ambient conditions and Mach number, the conditions at the fan entry may be calculated from compressible flow relations and the specified intake efficiency:

$$T_{o1} = T_a + \frac{C_a^2}{2c_{pa}}, \text{ and } p_{o1} = p_a \left(1 + \eta_i \frac{C_a^2}{2c_p T_a} \right)^{\frac{\gamma_a}{(\gamma_a - 1)}} \quad (3.3)$$

The change in conditions through the fan can be calculated by using the polytropic compression process expression and the specified FPR:

$$\frac{T_{o2}}{T_{o1}} = (FPR)^{\frac{n-1}{n}}, \text{ and } P_{o2} = P_{o1} \times FPR \quad (3.4)$$

The compression processes for the booster stages (LPC) and the HP Compressor are simulated in the same manner, using the exit conditions from the previous component as the entry conditions for the next:

$$\text{For the LPC: } \frac{T_{o3}}{T_{o2}} = (PR_{lpc})^{\frac{n-1}{n}}, \text{ and } P_{o3} = P_{o2} \times PR_{lpc} \quad (3.5)$$

$$\text{For the HPC: } \frac{T_{o4}}{T_{o3}} = (PR_{hpc})^{\frac{n-1}{n}}, \text{ and } P_{o4} = P_{o3} \times PR_{hpc} \quad (3.6)$$

Note that with FPR, OPR and PR_{lpc} specified, PR_{hpc} is fixed. The exit conditions at the HPC exit could be calculated directly using OPR, assuming the same polytropic efficiencies in each compression component. p_{o4} would be correct regardless since this is how OPR is defined; T_{o4} would not be the same if the three compressors have different polytropic efficiencies.

The total mass flow through the engine is split after the fan into a bypass and core mass flow. The core mass flow is modified by the incorporation of a basic HPT cooling model in the simulation. There are many different cooling strategies incorporated in current turbofan designs, making simulation of a specific engine fairly difficult. A simplified cooling system was assumed as follows: high pressure cooling air is bled off at the HPC exit as a fixed percentage of the total core mass flow. This bleed, designated bl , bypasses the combustion chamber and is added back at the HP Turbine stators and rotors, with 50% going to each. The cooling system that is simulated is shown in Figure 3.1.

The change in HP Turbine mass flow affects the HP Turbine inlet mass flow parameter, Q_5 , and hence the HP Spool match. Thus, for calculation of Q_5 , the mass flow

Figure 3.1 HP Turbine Cooling Bleed Schematic

through the combustion chamber is used, $m_h (1 - bl)$. The cooling air added at the rotor is not considered in the HP spool work balance, but at this point about 50% of the bleed air is assumed to have been returned, so the mass flow used for calculating HP turbine work is $m_h (1 - bl/2)$. No attempt is made to determine the effect of these cooling bleeds on the gas properties or conditions (total pressure or temperature). These would be very minor if the cooling system is designed correctly. By the time the core flow reaches the LP Turbine entrance, the bleed is considered to be completely returned.

Using the specified total mass flow m_a , BPR and bleed, the mass flows throughout the turbofan are calculated as follows:

$$m_c = m_a \frac{BPR}{BPR+1}; \quad m_h = m_a \frac{1}{BPR+1} \quad (3.7)$$

$$m_1 = m_a; \quad m_2 = m_3 = m_h; \quad m_{cc} = m_h (1 - bl) \quad (3.8)$$

$$m_{(HPT, \text{stators})} = m_{cc}; \quad m_{(HPT, \text{rotors})} = m_5 = m_h (1 - \frac{bl}{2}) \quad (3.9)$$

The two definitions for the mass flow through the HP Turbine are required to allow separate calculations of the HPT inlet mass flow function (Q_5) and the rotor work calculation.

The small fraction of mass added as fuel in the combustion chamber is neglected; in practise, an additional amount of HP Compressor bleed is used to cool the spools and pressurize bearings and is then dumped overboard. This small negative loss is assumed to be countered by addition of the fuel mass at the combustion chamber.

The expansion processes are simulated using power balances for each spool, which can be used to find the total temperature drops. Polytropic expansion processes are then used to calculate the resulting pressure drops, starting with the total pressure at the HP Turbine entrance. The procedures used are as follows:

The total temperature and pressure at HPT entry are:

$$T_{05} = TIT; \quad p_{05} = p_{04}(1 - \Delta p_b) \quad (3.10)$$

where Δp_b is the drop in total pressure in the combustion chamber, expressed as a percentage of the CDP. The power balance for the LP spools is:

$$m_1 c_{pa}(T_{02} - T_{01}) + m_2 c_{pa}(T_{03} - T_{02}) = m_h c_{pg}(T_{06} - T_{07}) \eta_{mech} \quad (3.11)$$

And for the HP Spool:

$$m_3 c_{pa}(T_{04} - T_{03}) = m_5 c_{pg}(T_{05} - T_{06}) \eta_{mech} \quad (3.12)$$

Solving the LP and HP power balances yields the temperatures at the hot end of the engine (i.e. the ITT and EGT). Note that if boosters are not desired, the PR_{hc} can be set to 1.0 which will yield the same total pressures and temperatures at stations 2 and 3. The second term in the LP power balance (left hand side) would likewise disappear. Omitting the LPC by essentially "lumping" the booster contribution to the OPR into the HPC pressure ratio has no effect on the on-design performance provided the same polytropic efficiencies are used. Boosters do, however, have an effect on the off-design performance of a turbofan by modifying the LP and HP spool matching.

Total pressures at each of the stations downstream of the HPT entry can now be solved using the TIT, ITT and EGT and polytropic expansion processes:

$$\frac{p_{o5}}{p_{o6}} = \left(\frac{T_{o5}}{T_{o6}} \right)^{\frac{n}{(n-1)}} = \left(\frac{TIT}{ITT} \right)^{\frac{n}{(n-1)}} \quad (3.13)$$

$$\frac{p_{o6}}{p_{o7}} = \left(\frac{T_{o6}}{T_{o7}} \right)^{\frac{n}{(n-1)}} = \left(\frac{ITT}{EGT} \right)^{\frac{n}{(n-1)}} \quad (3.14)$$

All temperatures and pressures within the engine are now known. What remains is the determination of fuel flow, nozzle conditions and areas and the resulting thrust generated with the choice of cycle.

Fuel Flow

The fuel flow is calculated using the approach outlined in the Cockshutt-Chapell Tables. The first step is to calculate the fuel/air mixture ratio, f , which can be calculated using the equivalent caloric value (ECV) of the fuel and enthalpy of the combustion air (h_a) at the entrance and exit of the combustion chamber:

$$f = \frac{h_{a5} - h_{a4}}{ECV_s + (h_{fF} - h_{fR})} \quad (3.15)$$

The values of ECV and h_a can be determined from the total temperature at the relevant station; polynomial relations provided by the Cockshutt-Chapell Tables are used to calculate these values at the appropriate combustion chamber inlet and exit temperature conditions. The second term in the denominator accounts for the sensible enthalpy of the liquid fuel at injection above that at a reference temperature; this term is generally very small in comparison with ECV and has been neglected. Once the fuel/air ratio is known, the actual fuel flow ($\dot{m}_f$) may be calculated using the air mass flow through the combustor

(m_{cc}) and the combustor efficiency:

$$m_f = \frac{f \times m_{cc}}{\eta_{cc}} \quad (3.16)$$

Determining Nozzle Sizes and Thrusts

The conditions at the entrances of the cold (bypass) and hot (core) nozzles have been calculated previously, and are the stagnation properties at Stations 2 and 7 respectively. These, in combination with the known ambient conditions, flight condition and nozzle mass flows, allow the two nozzles to be sized and thrust to be calculated. Recall that an accurate estimate of both (fixed) nozzle areas is essential to the off-design calculation procedures.

Apart from the difference in the hot and cold gas properties, the procedure is identical for the two nozzles. Using the hot nozzle as an example, since $p_{o,7}$ is known, the nozzle pressure ratio is also known ($p_{o,7}/p_a$). This ratio must be checked against the critical pressure ratio to determine if the nozzle is choked:

$$\frac{p_{o,7}}{p_c} = \left[1 - \frac{1}{\eta_{jh}} \left(\frac{\gamma_s - 1}{\gamma_s + 1} \right) \right]^{\frac{\gamma_s}{\gamma_s - 1}} \quad (3.17)$$

If the nozzle pressure ratio is greater than the critical pressure ratio, the nozzle is choked and the exit static conditions are calculated in the following manner:

$$T_9 = T_c = \left(\frac{2}{\gamma_h + 1} \right); \quad p_9 = p_c = p_{o,7} \left(\frac{p_c}{p_{o,7}} \right) \quad (3.18)$$

$$\rho_9 = \frac{p_c}{RT_c}; \quad C_{jh} = \sqrt{\gamma_h RT_c} \quad (3.19)$$

If the pressure ratio is equal to or less than the critical pressure ratio, the nozzle is unchoked. In this case, the static pressure at the nozzle exit plane is the ambient pressure. The other static conditions are calculated as follows:

$$T_9 = T_{o7} - \eta_{jh} T_{o7} \left[1 - \left(\frac{p_a}{p_{o7}} \right)^{\frac{(\gamma_h - 1)}{\gamma_h}} \right] \quad (3.20)$$

$$\rho_9 = \frac{p_9}{RT_9}; \quad C_{jh} = \sqrt{2c_{pg}(T_{o7} - T_9)} \quad (3.21)$$

The required nozzle area can now be calculated using the known mass flow and continuity:

$$A_h = \frac{m_h}{\rho_9 C_{jh}} \quad (3.22)$$

Finally, the net thrust generated by this nozzle may be calculated:

$$F_h = m_h(C_{jh} - C_a) + A_{jh}(p_9 - p_a) \quad (3.23)$$

The nozzle conditions, area and thrust for the cold (bypass) nozzle are calculated in precisely the same manner, with inlet conditions at station 2 and cold gas properties. The total net thrust generated by the turbofan is the sum of the hot and cold nozzle thrusts. Note that if a nozzle is choked, there will be a pressure thrust contribution, while the pressure contribution will disappear in the case of an unchoked nozzle.

Calculating Specific Values and Thrust Indicators

With the overall engine performance and internal cycle conditions calculated, the determination of the indirect thrust indicators, monitoring temperatures and specific performance parameters are straight forward. They are calculated as follows:

$$F_{net} = (F_c + F_h) ; \quad F_{spec} = \frac{F_{net}}{m_a} ; \quad SFC = \frac{FF}{F_{net}} \quad (3.24)$$

$$ITT = T_{o6} ; \quad EGT = T_{o7} ; \quad EPR = \frac{p_{o7}}{p_{o1}} \quad (3.25)$$

$$IEPR = \frac{(p_{o2}A_c + p_{o7}A_h)}{p_{o1}(A_c + A_h)} \quad (3.26)$$

Determining Non-Dimensional Performance Values

During on-design calculations, the design spool speeds are not used, however, the non-dimensionalized on-design LP and HP spool speeds are important since they are used during the off-design performance calculations. Similarly, the design values of the isentropic efficiencies and non-dimensional mass flows (Q) are required for each of the aerodynamic components, to allow scaling of generalized component characteristics. The non-dimensional mass flows are calculated as:

$$\text{For aerodynamic components: } Q = \frac{m\sqrt{T_j T_{std}}}{p_j p_{std}} \quad (3.27)$$

$$\text{And for nozzles: } Q = \frac{m\sqrt{RT_o}}{Ap_o} \quad (3.28)$$

T_{std} and p_{std} are the standard ambient conditions sea level (288.16 K and 1.01325 Bars respectively). Care is needed when calculating Q , since this must be the mass flow parameter at the stator entrance of the HPT, and not the flow parameter at the first stage rotor inlet. The design values for the isentropic efficiencies for the fan, LPC, HPC and turbines are calculated using the polytropic efficiency and PR for each component. The conversion formulas used are:

For compressions:
$$\eta_c = \frac{PR^{\frac{(\gamma-1)}{\gamma}} - 1}{PR^{\frac{(\gamma-1)}{\eta_c \gamma}} - 1} \quad (3.29)$$

For expansions:
$$\eta_e = \frac{1 - \left(\frac{1}{PR}\right)^{\frac{(\gamma-1)\eta_e}{\gamma}}}{1 - \left(\frac{1}{PR}\right)^{\frac{(\gamma-1)}{\gamma}}} \quad (3.30)$$

Finally, the design point values for the LP and HP spool speeds are calculated using the specified values of mechanical speed (expressed as a percentage of the maximum spool design speed) and local corrected temperatures. Due to the flexibility in selecting the design point of a turbofan the design mechanical speeds are not necessarily 100%. The design values for non-dimensional spool speed are required for each compressor component; namely the fan, booster, and HP Compressor. Normally, the boosters and Fan will have the same mechanical spool speeds, but in smaller engines it is possible to have a gearbox between these. However, since percentage speed is used for N_1 , the distinction between the fan and booster mechanical speeds is not necessary. The non-dimensionalized values of spool speed for each component, nr , is thus:

$$nr_{fan} = \frac{N_1}{\sqrt{\frac{T_{o1}}{T_{std}}}}; \quad nr_{lpc} = \frac{N_1}{\sqrt{\frac{T_{o2}}{T_{std}}}}; \quad nr_{hpc} = \frac{N_2}{\sqrt{\frac{T_{o3}}{T_{std}}}} \quad (3.31)$$

Calculation of the turbine thermodynamic speeds is not necessary, as will be discussed in Chapter 4, since the assumption of LP Turbine choking limits the operation of both turbines such that spool speed effects are minimal.

Chapter 4 Off-Design Simulation Methods

4.1 Assumptions

A number of assumptions have been used during the off-design performance simulations to simplify the solution. Several of these assumptions are the same as those previously described for the on-design simulation methods, while some are distinct to the twin-spool matching procedure used for the off-design calculations. The assumptions that were applied are:

(a) Constant Fluid Properties

As with the on-design simulation, fixed specific heats are assumed for the compressor and turbine gases. Also, a fixed gas constant ($R = 0.287 \text{ kJ/kg}\cdot\text{K}$) is used.

(b) Ambient Conditions are I.S.A. Standard

The ambient conditions are assumed to be International Standard Atmosphere (ISA) values for a specified flight altitude. Some flexibility exists in the case of ambient temperature, which can be defined to be different from the ISA by some amount, such as ISA +15°C which is commonly used to simulate a hot-day take-off condition.

(c) Fixed HPT Turbine Cooling Method

The advanced cycle turbofan simulation includes a fixed HPT cooling model which was described in detail in Section 3.3. Basically, this method is simulated by a fixed percentage of compressor delivery air being bled off, bypassing the combustion chamber and being re-introduced at the HPT. Included in this model is the assumption that this bleed air does not contribute to HPT work output, and that the cooling air is divided approximately evenly for stator and rotor cooling.

d) Fixed Geometry Unmixed Nozzles

The high bypass turbofans simulated in this study are assumed to have separate bypass and core nozzles of fixed geometry (area).

e) Definition of Spool Speeds

One of the more difficult concepts to reconcile with actual engine data is the concept of percentage mechanical speed. It has been assumed in this thesis that 100% mechanical speed is the maximum speed that a spool has been designed to withstand safely; this is intimately related to the stress levels and material limits in the fan, compressor and turbine rotors and these are themselves influenced by thermodynamic considerations, especially the TIT. In other words, 100% N_1 or N_2 has NOT been assumed to be the **design speed** of the rotor but rather the **design limit**. Owing to the flexibility in selecting the design point of a turbofan (as discussed in Chapter 2) it is probable that at the TOC design point, N_1 and N_2 will be somewhere in the range of 92-98%. In this thesis, the generic engine simulated has been assumed to have mechanical speeds of 100% N_1 for Hot Day Take-offs (when TIT is maximum), 97% N_1 for standard day take-offs (ISA SLS), 95% N_1 during climb, and 90% N_1 at cruise. N_2 is calculated as a result of the off-design matching methods, but a design point value is needed. In general, a value of N_2 at TOC will be selected with the aim of achieving 100% N_2 at HDTO conditions.

