STUDIES IN GAS TURBINE PERFORMANCE AND IN COMBUSTION

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SUMMARY

This thesis reports on the writer's investigations in gas turbine and in combustion.

Referring first to the gas turbine studies, these have been predominantly on the performance aspects, particularly on transient performance of aero gas turbines. The existing prediction methods, Continuity of Mass Flow (CMF) and Intercomponent Volume (ICV) have been developed and compared. The comparison extended to a two-spool turbofan having mixed exhausts (previously only a single-spool engine had been reported on). Noticeable differences in predictions were found only in the H.P. compressor while the Inlet Guide Vanes were turning and the air flow rate in the H.P. compressor was changing rapidly. The ICV method, which is the more valid, predicted lesser movements from the steady-running line than the CMF method.

A major part of the investigation has been a sequence of studies of the effects of heat transfers within the engine components on the transient dynamic response of the engine. Non-adiabatic flows, boundary layer changes in the compressor, efficiency changes due to tip clearance changes and seal clearance changes giving altered leakages were all modelled. Significant changes in dynamic response (i.e. speed, thrust etc) could all be predicted. The known tendency to surge the compressor when reaccelerating a "hot" engine – the Bodie transient – could also be predicted. The lengthening of the response time when accelerating on a hydro-mechanical fuel controller (lengthening typically by 30 per cent) could be predicted. Low thrust (typically by 2 per cent) at the conclusion of an acceleration, could also be predicted.

There are several locations in the engine where disc cooling flows etc return to the main flows. These flows may have a significant momentum transverse to the main flow direction. These "transversion injection" flows have been studied on a cold cascade rig. It was found that vortices could be generated originating from near the separation bubble formed behind the injection slot. The averaged losses of stagnation pressure could be modelled by a simple one-dimensional theory. The vortices did not seem to damage the flow in the succeeding blade row.

Developments in fuel controller strategy have been studied, including the use of a model-based observer controller.

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The writer's experience in modelling and predicting the dynamic behaviour of engines was called on by Rolls-Royce (R-R) when he contracted to provide the transient model of the new RB 183-03 "Tay" Engine. This was to be a two-spool engine of Bypass ratio 3.0 and having the Fan and Intermediate Pressure (I.P.) compressor on the L.P. shaft. This was a novel configuration to R-R: haste was needed. A novel means was devised of representing the inner and outer portions of the fan. The transient predictions for the engine design were provided. The danger of surging the I.P. compressor during a deceleration was predicted. This danger could be alleviated (a) by installing an air-bleed from I.P. compressor delivery into the bypass duct, or (b) by making the turning rate of the H.P. compressor Inlet Guide Vanes less rapid with non-dimensional shaft speed change. The writer was told by R-R that the latter choice was not feasible so the air bleed after the I.P. compressor was selected for the prototype engines. However Choice (b) was selected for the production engines.

The combustion studies have covered both pre-mixed and non-premixed gas-air systems, particularly where the air was given swirl. Flow patterns have been modelled by Swirl Numbers. For the non-premixed investigations, three different fuel injection schemes have been tested. These schemes are: (a) central axial, (b) radial outwards from the central axis and (c) peripheral from an injection slot/holes surrounding the main air jet entry. Peripheral injection offers advantages. Component velocity profiles have been measured, as have pressures and temperatures. Concentrations of pollutants have been measured. Commercial, and locally developed, prediction codes have been tested against the experimental data, with fair success.

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The author wishes to acknowledge with grateful thanks the encouragement of his late mother. He also wishes to acknowledge the support and forbearance of his wife, Mary, and family over many years.

The author also wishes to acknowledge the collaborations with many people. To name some is perhaps invidious, but there are several who must be thanked – Salah Beltagui, Andrew Grant, Asaad Kenbar, Pericles Pilidis, Oliver Qi and Frank Reford.

DECLARATION

The author declares that his contributions to the Published work given in this thesis are as indicated in "Publications by Author" on pages 41 - 48 of Volume I.

The text of the Thesis itself has been written entirely by the Author.

CONTENTS

CHAPTERS

CHAPTER 1

INTRODUCTION

The subject matter of this Thesis falls into the two major areas in which the author has worked – these being "Gas Turbine Performance" and "Combustion". Some of the work has been openly published, while other sections of work (for Rolls-Royce Ltd) have been given only restricted circulation. The work reported in this Thesis includes both the openly published and the restricted papers. Some of the investigations, and subsequent papers, have been the work of teams in which the author was a co-worker. The contribution of the author to each of the investigations is shown in the "List of Publications by Author" which follows Chapter 11 and "Reference Papers by Other Workers" in Vol. I of this Thesis.

In the first area of study – gas turbine performance – the investigations have concentrated on the prediction of the performance in transients of aero gas turbines. When the author started his study, in 1968, the principal procedures which had already been developed were the continuity of mass flow method and the inter-component volume method. Up to this stage, workers had generally assumed that the flows in compressors and turbines were effectively adiabatic, that the component efficiencies and capacities were the same as those measured under steady-running conditions and seal clearance flows were at design proportions etc. This set of assumptions can be referred to as "adiabatic" assumptions. The present author has made some contribution to the development of the above "adiabatic" continuity of mass flow and inter-component volume methods, and he has introduced a high-frequency gas dynamic procedure into the latter. These studies are reported in Chapter 2.

The "adiabatic" predictions procedures referred to above were unable to explain a number of anomalies which were being observed in "real" engine behaviour. Typical anomalies were that an engine at the conclusion of an acceleration could be developing a thrust significantly less than the eventual stabilised value, another being that the time taken to carry out an acceleration or deceleration was very frequently as much as 30 per cent longer than predicted by the adiabatic procedures. Yet another anomaly was that an engine could be more prone to compressor surge when re-accelerating from a "hot" condition than when accelerating from "cold". The present author has proposed that these anomalies can be explained by "thermal effects". His investigations into these are reported in Chapter 3 where the "thermal effects" are quantified and models to represent them are developed.

One of the thermal effects considered to be worthy of examination is the change of clearance of seals. Some of these seals control flows which are, for example, used for cooling turbine components such as discs. These flows then return into the main flow through the turbine. Usually these returning flows will have a transverse component of velocity when they return into the main flow. A study has been made of these transversely injected flows, in relation to the performance of turbines, where this effect probably most frequently occurs. This study is reported in Chapter 4.

The investigations reported in Chapter 2, 3 and 4 have been integrated in the form of Transient Performance Prediction Codes. These have been applied to a two-spool aero turbofan engine currently in production, and predictions are presented and discussed in Chapter 5.

The engine fuel control systems used in the studies reported in Chapters 2 to 5 had been of the contemporary hydro-mechanical type. The advent of electronic engine control systems allows for more sophisticated control strategies. The author has collaborated in investigations into developments in engine control systems. This work is reported in Chapter 6.

A major component of this Thesis is the work which the author carried out for Rolls-Royce Limited during the period December 1982 to December 1985 when he was engaged by that Company to provide the transient modelling of two of their engines – the existing RB183-02, Mk 555, "Spey" Engine and the then proposed RB183-03 "Tay" Engine. The former engine was already in service, performing very satisfactorily. The latter engine was, for Rolls-Royce, a new configuration having a Low Pressure (L.P.) shaft system of Fan plus Intermediate Pressure (I.P.) compressor on the same shaft, driven by the L.P. turbine. The High Pressure (H.P.) Core was to be essentially the same as for the Spey Engine. Purchasers of the new "Tay" Engine were being sought as from late 1982. The work which the present writer carried out for Rolls-Royce is reported in Chapter 7. This work was used in establishing the design of the Engine in 1983/84.

The investigations by the author on combustion are summarised in Chapters 8 to 10 inclusive. The first studies made by him were on flame stabilisation and on flame "spreading", and the three publications arising from that work are discussed in Chapter 8. The first two of these Papers had formed the basis of the author's Ph.D. Thesis, written in 1955/56.

Starting in 1965, the author undertook responsibility for a programme of research into the aerodynamics of swirling jet flows and their utilisation in furnace combustion. The first study was on cold jets in "free" surroundings, and then in an "isothermal" model of a furnace. This was followed by a study of burning "free" jets and then burning jets in furnaces. The swirl generators were of the inclined flat vane type and the fuel gas and air were premixed. This work is reported in Chapter 9.

Non-premixed combustion systems were next examined, at this stage still using only gaseous fuel. A range of fuel injection schemes has been studied – central axial, radial outwards from the axis and then peripheral around the main air jet. The swirl generator most frequently used was of a radial inwards flow type. The study was directed at observing, and predicting, the combustion patterns (flow velocities, temperature distributions, etc) and also the formation of pollutants (particularly oxides of nitrogen). In these studies, the Glasgow University workers have joined with a team at the National Engineering Laboratory (NEL), East Kilbride. A major portion of the experimental work was carried out on the NEL Furnace facility. The findings of these investigations are summarised in Chapter 10.

Finally, in Chapter 11, some concluding remarks are given on the work submitted by the author.

CHAPTER 2

PREDICTION OF TRANSIENT PERFORMANCE OF GAS TURBINES – ADIABATIC PROCEDURES

In the work described in this Chapter it is assumed that "heat transfer effects", or "thermal effects" can be ignored.

The two methods most commonly used to predict the transient behaviour of gas turbines are the "Continuity of Mass Flow" and the "Intercomponent Volume" Method. These two methods were in use prior to the present author beginning his studies in this area. Typically, the two procedures are described by Fawke and Saravanamuttoo (Ref. 1)*. In that paper the "Continuity of Mass Flow" Method is referred to as the "Iterative Method". The present author prefers to use the title "Continuity of Mass Flow" as this describes the thermodynamic basis of the method rather than the numerical procedure that has to be followed.

As the transient prediction work which will be introduced in Chapters 3 and 5 to 7 inclusive is essentially based on one or other of these methods, they are now explained in slightly more detail.

2.1/

* Ref. Numbers in brackets relate to "Reference Papers by Other Workers", listed after Chapter 11.

2.1 "Continuity of Mass Flow" (CMF) Method

In this method it is assumed that at any given instant the mass flow out of the engine matches with the mass flow into it, allowances having been made for bleeds and fuel flow. The calculation starts with a guessed pressure ratio, or mass flow, at the first compressor or fan. This then leads to a set of conditions at entry to the next component. The calculation, with further guesses where necessary, proceeds through the engine. The mass flow at the turbine(s) and nozzle must be consistent with the non-dimensional characteristics of these components. Calculation checks at these locations will cause iterations in which the initial guesses of pressure ratio(s) or mass flow(s) are revised until continuity of mass flow is achieved. Energy balances are now carried out on the shaft(s) of the engine and instantaneous acceleration rate(s) determined. The procedure of Newton is then adopted in which it is assumed that this acceleration will continue throughout the next time interval. This gives new shaft speed(s) which form the starting point for the calculation procedure at the next instant.

The number of iterative loops (frequently nested) which are required depends on the complexity of the engine. For example, for a single-spool engine operating in a range where the turbine is not choked, only one iterative loop is required. By contrast, five iterative loops may be required for a two-spool turbofan with mixed exhausts.

Some workers (Ref. 2) have experienced difficulty in achieving convergence of this procedure when attempting to simulate more complex engine configurations. The author however has been successful with up to two-spool turbotan engines. This is discussed in Paragraph 2.3 below.

It is to be noted that the initial guesses of pressure ratio(s) or mass flow(s) that are required to start a CMF calculation need not be accurate because no results emerge until the iterations have converged on the true pressure ratios etc. The weakness of the CMF procedure obviously is that no allowance is made for the accumulation of air, or gas, within the components and ducts of the engine during the transient.

2.2 "Inter-Component Volume" (ICV) Method

In this method, allowance is made for the accumulation of mass within the components and ducts. The procedure requires an initial estimate of the pressure distribution along the engine at the conditions corresponding to the start of the transient. Inter-component volumes are identified which include an appropriate proportion of the preceding component and an appropriate proportion of the next component. The initially estimated pressure distribution will give mass flows into, and out of, these inter-component volumes. In general, these mass flows will not match, thus air/gas mass will accumulate, or diminish, and the pressure in that volume will rise or fall during the subsequent time interval. The new pressure in each inter-component volume is determined using the equations which satisfy a mass flow balance and an energy flux balance. The new pressure distribution forms the starting point for the calculation at the next time instant. The calculation is essentially a straight-through procedure with no iterations. In practice some iteration may be required in order to achieve the correct working point on the characteristics of the first turbine. Because of the small values of the inter-component volumes compared to the large magnitudes of the air-gas mass flow rates, very short time intervals have to be used otherwise instabilities in the numerical calculation occur. Thus computing times are significantly longer than for the CMF method described previously. Also, poor initial estimated values for the pressure distribution in the engine will lead to erroneous results for the first number of time intervals. This can be overcome by having a 'stabilisation' period prior to the transient.

2.3 Comparison of CMF and ICV Methods – Procedures and Predictions

The CMF Method is iterative. As stated in Paragraph 2.1, some workers (Ref. 2) have had difficulty in achieving convergence when attempting to simulate the more complex configurations such as two-spool turbofans with mixed exhausts. On the other hand the author has been able to achieve satisfactory convergence for two-spool turbofans (Pub. 25, 27, 28, 30, 31, 35, 36, 43 and 65 to 73)*. The author's then Research student, Pilidis, has achieved convergence when simulating a three-spool turbofan with separate exhausts (Ref. 3).

* Pub. Numbers in brackets relate to "Publications by Author" listed after "Reference Papers by Other Workers", following Chapter 11. Copies of these Papers are given in Vol. II (or as Appendix to Vol. I for Reports to Rolls-Royce).

Referring to computing times, Fawke and Saravanamuttoo (Ref. 1) found little difference in computing times for single-spool engine simulations. In the comparisons which the present author has made for the two-spool turbofan (Pub. 43) it was found that computing times for the ICV method were longer by a factor of 5 to 10. This is mainly due to the efficient iteration procedures used in the CMF program and the necessity for short time steps (0.5 ms or less) required in the ICV program to avoid instabilities. With the CMF procedure, and its faster computing times, the possibility exists for predicting in real time. This is important for possible engine control applications.

Considering the predictions of thrust response, Fawke and Saravanamuttoo made no comment on the consistency, or otherwise, of the thrust responses predicted by the two methods for their single-spool engine. The present author has made the comparison for the two-spool turbofan as reported in Publication 43. He found that the CMF procedure tended to underpredict the time taken for the thrust response, compared with the predictions from the ICV procedure. The underprediction was typically about 4 per cent, i.e. noticeable but not large. The present author also found that for accurate predictions from the ICV procedure, the computation had to be carried out using double precision. The predictions from the ICV procedure are of course the more valid because that procedure does make allowances for the change that does occur during a transient in the mass of air/gas contained within an engine.

Finally, comparing trajectories within the compressor(s) predicted by the two procedures, Fawke and Saravanamuttoo (Ref. 1) have given results for the single-spool engine. In an acceleration, when the CMF prediction is used, in response to the jump in fuel flow the compressor trajectory jumps immediately to a higher pressure ratio point on the same constant speed line, while in the ICV prediction the pressure ratio rises more gradually over a series of time steps. Thus the predicted ICV trajectory is more "rounded" in the early stages. However, thereafter the two predicted trajectories were virtually identical.

The present author studied the predicted trajectories in the more complex configuration of the two-spool turbofan with mixed exhausts (Pub. 43). A common non-dimensional fuel schedule was used for both sets of predictions. The predicted trajectories in the Fan were essentially identical, hardly digressing from the steady-running line. In the Intermediate Pressure (I.P.) Compressor, again the trajectories predicted by both procedures were almost identical, in this case there being very significant departures from the steady-running line. It was only in the High Pressure (H.P.) Compressor that the predicted trajectories differed. During the period when the Inlet Guide Vanes (I.G.V.s) to the H.P. Compressor were turning, that is when there was rapid rate of change of demand for air flow into the H.P. compressor, the predicted trajectories using the CMF method moved significantly further from the steady-running line than those predicted by the ICV method. The over-prediction could be as high as 20 to 30 per cent (Pub. 74, p.18).

2.4 Inter-Component Volume Method with High Frequency Gas Dynamics

In the ICV method described in Paragraph 2.2 it was assumed that the pressure variation within a particular Inter-Component Volume occurred uniformly along the Volume. The physical reality of the changes during a transient is that pressure changes are associated with the propagation of pressure waves from the ends of the Inter-Component Volume. Models which incorporate this effect may be described as High-Frequency Gas Dynamic models. Models where these have been included are reported by Merriman (Ref. 4).

The present author, with colleagues, has also introduced these gas dynamic effects into the Inter-Component Volume methods already used (Pub. 64). For a two-spool turbofan engine, the introduction of these high-frequency gas dynamics models into the procedures had negligible effect on the predictions of speed and thrust response and on the predicted trajectories in the compressor as the engine was following a transient acceleration or deceleration. This observation is to be expected since the speed change of the engine is, in effect, a low-frequency transient. Accurate prediction of the low-frequency transient does not require the inclusion of the high-frequency gas dynamic effects. This finding is in line with a communication with Merriman (Ref. 5).

The finding that, for the prediction of the response of an engine in a speed transient, high-frequency gas dynamic modelling does not need to be included is an important result. It confirms the validity of the procedures previously used over many years which had ignored the possible high-frequency gas dynamic effects.

2.5 Summary of Findings by Author

The author has successfully developed models which can make predictions of the transient behaviour of gas turbines of types ranging from the simple single-spool engine to a two-spool turbofan with mixed exhausts and having, in the low pressure compression system, a fan and, for the core air, an intermediate pressure compressor. The models have been based on the Continuity of Mass Flow (CMF) and on the Inter-Component Volume (ICV) procedures. He has compared the predictions of these procedures for the two-spool turbofan configuration described above. The only noticeable differences in predictions lay in the thrust responses and in the trajectory in the High Pressure Compressor. The only previously published comparison known to the author has been for a single-spool engine, where no significant differences in predictions were reported.

The author, with colleagues, has extended the ICV procedure to include high-frequency gas dynamic effects. They showed that this development of the procedure is not necessary if it is only speed transient response predictions that are being sought.

CHAPTER 3

THERMAL EFFECTS IN GAS TURBINE TRANSIENTS

In the first attempts to predict the transient performance of gas turbines – as for example Reference 1 and the descriptions in Chapter 2 preceding, paragraphs 2.1 and 2.2 – "thermal effects" or "heat transfer effects" were ignored. However by the late 1960s a number of "anomalies" were being observed in engine behaviour during transients. The three most obvious anomalies which could not be explained by the adiabatic procedures, were:

(i) The time taken for an engine to complete a speed transient could be 20 or 30 per cent longer than that predicted by the adiabatic transient procedures (Refs. 6, 7).

(ii) When the speed transient had been completed the thrust developed could still differ significantly from that which would be developed when the conditions in the engine became fully stabilised (Pub. 6).

(iii) It was frequently observed that when engines are re-accelerated immediately following a deceleration – the so-called "Bodie" transient – surge in a compressor was more likely to occur than when accelerating the "cold" engine under equivalent control conditions (Refs. 8, 9).

The majority of the references cited above to illustrate the anomalies post date 1968, when the present author started his study. However, these problems were recognised within the industry prior to 1968.