(f) Fixed Duct Pressure Losses and Efficiencies

No attempt has been made to simulate variable pressure losses in the turbofan ducting, intake or nozzles. Thus, a fixed pressure drop is used for the combustion chamber, defined as a percentage of the compressor delivery pressure. The pressure losses in the intake and nozzles are simulated by fixed isentropic efficiencies. There are methods that can be used to estimate variable pressure losses in ducts, but these are essentially secondary effects and not considered important in this study since determining general performance trends was the main focus of the simulations. (Gas Turbine Theory, 3rd Edition, pp.342-343)

g) Simplified Turbine Characteristics

Figure 4.1 shows a typical turbine characteristic. At low speeds, it has been suggested that there is some dependence on corrected speed, but this can be approximated fairly accurately by a single line. A similar observation can be made for the efficiency of a turbine, which is essentially constant above the choking PR, but does exhibit some speed dependence at low PR. If high turbine speeds are assumed, a turbine characteristic may be modelled successfully by a single curve, making it essentially identical to a nozzle characteristic curve. The assumption that LP Turbine stators are choked, which is reasonable for the high speeds under consideration in this thesis, simplifies the situation further such that the LP Turbine operates in a region where the PR is high enough for both the non-dimensional mass flow (Q) and efficiency to be constant. Due to flow compatibility and the fact that the HP and LP turbines are in series, this causes the HPT operating point to be fixed. The details of this LPT choking concept will be shown later in this Chapter, but the important conclusion is that if LPT choking is a valid assumption, there is no longer a need to use actual characteristics for either turbine during off-design calculations.

Figure 4.1 Typical Turbine Characteristics (Gas Turbine Theory 3rd Ed., p.293)

4.2 Twin-Spool Matching Procedure

What follows is an overview of the twin-spool matching procedure used to simulate off-design performance of an advanced cycle turbofan. It is essentially identical to the procedure described in Chapter 9 of the text Gas Turbine Theory, 3rd Edition (Cohen, Rogers and Saravanamuttoo, 1986), but with modifications added to include the simulation of LPC (booster) stages downstream of the fan. It is presented here in simplified form; the detailed mathematics and procedures for component modelling are described in Section 4.3.

The assumption of LP Turbine choking results in fixing the operating point of the HP Turbine. This has the effect of limiting the operation of the HP Compressor to a single operating line. Once design point performance is determined, this operating line may be solved before starting the matching procedure, by finding the unique operating point for each corrected speed of the HP Compressor. This solution procedure is shown in Section 4.3. The pre-determination of this operating line simplifies the matching calculations for the HP Compressor, resulting in faster solutions.

- (a) For the specified flight condition (Mach, T_a and p_a at given altitude) the conditions at fan inlet (T_{01} and p_{01}) may be calculated.
- (b) From the specified fan mechanical speed (N_1) the corrected speed can be calculated as $nr_1 = N_1/\sqrt{T_{01}}$. A point on this speed line (on the fan characteristic) is selected, giving Q_1 , FPR and efficiency. From these three operating fan parameters m_a can therefore be calculated, as can the fan exit conditions: T_{02} , P_{02} and Q_2 .
- (c) The bypass nozzle pressure ratio (p_{02}/p_a) is known. Using nozzle relations, the non-dimensional mass flow that can be passed (Q_c) is calculated. From this Q_c , the cold nozzle mass flow (m_c) may be found from the known fixed cold nozzle area. The core mass flow and BPR are then calculated from $m_h = m_a - m_c$ and $BPR = m_c/m_h$. This defines the gas generator inlet conditions.

(d) The non-dimensional mass flow and corrected speed of the booster are now determined as follows:

$$Q_2 = \frac{m_h \sqrt{T_{o2}}}{p_{o2}}; \quad nr_b = \frac{N_1}{\sqrt{T_{o2}}} \quad (4.1)$$

The value of nr_b defines the appropriate speed line on the LP Compressor map, and using the calculated value of Q_2 , the corresponding values of PR and efficiency can be determined. Exit conditions are then calculated (T_{o3} , p_{o3} , and Q_3) which are the entry conditions for the HP Compressor.³

(e) The HP Compressor operating line due to LPT Choking is already known. The inlet flow conditions (Q_3) are used to find the exact point on the operating line that corresponds to this mass flow. This will be a unique point, providing PR_{hpc} , compressor efficiency and the corrected HP spool speed (nr_2). The temperature ratio (T_{o4}/T_{o3}) and rise (ΔT_{o34}) through the HP Compressor can now be calculated.

(f) The HP Spool mechanical speed (N_2) and OPR are now calculated:

$$N_2 = nr_2 \sqrt{T_{o3}}; \quad \left(\frac{p_{o4}}{p_{o1}} \right) = \left(\frac{p_{o2}}{p_{o1}} \right) \times \left(\frac{p_{o3}}{p_{o2}} \right) \times \left(\frac{p_{o4}}{p_{o3}} \right) \quad (4.2)$$

(g) The HP Turbine inlet pressure and mass flow can be calculated based on the defined pressure drop and HPT cooling bleed:

$$p_{o5} = p_{o4}(1 - \Delta p_b); \quad m_{cc} = m_h(1 - bl); \quad m_5 = m_h(1 - \frac{bl}{2}) \quad (4.3)$$

³. Step (d) is required if booster stages are to be simulated, otherwise conditions at HPC inlet (station 3) are identical to those calculated at Fan exit (station 2).

where Δp_b is the combustion chamber pressure drop as a percentage of the CDP, and bl is the percentage of HP Compressor air bleed used for HP Turbine cooling, as described in Chapter 3.

(h) The value of Q_5 is a fixed quantity if LP Turbine choking is assumed, and is the design value Q_{5d} calculated during on-design calculations. T_{05} , the Turbine Inlet Temperature (TIT), is now calculated:

$$T_{05} = \left(Q_{5d} \times \frac{p_{05}}{m_{cc}} \right)^2 \quad (4.4)$$

(i) The HP Turbine is operating at a fixed operating point due to LP Choking, which fixes the values of p_{05}/p_{06} and $\Delta T_{056}/T_{05}$, which were calculated during on-design performance determination. The conditions at the LP Turbine entrance are then calculated as:

$$p_{06} = p_{05} \times \left(\frac{p_{06}}{p_{05}} \right); \text{ and } T_{06} = T_{05} - \Delta T_{056} \quad (4.5)$$

The mass flow at LP Turbine entrance is m_h , with the assumption that all cooling bleed has been returned to the core flow. The conditions at the HP Turbine exit could also be calculated by doing a power balance for the HP Spool.

(j) The inlet conditions are known for the LP turbine, and the temperature drop can now be calculated by calculating the power balance for the LP Spool. The LP Turbine polytropic efficiency (assumed to be constant) is then used to calculate the pressure ratio for the LPT. The power balance for the LP Spool is the same as used for the on-design calculations:

$$m_c c_{pa} (T_{02} - T_{01}) + m_2 c_{pa} (T_{03} - T_{02}) = m_h c_{pg} (T_{06} - T_{07}) \eta_{mech} \quad (4.6)$$

The second term on the LHS disappears if booster stages are not present since in that case $T_{03} = T_{02}$. The conditions at the LP Turbine exit are now determined (T_{07} , P_{07}).

k) The LPT exit conditions are used to calculate the hot nozzle PR. The non-dimensional mass flow parameter (Q_h) can be calculated using this PR and the hot nozzle isentropic efficiency using compressible flow relations (Wittenberg, 1976). From the core mass flow and Q_h the required nozzle area (A_h) can be found since the non-dimensional mass flow parameter for a nozzle is defined:

$$Q_h = \frac{m_h \sqrt{RT_{07}}}{A_h p_{07}}; \quad \therefore A_h = \frac{m_h \sqrt{RT_{07}}}{Q_h p_{07}} \quad (4.7)$$

l) The calculated area (A_h) is compared with the fixed hot nozzle area as determined during the on-design calculations (A_{hd}). In general A_h will not agree with the design area A_{hd} . In this case, a different point is selected on the fan speed line and the process is repeated starting at step (b), until the calculated hot nozzle area agrees with the design value.

Once agreement is obtained between the calculated and fixed hot nozzle areas, the cycle conditions for the turbofan are known. Calculation of the fuel flow, nozzle thrusts and thrust indicators is then straight forward, following the identical procedures as done for the on-design performance.

For practical computer solutions, the above procedure must be modified slightly to provide some logic for the fan speed line search pattern; the main concern is to avoid impossible bypass ratios which result in unsolvable LP Spool power balances. The details of this logic will be discussed in Chapter 5. The source of the various component characteristics will be shown in the next section.

4.3 Component Modelling Methods

(a) Fan

To allow simulation of spool speed effects, it was necessary to use fan characteristic data. Detailed data on an exact fan or compressor is very difficult to obtain, being highly proprietary to a turbofan manufacturer. Since the objective of this study was to simulate typical high-bypass turbofan behaviour, a generic fan map was needed that would be representative of the high efficiency wide-chord type fans now common in turbofans. Fortunately, such a map was found for the JT15D-4 turbofan. The map was modified slightly by adjusting the positions of the speed lines. This fan map should provide a fair degree of accuracy for turbofans with design FPRs of between 1.4 and 1.8, and with polytropic efficiencies the same as the current state-of-the-art (88-92%). The modified fan map can be seen in Figure 4.2.

Figure 4.2 Generalized Fan Characteristic (Boyd, D.I., 1987)

For practical computer use of this characteristic, each speed line was divided into 10 points with the top point assumed to be on the surge line. The relative values of nr_1 , Q_1 , FPR and efficiency for each point were determined and this information was stored in a data file in tabular form. Interpolation was used to define a speed line or operating

point between these known points. For a given off-design matching calculation, the fan speed line has to be defined once, before the start of the iterative process. This is accomplished by using the corrected fan spool speed (found from the specified flight conditions, Mach number, T_{01} and N_1) and converting the relative performance parameters for the 10 points making up this line to actual fan performance values. Actual fan performance parameters are calculated by multiplying the design point performance values by the relative values obtained from the generalized fan map.

The computerized twin-spool matching procedure alters the choice of fan operating point by choosing a different speed line data point depending on the outcome of the hot nozzle area estimate (see step (b) in the previous section) calculated in the previous iteration. If the calculated nozzle is too big, a point further down the speed line (away from the surge line) is used; if the calculated hot nozzle area is too small, the opposite choice is made. (Gas Turbine Theory, pp.354-355)

(b) Boosters

The booster stages (LPC) were simulated in a manner similar to that described for the fan. A generalized LPC map was constructed using performance curves that were available from the literature for a high efficiency LP compressor. (Reynolds, et.al., 1991) In this case the available LPC map data was in dimensional form but fortunately the design point values for this unit were available from the map. A generalized LPC Map was constructed by dividing the actual map values by the design point parameters. In the case of PR_{ipc} , care was needed to allow future scaling of the relative map PR. Values for PR relative to the design PR_d were calculated by using the formula:

$$Rel\ PR = \frac{(PR-1)}{(PR_d-1)} \quad (4.8)$$

The resulting relative LPC Map used in the simulation can be seen in Figure 4.3.

Figure 4.3 Generalized LPC Characteristic (Reynolds, et.al., 1991)

The generalized LPC Map was used during the simulation in a manner similar to that for the fan. Each of the relative speed lines were defined by dividing them into 10 regularly spaced points, and the relative performance parameters were determined for each point and saved in a tabular form. During the off-design calculations, the calculated conditions after the fan were used in combination with the known LP mechanical speed to define the LPC corrected speed. The corresponding LPC speed line on Figure 4.3 is then constructed, interpolating between known speed lines as necessary and scaling the speed line to the design point operation of the LPC. Since the non-dimensional mass flow Q_2 is defined from the fan exit conditions and the previously solved BPR, a unique operating point on the LPC non-dimensional speed line can be determined, which will yield the corresponding values of PR_{lpc} and efficiency for the iteration. Additional logic is necessary in the computerized search procedure to avoid LPC operating points that are beyond the limits imposed by the constructed speed line.

(c) HP Compressor

The HP Compressor was simulated using a generic map that was derived from the HPC characteristic for the LM-2500 (AN, GE Product Brochure, 1980). The HPC characteristic, combined with knowledge of the design point characteristics of this unit, allowed the construction of a generalized map in a manner similar to that described for the booster stages. The LM-2500 HPC was chosen as representative of the high performance HP Compressors required in turbofans with OPR=35 or greater. It can be shown that given a modest FPR and booster PR, the high pressure compressor ratio in such an engine would be approximately the same as the LM-2500 HPC pressure ratio. Figure 4.4 shows the generalized HPC Map that was derived. A process identical to that described for the LPC was used to convert the dimensional information on the LM-2500 map into relative (generalized) information usable by the computer simulation. Each relative speed line was defined by dividing it into ten equally spaced points, and saving the information for each point in a formatted data file.

Figure 4.4 Generalized HPC Characteristic

The computer simulation of the HPC was simplified by determining the operating line on the HPC characteristic. This operating line could be calculated ahead of time by using the assumption that the LP Turbine stators are choked, which restricts HPC operation to a single line on its characteristic. This simplification becomes invalid at low HP spool speeds when the LPT may become unchoked, but such speeds were not considered in this study. The HPC operating line is calculated using an iterative approach, from flow and work compatibility for the core:

$$\frac{m_5 \sqrt{T_{o5}}}{p_{o5}} = \frac{m_3 \sqrt{T_{o3}}}{p_{o3}} \times \frac{p_{o3}}{p_{o4}} \times \frac{m_5}{m_3} \times \frac{p_{o5}}{p_{o4}} \times \sqrt{\frac{T_{o5}}{T_{o3}}} \quad (4.9, \text{ flow})$$

$$\frac{\Delta T_{o56}}{T_{o5}} = \frac{\Delta T_{o34}}{T_{o3}} \times \frac{T_{o3}}{T_{o5}} \times \frac{c_{pa}}{c_{pg} \eta_{mech}} \times \frac{m_3}{m_5} \quad (4.10, \text{ work})$$

The solution for each speed line is calculated by choosing points up and down each HPC speed line until both equations give the same temperature ratio (T_{o5}/T_{o3}), thus defining the HPC parameters for the point. This is done for each speed line in turn, defining the operating line through these points. An example of such an operating line can be seen in Figure 4.4. The operating line is solved in dimensional form; that is, the points on each speed line are defined in terms of actual HPC performance values as used in the matching procedure. These dimensional parameters are determined by multiplying the information on the generalized HPC Map information by known design point operation values for the HPC during the operation line solution procedure. The solution of the two equations given for T_{o5}/T_{o3} is greatly facilitated by LP Turbine choking since then Q_3 , p_{o5}/p_{o6} and $\Delta T_{o56}/T_{o5}$ are the fixed design values determined during on-design performance calculations.

During the off-design matching procedure, the HPC operating line is used to solve the HPC pressure ratio and efficiency for a given non-dimensional mass flow Q_3 which is known from the LPC exit conditions. The solution point is found by interpolating

between known points on the operating line as necessary; additional logic is used to ensure that impossible matches (such as too high or too low Q_1) are avoided.

(d) Combustion Chamber

The combustion chamber is simulated using Cockshutt-Chapell tables to calculate the fuel flow as for the on-design calculations. The pressure drop and mass flow through the chamber (m_{cc}) are both calculated under the assumption that there is a constant percentage of HPC delivery air being bled to the HPT for cooling, and that the pressure drop is a constant percentage of the CDP.

(e) Turbines

It has been mentioned that the performance of axial flow turbines can be simulated through the use of turbine characteristic curves which can be approximated by a single line showing the non-dimensional flow rate Q for a given turbine pressure ratio. This implies that there is little dependency on rotational speed, as detailed turbine characteristics attempt to indicate, for the range of rotational speeds being considered. With this simplification, the turbine characteristics are identical to those for fixed area nozzles. This assumption is seen to be generally applicable, especially when the effect of having two turbines and a nozzle in series is considered.

In the case of turbines, downstream component (another turbine or a nozzle) have a direct influence on performance due to the requirement of flow compatibility through the components. If the LP Turbine is assumed to be choked, which is a valid assumption for most of the practical operating range of modern turbofans, then the simulation of the two turbines is greatly simplified. Figure 4.5 shows the effect of two turbines in series.

Figure 4.5 Flow Compatibility for Two Turbines and a Nozzle In Series

It can be seen that LP Turbine choking restricts the operation of the HP Turbine to a fixed pressure ratio and non-dimensional flow rate. The non-dimensional flow rate of the LP Turbine would also be constant for PRs above the critical (choking) limit. Once the design point values of these variables are calculated, the need for using actual turbine characteristics is eliminated. This is the method that has been used during this study. As was described in the section on HP Compressor modelling, LP Turbine choking facilitates the determination of a unique operating line on the HP Compressor characteristic, simplifying the off-design calculation procedure.

(f) Nozzles

It has been mentioned that simplified turbine characteristics resemble the graphical representation of fixed geometry converging nozzle operating characteristics (characteristics). A typical nozzle characteristic will be as shown in Figure 4.5, with a horizontal (constant Q) portion above some pressure ratio, called the critical pressure ratio or choking limit of the nozzle. For ideal nozzles, this PR is constant depending only on the nature of the gas being passed through. For normal air (with $\gamma=1.4$) this critical

pressure ratio is 1.893, while for turbine gases (with $\gamma=1.333$) it is 1.853. For less than ideal nozzles, these critical pressure ratios will be slightly higher. The calculation of critical pressure ratio is derived from compressible flow relations, corrected for a specified isentropic nozzle efficiency.

It can be shown that the precise nature of nozzle characteristics (usually a line representing the non-dimensional mass flow with nozzle PR) can be described quite adequately by using compressible flow relations (Wittenberg, 1976). The only unique problem is if a particular nozzle is not 100% efficient, which modifies the shape of the curve. However, the effect of a less than perfect nozzle can still be simulated through use of compressible flow relations; the nozzle equations will then have to take into account the efficiency of the nozzle. Usually, the efficiency of a nozzle is stated as an isentropic efficiency which is assumed constant. The derivation of the relevant nozzle equations, which are based on isentropic flow relations for compressible flows is shown in detail in the report by Wittenburg (Wittenberg, 1976). The resulting nozzle equations are shown below for an unchoked nozzle,

$$\frac{m \sqrt{RT_o}}{Ap_o} = \frac{p_a}{p_o} \sqrt{\frac{2\gamma}{\gamma-1} \eta_n \left(1 - \left(\frac{p_a}{p_o} \right)^{\frac{\gamma-1}{\gamma}} \right)} \cdot \frac{1}{1 - \eta_n \left(1 - \left(\frac{p_a}{p_o} \right)^{\frac{\gamma-1}{\gamma}} \right)} \quad (4.11)$$

and for the case of a choked nozzle (when the nozzle PR is above the critical ratio value),

$$\frac{m \sqrt{RT_o}}{Ap_o} = \left(1 - \frac{1}{\eta_n} \frac{\gamma-1}{\gamma+1} \right)^{\frac{\gamma}{\gamma-1}} \cdot \sqrt{\gamma \frac{\gamma+1}{2}} \quad (4.12)$$

Thus, all that is needed is the PR of the nozzle (and knowledge of the type of fluid), and the non-dimensional mass flow of the nozzle may be calculated directly. If the

inlet conditions and actual mass flow are known, the required area can be calculated from Q . The opposite calculation is also possible, where area can be specified and then the mass flow that the nozzle can handle could be found. Using these nozzle formulae results in highly accurate nozzle simulations, and eliminates the need to solve the nozzle performance graphically through the use of characteristics.

The nozzle exit plane properties (static temperature and pressure) are calculated using the approach followed in the on-design simulation procedures. The calculation of fuel flow, thrusts and thrust indicators is then a straight forward procedure since a successful twin-spool matching calculation results in all internal cycle conditions being known for the engine. Once these conditions are known, the solution process is identical to that used for the on-design calculations.

Chapter 5 Turbofan Simulator Program Overview

5.1 Overall Program Structure

The turbofan simulation software was developed using a recent version of MicroSoft Fortran (Version 5), though in most respects this follows the Fortran 77 standard. Once compiled, it will run on an IBM PC or compatible computer with reasonable speed. The program was designed in modular format, with most of the basic computations conducted by individual subroutines and linked together during the compiling process. Additional comments and details on the programming aspects of this program may be found in the Operators Manual which has been prepared for the program. What follows is a description of the major features of the program, including the menu structure.

Figure 5.1 Overall TURBOFN4 Program Structure

TURBOFN4 Main Program

Figure 5.1 shows the overall organisation of the turbofan simulation package. As evident from this schematic, the simulator is organised around a menu-style structure, with the main program called TURBOFN4. This is the fourth version of the TURBOFAN simulation package developed by the author, abbreviated to conform to the filename length limits in DOS. TURBOFN4 contains the Main Menu which calls all other subroutines. Besides controlling the main menu, TURBOFN4 does all initialization functions for the simulation program, such as defining variables and loading in default program settings. The three main functions of the simulator, on-design, off-design and faulted engine simulations, are conducted in separate subroutines of the same name, and called from the TURBOFN4 main menu as Options 1 through 3 respectively. Other subroutines are called as needed, which set the flight conditions, faulted engine conditions and program defaults.

5.2 On-Design Module

The on-design calculation module is responsible for defining, editing, saving and retrieving the turbofan design parameters. The module itself is called ONDES.FOR, and it is a menu driven program which calls several sub-processes as shown in Figure 5.2. The menu display also shows the current engine design that is under consideration, complete with an abbreviated output of the design point cycle and the estimated performance of the design. The major features of the on-design calculation module will be described in the sections which follow.

Figure 5.2 On-Design Module Structure

CYCLE

The "heart" of the on-design calculation procedure, this subroutine does a single on-design calculation based on the current design point parameters. *CYCLE* uses the design point calculation methods described in detail in Chapter 3 to estimate the turbofan design performance, the indirect thrust indicators, and various design point parameters required by the off-design calculations. *CYCLE* itself calls several short subroutines (some are common subroutines shared by other programs) which are used to conduct the more frequent on-design calculations such as the conversion of polytropic to isentropic efficiency and determination of fuel flow. The results of the analysis are passed back to the calling program.

DESREAD, DESWRITE

These two subroutines are used to read in a previously saved design (*DESREAD*) or to write the current design for latter use (*DESWRITE*). *DESREAD* is automatically

used when TURBOFN4 is started, reading in the last design that was defined, whose design parameters are stored in a file called DESSET.DAT. Within the ONDES subroutine, designs may be read or written using user-defined names as desired. When ONDES or TURBOFN4 are exited, the current design is automatically written to DESSET.DAT by DESWRITE. This backup is done in case there is a program crash, and to speed up initialization of the main program.

EFFY, FUEL, HEATS, PROPS

These are small subroutines which conduct calculations repeated often by the turbofan modelling program. EFFY is used to convert a polytropic efficiency to the equivalent design point isentropic efficiency based on pressure ratio and whether a component is a compressor or turbine. FUEL calculates fuel flow by finding the fuel/air ratio based on the combustion chamber inlet and exit temperatures, the enthalpies at these temperatures, and the ECV of the fuel. HEATS is the subroutine which calculates these enthalpies and ECVs based on polynomial fits as given by (Cockshutt-Chapell, 1974). PROPS calculates the ambient temperature and pressure conditions at a specified altitude, based on standard conditions at 0 meters, and a linear variation in temperature up to the tropopause. Ambient pressure is calculated by using algebraic expressions based on hydrostatic type pressure variation with atmosphere depth. A change in ambient temperature from standard values may also be used (eg. ISA +15°C) which is applied after the standard temperature is calculated. This is assumed to have no effect on the ambient pressure.

NOZZLES, NOZLOOK, THRUSTS

NOZZLES is used to calculate the nozzle conditions in the manner described in Chapter 3, while NOZLOOK is used to determine the design nozzle area as discussed in Chapter 4. THRUSTS uses the nozzle areas and flow conditions to calculate the thrusts from each nozzle, overall net thrust and various thrust indicators.

OUTPUT

This outputs a detailed description of the turbofan design and design point performance behaviour. This may be used to match the performance of an existing engine if such details as design point internal cycle conditions or nozzle areas are known. This information may be output in three ways: to the screen, printer or a file. OUTPUT is a "shared" subroutine, as it is used in a similar fashion during off-design calculations.

5.3 Off-Design Module

The off-design calculations are initiated from the Main Menu in TURBOFN4, either for an unfaulted engine (Option 2) or faulty engine (Option 3). The off design module is not menu driven, but instead performs an automated off-design analysis of the current design with or without certain faults. The procedure is essentially identical to the methods detailed in Chapter 4, with the initialization procedures (mostly the pre-determination of the fixed HP Compressor operating line) accomplished at the start of the run. Off-design then cycles through the current set of flight condition data points (defined in the subroutine FLTSET, called from the Main Menu of TURBOFN4), determining the appropriate fan speed line and then passing control to the sub-routine MATCH which conducts the twin spool matching procedure. Upon a successful match result, MATCH returns the solved engine cycle conditions (total temperatures and pressures at all stations) to OFFDES which then calculates the overall performance at the flight condition. This is accomplished by calling NOZZLES, FUEL, and THRUSTS, which were described in the previous section. The subroutine OUTPUT then outputs the results of the twin spool match to one of three destinations; screen, printer or file. The structure of the OFFDES module may be seen in Figure 5.3, which also shows the program execution procedure. The individual subroutines are detailed in the following paragraphs.

FANLIM (LPCLIM), FANSPEED, FANLOOK

These subroutines are responsible for assembling the required fan speed line for each flight condition. FANLIM reads the speed limit ranges from the first line fan map

data file. A similar routine called LPCLIM is used to define the speed limits for the LPC (booster) stages, if these are to be included. FANSPEED calculates the corrected fan speed based on the flight conditions for the run, which include the mechanical speed, aircraft altitude and Mach number. Using this corrected speed, FANLOOK selects two sets of speed line data from the fan map data file and then determines the required fan speed line by interpolation. The output of this procedure is a set of 10 data points defining the fan speed line for the current off-design operating point.

Figure 5.3 Off-Design Module Structure

HPCGEN and HPCREAD

These subroutines are used at the start of all off-design calculations to determine the fixed HPC operating line defined by the LP Turbine choking assumption. The procedure used is an iterative one, following the mathematical matching process that was

outlined in Section 4.3(c). HPCGEN is used to calculate the set of operating points located on each HPC corrected speed line, which define this line. TURBOFN4 keeps track of whether or not changes to the HPC, HPT or LPT have occurred since the last off-design procedure. If they have not, then the HPC operating line should not have changed. In this case HPCREAD reads the operating line data from a small data file called HPCLINE.DAT. This data file is automatically created at the completion of the calculation procedures in HPCGEN, defining each of the HPC speed line operating points for later use and manual error checking.

MATCH

This subroutine conducts the twin spool matching calculation, based on the procedures outlined in Chapter 4. The subroutine makes extensive use of small subroutines to perform calculations for each of the individual components. This was done for two reasons. Each separate subroutine could be tested and debugged without having to compile and run the entire TURBOFN4 program. Secondly, due to the modular nature of the program, individual component modelling procedures may be altered and/or improved without changing MATCH itself. All that is required is the same organisation for the variable listing in the CALL statement to the subroutine. Most subroutines are quite straightforward, conducting component modelling procedures as was outlined in Chapter 4. Only those involving complex or important procedures will be detailed here.

Extensive logic is required to make MATCH find the correct twin-spool match in an optimum fashion. The procedure starts by choosing the top point ($i=10$) of the pre-generated fan speed line. The coarse search pattern then proceeds, with the off design performance calculated at this chosen point. The default search pattern is to decrement the search index (i.e. $i=i-1$) as long as the calculated hot nozzle area A_h is greater than the design area A_{hd} . This search pattern is illustrated in Figure 5.4.

Figure 5.4 Diagram Showing Fan Speed Line Iteration Procedure

The coarse search continues until one of the following conditions are met:

- (a) The calculated hot nozzle area is too small ($A_h < A_{hd}$) which means that the solution has just been passed. In this case, the search backs up to the previous pattern, and a fine search procedure is started.
- (b) The calculated area equals the design value or is within the error range desired. This results in a successful match, and control is passed back to OFFDES to find the overall performance at this operating condition.
- (c) The last point ($i=1$) is reached without passing the solution. Decrementing the search pattern is not possible, and so a legitimate fan match cannot be found. The search procedure is terminated, and control is passed to OFFDES, which alerts the operator to this problem. In practical terms, an unsuccessful match such as this must be due to a severe fan operating condition being specified. If option (a) is true for the first

search point ($i=10$) then moving up the fan speed line puts it into surge, which is not allowed by the simulator. This generates an unsuccessful search result.

The fine search pattern uses a binary search procedure, initially using the two known operating points (i and $i+1$) as the low and high data points. Then, one-half of the spacing between these is used, stepping down the fan speed line as before until the solution point is found or passed. Fan speed line points are determined by interpolation where necessary. The search loops back to the last point and the step is divided in half again. This procedure continues, resulting in successively finer search steps, until the solution is found or the search increment becomes smaller than the desired error range parameter. A typical core area error range was 0.005 m^2 , which corresponds to an overall error of approximately 1.5% for the size of engine simulated (core area of 0.32 m^2 or approximately 500 in^2).

Additional logic is required to allow selection of the appropriate LPC operating point and the match point on the HPC operating line. MATCH controls access to the subroutines used to find the LPC and HPC operating conditions, filtering attempts at matching points in either case which are out of range or which would result in a fatal program crash. The main purpose of the logic is to augment the selection of the fan speed lines, since some choices (especially for high FPR) result in inlet conditions at the LPC or HPC which are impossible for them to handle within the range imposed by the LPC and HPC map data. During both kinds of searches (coarse and fine) logical sections immediately after the LPC and HPC calculations are used to, when necessary, short circuit the normal fan speed line point selection.

In the case of the LPC (boosters) the logic is used to avoid problems with later calculations. Figure 5.5 shows the types of logic used for the LPC match, as detailed below:

- (a) Any I/O Errors are flagged, indicating a problem with the format of the LPC map data file. The matching procedure is terminated.
- (b) A successful LPC match causes MATCH to proceed immediately to the HPC calculations.

(c) If the mass flow Q_2 into the LPC is too low, it cannot be found on the specified LPC speed line. In this case, a point further DOWN the fan speed line is selected ($i=i+1$).

(d) If the mass flow Q_2 into the LPC is too high, a match on the LPC speed line will not be possible. Another fan speed line point must be selected closer to the surge line. This triggers a fine search in the event that this is flagged during the coarse search. If already within a fine search, this triggers a loop back in the procedure with a finer increment.