3.1 Suggested "Thermal Effects"

Following from his initial studies, the author, in Publications 6 and 8, proposed that the anomalies described above could be explained in terms of the following "thermal effects".

(i) Non-adiabatic flows in fans, compressors and turbines.

(ii) Changes in characteristics of compressors due to heat transfers, this effect being additional to that resulting from non-stabilised blade tip clearances.

(iii) Changes in efficiencies of compressors and turbines due to blade tip clearances not being stabilised values.

(iv) Air flows through seals differing from design proportions due to seal clearances during the transient differing from design stabilised values.

(v) Heat absorption in combustion chambers.

(vi) Delay in the response of the combustion process.

Bauerfeind (Ref. 6), in 1968, presented the "thermal effects" which he considered to be important. His list included all of the above with the exception of effect (ii). The present author has independently analysed in some detail all of the effects, with the exception of effect (vi). This author's results are now summarised, and his treatments are compared with the methods of Bauerfeind and the few others who have tried to analyse "thermal effects".

3.2 Heat absorptions or rejections in compressors and turbines

In compressors and turbines, convective heat transfers to or from the air-gas stream take place both at the blade aerofoil surfaces and at the surfaces of the platforms of the blades, and also at the casings. The writer has studied (Pub. 13) how this process can be modelled. Average heat transfer coefficients on the aerofoils may be estimated from correlations such as those given by Halls (Ref. 10). Considering the platforms, it can be argued that the convective heat transfer coefficient there and on the aerofoils will be similar as the boundary layer on the end-wall is continuously being restarted from row to row, as on the blade aerofoil surfaces. Looking at the blade platform – aerofoil combination, the aerofoil may be regarded as a fin mounted on the comparatively massive platform. It was found that a very satisfactory representation of the heat transfer rates to or from the platform-blade combination was provided by a "Revised Finned Model" in which the finning effect of the blade on the platform modifies as the transient develops.

This modelling technique was then used (Pub. 6) to study the heat transfer rates along a multi-stage compressor (in this case 17 stage). Heat transfer rates were predicted at the various stages of the compressor and compared with work transfer rates using a parameter, f, where:

$$f = \frac{\text{rate of heat transfer to air / gas in a stage}}{\text{rate of work transfer from air / gas in that stage}} \qquad \ldots 3.1$$

For a typical adiabatic, polytropic compression, across an element of the compression path e.g. a stage or blade-pair, one can write:

$$\frac{dT}{T} = \frac{\gamma-1}{\eta_{pc}\gamma}\frac{dP}{P} \qquad \ldots 3.2$$

$$= \frac{n-1}{n}\frac{dP}{P} \qquad \ldots 3.3$$

where P and T are the stagnation pressure and stagnation temperature respectively, η_{pc} is the polytropic, or small-stage, efficiency of compression and n is the polytropic (adiabatic) index. When the flow is not adiabatic, as in a transient, the temperature change, dT, has to be modified by the multiplier (1 – f) so:

$$\frac{dT}{T} = \frac{(1-f)}{\eta_{pc}}\frac{\gamma-1}{\gamma}\frac{dP}{P} \qquad \ldots 3.4$$

$$= \frac{m-1}{m}\frac{dP}{P} \qquad \ldots 3.5$$

the index of the polytropic non-adiabatic compression being represented by "m". At a given instant in a transient, the parameter f is reasonably constant along the compressor (in this case). Equation 3.5 can then be integrated to:

$$\left[\frac{T_{out}}{T_{in}}\right] = \left[\frac{P_{out}}{P_{in}}\right]^{\frac{m-1}{m}} \qquad \ldots 3.6$$

For turbines, by equivalent argument, the index ((m – 1)/m) in Equation 3.6, which again applies, is given by:

$$\frac{m-1}{m} = (1-f)\,\eta_{pt}\,\frac{\gamma-1}{\gamma} \qquad \ldots 3.7$$

The polytropic, or small-stage, efficiency will be the same in the case with heat transfer as in the adiabatic case, unless it has been altered by a mechanical means such as a change in tip clearance. This procedure adopted by the present author is more sound and more adaptable than that described by Thomson (Ref. 7) in which the efficiency parameter used changes with heat transfer rates.

Using the present writer's approach, work transfer rates are easily found using the changes in stagnation enthalpy given by Equation 3.6 coupled with the fractional heat transfer parameter, f.

[As an aside, it should be noted that in Publication 6 the present author used the, then, non-standard definition of the direction of heat transfer as being positive when it is from the working fluid. The then standard definition, and definition of f, as given in Equation 3.1 above, has been used in all subsequent publications and in this Thesis.]

With the above modelling of non-adiabatic compressions and expansions, the writer studied the behaviour of a single-spool engine (Rolls-Royce Avon) immediately following an acceleration at sea-level static conditions (Pub. 6). At 15s from the start of the acceleration, and some 3s after achieving maximum speed, the thrust was low by about 1.7 per cent, compared with the thrust which was developed some 45s later. This discrepancy was very satisfactorily explained as being mainly due to heat absorption in the turbine (f = -0.03), heat absorption in the compressor (f = +0.01) and low engine air flow (-0.4 per cent), presumed due to higher blade tip clearances. This transient in which the speed is maintained constant while thermal equilibrium is attained has been called by the writer the "Thermal Soak" transient.

The satisfactory agreement between predicted performance changes due to heat transfer during the thermal soak transient and those actually observed gave encouragement to further study.

The present writer considered it would be advantageous if the thermal representation of a multi-stage compressor or turbine could be simplified from the procedure in which the temperatures of the aerofoils and platforms in all rows of blades had to be followed. An alternative would be if a characteristic or representative blade row could be used. He examined this approach and found that very reasonable representation was obtained. Discrepancies in heat fluxes were predicted not to exceed 5 per cent except during the final 2s of a transient when they could rise to between 10 and 15 per cent (Pub. 24). This single "characteristic row" representation has been used to quantify heat fluxes in compressors and turbines in the engine transient programs, as subsequently described.

3.3 Changes in Characteristics of Multi-Stage Axial-Compressors due to Heat Transfers

Three mechanisms have been proposed by the author whereby heat transfers during transients might cause the characteristics of compressors to differ from the steady-running characteristics. These proposed mechanisms are:

(i) Changes in density due to the heat transfers which alter the distribution of axial component of velocity along the blade-pairs of the compressor – thus inter-stage matching is altered.

(ii) The heat transfers between the air flow and the blade aerofoil material could alter the development of the boundary layers on the blade aerofoils, leading to altered average turning angles of the flow and possible delayed or advanced separation of the flow over the suction surface.

(iii) Heat transfers with the blade platforms and casings might alter the development of end-wall boundary layers – in particular their displacement thickness. Thus end-wall blockage is altered.

The first mechanism above was theoretically investigated, in 1973 (Pub. 8), the second in 1977 (Pub. 17) and the third in 1982 (Pub. 26). These mechanisms, their analyses and the relevant Publications are now reported in more detail.

3.3.1 Effect of Density Changes due to Heat Transfer

As stated above, during a transient there will be heat transfers between the air as it flows through the compressor and the various blade aerofoils, platforms and casings. Thus at any given instant in the transient, for that particular inlet non-dimensional mass flow, the non-dimensional axial component of velocity in the later stages (or blade-pairs) of the compressor will differ from what they would have been under steady-running, i.e. adiabatic, conditions. Thus the inter-stage matching in the transient differs from that under steady-running conditions. Hence the overall pressure rise achieved in the complete compressor, and the efficiency of that compression, will differ from that obtained under steady-running at the same non-dimensional inlet mass flow and rotational speed conditions. The present author examined this effect, using the "stage-stacking" procedure described by Huppert and Benser (Ref. 11) and inserting between blade-pairs, or stages, the density change due to heat transfer. First results were reported in Publication 8. Significant movements of the constant-speed line and of the surge line were observed, surge line being moved advantageously in an acceleration, but disadvantageously in a deceleration, hence the perceived danger in a "Bodie" transient. The author referred to these effects as being due to "bulk" density changes.

In subsequent studies (Pub. 31) the author, supported by colleague Pilidis, proposed that the movement of the constant-speed line could be regarded as a change to a new "effective" non-dimensional speed, and the new "effective" non-dimensional speed could be related to the actual non-dimensional speed by:

$$\left[\frac{N}{\sqrt{T_{in}}}\right]_{eff} = \left[\frac{N}{\sqrt{T_{in}}}\right]_{act} + \frac{\Delta N}{\sqrt{T_{in}}} \qquad \ldots 3.8$$

where $$\frac{\Delta N}{N_{act}} = C_1 \quad \frac{\dot{Q}}{\dot{m} c_p T_m} \qquad \ldots 3.9$$

Symbol N represents the rotational speed (typically in r.p.m.). Symbol $\dot{Q}$ represents the summation of the thermal fluxes from the compressor materials to the air and $\dot{m}$ represents the instantaneous air mass flow. Symbols T_{in} and T_m represent respectively the air inlet stagnation temperature and the mean air stagnation temperature within the compressor. (Stagnation temperatures are taken as being very good approximations for the corresponding adiabatic wall temperatures). Subscripts "eff" and "act" refer to "effective" and "actual" conditions.

Similarly it was proposed that changes in predicted surge pressure ratio, at a mass flow, due to this effect could be modelled by:

$$\frac{R_{ht}-1}{R_{ad}-1} = 1 + C_2 f \qquad \ldots 3.10$$

where R_{ht} represents the surge pressure ratio under the transient heat transfer conditions and R_{ad} represents that under steady-running, i.e. adiabatic, conditions.

3.3.2 Changes due to Heat Transfer to or from Blade Aerofoils

The present writer proposed (Pub. 6) that heat transfers between blade aerofoils and the passing air stream might cause alterations in the development of the boundary layers on the aerofoil surfaces. In particular, in a proposal to Rolls-Royce Ltd in 1968, he suggested that heat transfers from the aerofoil to the air, such as occur during and following a deceleration, might lead to an earlier separation of the flow on the suction, or convex, surface. Rolls-Royce sponsored a research on this subject and a Research Assistant, A.D. Grant, was engaged. Both experimental testing on a large convex aerofoil type surface and predictions using a momentum integral code did indeed indicate a noticeable influence of heat transfer on separation on convex surfaces. Heat flow from the aerofoil surface, as during and following a deceleration, did lead to earlier separation (Refs. 12, 13).

The prediction code which had thus been experimentally validated was applied to the blade geometries of the twelve-stage H.P. Compressor of a two-spool turbofan. An acceleration rate, and the corresponding air flow changes, were taken from experimental results.

Predicted changes in the overall characteristics of the compressor were obtained, with new positionings of the constant speed lines and of the surge line (Pub. 17). These movements were in the same direction, in a given transient, as the predicted movements due to "bulk" heat transfers, described in Paragraph 3.3.1 above. [In this work for Publication 17 which came later than the first work on "bulk" heat transfer effects (Pub. 8), a row-by-row procedure for the prediction of the compressor characteristic was used, based on the correlations of Howell and Bonham (Refs. 14, 15), rather than the stage-stacking procedure first used.]

In subsequent work, the writer and his colleague Pilidis have proposed (Pub. 31) that the changes in "effective" non-dimensional speed could be related to the temperature difference between aerofoil and air:

$$\frac{\Delta N}{N} = C_3 \frac{T_b - T_{air}}{T_{air}} \qquad \ldots 3.11$$

where T_b represents the blade aerofoil temperature and T_{air} the stagnation temperature of the adjacent core air flow. It was also proposed that the change in surge pressure ratio, at a mass flow, due to this effect could be modelled by:

$$\frac{R_{ht} - 1}{R_{ad} - 1} = 1 + C_4 f \qquad \ldots 3.12$$

where the subscripts have the same meaning as in Eqn. 3.10.

Predictions of the combined alterations in compressor characteristics resulting from both "bulk" density changes and aerofoil boundary layer changes were presented in Publication 17, referred to above. Transients at altitudes, as expected, showed the greatest effects. At the conclusion of a deceleration of a two-spool turbofan when at 12,200 m altitude, Mach number 0.61, it was forecast that the "effective speed" of the HP compressor was reduced by about 0.3 per cent and the maximum pressure rise that could be achieved by the compressor (i.e. to surge) was reduced by about 10 per cent, at a mass flow rate. Although the change in effective speed seems very small, it is not trivial as the compressor is in the range where the Inlet Guide Vanes are turning and small speed changes are associated with substantial air mass flow changes.

The predicted changes quoted above resulting from the transient heat transfer were due in roughly equal proportions to the aerofoil boundary layer changes and the bulk density changes.

3.3.3. Changes due to Heat Transfers to or from End-Walls

In an adiabatic investigation, Koch (Ref. 16) demonstrated how the development of end-wall blockage at a particular blade-pair or stage of a compressor varied with the pressure rise that was being achieved by that blade-pair – the higher the pressure rise the thicker the end-wall boundary layer. The present writer took this method and developed it into a means of predicting the overall characteristics of a multi-stage axial-flow compressor when running under adiabatic, i.e. steady conditions (Pub. 26). The writer then considered the effects of heat transfer from or to the end-wall casings and blade platforms on the development of the end-wall boundary layers and how these changes will affect the overall characteristics of the compressor. Some results from this procedure are given in Publication 26, the procedure there being labelled Prediction Method "2". Just as the predicted movements in transients of the constant-speed lines and of the surge line due to aerofoil boundary layer effects have been in the same direction, or additive, to those due to "bulk" heat transfer effects, the movements due to end-wall boundary layer changes are also additive. In an acceleration, all three effects cause roughly similar changes towards increasing the "effective" speed and raising the surge line. However in a deceleration, the dominant effect seemed to be that due to density changes arising from bulk heat transfers. This small readjustment in the relative significances is attributed to the fact that the ratios of heat transfer to work transfer, i.e. parameter f, are larger during decelerations than accelerations.

In the later investigations (Pub. 31), the present writer and his colleague Pilidis proposed that the changes in "effective" speed could be modelled by:

$$\frac{\Delta N}{N} = C_5 \frac{T_w - T_{air}}{T_{air}} \qquad \text{... 3.13}$$

where T_w is the temperature of the end-wall. Similarly it was proposed that the changes in surge pressure ratio, at a mass flow, due to this effect could be modelled by:

$$\frac{R_{ht} - 1}{R_{ad} - 1} = 1 + C_6 f \qquad \text{... 3.14}$$

3.3.4 Summation of Predicted Changes due to Heat Transfers

The models for the three mechanisms described above may be added to give, for changes in effective speed:

$$\frac{\Delta N}{N} = C_7 \frac{T_b - T_{aie}}{T_{aie}} + C_8 \frac{\dot{Q}}{\dot{m} c_p T_m} \qquad \text{... 3.15}$$

For changes in surge pressure ratio, the composite expression becomes:

$$\frac{R_{ht} - 1}{R_{ad} - 1} = 1 + C_9 f \qquad \text{... 3.16.}$$

The values proposed in Publication 31 for constants C_7, C_8 and C_9 for the L.P. Compressor of a particular two-spool turbofan (RR Spey) were –0.07, -0.07 and 0.36. In the H.P. compressor the corresponding proposed values were –0.1, -0.1 and 0.36. Strictly, for the H.P. Compressor, which has moveable Inlet Guide Vanes (IGVs), the values of the constants C_7 and C_8 referring to changes in "effective" speed will vary depending on whether the transient is exclusively within or outwith the IGV turning range. In most cases the transient will partially cross the IGV turning range and the values of –0.1 and –0.1 given above for constants C_7 and C_8 are mean values in such transients for the two-spool turbofan.

CHAPTER 4

EFFECTS OF TRANSVERSE INJECTION

There are several locations in a gas turbine where an air/gas flow is injected transversely into the main, axially flowing, air/gas stream. The most frequent location is where air, tapped off from some plane in or after the HP compressor and then used to cool a turbine disc, is then returned to the main gas stream flowing through the turbine. This returning cooling flow will have a major component of momentum normal to the main gas flow.

The author thought that this should be studied to determine if this transverse injection led to any adverse effects in the main flow – other than the obvious loss of mass flow through the earlier nozzles, and possibly stages, of the turbine.

The author had the assistance of research students in these investigations (Pub. 18, 19, 21, 22) and also the work of a colleague (Pub. 23).

In the initial study, the flow mechanism was studied in a "cold", straight cascade of high aspect ratio (3.0). This aspect ratio was chosen in order to ensure that, at mid-blade height, there was minimal effect of secondary flows. The early investigation (Pub. 18) showed the creation of a separation bubble on the end wall behind the injection slot, more pronounced secondary flows outwith the boundary layer on the end-wall behind the injection slot, and the formation of a vortex-like flow originating at the suction surface end of the separation bubble behind the injection slot. In the traverse of the flow emerging from the cascade, when this continuing vortex was reached, there was a reduction in the main forward flow velocity relative to the unaffected plateau velocity. For the most extreme transverse injection rate observed, injection velocity 1.5 times the main velocity, the forward velocity in the core of the vortex at the cascade exit was lowered to about 0.82 of the unaffected plateau velocity.

Attempts were made to quantify the changes in the more obvious performance parameters, resulting from the transverse injection (Pub. 19). The changes in flow capacity and nozzle efficiency were in line with a simple theory. Further, attention was given to quantifying the strength of the vortex, mentioned above, which arose from the transverse injection. Three possible measures were offered, and results given.

As mentioned above, the aspect ratio of the blades in the above work was high, at 3.0. The next development in the study was to investigate the influence of aspect ratios. Results were reported (Pub. 21) for aspect ratios of 1.5 and 1.0. The simple theory, previously offered, again was in agreement with flow capacity and efficiency changes. The vortex created by the transverse injection was slightly reduced, but persisted.

A colleague (in Canada) had available an annular cascade and this allowed a useful validation (Pub. 23) of the results already obtained in straight cascades. Additionally, it was possible in this annular cascade to introduce skewing of the approaching end-wall boundary layer (the usual case in turbines). When there was no transverse injection immediately in front of the cascade, this skewing had produced considerable effects on the secondary flows in the cascade. However when transverse injection was introduce, these effects on secondary flows were noticeably reduced.

There remained the problem of what damage the vortices resulting from transverse injection, identified in Publication 18, might do to the flow in the later blade rows. This topic was the subject of another investigation, and reported in Publication 22. The flow in the next row of blades appeared not to be adversely affected, pressure losses possibly being reduced, and the vortices largely suppressed.

In view of the above studies, it was considered that cooling air flows to discs etc, subsequently injected transversely into the main air/gas flow, should incur, for engine performance calculations, only the penalty due to the loss of effective mass flow through the particular turbine blade pair (or stage).

CHAPTER 5

APPLICATION TO A TWO-SPOOL TURBOFAN ENGINE

The transient prediction procedures described in Chapter 2 were developed by introducing the "thermal effects" models of Chapter 3 and the "transverse injection effects" models of Chapter 4. This prediction procedure was then applied to a two-spool turbofan engine in which both the Fan and the I.P. Compressor are carried on the L.P. shaft. The engine used for this investigation was essentially the Rolls-Royce "Tay" Engine. (The present author had contributed considerably to the design of the "Tay" Engine, described in Chapter 7 of this Thesis). The more recent investigation, including "thermal effects", etc, was reported in Publication 43. This Publication also included a comparison of the Continuity of Mass Flow (CMF) and the Intercomponent Volume (ICV) Methods of transient engine prediction.