Figure 5.5 Diagram of LPC Match Procedures

The search/match logic followed in the case of the HP Compressor is similar to the LPC logic, but this time is simplified by the presence of a pre-determined operating

line. With a Q_3 specified for the HPC to swallow, a match should be found on the line unless this mass flow is excessively low or high. The logic followed for the HPC match can be seen in Figure 5.6. The details of this logic are:

(a) If the HPCMATCH procedure encounters problems with using the HPC map data, an I/O error is flagged which terminates MATCH in a non-fatal manner. Such an error indicates a problem with the data file; either the format is wrong or perhaps it is missing.

(b) If Q_3 is too small, a match on the operating line may not be possible since this would put it at a speed outside the lower limit of HPC speeds. In this case, another fan speed line point further down is needed.

(c) If Q_3 is too large, a match will not be found since the required mass flow is greater than the highest value on the operating line. In this case a fan speed line point closer to the surge line (with lower overall mass flow) is needed. If this occurs during a coarse search, a fine search is initiated. If already within a fine search procedure, this triggers a loop-back to the previous point and a finer increment being chosen.

During the initial iterations of the coarse and fine searches impossible hot end conditions could be generated, especially when the calculated Bypass Ratio is significantly higher from the design Bypass Ratio. This can lead to improbable LP Spool work balances and impossible pressure or temperature conditions at the LP Turbine exit. The principle cause for this situation is excessive Fan Pressure Ratio. To avoid calculating negative total temperatures or pressures below ambient (both of which will result in a fatal math error in the LPTURB or NOZLOOK subroutines) MATCH automatically executes a fan speed line point decrement away from the surge line, short circuiting the hot nozzle area estimate calculation.

Figure 5.6 Diagram of HPC Match Procedures

FAN

This subroutine calculates the exit temperature and pressure conditions based on specified performance parameters defined by the current choice of fan operating point along the speed line.

NOZLOOK

This subroutine uses analytical expressions based on compressible flow relations to calculate the non-dimensional flow Q for a convergent nozzle for a given pressure ratio. From Q , the mass flow which may be passed may be determined if the area of the nozzle is known (Mode 1). If the mass flow is known, the opposite calculation (determination of required nozzle area) may be conducted instead (Mode 2). Following the fan calculation, Mode 1 is used to find the cold mass flow that can be passed by the bypass nozzle which has a known (fixed) area. Following the LP Turbine calculations, Mode 2 is used to calculate the hot nozzle area required for the known core mass flow.

LPCOMP, LPCGEN, LPCMATCH

LPCOMP is the subroutine called from MATCH which determines the operating point and the resulting performance for the booster stages (if used). LPCOMP itself calls other subroutines which are used to find the LPC operation point. LPCGEN uses the corrected spool speed determined from fan exit conditions to generate the correct LPC corrected speed line. LPCMATCH then uses the Q_{ch} that the LPC must swallow to find the appropriate point on the generated LPC speed line. Following a successful match, LPCOMP calculates the overall performance of the booster stages, in particular the exit temperature and pressure conditions for the HP Compressor. An unsuccessful match is signalled to MATCH through the use of flags which are analyzed according to the LPC match logic protocol described previously.

HPCOMP, HPCMATCH

These subroutines model the performance of the HP Compressor. HPCOMP is called from MATCH, and controls the determination of HPC performance. HPCMATCH uses an iterative search procedure along the known HPC operating line until a match is found between the required Q_3 to be swallowed, and the Q_3 possible on the operating line. A successful match yields the Pressure Ratio and efficiency of the HPC, which are returned to HPCOMP. An unsuccessful match results in a premature termination, and return to MATCH which interprets the reason by use of error flags. Following a successful HPC match, HPCOMP calculates the performance of the HP Compressor, in particular the exit temperature and pressure conditions.

COMB, HPTURB, LPTURB

COMB calculates the combustion chamber mass flow and the combustor exit pressure. TIT is determined based on the combustion chamber mass flow and exit pressure and the assumption that the non-dimensional mass flow into the HP Turbine stators (Q_5) is fixed. HPTURB calculates the exit conditions after the HP Turbine through the use of the known (fixed) HPT pressure ratio and isentropic efficiency. LPTURB calculates the conditions after the LP Turbine by conducting a LP spool work balance.

5.4 Miscellaneous Program Features

Several additional features of the program are called from TURBOFN4 itself. These subroutines are used to define the operating parameters for the simulator. Another major feature is the simulation of various kinds of discrete faults (detailed description of the methods used for fault simulations are contained in Chapter 7). A brief description of the miscellaneous subroutines called from TURBOFN4 will be given here.

DEFSET, DEFREAD

These routines are used to set the default values used by the simulation. DEFSET is a menu driven routine for editing the current set of defaults. These include whether or not booster (LPC) stages are to be used, the level of detail desired for OUTPUT, the names of the component characteristic map data files, the default values to be used for the various gas properties, the values for standard sea level temperature and pressure, and the level of accuracy used during the twin spool matching calculation. Upon completion of DEFSET, these values are automatically saved by writing to a file called DEFAULT.DAT. This file is read by the subroutine DEFREAD when TURBOFN4 is initialized. Hence, the program maintains a record of the most recent default settings.

FLTSET, FLTREAD, FLTWRITE

These subroutines are used to set the flight conditions to be used during off-design calculations. The default flight conditions are stored in a file called FLIGHT.DAT which is automatically read in when TURBOFN4 is begun, by the subroutine FLTREAD. FLTSET is the subroutine used to edit the number of flight conditions to be used, and the parameters for each. To define a flight condition, the altitude, Mach number, change in temperature from ISA and the fan mechanical speed (as a percentage for the design speed) is required. The program then automatically determines the ambient conditions and actual flight speed for each point. It is possible to edit the design point flight condition within FLTSET (this is by default the first condition in the list), but this requires that ONDES be run to properly calculate the new design operation conditions

before any off-design calculations are conducted. Also, FLTSET can save and retrieve alternative sets of flight conditions using FLTWRITE and FLTREAD respectively, which may have any name provided they are of the proper format. The precise format required is discussed in Appendix A. FLTWRITE saves the flight condition information in the correct format, automatically upon exiting FLTSET (in the file FLIGHT.DAT) or when requested within FLTSET (in which case a different file name can be used).

FAULTSET, FALTREAD, FAULTED

These subroutines are used to set various component faults to simulate the effect of engine deterioration on performance. FAULTSET is used to edit the current set of fault conditions by defining a percentage change in some component performance parameter. Currently, deterioration in one of the major aerodynamic components (Fan, Compressors or Turbines) may be simulated by specifying a change in design point polytropic efficiency or non-dimensional mass flow (Q). Other types of deterioration such as problems with mechanical components (combustion chamber, bearings, bleed valves, nozzles or intake) have not been implemented in this version of TURBOFN4. FALTREAD is used to load in fault settings from a data file as required. When TURBOFN4 is started, the last set of fault settings are automatically read in from the default file FAULT.DAT. The contents of this file are automatically updated each time that the subroutine FAULTSET is terminated. Within FAULTSET, fault information can be saved and retrieved using user-defined filenames. When a faulted turbofan is to be simulated by choosing Option 3 from the TURBOFN4 main menu, control is initially passed to FAULTED. This subroutine performs several initialization processes which essentially save the unfaulted design-point component parameters before they are modified by the changes in FAULTED.DAT. Then, control is passed to OFFDES which conducts a full set of off-design performance calculations using modified values of the component design parameters. Following OFFDES, control is returned to FAULTED which reverts all design parameters back to unfaulted form before returning to the main menu of TURBOFN4.

Chapter 6 Nominal Simulation Results

6.1 Baseline On-Design Performance

Before focusing on a particular advanced cycle, it is interesting to note the effect of the design point engine cycle on overall performance and indirect thrust indicators. A series of on-design calculations were conducted for a range of cycles at two design points, defined as shown in Table 6.1. Essentially, OPR and TIT were held constant while BPR and FPR were changed. The TIT and OPR at each location are typical for modern high bypass engines with BPRs of 5 to 6. The TIT/OPR combinations at SLS and TOC are roughly equivalent, representing typical variation in these two parameters from take-off to TOC for modern turbofans.

Table 6.1 Range of Engine Cycles used for On-Design Estimates

Cycle Parameter	Sea Level Static (ISA)	TOC (Mach 0.8 @ 35000 ft)
BPR	2.0 to 9.0	2.0 to 9.0
FPR	1.2 to 1.8	1.2 to 1.8
OPR	32	35
TIT (K)	1550 ⁴	1400

Constant polytropic efficiencies of 90% were assumed for the main components (Fan, LPC, HPC, Turbines) at both design points. Other mechanical details were also kept constant; nozzle, combustion and spool efficiencies were assumed to be 99%, while

⁴. Thermodynamically equivalent to 1630 K for HDTO (ISA +15°C).

the intake efficiency was assumed to be 95%. The combustion chamber pressure drop was taken to be 5% of the CDP, and 10% HP Turbine cooling bleed was used, simulated by the simplified cooling model described in Chapter 3.

6.1.1 Specific Fuel Consumption and Specific Thrust

The variation of specific thrust and SFC for the two design points can be seen in Figure 6.1. The TOC conditions are equivalent to typical cruise conditions for the purposes of on-design calculations. It can be seen that high BPR engines have reduced specific thrust (for the same TIT and OPR) and thus larger engines are needed for the same net thrust, and more thrust is derived from the bypass nozzle. High BPRs also have a beneficial effect on SFC at both design points. Higher FPRs give increased specific thrust and reduced SFC for a given BPR, though there is a limit to FPR due to the energy extraction limit for the LP Turbine, when the amount of net thrust obtained from the bypass nozzle approaches 100%. This accounts for the nature of the high FPR lines at high BPR in Figure 6.1, with specific thrust rapidly dropping off. The FPR is also

Figure 6.1 Specific Thrust and SFC for Different Advanced Cycle Designs

limited for reasons of minimizing weight and noise, for which a single stage fan is considered essential. In practical terms, this limits the FPR to a maximum value of about 1.8. (Philpot, 1992) Lower values of FPR would only be used for ultra high BPR engines as the optimum FPR would be lower than the current 1.6 to 1.8 range most common in turbofans with BPRs of 5 to 6.

The effect of equivalent engine cycles moving from the take-off condition to the cruise/TOC design point is also evident from Figure 6.1. In general, the bypass ratio of a given engine does not change much from the take-off to cruise condition. FPR (and OPR) will in general be maximum at TOC, but less again after throttling back to the cruise condition. The drop in TIT (1550 K to 1400 K) is typical for this class of turbofan (BPR=5 and OPR=35) and cruise TIT would most likely be somewhat lower, in the range of 1300-1350 K. These variations in cycle parameters will be confirmed in later sections of this Chapter. Figure 6.1 shows that in moving from take-off to TOC/cruise, the SFC increases significantly while specific thrust decreases, even when major changes in cycle parameters are considered. However, at both design points, higher FPR and BPR result in improved specific fuel consumption, which explains the current trend towards higher BPR, especially at cruise.

6.1.2 Thrust Splits

The thrust splits at the two design points are shown in Figures 6.2 and 6.3, expressed as the percentage of total net thrust contributed by the cold (bypass) nozzle. It can be seen that the engine cycle choice has a major effect on the portion of thrust derived from the fan. Current high bypass turbofans have take-off BPRs of around 5. The percentage of thrust derived from the cold stream is approximately 70-88% at standard SLS conditions. For the same BPR but at TOC/cruise (when the FPR will be somewhat higher) the percentage of cold thrust is slightly lower, ranging from 60-82%. Both figures show that as BPR and FPR are increased, the portion of net thrust obtained from the bypass flow increases.

TIT=1550 K, OPR=32, 90% Polytropic Efficiencies

Figure 6.2 Net Thrust Split for Advanced Cycles at SLS

TIT=1400 K, OPR=35, 90% Polytropic Efficiencies

Figure 6.3 Net Thrust Split for Advanced Cycles at TOC

6.1.3 EPR and IEPR Variations

The thrust splits for different cycles are interesting when they are compared with the trends for EPR and IEPR for the same cycles. Figures 6.4 and 6.5 show the EPR for various cycles at standard SLS and TOC conditions, respectively. The trend for EPR is opposite to that observed for the thrust split, tending towards a value of 1.0 at high BPR and FPRs. These figures show that for current high bypass engines (with BPRs of 4.8 to 6), the EPR at ISA SLS will range from about 1.3 to 1.8, whereas the range at TOC (assuming typical changes in OPR and TIT) will be slightly higher, 1.4 to 2.0. Figure 6.5 shows that as BPR is increased, such as is the trend with upcoming ultra-high bypass engines, EPR drops below 1.5 and approaches 1.0. EPRs below 1.0 are possible for excessive FPR/BPR combinations, though it is expected that lower FPR be used for ultra high bypass turbofans (BPRs of 9 or more) since these are optimum.

Similar graphs are shown for IEPR in Figures 6.6 and 6.7. The variation of IEPR for both design point choices is somewhat different from EPR. High BPR results in lower IEPR, whereas higher FPR in general results in higher IEPR except at high BPRs when IEPR starts to fall off again past a particular value of FPR. This is due to the IEPR being a weighted average of the PRs of both nozzles. The degree of IEPR variation is much lower than the corresponding change in EPR, showing the moderating effect of this averaging process. For typical modern turbofans, the IEPR at sea level static conditions will vary from about 1.47 to 1.65. The equivalent engine at TOC conditions will have an IEPR of between 1.6 and 1.75.

Advanced Twin Spool, OPR=32, TIT=1550, 90% Poly

Figure 6.4 EPR Trend for Advanced Cycles at ISA SLS Design Point

Advanced Twin Spool, OPR=35, TIT=1400, 90% Poly

Figure 6.5 EPR Trend for Advanced Cycles at TOC Design Point

Advanced Twin Spool, OPR=32, TIT=1550, 90% Poly

Figure 6.6 IEPR Trend for Advanced Cycles at ISA SLS Design Point

Advanced Twin Spool, OPR=35, TIT=1400K, 90% Poly

Figure 6.7 IEPR Trend for Advanced Cycles at TOC Design Point

6.2 Advanced Cycle Design Choice

To simulate the behaviour of a generic advanced cycle turbofan, the design point engine cycle had to be defined before conducting any off-design calculations. A typical modern turbofan design was selected at its aerodynamic design point, assumed to be at the Top-of-Climb (TOC) condition. The design details for this engine are given in Table 6.2. This combination of cycle parameters is very similar to the CFM56-5 family of engines, in particular the CFM56-5B1 engine, and was selected to give the same take-off performance as quoted in available engine brochures. The combination of spool speeds used at TOC results in approximately 100% N_1 and N_2 at HDTO conditions. The CFM56-5B1 is flat-rated (up to ISA +15°C) to provide 133.5 kN of SLS Thrust, and has a BPR=5.5 and $\dot{m}_a=428$ kg/s at ISA SLS conditions. (AN, CFM56 Family of Engines Brochure, 1990)

Table 6.2 Design Data for Advanced Cycle Twin-Spool Turbofan

Intake Efficiency	95%
Polytropic Efficiencies	90% (Fan, Boosters, HPC, HPT, LPT)
Nozzle Efficiencies	99%
Spool/bearing Efficiencies	99%
Combustion Efficiency	99%
Combustion Pressure Drop	6.5% of CDP
HPT Cooling Bleed Flow	10% Total (5% Stators, 5% Rotors)
Cycle Conditions @ TOC Design Point (Mach 0.8 @ 35000 ft):	
Fan Pressure Ratio	1.7
Bypass Ratio	5.3
Overall Pressure Ratio	35.4
Turbine Inlet Temperature	1400 K
Design Air Flow	176 kg/s
Spool Mechanical Speeds	$N_1 = 95\%$, $N_2 = 93\%$
Nozzle Areas (m ²)	Cold = 1.023 Hot = 0.321

The off-design performance was simulated by running the engine detailed in Table 6.2 at a variety of flight conditions simulating typical take-off, climb and cruise performance. Simulating climb performance required some knowledge of typical airliner climb procedures, particularly the True Airspeed (TAS) at different altitudes. A climb profile was constructed based on data for a DC-10 airliner. This profile can be seen in Figure 6.8. This profile is based on take-off rotation of 165 kts, 250 kts (knots, indicated airspeed) to 10000 ft, 340 kts to 27000 ft, and then a constant Mach number of 0.8 until a cruising altitude of 35000 ft is reached. The latter Mach number was modified to accord with the choice of Mach number used in this study.

Figure 6.8 Typical Climb Profile for Modern Airliners (Shevell, 1989)

For simulations of climb performance, a constant N_1 was assumed. Take-off performance simulation involved varying ambient conditions (all for SLS) and fan spool speeds. Cruise performance was simulated by varying flight Mach number and throttle setting (N_1) for a constant flight altitude (35000 ft).

6.3 Off-Design Performance Estimates

6.3.1 Climb Performance

The climb performance of the hypothetical advanced cycle engine was determined using the simulation software by conducting off-design calculations at discrete points on the climb profile shown in Figure 6.8. It is important to note that for purposes of this study, a constant mechanical fan speed ($N_1=95\%$) was assumed during climb. Also, an N_1 of 100% was used at ISA SLS to simulate take-off. This will over estimate the take off thrust since 100% N_1 is likely to be used only on hot day take offs. This will be discussed in the section on take-off thrust.

(a) Thrust and Fuel Flow

Figures 6.9 and 6.10 show the variation of net thrust and fuel flow over the climb profile used. Figure 6.9 shows that the simulator is able to predict the expected trend, with gradual decrease in thrust with increasing altitude and Mach number. The effect of throttle-back at the Top of Climb (95% down to 90% N_1) is also evident. The fuel flow trend is predicted correctly, as shown in Figure 6.10, decreasing with increases in altitude and increasing with Mach number. The simulator predicts that the SFC at take off will be 0.0334 kg/N•hr (0.33 lb/lb•hr) while the cruise SFC (assuming a throttle setting of 90% N_1) will be 0.0575 kg/N•hr (0.564 lb/lb•hr). These estimates are quite reasonable when compared with actual SFC values for current high bypass turbofans. (Collins and Hill, 1991)

(b) EPR and IEPR

The variation of EPR and IEPR during climb can be seen in Figures 6.11 and 6.12 respectively. Both parameters decrease with increasing forward speed (i.e. Mach number) but there is also an altitude effect for both indicators. The changes in EPR appear to be more marked than corresponding changes in IEPR. The nature of the climb profile used (gradually increasing Mach number and altitude) is such that up to 15000 ft, EPR

decreases to a minimum value of approximately 1.3, but then starts to increase with further changes in altitude, reaching a maximum value of about 1.58 at TOC. The changes in EPR at constant Mach number (the vertical trend at Mach 0.8) is due to changes in ambient conditions with altitude, and the effect of throttle-back to cruise is quite evident, since this causes a reduction of EPR to 1.3. The variation of IEPR is less dramatic, starting at a take-off value of 1.62, gradually decreasing to a value of 1.5 just under 10000 ft, then increasing to 1.67 at TOC. Throttle-back to cruise (90% N_1) causes a drop in IEPR to 1.52.

(c) Variation of Spool Speeds

While a constant LP spool speed was used, it is interesting to note how N_2 changes during climb. Figure 6.13 shows the predicted variations of N_1 and N_2 during a typical climb. Following take-off (when N_1 is assumed to be 100%), the fan speed is reduced to 95%, with N_2 dropping to 97% at 2000 ft. With N_1 kept constant during climb N_2 drops, eventually reaching a value of 93% at about 25000 ft. From this altitude to the top of climb altitude of 35000 ft, the simulator predicts that N_2 will remain more or less constant. When N_1 is throttled back at cruise, it can be seen that N_2 drops by a similar amount, such that a cruise N_1 of 90% gives a HP Spool speed of 88%. The choice of N_2/N_1 combination at the TOC is highly dependent on the engine cycle (especially FPR and BPR), and a number of iterations were necessary to find the right combination that gave the desired result of 100% N_1 and N_2 at the hot day take-off condition while also providing reasonable estimates of other take-off cycle parameters. The take-off (0 ft) condition on Figure 6.13 is for standard day, when N_2 is slightly less than 100%.

(d) Cycle Temperatures during Climb

The variation of cycle temperatures has been shown in Figure 6.14. In general, all three hot end temperatures tend to decrease with increases in altitude, due to the decrease in ambient temperature with altitude. However, this effect is at least partially counteracted by increases in forward speed (i.e. Mach number), and in several points in the climb, the trends for TIT and ITT reverse. This happens to TIT and ITT between 10000 and 13000 ft. Another aspect which should not be ignored is that the corrected value of TIT is not decreasing. Indeed, following the throttle decrease at the start of climb, the corrected value of TIT increases steadily during climb, apart from the range of altitude mentioned when it drops (corresponding to the reversal of the ITT/TIT trend). At the TOC condition, the corrected TIT is approximately the same as the HDTO value (1635 K) which supports the contention that the TOC and HDTO flight conditions represent two equivalent design points for the turbofan cycle chosen.

Figure 6.9 Thrust Trend during Climb

Figure 6.10 Variation of Fuel Flow during Climb

Figure 6.11 Variation of EPR during Climb

Figure 6.12 Variation of IEPR during Climb

Figure 6.13 Variation of Spool Speeds during Climb

Figure 6.14 Change in Cycle Temperatures during Climb

6.3.2 Cruise Performance

Cruise performance for the advanced turbofan was simulated for a typical flight level (35000 ft) but at variable Mach numbers. The effect of throttle adjustments at cruise were simulated by varying the fan spool speed (N_1) from 88% to 96%, which also causes a proportional change in other thrust setting parameters. A setting of N_1 95% is equivalent to the Top-of-Climb setting used as the design point, as was discussed in Section 6.2. The most important results from these simulations was the variation of thrust when different thrust indicators were used. Of additional concern was the variation of fuel flow with a desired flight speed.

(a) N_1 as Thrust Setting

Figure 6.15 shows that the variation of cruise thrust with fan speed is approximately linear over the range of Mach numbers and spool speed settings considered. While increases in Mach number appear to give less thrust for a given N_1 setting, it can be seen that this effect is relatively minor, and the thrust variation can be approximated by a single curve. The cruise thrust may thus be defined in terms of the fan speed setting, with a 1% change in spool speed yielding a difference of 1.25 kN (281 lb) cruise thrust. Using the 90% setting as the base thrust (22 kN), this increase or decrease in thrust is approximately 5.7%.

(b) Fuel Flow as Thrust Setting

The variation of cruise thrust when fuel flow (FF) is used as a thrust indicator may be seen in Figure 6.16. It can be seen that the effect of Mach number increases is more pronounced when compared with Figure 6.15, with higher FF required to maintain the same thrust (or alternatively, less thrust being obtained for the same FF). However, the variation of thrust for each Mach number appears to be the same and linear, with an increase of approximately 1.6 kN (329 lb) for each 100 kg/hr additional fuel. Using the 90% N_1 cruise condition (FF = 1250 kg/hr) as the base cruise thrust, this means an increase of 0.89% thrust per 1% of additional fuel flow.

Figure 6.16 also serves to show the fuel flow required for a given cruise speed. If an increase in aircraft speed is desired (i.e. Mach number increase), more FF is needed just to maintain the same thrust level. However, higher cruise speeds will also require more thrust since drag will in general increase with Mach number. From the graph, it can be seen that these two effects are additive, contributing to an overall increase in the required fuel flow. For example, if it is desired that the aircraft accelerate from Mach 0.8 to 0.85, this will require approximately 50 kg/hr extra fuel assuming the same thrust level. However, drag is likely to increase proportionally to the square of the forward velocity. As a first approximation, this will mean approximately 13% more drag or about 2.8 kN more thrust. This increase would require another 170 kg/hr, for a net increase of approximately 220 kg/hr, or 17.6% more FF to cruise at Mach 0.85 rather than Mach 0.8.

(c) EPR and IEPR as Thrust Settings

Figures 6.17 and 6.18 show the variation of net thrust when EPR or IEPR is used as the thrust setting parameter. These appear to indicate that increases in Mach number will result in more net thrust for the same EPR or IEPR setting, quite contrary to what happens when N_1 or Fuel Flow are used. This appears to be due to the effect of increased Ram Pressure Ratio (RPR) which will cause a decrease in EPR or IEPR. In each case, the variation of thrust appears to be linear once again, especially in the case of IEPR. The change in net thrust with an adjustment to IEPR appears to be approximately twice the change if EPR is used. For the engine simulated, a 0.1 EPR increase will yield 2 kN more thrust (+9.1%), while the same IEPR increase will give 4 kN additional thrust (+18.2%).

Table 6.3 summarizes the results obtained for each thrust setting indicator when used to set cruise thrust.

Table 6.3 Summary of Cruise Thrust Setting Parameters

Thrust Setting Parameter	Base Cruise Setting (22 kN thrust)	1% Change in Parameter at Cruise	Change in Thrust (kN) (%)
N_1 (fan speed)	90%	1%	1.25 kN (5.7%)
FF (kg/hr)	1250	12.5	0.196 kN (0.89%)
EPR	1.3	0.013	0.26 kN (1.2%)
IEPR	1.52	0.0152	0.62 kN (2.8%)

From Table 6.3, it appears that N_1 is the most sensitive parameter for setting cruise thrust, while FF seems to be the least sensitive in terms of percentage change. However, it should be appreciated that the 1% change in EPR or IEPR is quite small, especially when changes in flight conditions are considered, in particular Mach number. N_1 (as predicted by the simulator) seems less likely to be influenced by external flight conditions at cruise, as evidenced by the slight change due to forward aircraft speed (Mach number). Thus, setting thrust at cruise is likely to be more critical when either EPR or IEPR are used, while not quite as critical if N_1 is used. FF is rarely used as the main thrust setting parameter in actual airline practise, though it is used (in conjunction with Gas Generator EGT and N_2) to indicate the relative health of the engine and the economy of the cruise condition. This latter consideration is of prime importance for commercial airlines (Kenward, 1976)

Figure 6.15 Cruise Thrust vs. N_1

Figure 6.16 Cruise Thrust vs. Fuel Flow

Figure 6.17 Cruise Thrust vs. EPR

Figure 6.18 Cruise Thrust vs. IEPR

6.3.3 Take-off Performance

Since the issue of adequate thrust is likely to be of concern during take-off, detailed simulations of the take-off performance of the engine were conducted for various take off conditions. Three typical take-off conditions were used:

- (i) Hot Day Take-off (HDTO): Sea level static condition, but with an elevated ambient temperature (ISA +15°C), simulating a hot day condition (ambient pressure was still assumed to be the standard sea level value).
- (ii) Standard Day Take-off (ISA SLS): Standard ISA sea level static conditions (288.16 K ambient temperature, 1.01325 Bar ambient pressure, Mach 0.0). Equivalent to take-off when ambient temperature is 15°C.
- (iii) Cold Day Take-off (CDTO): Sea level static conditions, but with a lower ambient temperature (ISA -15°C).

The engine was simulated by using a range of Fan spool speeds (N_1 from 88% to 104%). This simulates different throttle settings at take-off for each of the take-off conditions listed above. The results of these simulations were then analyzed to determine the effect of using various types of thrust measurement parameters for setting thrust at take-off.

(a) The Flat-rated Thrust Concept

It is important to realize that for almost all modern turbofans, the sea level (Take-off) thrust is held constant by a control system up to some limiting ambient temperature (ISA +15°C is typical). This control is usually accomplished by automatic control of the fuel flow into the engine, most likely through sensing the GG exhaust temperature (ITT), which in turn controls the speed at which the engine runs. This can be simulated with the present model by adjusting N_1 to hold thrust constant for a wide range of SLS ambient temperatures, but for purposes of showing the baseline engine performance this will be omitted.

(b) Delivered Thrust versus Various Thrust Setting Parameters

Figure 6.19 shows how net thrust varies with fan speed for a range of SLS conditions. Note that the HDTO thrust at a fan speed $N_1=100\%$ was used as the flat rated thrust. From this graph, the speed setting needed to obtain 100% thrust can be determined for each set of take-off conditions. It is clear that ambient conditions have an effect on the thrust delivered for a given N_1 setting. On a cold day $N_1=95\%$ is required, whereas on a standard (ISA) day $N_1=97.5\%$ is needed to produce the flat-rated thrust. A 100% N_1 setting will yield 100% thrust in the case of hot day take offs. One may conclude that increased ambient temperature has a deleterious effect on delivered thrust for a given N_1 setting, requiring higher LP spool speeds to compensate for the decreased thrust performance. Also, the variation of thrust with N_1 is approximately linear (for the range 90% to 100%) for the three take-off conditions, with a 5% increase in N_1 providing an additional 14% delivered thrust.

Figure 6.20 shows what happens when fuel flow (FF) is used as the thrust setting parameter. In general, the same trend as observed in Figure 6.19 may be seen. Increases in ambient temperature mean that more fuel flow is needed to give the same thrust. For 100% of flat-rated thrust a FF setting of 4520 kg/hr is needed for HDTO. For standard day take-offs, a FF setting of 4360 kg/hr is required while 4210 kg/hr is required on cold days. Once again, the variation of thrust with FF appears to be linear with the same trend for all three SLS conditions. From Figure 6.20, an additional 1000 kg/hr (22% of the 100% HDTO fuel flow) will give 17.5% more take-off thrust.

It is interesting to compare Figures 6.19 and 6.20 with Figures 6.21 and 6.22, which show the variation of Net Thrust when EPR and IEPR are used as the thrust setting parameters. Figure 6.21 shows that the net thrust variation for all three conditions falls on a single curve when EPR is used. Thus, an EPR of approximately 1.44 should give 100% of the flat-rated thrust for the range of ambient temperature conditions studied (ISA -15°C to ISA +15°C). When IEPR is used as the thrust setting parameter, a similar phenomenon is observed. In this case, an IEPR of 1.56 will provide 100% flat-rated take-off thrust for the range of SLS conditions studied. EPR and IEPR hence appear to have the unique property of being self-correcting to changes in ambient conditions, unlike FF.

and N_1 . The variation of thrust with IEPR appears linear, while the variation with EPR can be approximated fairly accurately by a straight line over the range 1.35 to 1.55. A 0.1 variation in EPR (6.8% change relative to the HDTO 100% thrust setting) will deliver 13% more thrust. A similar variation of IEPR (6.8% of the HDTO 100% setting) will result in 17.5% more thrust. These properties of EPR and IEPR may explain why they are still popular choices for thrust setting parameters with some engine manufacturers, since then take-off thrust could be expressed by a simple curve-fit equation allowing direct calibration between EPR/IEPR and delivered thrust. Whether this is valid when an engine deteriorates is another matter.

Table 6.4 below summarizes the results of simulating take-off performance, and the relative performance of the thrust measurement parameters when setting take-off thrust for the most critical case (HDTO).