The principal conclusions of this study were:

1. The CMF method of prediction generally agreed with those of the more computer intensive ICV method, except when the engine was accelerating most rapidly as the Inlet Guide Vanes (I.G.V.s) were opening and the H.P. Compressor Bleed valve was closing. The more true predictions of the ICV method were always towards less extravagant trajectory movements from the steady-running line. Thus, if an engine's design were based on the CMF predictions, it would be erring on the safe side. (No comparison of these two prediction methods for an engine of this complexity had hitherto been published.)

2. With regard to the predicted trajectories in the compressors during transients, and considering first the adiabatic case: (a) trajectories in the Fans all lay close to the steady-running working line, (b) in the I.P. compressor the trajectories moved markedly from the steady-running line whenever the H.P. compressor Inlet Guide Vanes (I.G.V.s) were turning – the trajectory moved to a lower pressure ratio during the acceleration, and to a higher pressure ratio when decelerating (danger of surge (!)), (c) the trajectories in the H.P. Compressor were similar to those in the

compressor of a single-spool engine – towards the surge line in an acceleration and below the steady-running line during a deceleration.

3. Considering the alterations to the predicted trajectories when "thermal effects", as described in Chapter 3, are allowed for: (a) the "heat transfer" effects had insignificant influence on the predictions in the Fan (outer and inner) and in the I.P. compressor, (b) in the H.P. compressor, during an acceleration of a "cold" engine, inclusion of heat transfer effects indicated that the predicted trajectory was moved less markedly towards the surge line, and in the deceleration the predicted trajectory did not move as far below the steady-running line as predicted for the adiabatic situation – but more importantly when a re-acceleration of a "hot" engine (the "Bodie" transient) was attempted, again using the hydro-mechanical fuel controller, then the trajectory in the H.P. compressor moved markedly further above the steady-running line, towards the altered surge line, and could encounter surge. This was in contrast to the situation of accelerating the same engine when "cold" (with the same hydro-mechanical fuel control system and schedule). For the "cold" engine, the acceleration was predicted to be free from surge.

4. Considering the thrust response, again using the conventional hydro-mechanical fuel controller and schedule, the time to accelerate from idle to maximum speed was predicted to be about 25 per cent longer, when the "thermal effects" were allowed for, as compared to adiabatic predictions. Of course the thrust response followed the shaft speed response, particularly the L.P. shaft speed.

5. It was additionally predicted when "thermal effects" were allowed for, that the thrust developed by the engine when it had reached, and steadied at its maximum speed (as dictated by maximum fuel flow) was about 2 per cent, or more, below its final value. It was predicted that the final value would not be attained until the engine had been running for about 3 minutes at its maximum fuel flow.

The predictions, when "thermal effects" are included, quoted under headings "3" and "4" above – namely surge danger in Bodie transient, slower accelerations of engines cf adiabatic engines, and low thrust when maximum speeds are first attained following accelerations – are in line with observations of actual engines.

The author considers that the "thermal effects" allowances should be included in transient prediction procedures for actual engines.

CHAPTER 6

DEVELOPMENTS IN CONTROL OF GAS TURBINE ENGINES

In the transient procedures reported in Chapters 2 to 5, it had been assumed that the engine was using a conventional hydro-mechanical fuel control system. This system is in fact used for the Rolls-Royce "Spey" and "Tay" Engines. In these cases, the fuel schedules are in the form, for the "Spey" Engine, of $(\dot{f}/N_{HP}P_2)$ as a function of H.P. compressor ratio (P_3/P_2). Symbol $\dot{f}$ represents the fuel flow, N_H the H.P. shaft speed and P_2, P_3 the pressures at inlet to and exit from the H.P. compressor. In the studies on this engine (Pubs. 30, 31) it had been predicted that a "cold" engine accelerated slowly, but used a diminished amount of the available surge margin in the H.P. compressor. On the other hand, when the engine was accelerating when "hot" (the Bodie transient) it accelerated very rapidly, but the trajectory in the H.P. compressor was in danger of encountering surge. This led the author to think of compensating the accelerating fuel schedule such that the fuel flow was enhanced when the engine was cold, but was reduced when the engine was hot. Two such proposed methods of compensation involved (a) the temperature of a characteristic blade in the H.P. compressor and (b) a "delayed" H.P. shaft speed. The results of this study were reported in Publication 35. The "delayed" H.P. shaft speed appeared to offer the more effective compensation.

More sophisticated methods of fuel control are being utilised in the industry – for example the Full Authority Digital Electronic Control (FADEC) system. With a view to taking advantage of electronic control systems, the author was privileged to collaborate with Professor P.J. Gawthrop and O.F. Qi. The first task was to use a multivariable nonlinear controller. It was shown (Pub. 50) that this could improve the engine dynamic response. This was developed in Publication 51. Subsequent studies involved a gain-scheduled controller (Pub. 53) and a "model-based observer" (Pubs. 52, 63).

The adaptations taken by a model-based observer should accept some of the engine abnormalities caused by thermal effects e.g. loss of efficiency of components due to transiently increased tip clearance, and increased seal clearance flows. However they will not recognise changes, due to thermal effects, in the characteristics of the compressors.

Further study is needed here to blend compressor characteristic changes – namely surge line and speed line movements – with the beneficial features of "model-based observer" controllers.

CHAPTER 7

DESIGN OF ROLLS-ROYCE RB183-03 "TAY" ENGINE

The author was approached, in December 1982, to provide a transient prediction program for an engine which Rolls-Royce was proposing – the RB 183-03 Engine (later christened with the name "Tay"). The author accepted this invitation. The responses to this agreement are given in Publications 65 to 73 inclusive, contained as Appendix in Volume I of this Thesis.

The first step in the contract was that the author should produce a satisfactory transient model for the RB 183-02 "Spey" Engine – which was currently in production, and providing satisfactory performance for customers. A satisfactory model was produced and reported in Publication 65 (March 1983).

The next step was to create a simulation model for the "Tay" engine. The characteristics for the compressors provided by Rolls-Royce were all to a base line of non-dimensional mass flow. This was completely satisfactory for the Intermediate Pressure (I.P.) and High Pressure (H.P.) compressors of the Tay Engine. However for the Fan, this component was divided into an "Inner" and an "Outer" section, each with its own pressure ratio (and efficiency) characteristic to the base line of non-dimensional mass flow. To the present author, this seemed virtually to constrain the engine to operate at a fixed bypass ratio for any given non-dimensional speed. This seemed inappropriate for transients. Therefore the model which this author evolved incorporated allowance for movement of the streamline which arrived on the splitter behind the fan dividing the core and bypass flows. The factor which quantified this adjustment was labelled the "fraction of split" (FCSP). The method of the determination of this variable FCSP is illustrated on Fig. 7 of Publication 73.

The first results, for sea level conditions, for simulation of the "Tay" engine were reported in Publication 66. The predictions were then extended to altitude conditions (Pub. 67).

The potential risk of surge in the I.P. compressor when decelerating was recognised. An air bleed (to the bypass duct) from between the I.P. and H.P. compressors was considered, and predictions presented (Pub. 68).

Time was not available to incorporate the thermal capacities, heat transfer areas, etc, necessary for the creation of the thermal transient models being developed in Chapter 3. A "fix" that had been used, with realistic effect, had been to scale up the polar inertias of the shafts by a factor of, typically, 1.3. This factor was introduced in Publication 69, along with studies of turbine efficiency changes.

An examination of the potentials of changing the transient fuel schedule under the hydro-mechanical fuel controller was then made (Pub. 70). The author also proposed in that Publication that the ($N_H/\sqrt{T}$) range of the turning of the Inlet Guide Vanes (I.G.V.s) of the H.P. compressor be extended, such that in a deceleration, the IGVs did not finish "closing" until a lower ($N_H/\sqrt{T}$).

An examination of gross inertia changes was then made for Publication 71. The case was examined of the inertia of the L.P. shaft being reduced by a factor of 4.3 (to bring it to the value of the Spey Engine). As expected, the deceleration trajectory in the I.P. compressor now made only slight excursion from the steady-running line towards the surge line.

The case of a Spey Engine with an L.P. shaft inertia scaled up by the factor of 4.3 was also examined. While the deceleration trajectory movements in the L.P. compressor were now more marked, they did not encounter surge (Pub. 71).

The design of the Engine was revised on 13 June 1983 such that from that date the inertia of the L.P. shaft was reduced from 415 lb ft^2 to 260 lb ft^2. Revised predictions were produced (Pub. 72). Surge margin usage in the I.P. compressor during decelerations, under the same fuel schedule, was reduced. Rates of thrust reduction were increased.

An alternative I.G.V. turning range schedule was also examined in this report (Pub. 72) – this schedule having an ($N_H/\sqrt{T_{26}}$) range from 513 to 586 as against the range 552 to 586 of the original specification H.P. compressor. (T_{26} represents the temperature at I.P. compressor delivery). This extension of the I.G.V. turning range to lower the "closed" ($N_H/\sqrt{T_{26}}$) value, while retaining the "fully open" value, thus slowing the rate of turning, gave significant easing of the deceleration trajectories in the I.P. compressor. A development of this nature was suggested by the author, but he was told at that time that the I.G.V. turning range of the H.P. compressor could not be changed, for other reasons.

Early in July 1983 the author was informed by Rolls-Royce that the surge line in the I.P. compressor had been revised downwards. The original surge line and this revised surge line (marked as Jan 1984) are shown in Figs 13, 15 etc of Publication 73.

Later that month (July 1983), the design of the RB 183-03 was being finalised. In view of the predicted marginal surge in the I.P. compressor when decelerating under (0.9 x (CASC211) – see Fig 13 of Pub. 73 – it was decided that the design should include a provision of a 5 per cent air bleed from the I.P. compressor delivery to the Bypass duct – to be activated only in decelerations.

The Reports to Rolls-Royce (Publications 65 to 72 incl.) had been written for specific requirements, as the design of the engine, and airframe manufacturer's specifications, progressed. These Reports were summarised in the later Report RR/1, Publication 73, of August 1984.

An important outcome of the investigation was the appreciation of the influence of the H.P. compressor I.G.V. schedule on the potential for surging of the I.P. compressor in a deceleration (Pub. 70, 72 and 73, summary point 2, Section 4 "Discussion", p.7). As previously indicated, the data provided for the design engine to the author specified that H.P. Compressor I.G.V. turning range was 552 to 586 ($N_H/\sqrt{T_{26}}$) – see Fig. 26 of Publication 73.

Initial tests of the prototype engines suggested that the I.P./H.P. Bleed was not necessary for decelerations. (An unintended air leak from I.P. compressor delivery was subsequently detected. This leak was later sealed.)

For production engines, the 5 per cent I.P./H.P. Bleed was indeed removed.

However it has been noted that the ($N_H/\sqrt{T_{26}}$) range of I.G.V. movement for the H.P. compressor has now been modified to about 495 to 590 (very similar to the range proposed by the author in Publication 72). The author was not informed directly of this change, but this has been ascertained via students working in the company. This slowing of the rate of turning of the I.G.V.s greatly eases the blockage which the I.P. compressor faces in a deceleration, and therefore eases the excursion of the trajectory in that compressor, lessening the risk of surge, and obviating the need for the I.P./H.P. Bleed.

As a final remark the author wishes to report that it has been informally reported to him that the predictions given in his reports have been completely fulfilled.

CHAPTER 8

STUDIES IN PREMIXED COMBUSTION

Premixed combustion requires firstly flame stabilisation, followed by flame propagation. The author has worked in both these areas. His first two publications (Pubs. 1, 2) were based on some of the work of his Ph.D. Thesis submission of 1956.

1. Flame stabilisation

It has been shown that flame stabilisation limits on circular burners can be correlated by the boundary velocity gradient (Refs. 17, 18). The present author extended the correlation to rectangular burners in laminar and turbulent flow (Pubs. 1, 5). Flame stabilisation in high velocity systems using cans or pilots was also studied for Publication 2.

2. Flame Propagation

The present author investigated flame propagation, or flame spreading, in a range of flow systems. Laminar, moderately turbulent and highly turbulent systems were considered (Pub. 2). The benefit of using flame spreading devices such as inclined fingers in highly turbulent combustors e.g. as in ramjets was demonstrated.

CHAPTER 9

THE USE OF SWIRL IN PREMIXED COMBUSTION

Giving swirl to the air as it enters a combustion chamber has long been recognised as having possible beneficial effects, such as in flame stabilisation, or control of flame shape. An early example was in the combustion chambers of the first Whittle Jet Engines (Welland) of the 1940's.

In 1965 the present author undertook responsibility for a continuing programme of research into the aerodynamics of swirling jet flows and their utilisation in furnace combustion. This work had been started by N.M. Kerr and D. Fraser, and their pioneering investigations had been reported in References 19 and 20. The next step was to quantify the velocity profiles of a "cold" air jet issuing from a vane swirler, firstly when in free surroundings and secondly when confined (Pubs. 3, 4). The necessary degree of overlap of the vanes was established, a swirler pressure drop correlation developed and established, recirculations measured, and static pressure and velocity profile decays observed. A swirl number based on the tangent of the vane angle (to axial) was confirmed.

The next development was to introduce combustion. Fuel gas was mixed with the air stream prior to entering the swirler. The first series of measurements were taken in the unconfined situation. Velocity profiles of the flow in the developing jet flame were obtained, as were profiles of temperature and static pressure (Pub. 7). These profiles were, as expected, wider than those in the "cold" free jet. Also, a prediction procedure, developed at Imperial College, London, was used to give comparisons against the experimental results of velocities and temperatures. Difficulty was experienced in "stabilising" combustion in the calculation procedure, but when a forced combustion was imposed, predictions were in reasonable agreement with experiment (Pub. 7).

The stability limits of these "free" jet flames were also examined. It was found that the weak stability limit, for varying degrees of swirl, could be correlated by consideration of the effective fuel/air ratio, after entrainment of external air, at the edge of the recirculation zone adjacent to the swirler (Pubs. 9, 37, 38). This, to the author's knowledge, was the first time that this entrainment effect had been quantitatively used in correlation or prediction of combustion results.

The next stage in this programme of study of swirl, and combusting swirl, was to examine combusting, swirling, premixed flows in "confining" furnaces. Two furnaces were used, having furnace to burner (swirler) diameter ratios of 5.0 and 2.6. Again, profiles of velocities, pressures and temperatures were measured. Also, carbon dioxide (CO_2) concentrations were taken. Combustion was seen to have a marked effect on the flow profiles, particularly on the establishment or otherwise of a central recirculation zone (CRZ). A simple theory based on momentum fluxes, and using the predicted drop in static pressure due to combustion, was found to correlate the transition between flow patterns (Pubs. 10, 11, 12, 15, 16, 45, 54).

CHAPTER 10

THE USE OF SWIRL IN NON-PREMIXED COMBUSTION

The previous application of swirl to premixed combustion was next extended to a study of the use of swirl in non-premixed combustion systems.

Initially, gaseous fuel was selected. The present author was fortunate to have the support of a Research Student, A.M.A. Kenbar, and the collaboration of colleagues S.A. Beltagui (Glasgow University and National Engineering Laboratory (N.E.L.)) and T. Ralston (N.E.L.).

An early study was made of the possible benefits of injecting the fuel at the outer periphery of the incoming swirling air. This would enable centrifugal buoyancy effects to enhance the mixing of air (near the axis) with the burning low density products (in the outer region). The experiments were carried out in the Glasgow University furnace. This first investigation was reported in Publications 39 and 40. In comparison with central fuel injection systems, the peripheral injection systems, with even weak swirl, give much more rapid mixing and combustion. This was one of the earliest published studies of the peripheral fuel injection process.

Comparisons of central axial, central radial (from axis) and peripheral fuel injections of a gaseous fuel (natural gas), again into swirling airflows, were presented in Publication 41. Two combustors or furnaces (at Glasgow University), having furnace to burner diameter ratios of 1.0 and 2.5, were used for the peripheral fuel injection studies. A furnace of furnace to quarl exit diameter ratio 5.0 (N.E.L.), was used for the radial injection experiments. The peripheral fuel injection exploited the benefits of shear-layer mixing and centrifugal buoyancy mixing. For combustion performance equivalent to axial injection systems, lower swirl, thus less fan power, was required. The radial outwards (from the axis) fuel injection schemes also could give good combustion performance with only modest swirl requirements – the radial outwards fuel jets helped establishment of the central recirculation zone.

The importance of the formation of pollutants was now being recognised (post 1985). The study of pollutant formation greatly influenced subsequent investigations – particularly in the work carried out by Research Student A.M.A. Kenbar and associates using the N.E.L. Furnace. A parametric study was reported (Pub. 42). This included models for NO_x formation. The study of burner parameters was extended in Publications 44 and 46.

The study of peripheral fuel injection was extended to its introduction in the N.E.L. Furnace with its high furnace to quarl exit diameter ratio (5.0) – Publications 47, 58. Measurements of NO_x concentration were reported.

Heat transfer rates in the furnace, by convection and radiation, were reported (Pub. 55). Extensive experimental measurements were taken, for validation of computational fluid dynamic codes that were under development at Harwell (sponsored by Heat Transfer and Fluid Flow Services (HTFS)). Again it was shown that peripheral and radial (outwards) fuel injection systems have advantages over axial fuel injection both for heat transfer and combustion, including pollutant control.

Predictions were next made of the fluid dynamics of the peripheral fuel injection system (Pub. 56). The predictions were made by the PHOENICS code of Ludwig et al (Ref. 21), and compared with experimental results from Beltagui et al (Ref. 22) and others in that series. The main flow parameters were reasonably predicted.

The experimental results from the peripheral fuel injection case were extended and more widely reported in Publication 58. The observations included NO_x concentrations.

The pollutant measurements in the combusting flows in the N.E.L. furnace now enabled a further comparison among axial fuel injection, radial outwards injection and peripheral injection. These further results were reported in Publication 59. The peripheral fuel injection was the only scheme to offer reduction of NO_x by lean combustion, by fuel-staging and by air-staging.

Predictions of NO_x formation were now being made using a well-stirred reactor model based on the Zeldovich mechanism with account for species concentration fluctuations. Model predictions were seen to compare quite satisfactorily with measurements (Pub. 60).

In 1993 the present author was invited to present Papers to a Symposium in Budapest on "Measurement and Control Techniques on Environmental Protection". The writer drew heavily on the collaborations with his colleagues, presenting two Review Papers – Publications 61 and 62 – which referred respectively to measurements of pollutants and combustion patterns associated with modelling of pollutant formation.

CHAPTER 11

CONCLUDING REMARKS

First, with reference to the work on gas turbines, the writer's major contributions have been in modelling the "thermal effects" that occur during speed transients of a gas turbine engine. The significant thermal effects that should be considered are heat absorptions or rejections in the compressions and expansions, tip and seal clearance movements, compressor characteristic modifications, heat absorption in combustor materials. The writer has probably pioneered attempts to quantify and model these effects. It is perhaps relevant to quote from an AIAA Paper by Crawford and Burwell, written in 1985 (Ref. 9)

– p.2 – *Literature Background Investigation* – second paragraph –

"The seven papers (3 – 9) were the works of four authors who had either worked together or had detailed knowledge of one another's works. The collected results of Grant, MacCallum, Elder and Larjola establish the baseline for the University of Tennessee Space Institute investigation. The importance of the successful literature search to this investigation cannot be underestimated due to the many man-years of research and analysis contained in the published reports. The fact that all the efforts were accomplished outside the U.S. supports the importance of international technical symposiums and cooperative technical exchanges.

The conclusions which can be drawn from Refs 3 – 9 are that sound theoretical models predict a significant influence of transient heat transfer on compressor stability, however no transient turbine engine data were available to validate model predictions. This investigation (Crawfurd and Burwell's) utilizes high quality AEDC transient turbine engine data to quantify and support the above conclusions. "

(Grant, referred to above, was a Research Assistant under the writer's supervision. Larjola consulted the writer before starting his research.)

The above favourable comments were written before publication of the writer's subsequent collaborations with Pilidis and Qi, with the useful modelling of tip clearance and seal clearance movements, also innovative control strategies.

Further to work in this area of investigation, the author understands that at Cranfield University's "University Technology Centre in Performance Engineering" (largely sponsored by Rolls-Royce plc) investigators are currently (year 2000) pursuing studies on predicting the changes in compressor characteristics resulting from transient heat transfers. The investigators, it is understood, are starting from models such as those introduced in the author's Publications 8, 13, 17 and 31.

In other gas turbine transient work, the writer provided for Rolls-Royce the transient model of the RB 183-03 "Tay" engine. The predictions given by that model when the engine was being designed were all subsequently validated.

In the combustion field, the writer's contributions have mainly been in experimentally quantifying swirling flow. This research moved through cold flow, open and enclosed, to combusting premixed flow, again open and enclosed, and on to combusting non-premixed flows in furnaces. The majority of the experimental work was carried out by research students, the writer being the research supervisor.

The writer feels that useful contributions have been made in defining combusting flow patterns and in modelling their development.

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"Publications" 65 to 73 (Reports to Rolls-Royce) in Vol. I.

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Tay Project Report No.2, 'Initial Predictions of Transient Response of Engine RB183-03 Tay', 29 April 1983. 100%

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Predicted Transient Response of Engine RB183-02 Mk555 Spey

TAY PROJECT - Report No. 1 12 March 1983

N.R.L. Maccallum

1. INTRODUCTION

This Report gives the steady-running and transient performance of the RB183-02 Mk 555 Spey Engine at Sea Level, Static conditions on an ISA day.

2. RESULTS

2.1 HP/LP Shaft Speed Relationships

The predicted shaft speed inter-relationship as predicted by Maccallum's programme (marked "NM") are given in Fig. 1. It is seen that for steady-running the results coincide almost identically with those given by the Rolls-Royce Synthesis program (Q538).

For the transient cases, the standard CASC 211 schedules were taken as controlling the fuel flow. The nominal inertia values of the two shaft systems were used. It is seen from the results illustrated in Fig. 1 that in the early part of the acceleration the LP shaft "leads" the HP shaft. Then when the Inlet Guide Vane(IGV)turning range of the HP Compressor is reached, the two shaft speeds advance more or less in step with the steady-running shaft speed relationship.

2.2 Shaft-Speed Responses in Acceleration

The responses of the LP and HP shafts in the acceleration are shown in Fig. 2. The HP shaft accelerates reasonably rapidly from the start of the acceleration, with a gradually increasing acceleration rate until the maximum fuel flow is reached.

The LP shaft acceleration is modest and steady until the HP compressor reaches the IGV turning range, with Bleed Valve closure. This is reached at about 4.0s. The LP shaft speed acceleration then becomes very rapid until the fuel flow reaches the imposed limit of 0.74 kg/s.

2.3 Shaft Speed Responses in Deceleration

Predicted Shaft speed responses, when following the CASC 211 Deceleration Schedule, are given in Fig. 3.

2.4 Predicted Running Line and Trajectories in LP Compressor

These results are given in Fig. 4. The steady-running line predicted by Maccallum's program agrees virtually identically with that given by Rolls-Royce Synthesis Program (Q538). In the acceleration the trajectory lifts above the steady-running line. This fits with the LP shaft "leading" the HP shaft. Note that this engine has mixed exhausts.

In the deceleration the predicted trajectory drops below the steady-running line.

2.5 Predicted Running Line and Trajectories in HP Compressor

These are shown in Fig. 5.

For steady-running, again there is excellent agreement between the predictions of Maccallum's program and those of Rolls-Royce Synthesis Program, Q538.

In the acceleration the trajectory rises, as expected for an HP system. It is to be noted that the trajectory is predicted just to touch the surge line at about 4.2 s into the transient. However, service experience with this engine does not indicate that surge occurs under these conditions. It is therefore concluded that the predictions of trajectory in transients may be erring on the extreme side. This is not a bad fault in the design stage.

3. CONCLUSIONS

A transient simulation model (CMF type) has been produced for the RB183-02, Mk555, Spey Engine. At steady-running conditions its predictions match excellently with those of the Rolls-Royce Synthesis (Q538).

It was not possible to compare transient predictions with those from another program as none was available. The transient predictions obtained appear reasonable.

Table 1 Shaft speeds at starts and termination of transient
Conditions: Sea level, static.

	Starting Speeds NL	Starting Speeds NH	Finishing Speeds NL	Finishing Speeds NH	Starting $\dot{f}$	Finishing $\dot{f}$
Acceleration	2572	6216	8931	12218	0.08	0.74
Deceleration	8759	12085	3077	7217	0.71	0.10

(Speeds in r.p.m., fuel flows in kg/s)

LOGIC RB 183 - 02

FIG 1 RB 183-02 Mk 555

INTERRELATION OF SHAFT SPEEDS
SEA LEVEL STATIC

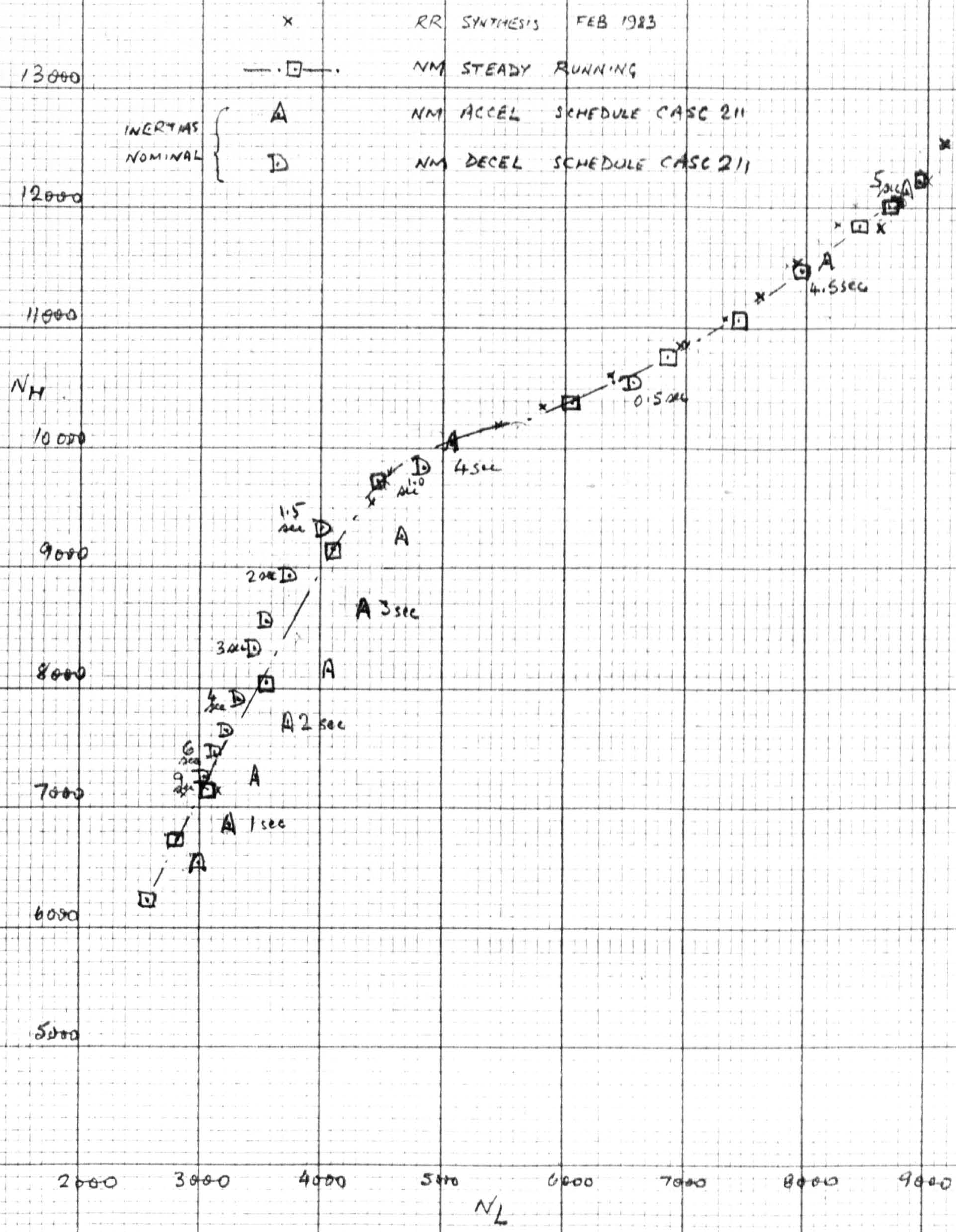


FIG 2 RB 183-02 Mk 555

SEA LEVEL STATIC

ACCEL. — NOMINAL INERTIAS

FUEL SCHEDULE - CASC 211

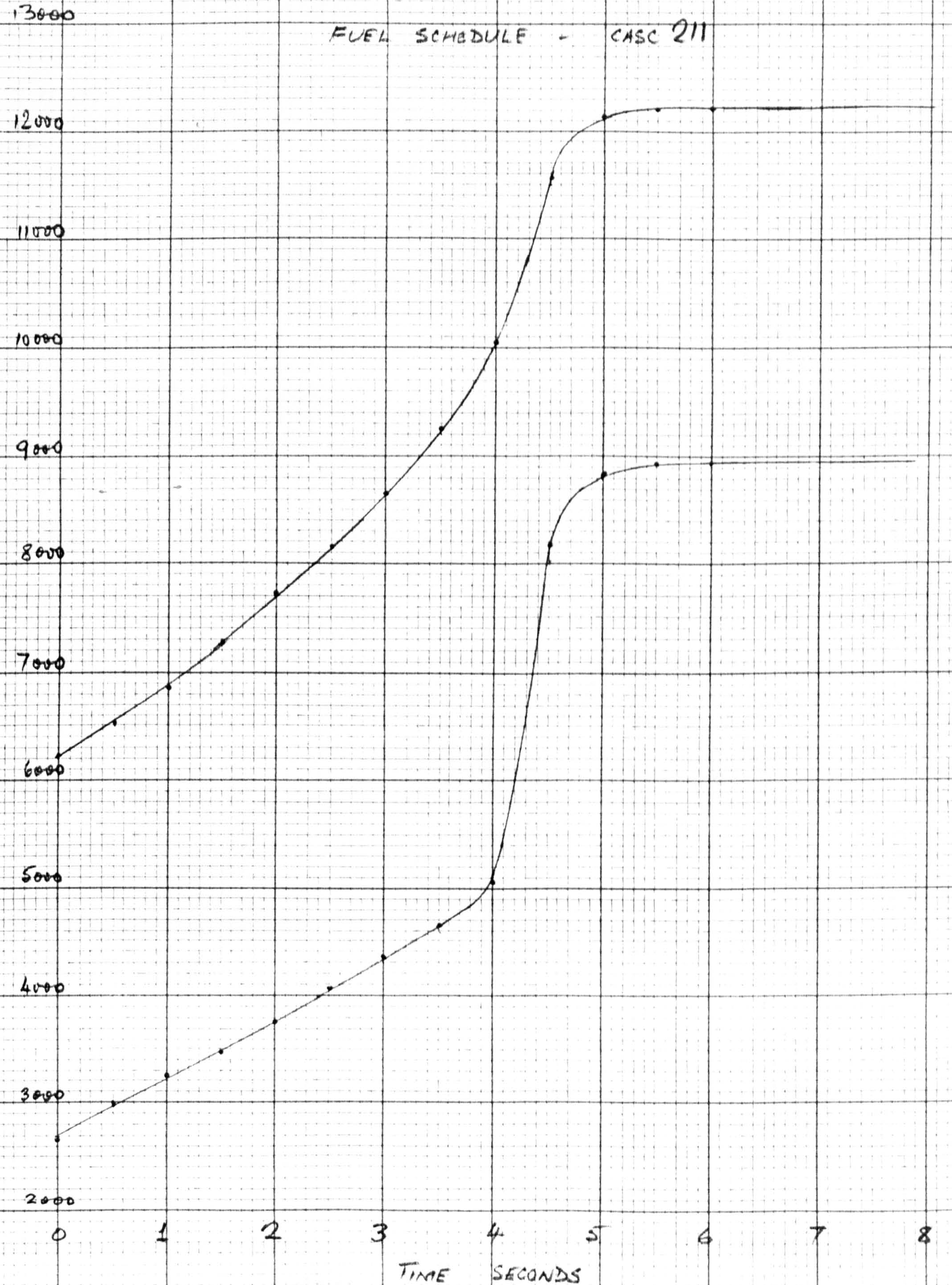


FIG 3 RB 183 - 02 Mk 555

SEA LEVEL STATIC

DECELERATION NOMINAL INERTIAS

FUEL SCHEDULE CASE 211

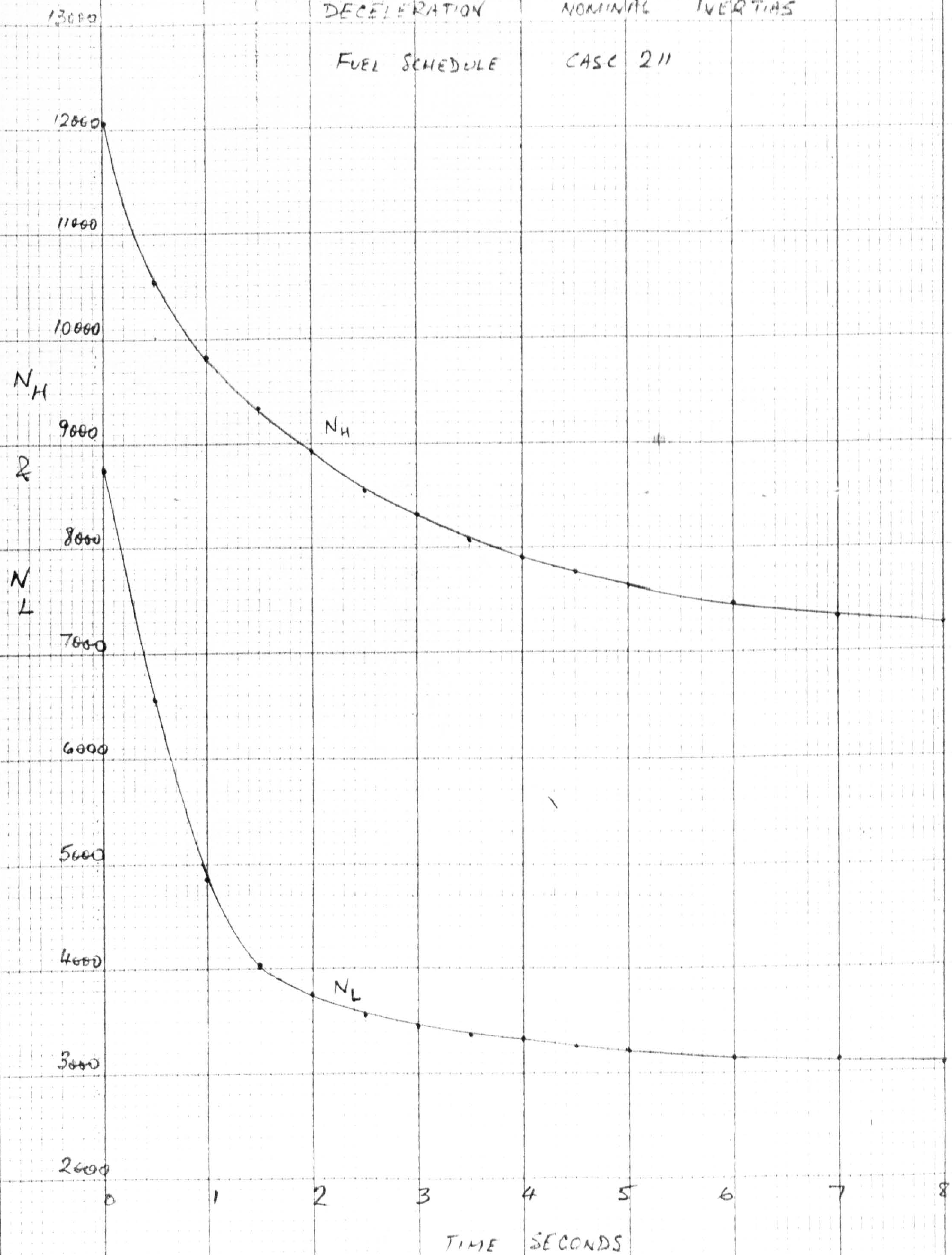


FIG 4 SPEY RB 183-02 MK 555-15P

L.P. COMPRESSOR

FIG 5


SPEY RB 183-02 MK 555-15P
& ~~TAY RB 183-03~~
H.P. COMPRESSOR
SEA LEVEL STATIC
R-R SYNTHESIS Q 538 (2 FEB 1983) -2 MK555
NM STEADY-RUNNING
NM ACCEL CASC 211
NM DECEL CASC 211
ACCEL
DECEL
P_3/P_2
$\dot{m}_2\sqrt{T_2}/P_2$
300
441
500
549
560
557
576
584
595
614
635
644
650
663

Initial Predictions of Transient Response of Engine RB183-03 Tay

TAY PROJECT - Report No. 2 29 April 1983

N.R.L. Maccallum

1. INTRODUCTION

A transient model of the RR-Tay Engine is now working. The model is based on the CMF procedure. The model cannot yet cover the complete speed range, but the predictions for the range covered give a good guide to the transient behaviour of the engine. The speed range covered in this Report is given in Table 1.

2. RESULTS

2.1 HP/LP Shaft Speed Relationship

These are given in Fig. 1 for steady-running and transient cases - sea level, static. The steady-running predictions may be compared with those of Rolls-Royce Synthesis Program G519. Agreement is very satisfactory except in the high speed range - HP speed greater than 11000 r.p.m. and LP speed correspondingly above 6500 r.p.m.

For the acceleration, in the early part, while the HP compressor IGV's are stationary and closed, the relation follows closely to the steady-running relation.

However, when the IGV turning range is reached the HP shaft moves, relatively, ahead of the LP shaft. In the deceleration the inverse happens.

2.2 Shaft Speed Responses in Transients

These are given in Fig. 2 for the acceleration and Fig. 3 for the deceleration. The acceleration/deceleration fuel schedule used was CASC 211, factored by a multiple of 0.9. This guessed factor of 0.9 was used as the engine is expected to be about 10 to 15 per cent more fuel efficient than the Spey. However this improvement should not necessarily read directly proportionately across to the fuel schedule. It is seen that the speed responses are very rapid. At this stage, the nominal inertias of the two shafts have been used. It is realised that thermal effects slow transient speed (and thrust) responses down by typically 30 per cent (A. Yarker, 1973). To simulate this effect it is suggested that the nominal inertias be scaled up by a multiplier of 1.3.