Table 6.4 Summary of Take-off Thrust Setting Parameters

Thrust Setting Parameter	100% HDTO Thrust Setting	1% Change in Parameter at Cruise	Change in Thrust (%)
N_1 (fan speed)	100%	1%	2.8%
FF (kg/hr)	4520	45.2	0.78%
EPR	1.44	0.0144	1.91%
IEPR	1.56	0.0156	2.57%

As with the cruise performance, N_1 again appears to be the most sensitive parameter while FF is the least. Note, however, that the relative sensitivity of EPR and IEPR is not much lower than for N_1 , unlike the results for cruise. The absolute changes in EPR or IEPR are still quite low, but in theory will not be as sensitive to ambient temperature conditions as N_1 and FF. This is quite different from the situation predicted for setting cruise thrust by these four parameters.

(c) **Variation of Cycle Temperatures at Take-off (Temperature Limiting)**

The concept of flat-rating take-off thrust is closely connected with the variation of cycle temperatures at take-off. As was expected, the highest cycle temperatures occur during hot day take-offs, and thus represent the most critical conditions. The variation of the hot-end temperatures (TIT, ITT, and EGT) with the two most commonly used thrust setting parameters (EPR and N_1) shows similar trends. These may be seen in Figures 6.23 and 6.25 for the case of using N_1 and EPR respectively. Figure 6.24 shows the same information when plotted against the gas generator speed (N_1).

The most critical cycle temperature is the Turbine Inlet Temperature (TIT) which must be limited so that the thermal limit of the HP Turbine rotor material is not exceeded. In practise, TIT is difficult to measure so the gas generator exhaust gas temperature (called ITT in this thesis) is usually used instead. The results shown in Figures 6.23 through 6.25 indicate that there is in general a good correlation in the variation of ITT and TIT. In practical terms, an ITT limit will be specified which is deemed the best compromise between delivered performance (thrust) and hot end overhaul life which deteriorates with increasing cycle temperatures. This limit is likely to correspond to the 100% thrust level for the flat-rate temperature, typically ISA +15°C, which is the HDTO condition used in this study. (Philpot, 1992) From the graph, this ITT limit is approximately 890°C (1163 K). The corresponding value for TIT is predicted to be 1635 K, which is reasonable when compared with actual high bypass turbofans designs such as the CFM56-5B engines (AN, CFM56 Family of Engines, 1990).

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PM-1 3 1/2" x 4" PHOTOGRAPHIC MICROCOPY TARGET
NBS 1010a ANSI/ISO #2 EQUIVALENT

Figure 6.19 Take-off Thrust Variation vs. N_1

Figure 6.20 Take-off Thrust Variation vs. Fuel Flow

Figure 6.21 Take-off Thrust with EPR as Thrust Setting

Figure 6.22 Take-off Thrust with IEPR as Thrust Setting

Figure 6.23 Variation of HDT0 Cycle Temperatures vs. N_1

Figure 6.24 Variation of HDT0 Cycle Temperatures vs. N_2

Figure 6.25 Variation of HDTO Cycle Temperatures vs. EPR

6.4 Validation of Turbofan Simulator

6.4.1 Simulation of the JT9D-7A

The ability of the simulator to accurately predict both overall performance and the various indirect thrust measurement systems such as EPR and IEPR has to be demonstrated, and the simplest way is to attempt to simulate the performance of an actual engine. Unfortunately, information on typical values for EPR, IEPR, or spool speeds are rare for a specific engine. However, some data is available for the JT9D, from data collected from actual B747 flight decks (Saravanamuttoo, 1977-1985). In most cases this data is for stabilized cruise, and there is some scatter in the EPR and EGT values due to different ambient conditions. Fairly good information is available for the cycle conditions and overall design of the JT9D-7A, the engine most common for the B747 flight data that is available (Borger, 1981). Table 6.5 summarizes what is known about the JT9D-7A.

Table 6.5 JT9D-7A Cycle and Operation Data

Engine Cycle	ISA Take-off	Climb (TOC)	Cruise ⁵
BPR	5.1		
FPR	1.52		
OPR	22.5		
TIT	1469 K		1361 K
Mass Flow	699 kg/s		
Performance and Indicators			
Max Thrust (lbs)	205.5 kN (46150)		44 kN (9890)
SFC (kg/N*hr)	0.040		0.0708
EPR (flight data) ⁶	1.46 (max)	1.45 to 1.55	1.4 (avg)
N ₁	100% (ISA SLS)	95% to 98%	90% (avg)

⁵. Mach 0.85 at 35000 ft.

⁶. The Cruise settings shown are the average values for Mach 0.85, 35000 ft.

Blank entries in Table 6.5 indicate that no definite information is known for that parameter. Unfortunately, this lack of information includes most of the TOC and cruise cycle parameters which are necessary to define the design point for the JT9D.

The simulation program was used to try and simulate JT9D-7A performance as closely as possible to the values given in Table 6.5. Attempts at using the ISA SLS conditions as the design point were unsatisfactory, since the off-design performance at the TOC and cruise conditions were under-estimated by a large margin. Using the cruise condition as the design point was hampered by the lack of information on other cycle conditions such as FPR, OPR and BPR. When reasonable values were used, the take-off turbine inlet temperature was greatly over-estimated. Therefore, based on similar findings for the Advanced CYCLE/CFM56 design, the top of climb (TOC) was used as the design point. Successive iterations were necessary, but eventually a design was found that adequately simulated the JT9D at other known operating points.

It was noticed that the effect of using different "design" values for the mechanical speeds of the LP and HP spools was quite important, especially for accurate simulation of the TIT at take-off. Also, extensive iterations were needed to find the right combination of TOC cycle conditions that gave the known conditions at SLS, while also providing the same thrust for the right air mass flow. When 95% was used as the design speeds for both spools at TOC, it was noted that a LP Speed of 98% at SLS conditions gave the best take-off match, which suggests a reduced power take-off for standard take-off conditions. Of course, another factor may be the influence of the LM-2500 HPC Map that was used as the basis for the simulator, most particularly the shapes of the speed lines (fairly steep near the 100% non-dimensional speed line).

Table 6.6 summarizes the simulation results, and where possible the errors present in the simulations. It is evident that the simulation results are quite good apart from the slight under-prediction of the cruise thrust, EPR and TIT. These predictions may be improved somewhat if a slightly faster fan speed setting was used as the maximum cruise condition, or if a different set of TOC spool speeds was defined.

Table 6.6 Simulation of the JT9D-7A

Top-of-Climb (Mach 0.85, 35000 ft) Engine Design Details			
Intake Efficiency	95%	Nozzle Efficiencies	99%
Spool/Bearings Efficiencies	99%	Combustion Efficiency	99%
Polytropic Efficiencies	Fan/LPC: 87% HPC: 89% Turbines: 88%		
Cooling Bleed	10% Compressor Air	Combustion Pressure Drop	4.4% CDP
Design Point Engine Cycle	FPR = 1.58 BPR = 5.3 OPR = 25.0 TIT = 1380 K Mass flow= 300 kg/s		
ISA Take-off Engine Cycle and Performance			
	Predictions	Known Values	% Error
BPR	5.27	5.1	+3.3
FPR	1.52	1.52	+0.0
OPR	22.6	22.5	+0.4
TIT	1507	1469	+2.5
Mass flow (kg/s)	691.6	699	-1.1
Thrust (kN)	203.9	205.5	-0.8
EPR	1.475	1.46	+1.0
Cruise Cycle and Performance			
BPR	5.5	n/a	n/a
FPR	1.52	n/a	n/a
OPR	22.3	n/a	n/a
TIT	1325	1361.1	-2.7
Mass flow (kg/s)	285.1	n/a	n/a
Thrust (kN)	38.1	44	-13.4
EPR	1.388	1.4 (avg)	-0.85

The variation of cruise EPR and N_2 with fan speed (N_1) was also simulated since actual flight data was available for these, allowing the accuracy of the simulator to be checked. The variation of N_2 with cruise N_1 setting (for approximately the same cruise condition as used for the simulations presented in Table 6.5) can be seen in Figure 6.26. For interest, similar information has been plotted for the CF6-80 engine as installed on the Boeing 767 airliner. The turbofan simulation program was then used to simulate cruise thrust settings of $N_1=88\%$ to $N_1=95\%$ for constant altitude and Mach number. As can be seen from Figure 6.26, the correlation between the predicted values of N_2 and actual engine data is quite good, with a maximum error of about 1% N_2 .

Similar results for EPR were obtained, and these may be seen along with actual JT9D-7A flight data in Figure 6.27. Where possible, EPRs for other types of aircraft/engine combinations have been shown in addition to the B747 flight deck information. The accuracy of the JT9D simulation is evident with a maximum error in EPR of about 0.05 (which is only 3.2% of the TOC EPR used as the design value). The take-off values for the JT9D and JT3D have been shown for comparison. It is interesting to note that in the much earlier JT3D (and JT8D) turbofans, which were basically just modified turbojets, EPRs approaching (or exceeding) 2.0 were normal. Since the advent of higher bypass P&W engines, EPRs of about 1.5 at take-off have become more common. Unfortunately, comparison with other modern (non-P&W) high BPR engines is not possible since it appears that only Pratt & Whitney high BPR designs use EPR. However, can be expected that those engines with similar engine cycles would have approximately the same EPR trends. The simulated take-off EPR for the JT9D-7A is quite close to actual maximum take-off EPR, with an error of about 1%.

Figure 6.26 Variation of Spool Speeds at Cruise (JT9D and CF6-80)

Figure 6.27 Variation of Cruise EPR (IEPR) for Several Engines

6.4.2 Simulation of the JT15D-4

Detailed information on the climb performance of modern high bypass turbofans is rare. Fortunately, information was available for the JT15D-4 engine which is of moderate BPR. Despite the fact that the JT15D-4 has a centrifugal HPC compressor (with some axial stages upstream) with a much lower PR than the LM-2500 HPC, the turbofan simulator was able to predict fairly accurate take-off and climb performance for this engine. The simulations were performed using state-of-the-art assumptions for unknown design point parameters for the engine and the assumption that the JT15D-4 has the TOC condition as its aerodynamic design point. Part of this success is most likely due to the fact that the JT15D-4 fan was used as the base for the generalized fan map used by TURBOFN4. The design point parameters which provided the best match with published performance and design information to simulate the JT15D-4 are as listed in Table 6.7.

Table 6.7 TOC Design Point Data used to Simulate the JT15D-4

Parameter	TOC Design Value (Mach 0.8 @35000 ft)
BPR	2.7
FPR	1.75
OPR	11.5
TIT	1150 K
Mass Flow	15 kg/s
Polytropic Efficiencies	Fan=88% HPC=81% Turbines=87.5%
Mechanical Details and Efficiencies	Intake=95% Nozzles=99% Spools=99% Combustion=95% Δp_b = 9% Cooling=nil
Spool Speeds (TOC)	N_1 =95%, N_2 =96%

Take-off thrust performance was predicted by carefully adjusting the N_1 setting over the range of take-off ambient conditions considered. Climb performance was

similarly predicted by varying Mach number at each flight altitude considered and adjusting N_1 as required. The best climb thrust and FF correlation occurred when N_1 was increased gradually with Mach number and altitude. This would appear to indicate that the JT15D-4 is operated such that the fan speed is allowed to change during climb. Accurate FF simulations were obtained only when a reasonable TOC turbine inlet temperature was used, along with a reduced combustion efficiency and with insignificant HPT cooling bleed. The accuracy of the simulator in predicting known values for the take-off cycle conditions was also checked, as detailed in Table 6.8. It is clear that the simulated JT15D-4 is quite close to actual engine data, with the worst error being a 7.2% over-estimate of take-off thrust.

The performance predictions for the JT15D-4, compared with Pratt & Whitney data sheets, may be seen in Figures 6.28 through 6.30. It is evident from these graphs that the simulations are quite accurate, especially when one considers the number of assumptions that were required to establish a reasonable TOC design cycle.

Table 6.8 Predicted JT15D-4 Take-off Performance and Engine Cycle⁷

	Predictions	Known Values ⁸	% Error
BPR	2.70	2.70	+0.0
FPR	1.75	n/a	n/a
OPR	10.4	10.1	+3.0
TIT	1287.4	1288	< 0.1
Mass flow (kg/s)	36.5	35.3	+3.4
Thrust (kN)	11.9	11.1	+7.2
SFC (kg/kN*hr)	0.0564	0.0573	-1.6

⁷. Assuming a standard day take-off with $N_1=97\%$. Similar performance is simulated for HDTO (ISA +15°C) if 100% N_1 is used, but TIT is then increased to 1348 K.

⁸. From JT15D-4 engine data sheet (AN, Pratt & Whitney Canada Inc.).

Figure 6.28 JT15D-4 Climb Thrust

Figure 6.29 JT15D-4 Climb Fuel Flow

Figure 6.30 JT15D-4 Take-off Thrust

Chapter 7 Deteriorated Engine Performance Simulation

7.1 Fault Simulation Methods

The purpose of this study was to investigate the effect that various kinds of turbofan engine faults had on the delivered performance and also the influence that these faults had on the various indirect thrust indicators commonly used in modern turbofans. The goal was to determine the relative effectiveness or accuracy of one indirect measurement method over the others. Before this could be accomplished it was first necessary to decide on the types of engine deterioration to be considered, and the method to be used to simulate them.

7.1.1 Types of Engine Faults Considered

Given the wide variety of engine faults possible, it was necessary to limit the scope somewhat to include only those faults considered to be most likely to occur in practice. Also, since the principal aim was to determine the effects of faults on several indirect thrust indicators using a thermodynamic turbofan simulation program, these faults had to be specified in a manner which the computer program could use. A list of the types of faults that were considered in this study is shown in Table 7.1.

Table 7.1 Probable Turbofan Faults

Types of Faults	Probable Cause(s)	Simulation Method
Fan Deterioration	Foreign Object Damage	η_∞ Drop
LPC Deterioration	Foreign Object Damage (FOD)	η_∞ Drop
HPC Deterioration	Tip Rubs, FOD, Fouling	η_∞ and/or Q Drops
HPT Deterioration	Overheat/Erosion	η_∞ Drop
LPT Deterioration	Erosion	η_∞ Drop

Other kinds of engine damage and deterioration are possible, including various kinds of mechanical damage such as worn bearings, broken seals, nozzle blockages or erosion, but the deterioration of aerothermal components is seen to have a much greater impact on overall engine performance and efficiency. (Collins and Hill, 1991) Another serious fault in modern turbofans is a fault in the control system(s), especially in engines which employ variable geometry technology such as variable position stators or nozzles. Variable geometry can not be simulated by the present turbofan simulation software since this would require very specific knowledge for a given engine to be simulated properly. Since overall trends are the goal in this study, the simulation of variable geometry engines or their control systems were not considered.

7.1.2 Simulation Methods Applied

It is believed that the engine faults listed in Table 7.1 may be simulated with a fair degree of accuracy by imposing decrements to the design point operating parameters, specifically the efficiency, non-dimensional mass flow or pressure ratios. The precise effect of various kinds of gas turbine damage or deterioration is not known definitely, and is the current subject of ongoing research by other investigators. However, previous studies into various kinds of gas turbine faults have shown that the faults listed in Table 7.1 may be simulated quite adequately by altering the polytropic efficiency only, while keeping the other operating parameters unchanged. (Dupuis, Saravanamuttu, and Rudnitski, 1986) In the case of the compressor components (Fan, LPC or HPC) this has the effect of causing a shift in the component characteristic which has a direct impact on the twin spool match.

Other effects such as an increase or decrease in the design PR or non-dimensional flow rate (Q) were not considered in this study, except in the case when HPC fouling was simulated. The details concerning each type of fault simulated will now be described.