2.3 Running Lines and Trajectories in Compressors

2.3.1 Fan

A movable "split" boundary between the flows going through the Inner and Outer Sections of the Fan has been used. This "split" position is quantified by a "fraction of split" or "factor of split". The nominal value of this is 1.0, corresponding to the design bypass ratio of 3.0. In the calculation procedure the appropriate fraction of split is calculated for each instant. A steady-running line and transient trajectories are shown in Fig. 4 and Fig. 5 for the Inner Fan and Outer Fan respectively. The important prediction to be noted is that in both sections of the Fan the transient trajectories scarcely deviate from the steady-running lines.

2.3.2 Intermediate Pressure (IP) Compressor

The predictions are shown in Fig. 6. It is predicted that there are very significant movements of trajectory away from the steady-running line both in the acceleration and in the deceleration. These movements coincide with the periods when the HP compressor is moving through its IGV turning range. In the acceleration, in the IGV turning range, the HP compressor is seeking a rapid increase in non-dimensional air flow entering it. The LP shaft is accelerating at only a modest rate, so the increase in non-dimensional air flow at the exit from the IP compressor is achieved only by a reduction of the IP compressor pressure ratio from the steady-running value at that $(N/\sqrt{T})$. In the deceleration, the opposite occurs and the trajectory rises significantly, towards surge. In the present case the prediction is that surge is avoided, but attention must be maintained on this situation.

2.3.3 H.P. Compressor

Here, the trajectories, as shown in Fig. 7 are much as expected. The fuel controller, following the CASC211 fuel schedule factored by 0.9, successfully prevents surge in the acceleration.

3. FUTURE WORK

(a) Extend the speed range of the simulation

(b) Improve the shaft speed inter-relationship at high speed end

(c) Study logic of the movable "split" boundary between flows through Inner Fan and Outer Fan

(d) Actions to relieve danger of surge in IP Compressor in deceleration:

  i) Effect of varying final nozzle size

  ii) Effect of varying inertias of shafts

  iii) Effect of Bleed Valve - to bypass duct - between IP and HP Compressors

  iv) Effect of revising the HP Compressor Bleed Schedule

(e) Carry out simulation at flight cruise condition at altitude.

4. CONCLUSIONS

Convergence has been achieved on a CMF simulation of the Tay Engine.

Steady-running and transient predictions have been achieved over a fair speed range, at sea level static conditions.

The major potential danger is of surge in the IP Compressor during a deceleration.

Table 1

Speed Range Covered in Simulation

Conditions - Sea level, static

	Starting Speeds		Finishing Speeds		Starting	Finishing
	NL	NH	NL	NH	$\dot{f}$	$\dot{f}$
Acceleration	2770	8580	8262	11792	0.10	0.74
Deceleration	7367	11483	2782	8588	0.60	0.10

(Speeds in r.p.m., fuel flows in kg/s)

FIG 1 RB183-03

SHAFT SPEED INTER-RELATION

SEA LEVEL INERTIAS AS NOMINAL

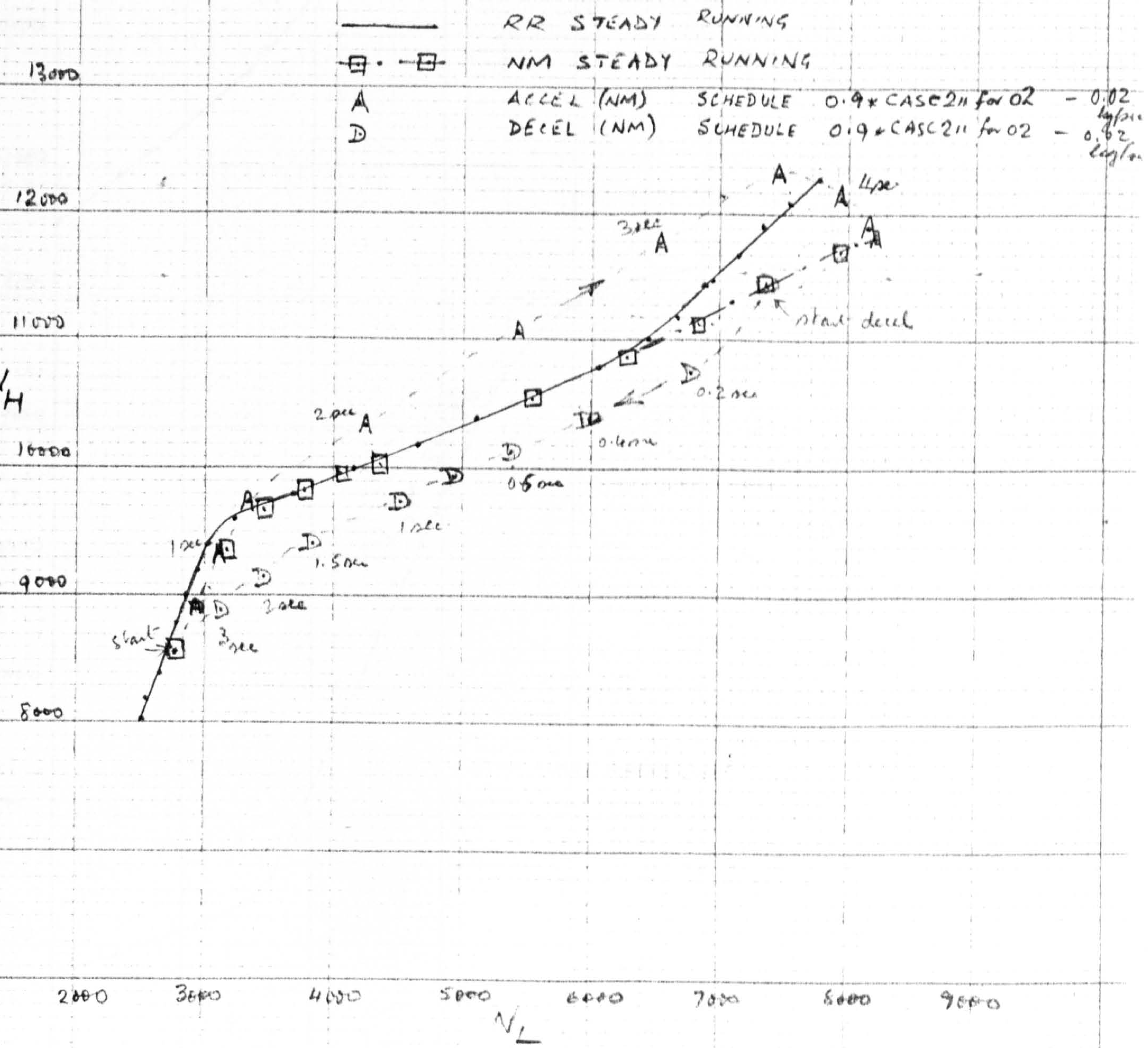


FIG 2 RB 183 — 03 ENGINE

SEA LEVEL ACCEL

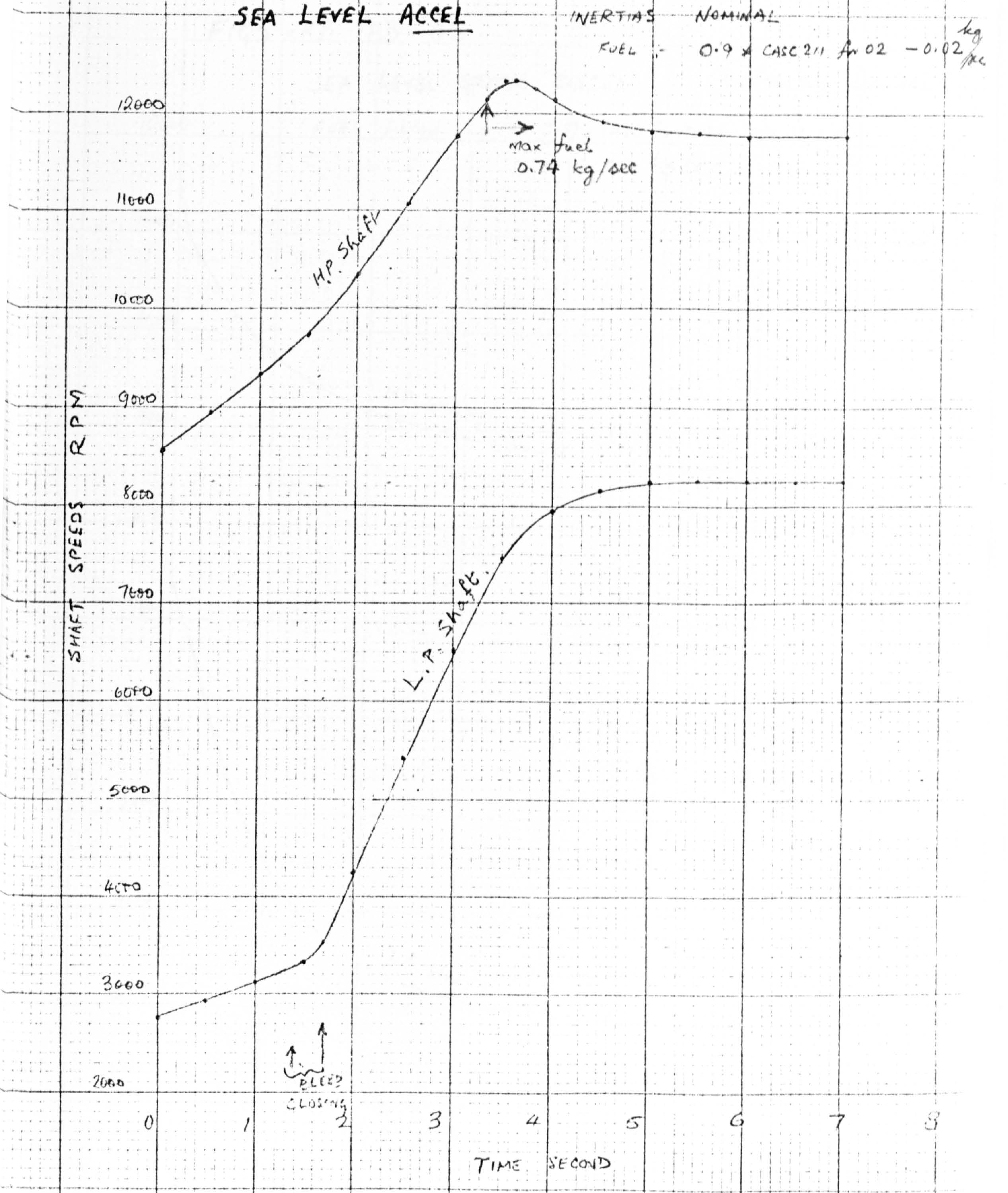


FIG.3 RB 183-03

SEA LEVEL STATIC DECEL INERTIAS NOMINAL

FUEL FLOW = 0.9 * CASC SCHEDULE FOR 02 ENG − 0.02 kg/sec

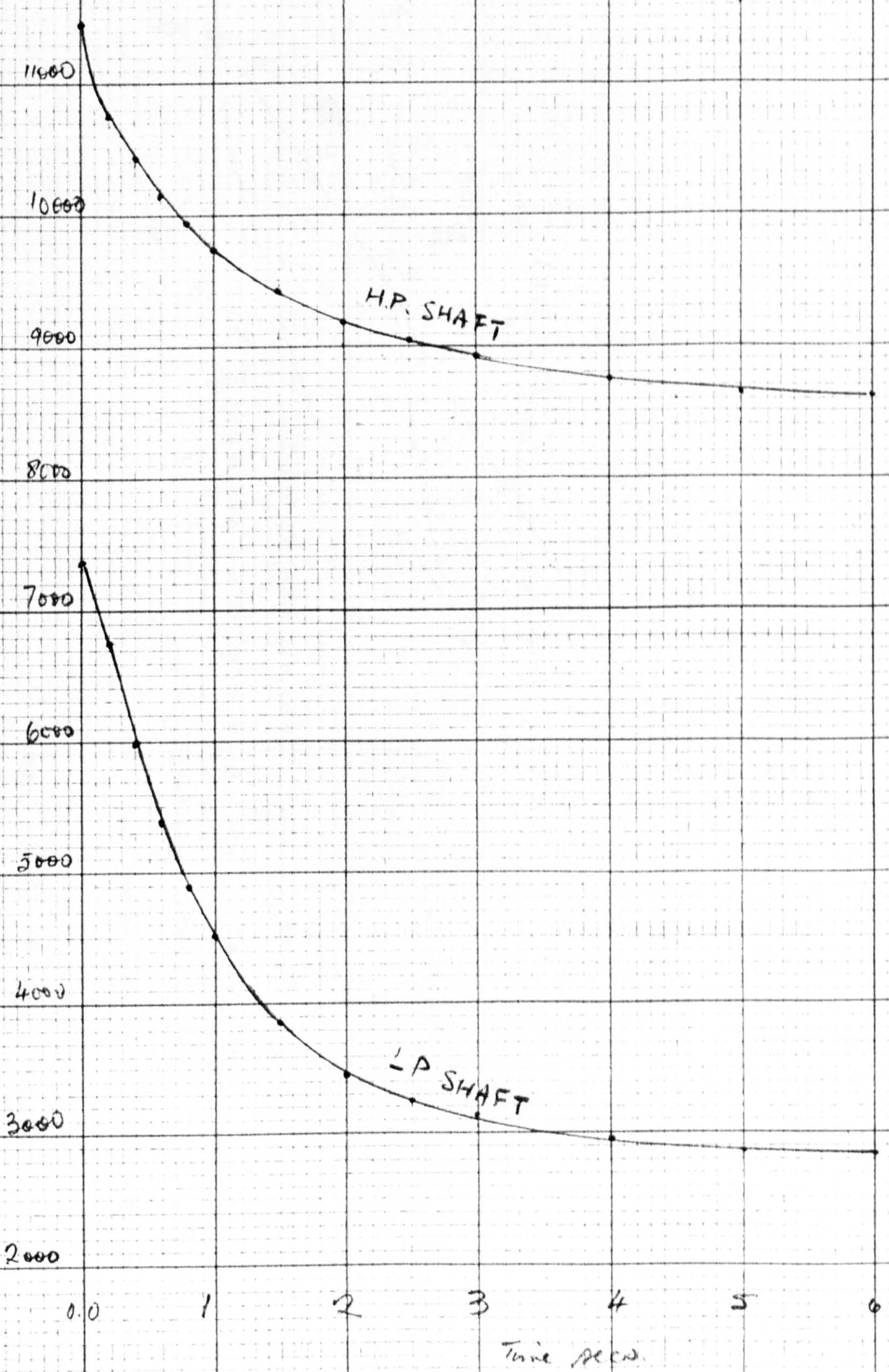


FIG. 4 03 ENGINE

INNER FAN SEA LEVEL STATIC

FIG. 5 - 03 OUTER FAN

SEA LEVEL STATIC

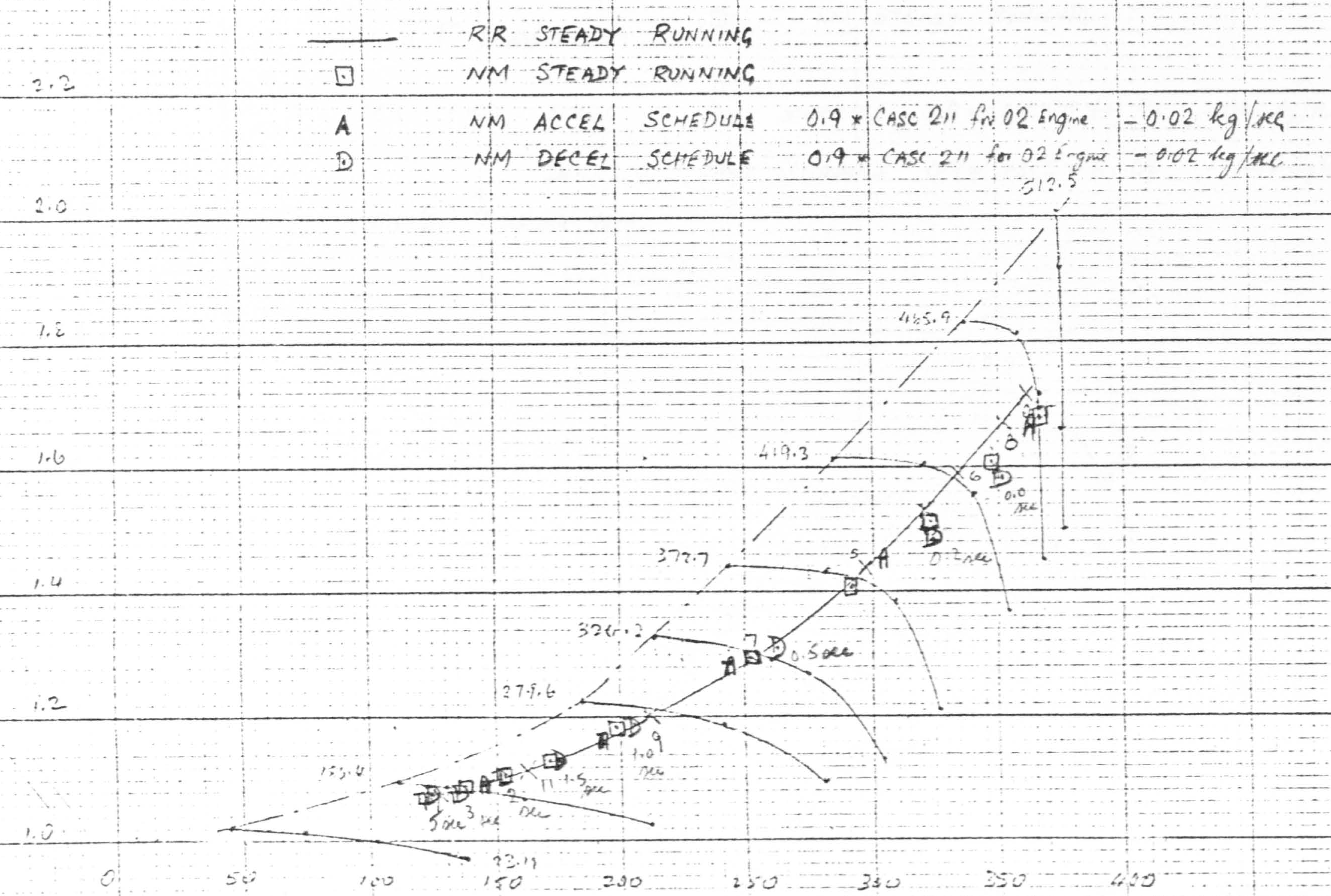


Fig 6 RB183 - 03 ENGINE

FIG. 7


SPEY RB 183-02 MK 555-15P
TAY RB 183-03
H.P. COMPRESSOR
MK555 -02 — R-R SYNTHESIS Q 538 (2 FEB 1983) -02 MK555
RB183 -03 TAY
X RR SYNTHESIS
⊡ NM STEADY RUNNING
A NM ACCEL
D NM DECEL
FUEL SCH. 0.9 × Accel CASC 2'' -0.02
Decel Sch.
P_3/P_2
$\frac{\dot{m}_2 \sqrt{T_2}}{P_2}$
8.0
7.0
6.0
5.0
4.0
3.0
2.0
1.0
10
15
20
25
30
35
40
45
50
55
60
644
650
663
635
614
595
584
576
567
560
549
500
447
300
0.10 sec
0.1 sec
0.0 sec
D 0.5 sec
3.2
25
26
27
23
22
21
32
33
35
36

Predicted Transient Response of Engine RB183-03 Tay - Altitude Cruise

TAY PROJECT - Report No. 3 4 May 1983

N.R.L. Maccallum

1. INTRODUCTION

This Report gives the predicted altitude cruise performance of the Tay (RB183-03) Engine. The altitude cruise conditions were 0.7 Mach number at 30,000 ft.

The prediction model used was the same as that used for the initial set of transient forecasts - Report No. 2 (29 April 1983) - which had been at sea level, static conditions. The shaft speed ranges covered in the transients in this Report are given in Table 1.

2. RESULTS

2.1 HP/LP Shaft Speed Relationships

The relationships for both steady-running and transients are given in Fig. 1. For the steady-running results, these should be compared with the steady-running relationships at sea level, static, previously obtained and shown in Fig. 1 of Report No. 2 (29 April 1983). It is seen that for equivalent $(N_H/\sqrt{T_{26}})$ conditions in the HP compressor, the LP shaft runs at a higher $(N_L/\sqrt{T_1})$ in the altitude cruise case. This is because in the altitude cruise at Mach number 0.7, the final nozzle is choked. The flow capacity (non-dimensional) of this choked final nozzle is greater than the flow capacity when the engine is stationary, with a reduced pressure ratio available across the final nozzle (taken simply as a convergent nozzle in this work). With the reduced final nozzle flow capacity in the static, sea level, case, the Fan on the LP shaft is meeting an increased flow resistance so its speed is reduced.

Considering the transient predictions, in most of the speed range covered the IGVs of the HP compressor are in their turning range. Hence there are rapid changes in air demand for the HP compressor for modest changes of $(N_H/\sqrt{T_{26}})$. This, as in the sea level static case (Report No. 2), causes the transient acceleration HP shaft speed to lead the LP shaft speed, when compared with steady-running. In the deceleration, as before, the HP shaft speed drops more rapidly than does the LP, compared to steady-running.

2.2 Shaft Speed Responses

The individual shaft speed responses, as functions of time, are shown in Fig. 2. Both acceleration and deceleration are fairly rapid. The deceleration is less rapid, however, than the deceleration at sea level. This is to be expected since at altitude the power transfers in the compressor and turbine are less than at sea level, due to the reduced air density at inlet, whereas the inertias of the shaft systems are still the same.

2.3 Running Lines and Trajectories in Compressors

2.3.1 Fan

Results are shown in Figs. 3 and 4 for the Inner and Outer Sections of the Fan respectively. Again, the movable "split" boundary between the two sections of the fan was used (see Report No. 2).

The steady-running and trajectory lines in both Fans in this altitude cruise case are lower than those predicted in the static, sea level, case. This is because, as explained in Para. 2.1 above, at Mach 0.7 (the altitude cruise case) the final nozzle, being choked, has a larger flow capacity than when the engine is static (at sea level). This higher capacity allows the working lines in the Fan to drop.

Considering the transient trajectories relative to the steady-running lines, the transient lines virtually coincide with the steady-running conditions. Thus no surge difficulties are expected in the Fan during transients.

2.3.2 Intermediate Pressure (IP) Compressor

Predictions are shown in Fig. 5. It is to be noted first that the steady-running line here is significantly higher than at sea level, static. This is because of the higher $(N_L/\sqrt{T_1})$ for that $(N_H/\sqrt{T_{26}})$, due to higher pressure ratios across final nozzle as discussed in Paras. 2.1 and 2.3.1 above.

Similarly to the situation at sea level (Report No. 2), in an acceleration the trajectory is predicted to drop significantly below the steady-running line, and in deceleration it rises above the steady-running line. The latter case of course presents the danger of surge. The predictions indicate that surge is just avoided in the present case.

2.3.3 HP Compressor

Trajectories are shown in Fig. 6. The predictions are in line with expectation. The CASC 211 schedule, factored by 0.9, is satisfactory in just avoiding surge in the acceleration.

3. CONCLUSIONS

The model, as it stood on 29 April 1983, has been applied to transients at 30,000 ft., 0.7 Mach number, which represents typical altitude cruise conditions.

The foreseen major area of potential difficulty is a near surge in the IP compressor in a deceleration.

Table 1 Speed Range Covered in Simulated Transients

Conditions:		
	Altitude	30,000 ft
	Mach number	0.7
	Intake Recovery	0.995

	Starting Speeds		Finishing Speeds		Starting	Finishing
	NL	NH	NL	NH	$\dot{f}$	$\dot{f}$
Acceleration	3540	8890	6720	10490	0.04	0.20
Deceleration	6722	10495	3540	8890	0.20	0.04

(Speeds in r.p.m., fuel flows in kg/s)

FIG 1 RB 183-03

30,000 FT 0.7 MACH NO 0.995 RECOVERY

INTERRELATION OF SHAFT SPEEDS INERTIAS AS NOMINAL

⊡ NM STEADY RUNNING

A NM ACCEL FUEL 0.9 * FUEL SCHEDULE CASE 211 for 02 Engine

D NM DECEL FUEL 0.19 * FUEL SCHEDULE CASE 211 for 02 Engine

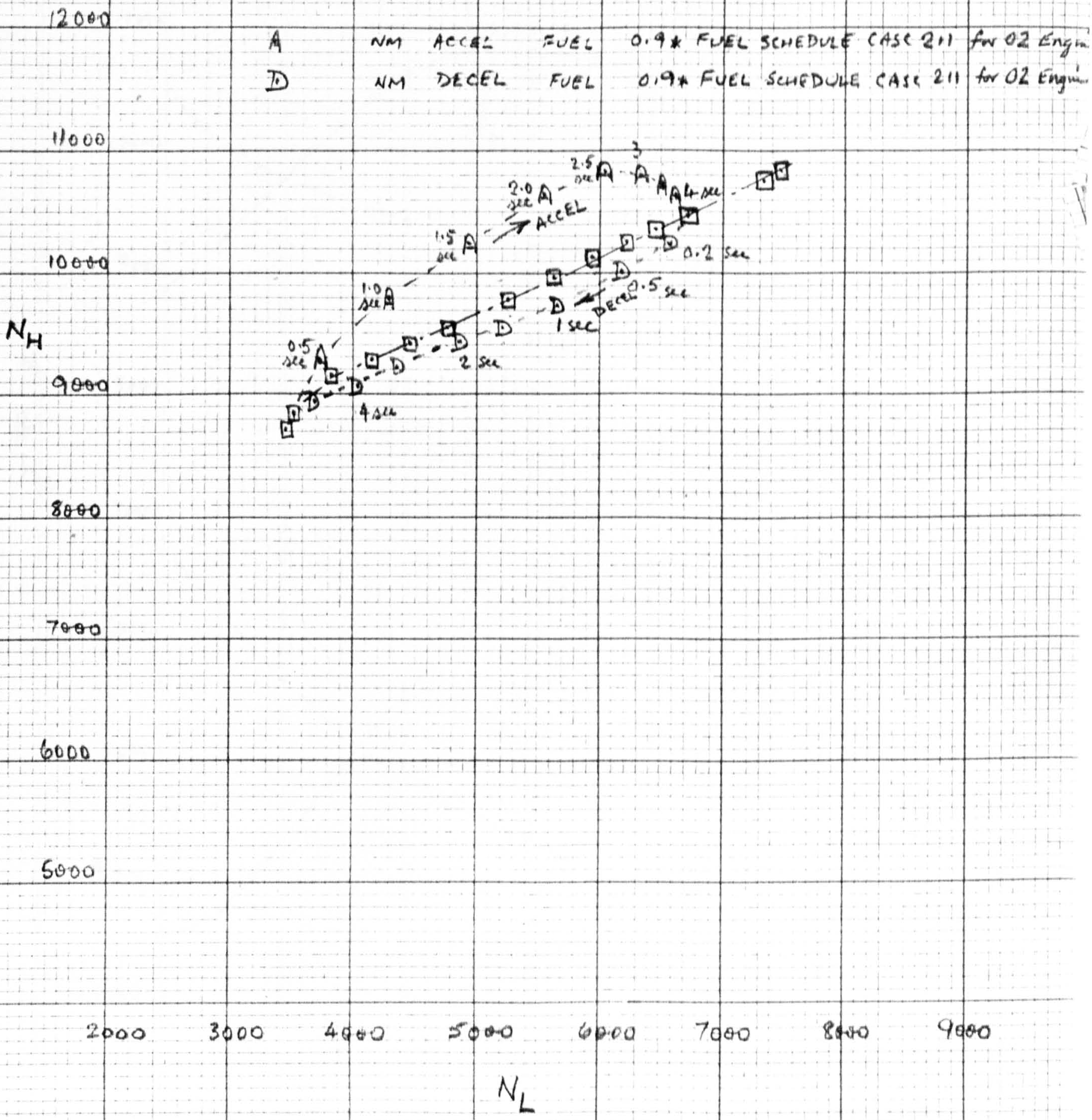


FIG 2 RB 183 - 03

30,000 ft 0.70 MACH No 0.995 Recovery

Nominal Shaft Inertias

TRANSIENT SPEED RESPONSES

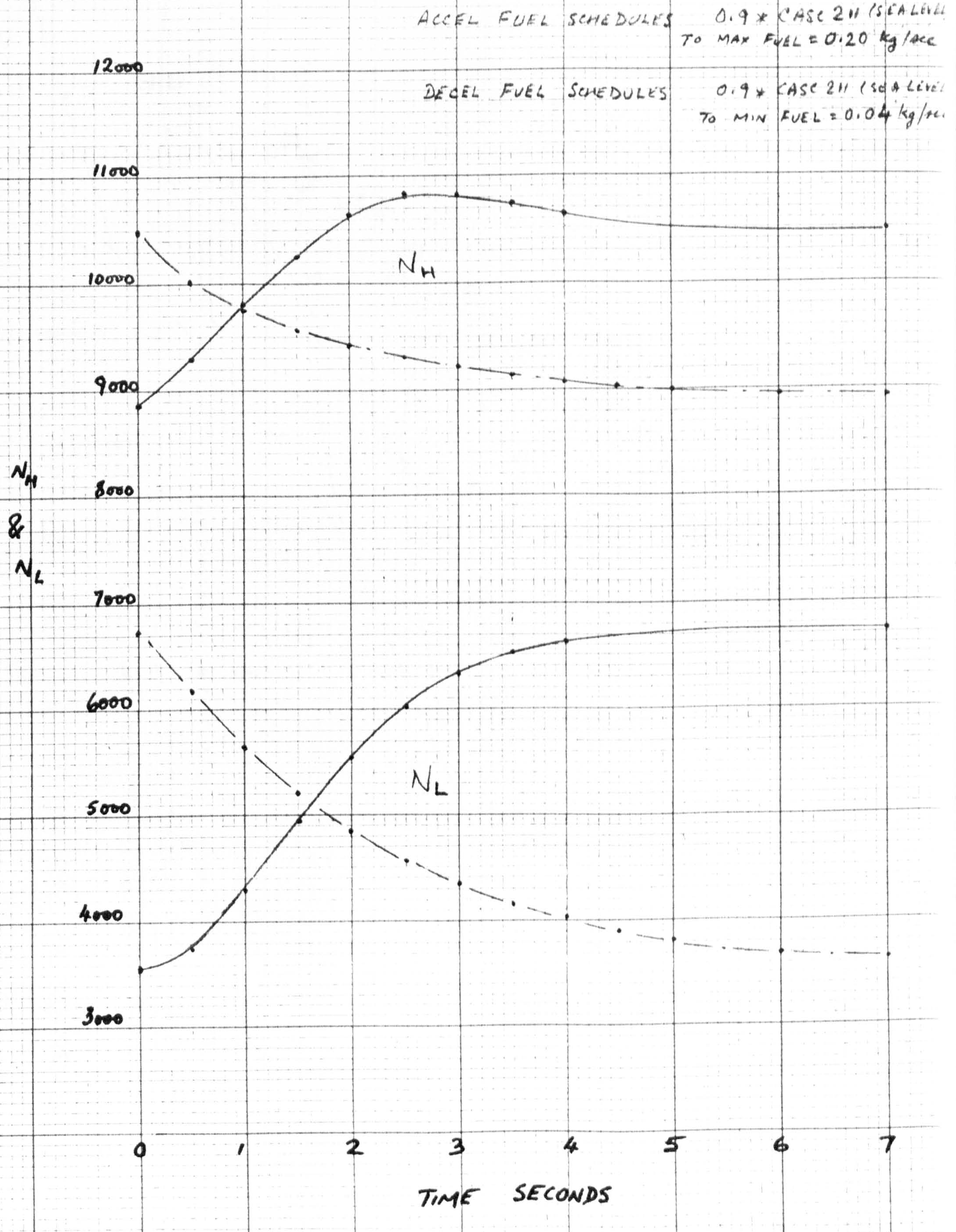


Fig 3 03 ENGINE
INNER FAN
30,000 FT 0.7 MACH NO
RR SYNTHESIS SEA LEVEL STATIC
NM STEADY - RUNNING
A NM ACCEL FUEL SCHED. 0.9 * CASE 211
D NM DECEL FUEL SCHED 0.9 * CASE 211
465.9
419.3
372.7
326.2
279.6
186.4
X13
1.8
1.7
1.6
1.5
1.4
1.3
1.2
1.1
10
20
30
40
50
60
70
80
90
100
110
120
130
140