(a) Fan and Booster Deterioration

Foreign Object Damage (FOD) is of primary concern in modern high bypass turbofans, causing (in general) a decrease in the performance of the LP Compression system, and can lead to an In Flight Shutdown (IFSD) of the engine in the event of major blade damage (such as a severe bird strike). This decrease in performance is due to damage to the fan or booster blades in the form of splitting, cracking, dents or actual blade sections missing (in the case of severe damage). FOD is less likely to occur in the LPC, since ingested foreign material tends to be centrifuged out the bypass duct. However, events of this type of damage (and general erosion damage) have been recorded on recent high bypass designs. (Hovey, 1990) Fan or booster deterioration was simulated by a 0-5% drop in the polytropic efficiency in either the Fan or LPC, while keeping other parameters unchanged. It was noted during simulations that an excessive drop in Fan efficiency could result in an unsolvable twin-spool match. For the turbofan cycle under consideration, this was found to be a decrease of more than 3% for the same nozzle sizes, which forced the HP Spool to run faster than the highest speed available on the HPC characteristic. A standard drop of 2% in polytropic efficiency was used for comparisons with other faults.

(b) HPC Deterioration and Fouling

Two kinds of deterioration were considered. The first kind of HPC deterioration was damage to the rotor or stator blades either from ingestion of foreign objects, erosion of blade surfaces from finer materials, or tip clearance increases from blade tip rubs. Major damage to the HPC due to ingestion is fairly rare in high bypass engines due to the tendency of the LP Compression System (Fan) to centrifuge large objects to the outer casing and out the bypass duct. However, wear of rotor blade seals (from rubs) or airfoil erosion will cause slight changes in tip clearances or blade geometries and hence will alter the performance of the HPC. This kind of damage was simulated by a drop in polytropic efficiency, and the design point isentropic efficiency was then re-calculated based on the design value of PR_{hpc} and the faulted polytropic efficiency. It was noted that

excessive drops in efficiency (5% or greater) again resulted in unsolvable twin-spool matches. A standard drop of 2% was found to give the most reasonable results and allowed comparisons with other engine faults.

Another kind of deterioration, HPC fouling, was also simulated since this phenomenon is a common problem with modern gas turbine units and has the potential of causing major changes in performance. Following the suggestion of previous investigators, HPC fouling was simulated by imposing a decrease in polytropic efficiency coupled with a decrease in the design point non-dimensional flow rate Q_3 . (Aker and Saravanamuttoo, 1988) Several combinations of efficiency and flow drops were used and were shown to have a marked effect on the HP versus LP spool speed matches and hence overall performance. A decrease in polytropic efficiency of 2% and a decrease in Q_3 of 5% were used for comparison with simple efficiency drops, since from what limited information is available, percent changes in Q tend to be 2 to 4 times more than the drop in efficiency. Fortunately, this level of HPC fouling is rarely seen in modern turbofans, quite unlike advanced ground-based gas turbine units. (Aker and Saravanamuttoo, 1988)

(c) HPT and LPT Deterioration

Deterioration in either turbines was simulated by imposing decreases in the design point polytropic efficiencies only. HPT deterioration is likely to result due to the effect of thermal cycles. This in general causes erosion or wear of the HP Turbine blades or annulus seals. Another source of deterioration is corrosion which is accelerated in the high thermal stress environment. Damage or deterioration of the LPT is less likely since the rotation speeds and temperatures are lower. Of course, severe wear can occur in both turbines if abrasive foreign material (such as sand) is ingested by the engine and passes through the hot end components. Severe damage will most likely cause an engine failure but less severe damage/deterioration will likely cause a drop in performance; this deterioration was simulated by using a polytropic efficiency drop of 2% to allow comparison with other component faults.

7.1.3 Implementation of Fault Simulation in TURBOFN4

The organisation of TURBOFN4 was discussed in Chapter 5, including the menu structure used for fault modelling. A small program called FAULTSET is used to define the current engine fault to be used. This set of data defines the changes to be imposed on design point values of the polytropic efficiency or non-dimensional flow rate for each of the components, in terms of a percentage increase or decrease. This data is automatically saved upon exiting FAULTSET in a file called FAULT.DAT. While within FAULTSET, the faulted condition definition may be saved to a user-named file or previously saved sets of fault data may be recalled from other user-defined files.

From TURBOFN4, the simulation of faults is initiated by selecting Option (3) which calls the subroutine FAULTED. This routine initializes the simulator for simulation of a faulted engine, before passing control to OFFDES. FAULTED uses the set of defined component efficiency or Q changes contained in FAULT.DAT to change the appropriate design point parameters while the unfaulted parameters are temporarily stored during the subsequent off-design calculations for later recovery. Then, the design point isentropic efficiencies of the components are recalculated before calling OFFDES.

OFFDES conducts a full off-design matching procedure for the faulted engine, using the set of flight conditions defined in the file FLIGHT.DAT. Output from OFFDES may be stored in three ways (screen output only, printer, or to a data file) but the engine name normally contained in the header information is replaced by the name of the engine fault being simulated.

Following the completion of OFFDES, control is returned to FAULTED to allow the nominal (unfaulted) engine to be restored. This is accomplished easily since the unfaulted design-point parameters of the five components were stored before conducting the fault modelling. Once the nominal engine is restored, control is returned to the Main Menu of TURBOFN4.

It is important to note that OFFDES calculates a new HPC operating line based on the faulted component parameters, at the start of the off-design matching procedure. However, on-design calculations are not conducted using the modified components, so all design point values not directly affected by the fault definition in FAULT.DAT will be unchanged. This is important, since the engine must still satisfy the flow conditions for the same nozzles which were calculated for the nominal (unfaulted) engine. Excessive changes in the component design point operation could result in unsolvable twin-spool matches due to this fact and the limited range of speeds on the compressor characteristics.

7.2 Take-off Performance with Faults

The faults discussed in the previous section were each imposed on the advanced cycle design used in Chapter 6. TURBOFN4 was then used to simulate the variation of take-off thrust with different throttle settings, for cold day (ISA -15°C), standard day (ISA) and hot day (ISA +15°C) take-offs. The performance of the engine with each of the single faults was compared with the unfaulted performance discussed in Chapter 6.

7.2.1 Take-off Thrust for Different Ambient Conditions

Figures 7.1 and 7.2 show what happens when N_1 or EPR are used to set thrust at take-off when one of the worst faults (Fan deterioration) is present. In Figure 7.1 it is evident that at each take-off condition, the fan fault actually results in a slight (+1%) increase in thrust for the same N_1 speed setting. Similarly, EPR still appears to correct itself for the three different ambient conditions as evident in Figure 7.2, with the delivered thrust still falling on a single line. The delivered thrust for a constant EPR appears to be slightly lower in the case of the fan fault, but this drop is very slight (less than 1%).

It was noted that in most cases, a 2% reduction in polytropic efficiency in the five main aerodynamic components results in minor changes in the net delivered thrust, and in some cases even resulted in a slightly higher thrust! This seems to contradict what one would expect, but it is important to realize that in general, lower efficiency in the compressor components results in higher TIT which can lead to higher thrust. In actual engines, the presence of temperature limiting control systems will prevent higher hot-end cycle temperatures and would generally impose a limit which would result in lower take-off thrust.

Figure 7.1 Take-off Thrust with Fan Fault using N_1 as Setting

Figure 7.2 Take-off Thrust with Fan Fault using EPR as Setting

7.2.2 HDTO Thrust without Temperature Limiting

Since the concept of temperature limiting is likely to be critical at the flat-rated temperature limit (typically ISA +15°C) the effect of various faults on the hot day take-off (HDTO) thrust was examined. The effect of using N_1 , fuel flow (FF), EPR, or IEPR to set thrust when faults are present may be seen in Figures 7.3 through 7.6. For clarity only the most severe fault effects have been shown on each graph. The worst variations of HDTO thrust that were obtained using 2% efficiency faults have been summarized in Table 7.2.

Table 7.2 Setting HDTO Thrust (No Temperature Limits)

Fault Type	Maximum Change in Thrust with Constant Setting:			
	N_1	FF	EPR	IEPR
Fan η_{∞} -2%	+1%	-5%	<-1%	<+1%
Booster η_{∞} -2%	no effect	<-1%	no effect	no effect
HPC η_{∞} -2%	< +1%	-4.5%	+1%	no effect
HPC η_{∞} -2%, Q_3 -5% (fouling)	< +1%	<-1%	+1%	no effect
HPT η_{∞} -2%	no effect	-2.5%	no effect	no effect
LPT η_{∞} -2%	+2%	-4.5%	-1%	<+1%

When N_1 is used as the thrust setting parameter (Figure 7.3) it can be seen that the net thrust against N_1 falls along a line, with faults shifting this line upwards slightly (such that a slightly increased thrust results in the faulty engine when compared with the nominal engine at the same N_1 setting). The maximum increase, however is of the order of 2% for the case of the LPT fault and 1% for Fan fault. This latter effect is mainly due to increased hot end cycle conditions which may not be permissible at HDTO conditions.

Figure 7.4 shows that using FF to set take-off thrust will result in lower thrust when faults are present for the same FF setting (or alternatively, more FF for the same thrust). The worst fault appears to be when the Fan efficiency is dropped, which results in a drop in delivered take-off thrust of about 5%. LPT and HPC efficiency decreases also appear to have a fairly major impact (4-5% thrust decrease). HPT efficiency drops appear to result in 2-3% less thrust for the same FF setting, while LPC efficiency drops and HPC fouling appear to have only minor effects.

Figures 7.5 and 7.6 show what happens when either EPR or IEPR are used to set HDTO thrust. It is evident from Figure 7.6 that IEPR appears to be a slightly more accurate indicator. With faults, the thrust curves appear to fall along a single curve, with a maximum increase of less than 1% thrust in the case of Fan or LPT faults and negligible changes from the other faults. Figure 7.5 shows that faults make EPR a slightly less precise thrust setting parameter, with HPC fouling and HPC efficiency drops resulting in maximum increases of 1% in delivered thrust versus the unfaulted engine at the same EPR setting, and LPT faults resulting in a 1% decrease in thrust. Fan and HPT faults appear to have only minor effects on the delivered thrust when EPR or IEPR are used.

Figure 7.3 HDTO Thrust Variation with N_1 for Different Faults

Figure 7.4 HDTO Thrust Variation with Fuel Flow for Different Faults

Figure 7.5 HDTO Thrust Variation with EPR for Different Faults

Figure 7.6 HDTO Thrust Variation with IEPR for Different Faults

7.2.3 HDTO Thrust when Temperature Limiting is Applied

The engine faults modelled cause an increase in the hot end cycle temperatures, particularly the TIT and ITT. This was the reason that net delivered thrust actually increased with some kinds of faults. However, modern turbofans control the hot end temperatures in such a manner that they are limited to some level, usually the flat-rated temperature (ISA +15°C was used in this study). As mentioned in Chapter 6, TIT is rarely monitored in high bypass turbofans due to measurement difficulties. A more common approach is to monitor the gas generator exhaust gas temperature, or ITT, which provides a good indirect indicator of the TIT. This ITT is limited to some hot rating, and for purposes of this study the ITT which gives 100% thrust at HDTO conditions has been used as the limit. This was found to be approximately 890°C (1163 K) which correlates fairly well with quoted ITT red-lines for the CFM56 family of engines (AN, SNECMA CFM56 Family of Engines Brochure, 1990).

The mean ITT increases observed when faults were imposed have been summarized in Table 7.3 below. It can be seen that the effects are virtually identical for either constant N_1 or EPR.

Table 7.3 Mean ITT Increases with Faults at HDTO Conditions

Fault	Increase (°C) over Nominal HDTO ITT at Constant	
	N_1	EPR
Fan η_∞ -2%	+25	+22.5
Booster η_∞ -2%	+5	+5
HPC η_∞ -2%	+20	+25
HPC η_∞ -2%, Q3 -5% (fouling)	+30	+32.5
HPT η_∞ -2%	+15	+18.8
LPT η_∞ -2%	+12	+15

Figures 7.7 and 7.8 show the effect that faults have on the ITT at HDTO conditions, when using N_1 or EPR as the thrust setting parameter. HPC faults appear to cause the greatest increase, with HPC Fouling having the greater effect. This result is reasonable when one considers that the majority of the OPR is due to the HPC, and that high efficiency is most critical in this component in high BPR/OPR engines to limit the HPC exit temperature. Fan faults also appear to cause a rather large increase in ITT, while turbine faults have about half the effect. Booster efficiency drops seem to have very little effect on ITT, due to the small contribution they make to OPR.

The horizontal shaded line in Figures 7.7 and 7.8 represent the hot day take-off ITT limit (corresponding to the 100% thrust condition for the unfaulted engine) which is approximately 890°C (1163 K). If this limit is imposed on the engine, it is evident that engine faults will actually cause the control system to limit the take-off thrust to a lower value than would otherwise result due to increased ITT (and hence TIT). This modifies the finding of the previous section, where some faults were found to actually cause an increase in take-off thrust. The maximum thrust at HDTO that would be available in the case of a faulty engine appears to be dependent on the choice of the thrust setting parameter. The greater the increase in ITT due to faults, the lower will be the maximum thrust setting (N_1 or EPR) and therefore the lower the thrust. The results shown in Figures 7.7 and 7.8 may be used in conjunction with the previous results (Figure 7.3 for faulted HDTO Thrust using N_1 , Figure 7.5 for faulted HDTO Thrust using EPR) to determine the actual maximum HDTO thrust that would be permitted by ITT limiting control. These are summarized in Table 7.4.

Table 7.4 Maximum HDTO Thrust with ITT Temperature Limiting

Fault Type	Maximum Thrusts Permitted (% Flat-rated Thrust)			
	Max N_1 (%)	Max Thrust	Max EPR	Max Thrust
No Fault	100	100	1.44	100
Fan η_∞ -2%	96	89.8	1.352	90.3
Booster η_∞ -2%	98	97.5	1.42	97.8
HPC η_∞ -2%	96	89.8	1.345	89
HPC η_∞ -2%, Q_3 -5% (fouling)	94.3	85.8	1.32	86
HPT η_∞ -2%	96.7	91.5	1.36	92.5
LPT η_∞ -2%	96.7	91.5	1.375	92.6

Both indicators appear to give approximately the same maximum HDTO thrust when faults are present. The worse fault conditions appear to be those affecting the HPC, with HPC fouling apparently causing the worse decrement in take-off thrust. It should be appreciated, however, that such a high degree of HPC fouling is unlikely to occur from external contaminants due to the high cruise altitudes where turbofans operate, with the notable exception of flying through volcanic plumes when fouling of most engine components is much more serious! (Grove, 1992) Fan faults appear to cause a fairly major decrease in HDTO thrust when ITT limiting is applied. This is of greater concern since Fan deterioration from FOD is quite common in high bypass turbofans. (Hovey, 1990) One may also conclude that turbine or booster faults will not have as major an impact on HDTO thrust.

Figure 7.7 Increases in HDTO ITT with Faults when N₁ is Thrust Setting

Figure 7.8 Increases in HDTO ITT with Faults when EPR is Thrust Setting

7.3 Cruise Performance with Faults

Of secondary importance is the effect that faults have on the accuracy of setting cruise thrust using one of the four thrust setting parameters being examined. The matter of sufficient cruise thrust is not necessarily one of safety, but rather economics. Of particular concern to commercial airline operators is fuel burn, which should be as low as possible for a given cruise flight condition. This requires obtaining the optimum specific fuel consumption (SFC) which means getting the most thrust for the least fuel expended. Inefficient operation of turbofans (higher SFC) leads to increased fuel burn (and hence operating costs) and can cause a reduction in cruise range. Minimizing SFC is of such concern that it has led one airline to switch engine types when guaranteed SFC estimates proved optimistic. (Bailey, 1991)

This leads one to the issue of how to set cruise thrust, since once again an indirect measurement will be required. A brief study was conducted to examine the accuracy of N_1 , FF, EPR and IEPR as cruise thrust setting parameters, when engine faults are present. The faults simulated were those listed in Table 7.1, and the methods used to simulate these were the same as done for the take-off thrust analysis. The performance at a typical cruise condition (Mach 0.8 @35000 ft) was then simulated for a range of thrust settings, by varying N_1 from 85 to 98%. This was done with each fault condition, and the results were then compared with the nominal cruise performance generated in the previous chapter.

The first thing evident from the simulations is that the faults imposed result in minor changes in cruise thrust, similar to the situation at take-off. Figure 7.9 shows the effect of using N_1 as the thrust setting parameter. The thrust at cruise for the faulty engine falls pretty well on the same line as for the unfaulted engine. Careful examination of the plot does show that, like HDTO thrust, the most severe faults cause a slight increase in cruise thrust, amounting to approximately +2% more thrust (versus a nominal thrust rating of 22 kN at 90% N_1) for a given N_1 setting if either a Fan or LPF fault is present. HPC and HPT appear to cause very slight increases, while the LPC fault appears to have almost no effect.

Figure 7.10 shows the situation when fuel flow (FF) is used to set cruise thrust. It appears that engine faults cause a decrease in thrust for a constant setting, or more fuel for the same thrust. This seems reasonable since polytropic efficiency decrements will have a direct impact on the overall propulsive efficiency of the turbofan. Fan faults appear to have the greatest impact, with 2% efficiency deductions causing a similar reduction in thrust for a given FF. HPC faults appear to be almost as bad, with a drop in thrust of 1.8 to 2.0%. Turbine faults appear to have a smaller effect, with the 2% reductions in efficiency causing a 1.8% reduction in thrust with the LPT fault, and 1.5% reduction with the HPT fault. Once again, LPC faults appear to have little or no effect.

FF is rarely used as a primary thrust setting parameter but is monitored to give an indication of engine health and cruise economy. If seen from the point of view of maintaining the same thrust (based on another indicator) more fuel will be required depending on the severity of the fault. This is summarized in Table 7.5 below.

Table 7.5 Effect of Faults on Cruise Fuel Flow

Fault Type	Changes in Cruise FF at Constant Thrust	
	(kg/hr)	% Increase
Fan η_{∞} -2%	+40	+3.2
Booster η_{∞} -2%	slight increase	slight increase
HPC η_{∞} -2%	+35	+2.8%
HPC Fouling	+38	+3%
HPT η_{∞} -2%	+20	+1.6%
LPT η_{∞} -2%	+30	+2.4%

Figures 7.11 and 7.12 show the effect of engine faults on setting cruise thrust using either EPR or IEPR respectively. Once again, IEPR appears to be the most stable indicator, with practically no deviation from the cruise thrust of the unfaulted engine. Figure 7.10 shows that the same is not true for EPR, which shows a slight deviation of thrust from the nominal curve. Fan, LPC and HPT efficiency drops appear to have almost no effect on cruise thrust versus EPR. Both HPC faults appear to cause increased cruise thrust, 1% with efficiency decrease only, 2.3% with severe fouling. The LPT efficiency fault appears to cause of 2.3% reduction in cruise thrust.

The changes in setting cruise thrust to various thrust setting parameters (when faults are present) have been summarized in Table 7.6. From these results, one may conclude that IEPR appears to be the best thrust setting parameter at cruise, while fuel flow appears to be the least appropriate parameter as far as accuracy is concerned. N_1 and EPR are two of the more popular parameters (EPR being used by most Pratt & Whitney Engines, N_1 by General Electric, SNECMA, Garrett and P&WC) despite the fact that both appear to be susceptible to inaccurate indications when various engine faults are present. The choice of one over the other does not appear to be clear, but when one considers the probability of faults actually occurring (with Fan, HPC and HPT deterioration very likely), EPR appears to be the slightly better candidate provided instrumentation accuracy can be assured. EPR, however, still suffers from the problem of not giving a "feel" for its impact on delivered thrust, while the interpretation of Fan spool speed (N_1) is intuitive to most pilots.

Table 7.6 Effect of Faults on Setting Cruise Thrust

Fault Type	Changes in Cruise Thrust at Constant Thrust Parameter ⁹			
	N_1	FF	EPR	IEPR
Fan η_∞ -2%	+2.3%	-2%	no effect	n/a
Booster η_∞ -2%	no effect	no effect	no effect	n/a
HPC η_∞ -2%	<+1%	-1.8%	+1%	n/a
HPC η_∞ -2%, Q_3 -5% (fouling)	no effect	-2%	+2.3%	n/a
HPT η_∞ -2%	no effect	-1.5%	no effect	n/a
LPT η_∞ -2%	+2.3%	-1.8%	-2.3%	n/a

⁹. At Mach 0.8 and 35000 ft cruise.

Figure 7.9 Cruise Thrust vs. N_1 for Faulty Engine

Figure 7.10 Cruise Thrust vs. Fuel Flow for Faulty Engine

Figure 7.11 Cruise Thrust vs. EPR for Faulty Engine

Figure 7.12 Cruise Thrust vs. IEPR for Faulty Engine

Chapter 8 Selection of Thrust Indicators

8.1 Observations Concerning Thrust Indicators

It is clear from the results outlined in Chapters 6 and 7 that the selection of the "best" indirect thrust indication system is not simple. However, the following observations may be made concerning the various indicators studied:

a) Fan Speed

Fan spool speed (N_1) is a popular thrust indicator because it is easy to measure directly and accurately. It also has the unique advantage of having some physical meaning to pilots, quite unlike EPR or IEPR numbers which could seem arbitrary. Most people instinctively understand the concept of 100% speeds providing 100% power. However, manual setting of take-off thrusts will require additional calibrations. This latter concern is perhaps minor as modern engines become increasingly computerized. Fan spool speed therefore appears to be the most logical choice of thrust setting parameter for current civil airline operations. N_2 should be used to monitor the engine core since engine faults can affect the cruise value of this parameter. Similarly, fuel flow should be used to monitor the cruise flight fuel economy.

b) Engine Pressure Ratio

In the absence of advanced computerized controls, EPR seems a logical choice for setting take-off thrust due to its unique property of being self-adjusting to different ambient temperature conditions. Engine faults do not appear to destroy this characteristic, and temperature limiting (if applied to the ITT) has almost the same effect on EPR thrusts as N_1 thrusts. However, since EPR involves the measurement of total pressures (and thus the requirement of accurate total pressure probes) it may be more prone to instrument error than N_1 . In fact, there has been at least one fatal accident where failure in the

intake total pressure ports (due to ice blockage) resulted in an inaccurate thrust setting during a foul weather take-off. It should be mentioned that while the EPR readings during this tragedy were erroneous, the other thrust indicators (especially N_1) were consistent with a low thrust setting. ("NTSB Report Raises 737 Crash Issues", Aviation Week and Space Technology, October 18, 1982)

c) Integrated Engine Pressure Ratio

Of the indicators that were studied, IEPR appears to be the most stable indicator (even when faults were considered) though setting cruise thrust may still be a problem if a wide range of cruise conditions are considered as the normal operating regime of the engine. It should be realised that the accurate measurement of IEPR requires extensive pressure probe instrumentation in the fan exhaust. The theoretical basis of this indicator appears to be the most logical, however it is not the most practical (or easiest) to implement.

8.2 Effect of Faults on Thrust Indication Accuracy

When faults occur in the compression systems (Fan, boosters or HPC) they tend to cause a rise in downstream temperatures. If uncontrolled, this can actually lead to higher delivered thrust due to increases in TIT, but at the penalty of increased fuel consumption and decreased engine life. This tendency will be altered in actual turbofan operations when ITT limiting is used.

a) Setting Take-off Thrust

ITT temperature limiting is likely to be critical only when setting take-off thrust. When this was applied to the faulty engine simulations, it was noted that HPC faults have the greatest effect. Fan deterioration also has a fairly major impact on maximum allowable thrust levels, while turbine deterioration has less of an effect. Such deteriorations appear to affect N_1 , EPR and IEPR settings for the same thrust level, but the difference between using the two most popular indicators (N_1 and EPR) appears to be

very minor when used with current advanced engine cycles. IEPR was the indicator least affected, which seems to suggest that it would make the best candidate for setting take-off thrust.

b) Setting Cruise Thrust

If engine faults are present when setting cruise thrust (assuming a constant cruise condition of Mach 0.8 @ 35000 ft), the maximum error is when Fan or LPT faults are present. If N_1 is used, either of these faults result in a slight increase in cruise thrust for the same N_1 setting. With a constant EPR setting, LPT faults result in reduced cruise thrust, while Fan deterioration has a minor effect. HPC faults appear to have greater impact when setting cruise thrust with EPR.

The effect of faults appears to be very minor when using IEPR to set cruise thrust. The same cannot be said for fuel flow, since in all cases, faults resulted in a decreased thrust for a given fuel flow setting. To maintain the same thrust level, assuming that some other thrust indicator is used, the result will be increased fuel flow. Fan and HPC faults appeared to have the greatest effect on FF at cruise. FF therefore seems to be the least suitable primary thrust setting indicator.

Chapter 9 Conclusions and Recommendations

9.1 Conclusions

9.1.1 Thrust and Cycle Trends with Modern Turbofans

Modern turbofans are not designed around a single design point, but rather are optimized to provide good performance at several key operating points. The primary concern at take-off is adequate thrust, while at cruise, fuel economy is more important. The Top-of-Climb condition appears to be the aerodynamic design point for the majority of modern engines, and using this as the overall "design point" provides the most accurate computer simulations of engine performance based on generalized component characteristics.

Current high bypass turbofans derive a majority of net thrust (70-80%) from the bypass nozzle. Higher BPR also means lower specific thrust resulting in the need for larger engines. This also provides improved SFC at cruise, and the trend for future high performance engines is towards lower specific thrust and increased BPR.

9.1.2 Use of Indirect Thrust Setting Parameters

Thrust from modern turbofans cannot be measured directly, so indirect thrust measurement schemes are required. However, the selection of the "best" indicator is not clear, since the results of this study has shown very little difference between the most popular methods used.

The dominance of Pratt & Whitney in the civil jet engine industry in the 1960s and 1970s has resulted in a large number of EPR-equipped engines (such as the JT8D and JT9D) in civil airliners. These engines, and more recent advanced Pratt & Whitney designs, will continue to be used well into the next century. Most new entries into the civil engine market (such as General Electric or SNECMA engines) have tended to use

N_1 (fan mechanical speed) as a primary thrust setting parameter in civil airline engines. Meanwhile, Rolls-Royce uses IEPR in most of their high bypass turbofans. EPR continues to find wide use in most military engines where the low bypass ratio afterburning cycle is very similar to the pure turbojet.

The best choice of thrust setting parameter is not clear, as there are very minor differences when used in current high bypass engines, even when engine deterioration is considered. EPR and IEPR have the unique advantage of being self-correcting to a wide range of ambient take-off temperatures. Thus, only a single EPR (or IEPR) has to be specified to give a particular take-off thrust level. EPR and IEPR are affected by changes in altitude and forward speed (i.e. Mach Number) so setting cruise thrust is not as straight-forward. EPR and IEPR are also affected by engine cycle, particularly BPR and FPR, and this may influence whether these indicators will continue to be used. This is particularly true with the advent of Ultra High Bypass (UHB) Engines, when extremely low values of EPR or IEPR make these values fairly meaningless as indicators.

N_1 appears to exhibit different behaviour at take-off and cruise operation. Different ambient take-off conditions affect the N_1 required for a given thrust. This requires the added complication of determining the required thrust setting by taking into account aircraft weight and ambient conditions. This is perhaps of less concern with recent engines as advanced thrust management systems find wider use. N_1 (and N_2) are also fairly independent of engine cycle, with design values of these speeds being the result of compromises between the rotational speeds of the aerothermal components. Setting cruise thrust appears to be less involved when using N_1 since different flight Mach numbers and altitudes appear to have little effect. N_1 also has the advantage of being intuitive to pilots, unlike the sometimes arbitrary EPR or IEPR settings.

Fuel flow is directly affected by different take-off and cruise conditions and hence is unreliable as a primary thrust setting parameter. Engine deterioration appears to have a major effect at both take-off and cruise operating conditions, generally resulting in increased fuel consumption.

9.2 Recommendations for Further Work

9.2.1 Simulation of Other Faults

The fault simulations used during this study concerned changes to the polytropic efficiencies and non-dimensional mass flow for the HPC only. This was done to limit the scope of the study to only those faults considered to be most likely to occur in high bypass engines. It would be instructive to examine the effect of several kinds of faults in combination. This could be done quite easily with the existing model, provided faults are simulated through changes in efficiency or non-dimensional mass flow.

The effect of various kinds of faults on component performance is not understood fully, though previous studies have shown that actual engine deterioration could be simulated fairly accurately using the methods shown in Chapter 7. It is likely that faults to the compressor components may affect the performance curves themselves and not just the polytropic efficiency. (Saravanamuttoo and Lakshminarasimha, 1985) If this is correct, the faulted component would be changed to the extent that the original (unfaulted) component characteristic would no longer apply. However, assuming the component still functions in a manner similar to the present generalized maps, this could be simulated through changes in all three design parameters (Q, efficiency and PR), which would cause the component map to be completely re-scaled. This kind of deterioration could be done with the current simulator, though changes to design point PR would necessitate the modification of the fault simulation sub-routines (FAULTSET and FAULTED) to include a third fault array.

9.2.2 Enhancements to the Computer Model

The current turbofan simulation package could be enhanced with the following additions. Detailed documentation of the program, including compilation information, source code listings and a variable catalogue may be found in the Users Manual that has been prepared for the program. Any changes to the program should be conducted after consulting this manual to avoid possible compiling problems.

(a) Addition of Mixed Nozzles

The addition of mixed nozzles would be of secondary interest, since they are quite rare in high bypass engines. However, older low bypass engines (and most military engines) could be simulated by the present model with modified component characteristics and alterations to the on-design and off-design calculation procedures.

Mixed nozzles may be simulated through the use of momentum, enthalpy and mass balances between the core flow and bypass flows before the mixer, and the flow after mixing. There is an additional need to determine the Mach numbers to enable the calculation of static properties for these three flows, and this is assisted by the requirement that equal static pressures must exist at the mixing plane. For on design calculations, this procedure is fairly straight-forward, but the current on-design modules (such as CYCLE) would need modification to deal with a mixed single nozzle.

The inclusion of a mixed nozzle during off-design calculations will require significant modification of the subroutine MATCH. This is necessary to allow the provision of matching the conditions in the bypass duct with those at LPT exit, particularly the mass, energy and momentum balances. This may include an additional iteration loop involving a search to find the most appropriate BPR since this parameter could no longer be determined using the methods for separate nozzle turbofans.

(b) Graphical Display of Characteristics

Limited time did not permit the inclusion of graphical outputs for the turbofan simulation software. However, Microsoft FORTRAN version 5.0 does have an extensive set of graphical commands which could be used to display the component maps in scaled form with operating points or lines indicated. Continuous displays of the fan and compressor maps during OFFDES and MATCH procedures could be used to replace the present text screens and could provide some insight into the matching procedure. The digitized nature of the component maps as they presently exist lend themselves to such graphical displays. Instructions on how to use such graphics may be found in the Microsoft FORTRAN Reference Manuals.

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Additional Readings

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