Fig. 4 - 05 OUTER FAN

30,000 FT 0.7 MACH NO

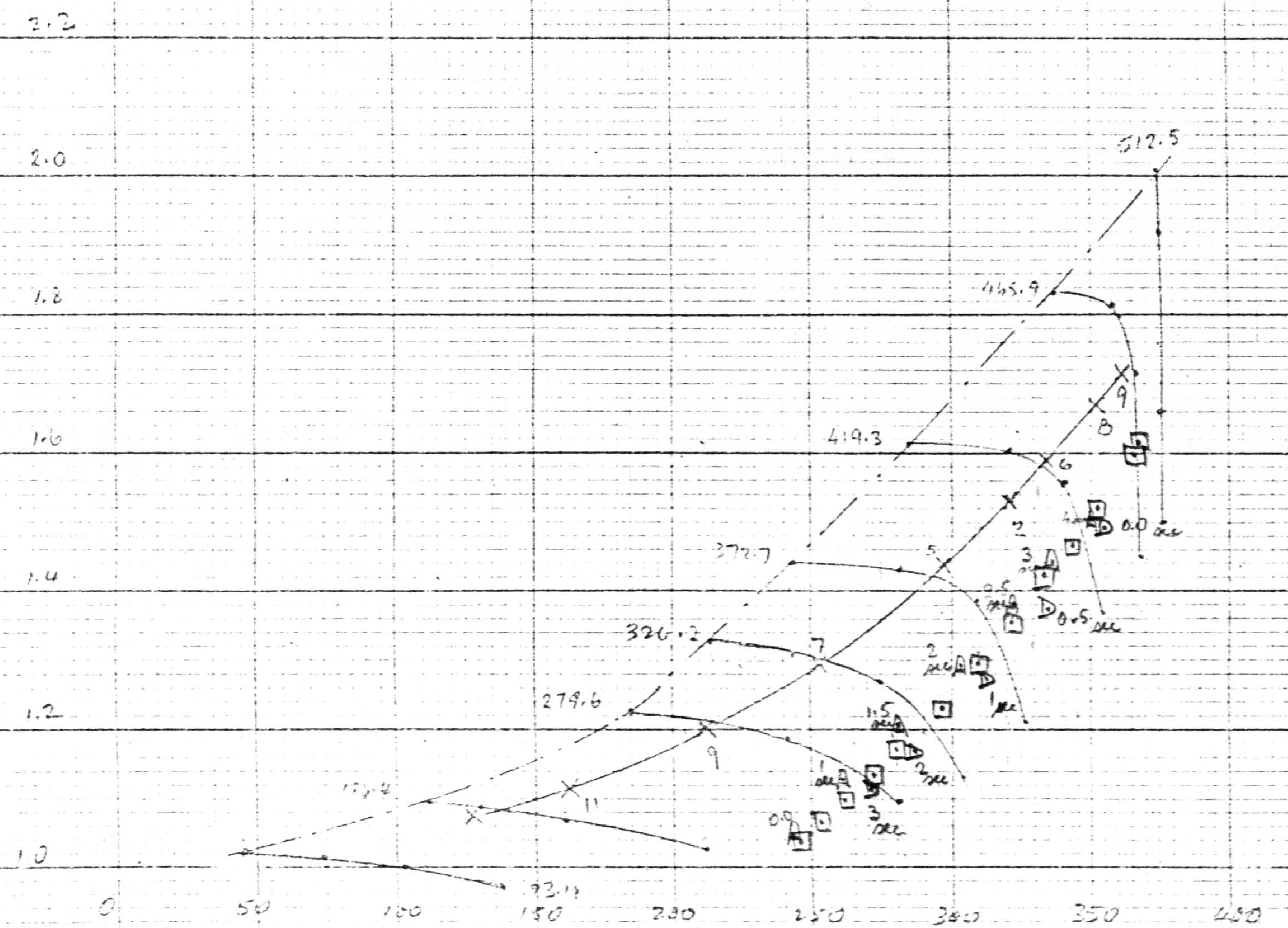


Fig 5 - 03

I P Compressor

30,000 ft   0.7 Mach No   0.995 Recovery

Nominal Inertias

⊡ NM Steady Run

A Accel SCHED 0.9 * CASC 2H

D Decel SCHED 0.9 * CASC 2H

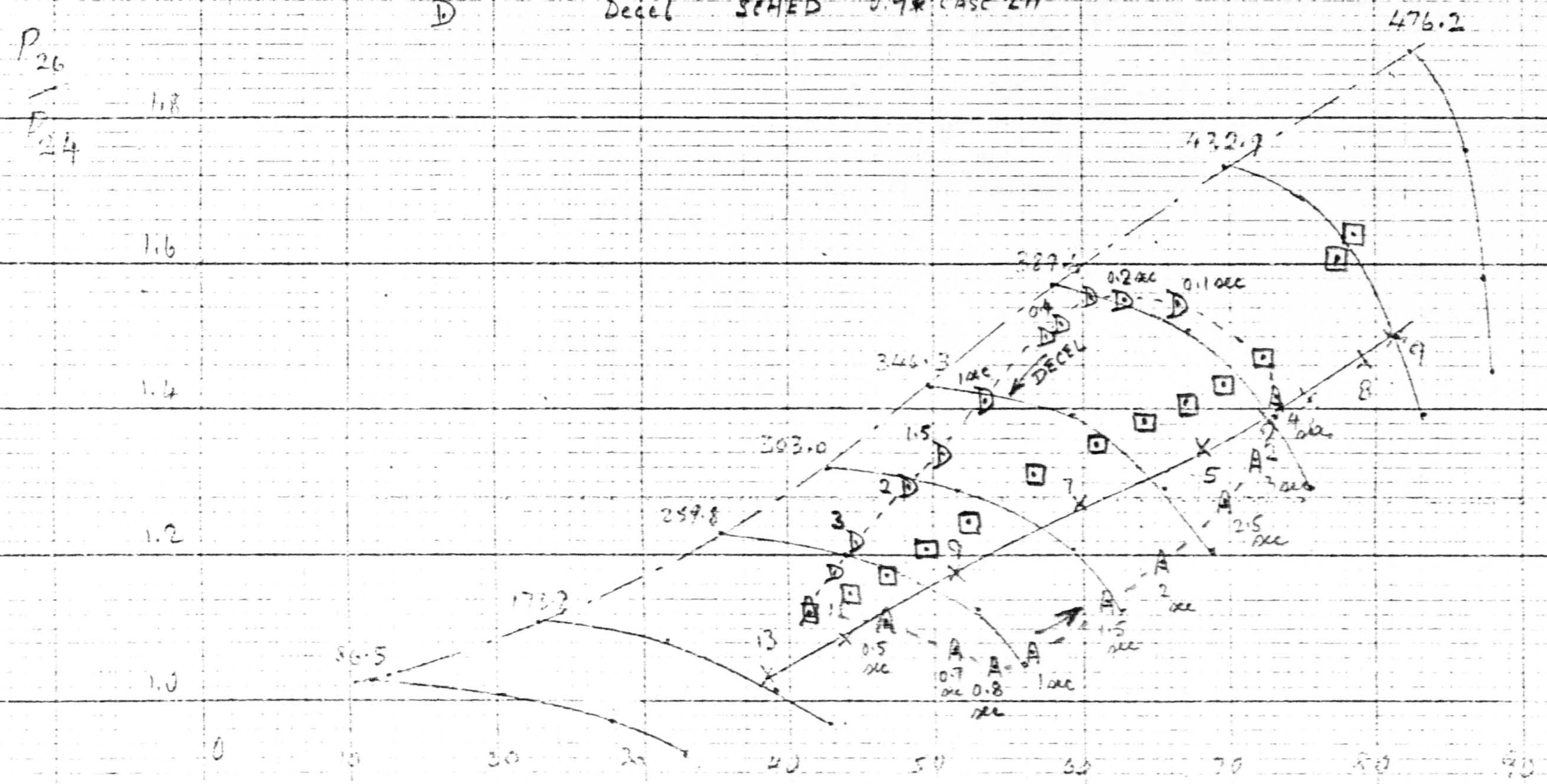
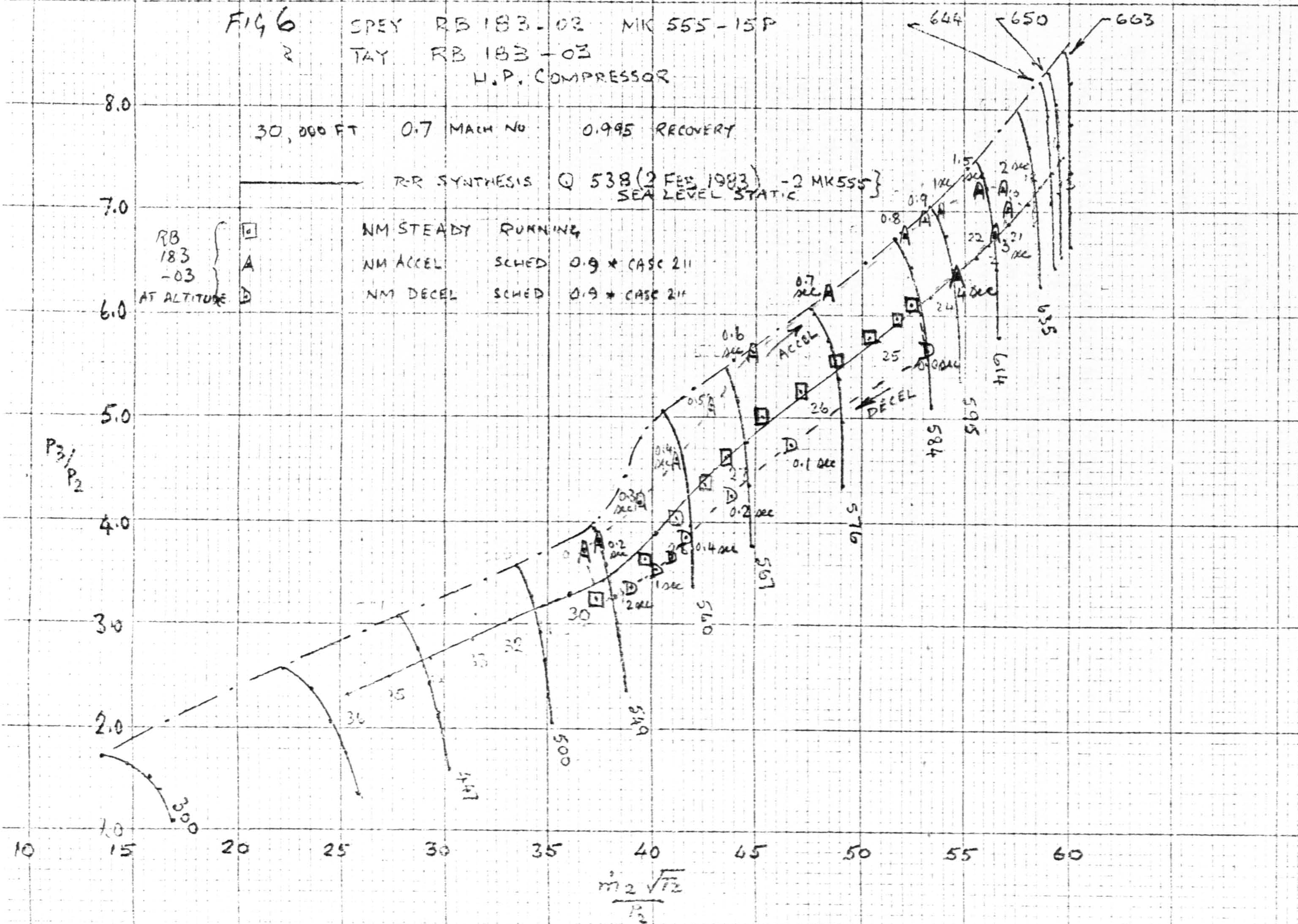


FIG 6


SPEY RB 183-02 MK 555-15P
& TAY RB 183-03
H.P. COMPRESSOR
30,000 FT 0.7 MACH No 0.995 RECOVERY
R-R SYNTHESIS Q 538 (2 FEB 1983) -2 MK555 } SEA LEVEL STATIC
RB 183 -03 AT ALTITUDE
NM STEADY RUNNING
NM ACCEL SCHED 0.9 * CASE 211
NM DECEL SCHED 0.9 * CASE 211
ACCEL
DECEL
644
650
663
635
614
595
584
576
567
560
549
500
447
300
P_3/P_2
$\frac{\dot{m}_2 \sqrt{T_2}}{P_2}$

Improved Modelling of Transient Response of Engine RB183-03 Tay - with IP/HP Compressor Bleed

TAY PROJECT - Report No. 4 21 May 1983

N.R.L. Maccallum

1. INTRODUCTION

Maccallum's transient program as at 29 April had not been matching, when set to produce steady-running results, with the high-speed shaft interspeed relationships as given by the Rolls Royce Synthesis Program G519. This has now been remedied and the transient program has been used to predict deceleration transients with two alternative deceleration schedules and a range of IP Bleed ratios.

2. LINE-UP OF MACCALLUM'S TRANSIENT PROGRAM

The latest version of Rolls-Royce Synthesis Program G519 was examined. It was found that several of the factors on efficiencies and capacities had now been assigned values slightly different from those given to Maccallum in February. The factor change which had caused the greatest effect had been on the capacity of the HP turbine. When these factor changes had been incorporated and also allowances made for the return of cooling air into the HP turbine, and for Reynolds number effects in the HP and IP compressors, the agreement on stabilised shaft speed interrelationships between the predictions of G519 and Maccallum's program became very satisfactory (Fig. 1). The stabilised working points in the compressors predicted by Maccallum agree closely with those given by G519 - although it should be noted that the agreement before the latest adjustments to the program had been generally satisfactory. The predicted steady-running lines are shown in Figures 2 to 5 inclusive.

3. PREDICTIONS FOR DECELERATIONS - SEA LEVEL STATIC

The basic transient fuel schedules remain those of the CASC 211 as used for the Spey Engine RB183-02 Mk 555. For the present work the transient schedules are factored by some constant, as selected. The basic schedules are shown on Fig. 6 - also shown are the steady-running lines for the RB183-02 Spey Engine as predicted by Rolls Royce Q538 and by Maccallum (Report No. 1), and the latest prediction by Maccallum for the RB183-03 Tay Engine.

3.1/over

3.1 Speed Responses in Decelerations

To examine a less severe deceleration than that covered in Report No. 2, a scaling multiplier of 0.94 on the Deceleration Schedule has been used. The predicted speed responses, with zero IP/HP Compressor Bleed and with 15 per cent Bleed, are shown in Fig. 7. Comparing this prediction with zero bleed with that given in Report No. 2 with zero bleed, but Fuel Schedule Factor of 0.90, the time for speed decay is lengthened only by 3 to 5 per cent and the effect on the trajectory in the IP Compressor (Fig.8&9) is small. It is suggested that a significantly less severe deceleration schedule be tried e.g. with multiplying factor of 1.0 or even 1.1. Results are also shown with a 15 per cent IP/HP Bleed, when the shaft deceleration rates are only slightly altered.

Considering the general deceleration rates, it seems that the decelerations are all, with the schedules tested to date, extremely rapid and could afford to be slowed down somewhat.

4. Trajectories in IP Compressor in Deceleration

Trajectories are shown (Fig.8) for decelerations with zero and 15 per cent IP/HP Bleed. The fuel schedule factor is 0.94. Bleeds of 5 per cent and 10 per cent were also tested. The effects on the usage of surge margin in the deceleration at 0.3 sec, when the trajectory is nearest to surge, are given in Table:

IP/HP Compressor Bleed	Surge Margin Used (at 0.3 sec)
zero	75-80 per cent
5 per cent	60 per cent
10 per cent	45 per cent
15 per cent	20 per cent

("Surge Margin" used as reference is the value from zero bleed steady-running to surge line).

It is to be expected from the above that with this fuel schedule a bleed of about 5 to 10 per cent will be more than adequate to cope with contingencies.

5. SUGGESTED ACTIONS

(i) Compare thrust responses in deceleration of Spey Engine RB183-02 and Tay Engine RB183-03 with range of fuel schedules.

(ii) Tay Engine RB193-03 with fuel schedule to match Spey: study decelerations, with inertias factored 1.3 to compensate for thermal effects.

LOGIC RB 183 -03

FIG 1 O3 ENGINE

SHAFT SPEED INTER RELATION

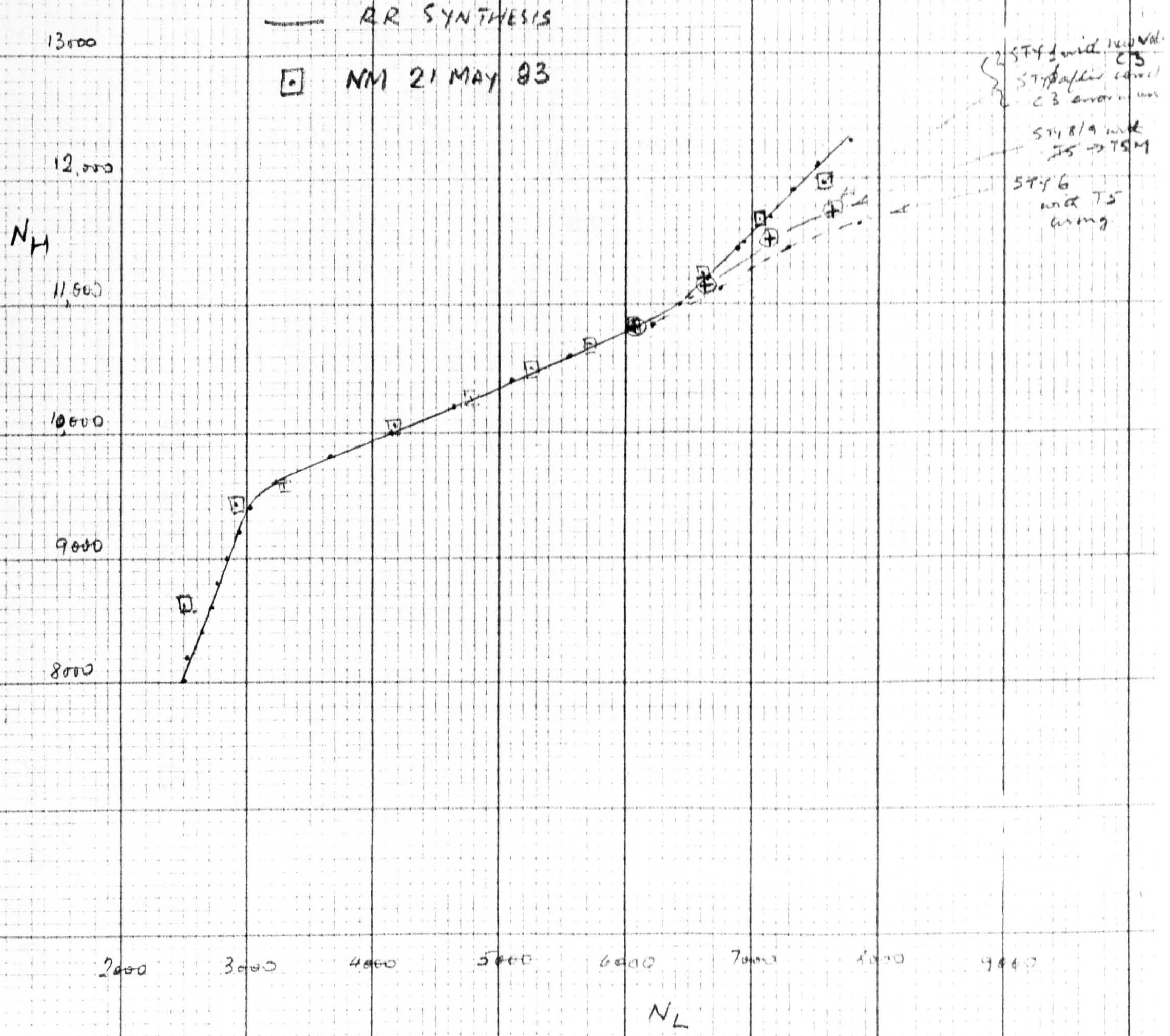


Fig 2 03 ENGINE
INNER FAN

FIG. 3 - 05 OUTER FAN

Δ STEADY Q3 ENGINE NM 21 May 83

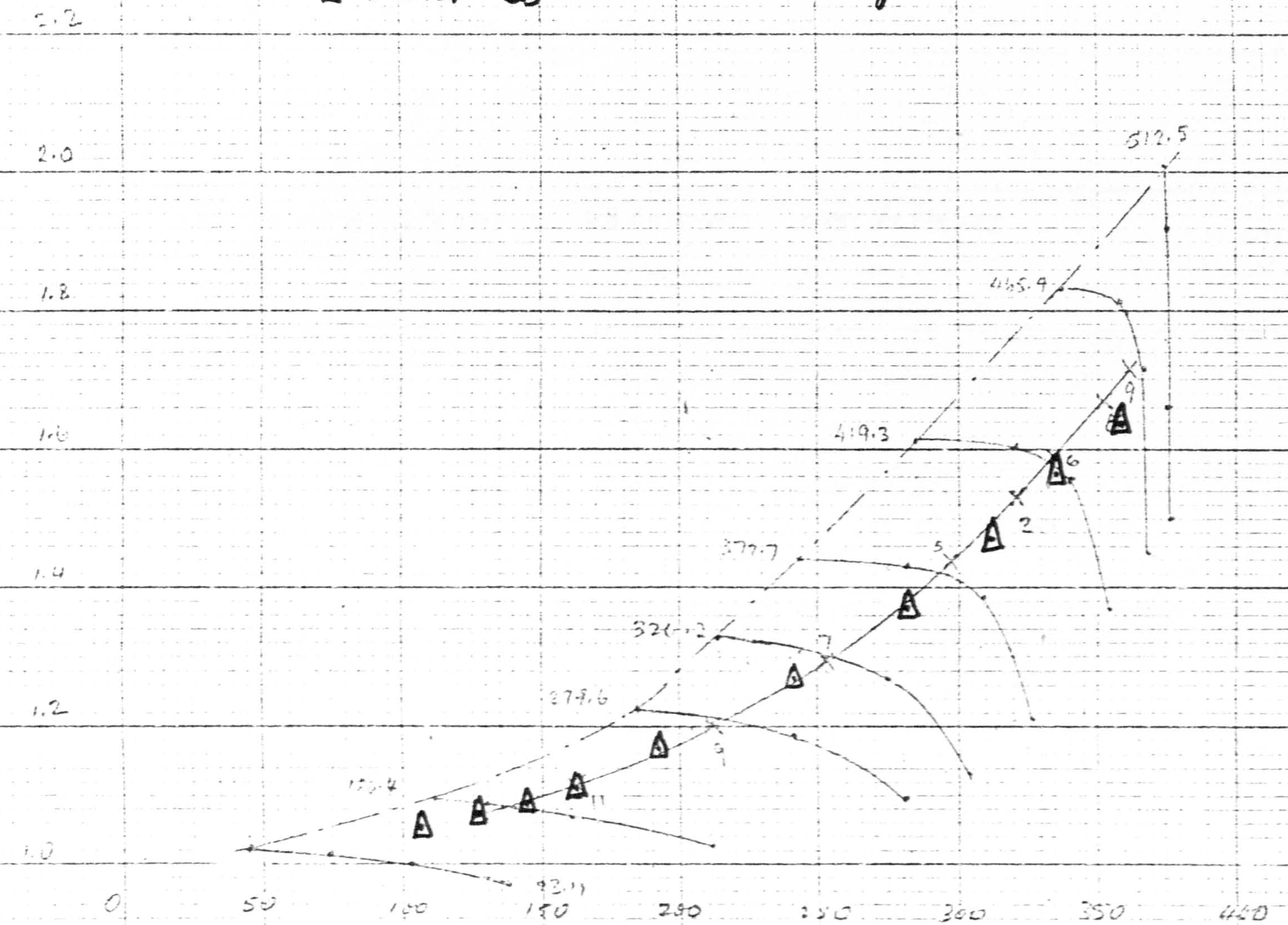


FIG 4 - 03 ENGINE

I P Compressor

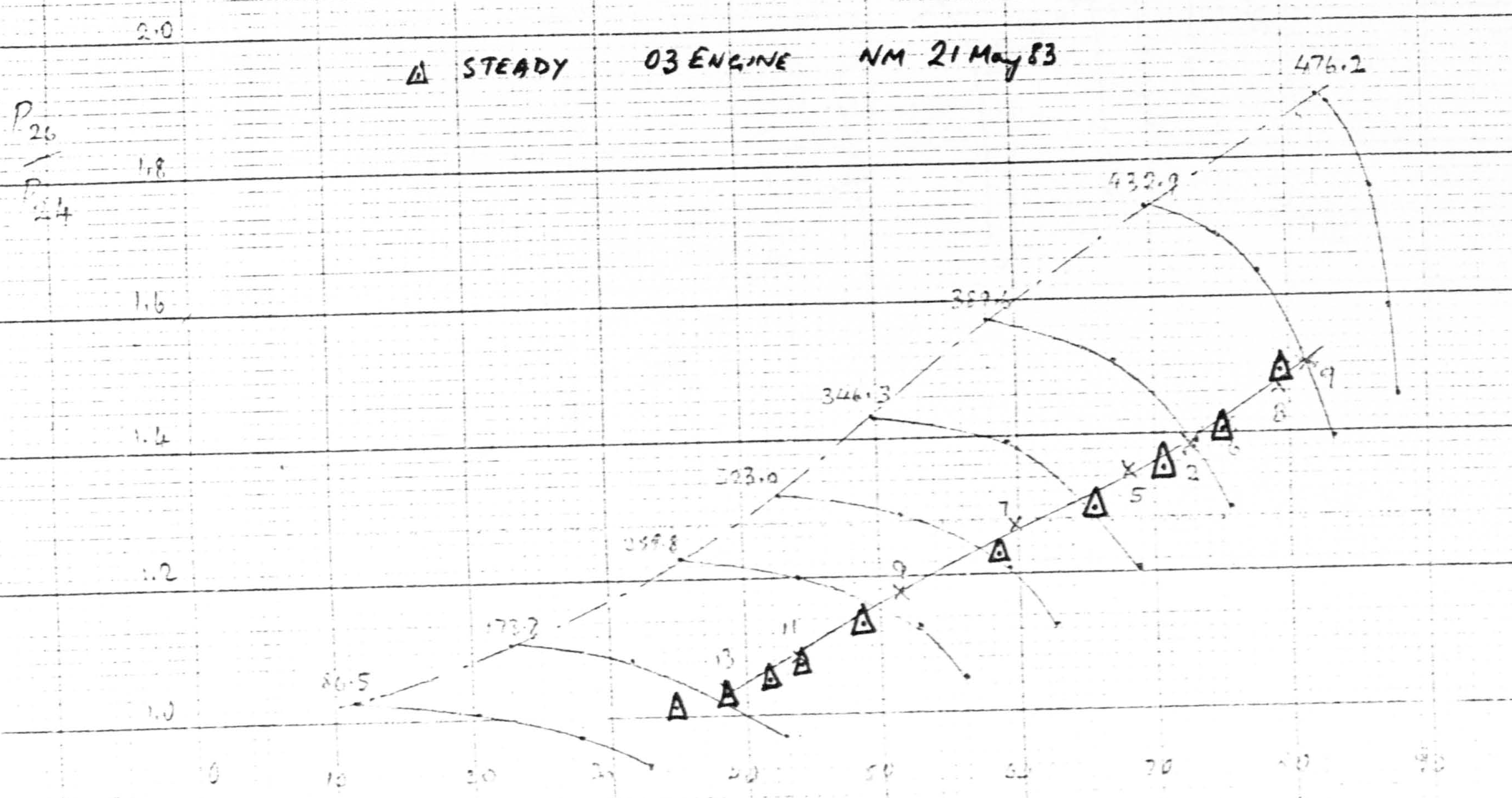
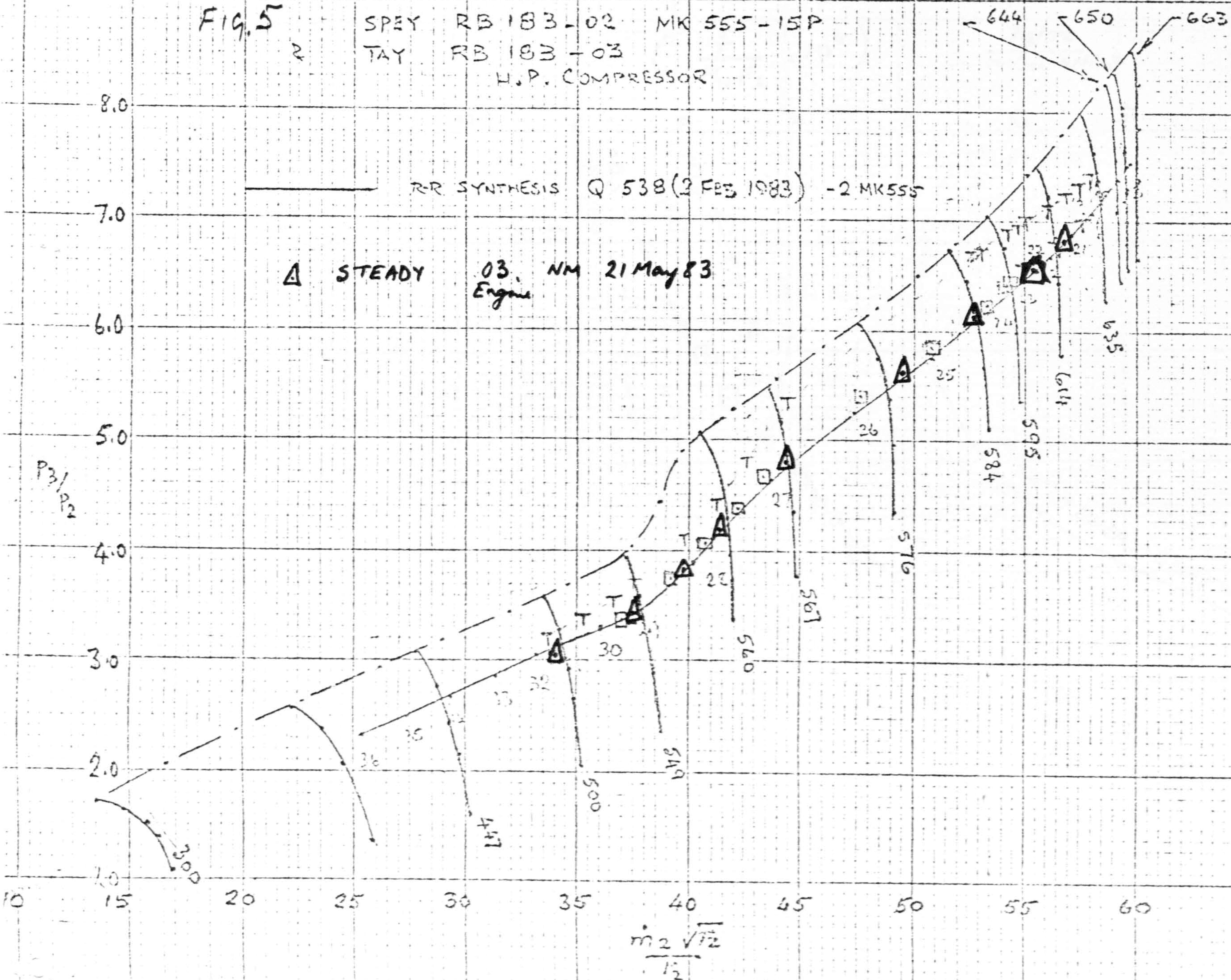


Fig. 5



2
SPEY RB 183-02 MK 555-15P
TAY RB 183-03
H.P. COMPRESSOR
R-R SYNTHESIS Q 538 (2 FEB 1983) -2 MK555
STEADY 03. Engine NM 21 May 83
644
650
663
635
614
595
584
576
567
560
549
500
447
300
8.0
7.0
6.0
5.0
4.0
3.0
2.0
1.0
10
15
20
25
30
35
40
45
50
55
60
P_3/P_2
$\frac{\dot{m}_2\sqrt{T_2}}{P_2}$

FIG 6


ACCELERATION FUEL SCHEDULE
C.A.S.C. 211 (20.2.74)
20
18
16
14
12
10
8
6
4
2
kg/sec / rev/sec kN/m²
(f / N_H P_2 x 10^6)
CASC 211 LUCAS SCHEDULE. ACCEL
STEADY RUNNING Q 538
CASC 211 DECEL
-02 Engine
-0.3 Engine
21 May 83
1
2
3
4
5
6
7
8
P3/P2

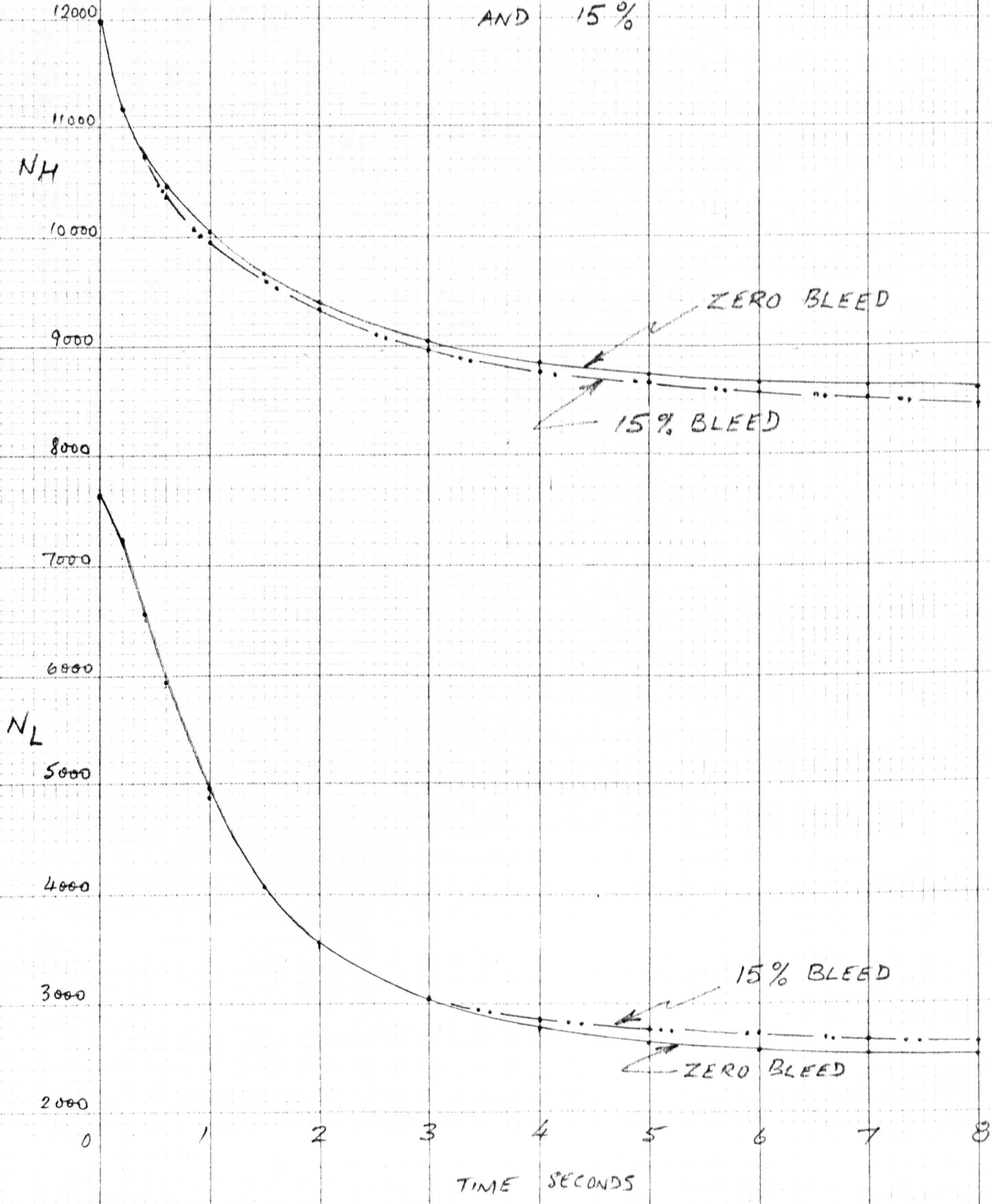
FIG 7 RB 183-03

SEA LEVEL, STATIC

DECELS FUEL = 0.94 × CASC 211

INERTIAS NOMINAL

IPC BLEED ZERO AND 15%



21 MAY 83

FIG 8 - 03

SEA LEVEL ; STATIC

DECELS FROM $N_L = 7635$

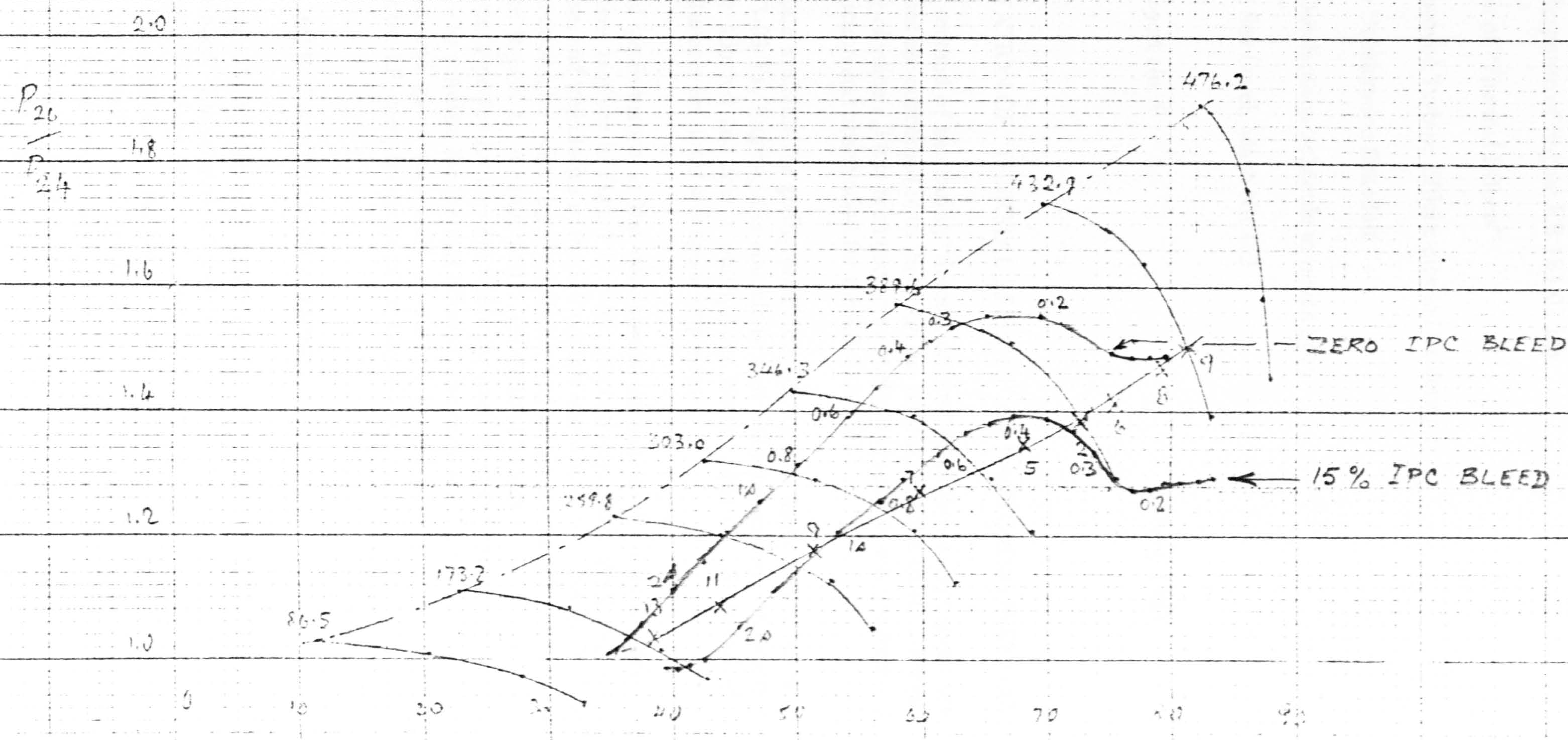
$N_H = 11995$

I P Compressor

NOMINAL INERTIAS

FUEL = 0.94 * CASC 211

TO FUEL MIN = 0.10 kg/sec



21 May 1983

Predicted Transient Response of Engine RB 183-03 Tay

1. Representation of "Thermal Effects"
2. Effects of Improved Turbine Efficiencies in Deceleration
3. Alterations to Deceleration Schedule
4. Selection of Deceleration Schedule to match Spey
5. Predictions at Sea Level, Mach No. 0.2

TAY PROJECT – Report No. 5 28 May 1983

N.R.L. Maccallum

1. REPRESENTATION OF "THERMAL EFFECTS" BY FACTORING INERTIAS

An empirical method of simulating "thermal effects" in so far as shaft speed and thrust responses are concerned is to factor up the inertia(s) of the shaft system(s). A multiplying of value 1.3 has been used previously to give satisfactory agreement between prediction and observation for the RB 211-524.

In the time scale available for giving predictions of the real transient performance of the RB 183-03 Tay Engine, it is considered that a similar practice of factoring the inertia be adopted and that a multiplying factor value of 1.3 should give reasonable estimates for thrust and speed responses.

The predicted performance in the deceleration at sea level, static conditions, nominal inertias, is given in Figs. 1 to 3 for, respectively, shaft speed responses, thrust response, and trajectories in the I.P. compressor (the critical design feature) – similar predictions, but with slightly different fuel schedules, have been given in Figs. 7 to 9 of Report 4. A range of I.P. compressor bleeds is considered. The predictions were repeated for the case where the shaft inertias were scaled up by a factor of 1.3 – to simulate the thermal effects. The results are given in Figs. 4 to 6. The thrust and speed responses are slowed by about 30 per cent, but the trajectories in the I.P. compressor are not noticeably different.

2. EFFECT OF IMPROVED TURBINE EFFICIENCIES

There was a possibility that in a deceleration the efficiencies of the Turbines might be enhanced. The possible effect this might have on the transient behaviour has been examined – Figs. 7 to 9. Turbine efficiency improvements of (+ 0.01) and (+0.03) were considered. The effects on the speed and thrust responses in the deceleration were small – shaft speeds and thrust dropped a little less rapidly. Considering the deceleration trajectories in the I.P. compressor (Fig. 9), the turbine efficiency improvements allowed a slight easing of the trajectories. This was primarily due to the slightly slower shaft speed decelerations.

3. ALTERATIONS TO DECELERATION SCHEDULES

In previous Report No.4 it had been suggested that a noticeably less severe than (0.9 x CASC 211) deceleration schedule be examined. A deceleration schedule of (1.1 x CASC 211) has therefore been studied. Results are given in Figs. 10 to 12. With this schedule the deceleration trajectory in the I.P. compressor was greatly eased, only a maximum of about 60 per cent of the available surge margin being used (compared with 75 to 80 per cent of margin when using schedule (0.94 x CASC 211)). The time to 25 per cent maximum thrust is however lengthened from about 1.7 s to 2.5 s (more description in paragraph 4 below).

4. DECELERATION SCHEDULES FOR TAY ENGINE TO MATCH SPEY ENGINE DECELERATION

The deceleration rate of the Tay Engine, when using 0.9 x CASC 211, is very rapid. This imposes a severe strain on I.P. compressor surge margin.

Need Tay Engine decelerate as rapidly as the Spey Engine? The Spey (Mk 555) Engine decelerations, when using the CASC 211, are predicted – Figs. 13 to 15. The Spey Engine decelerates very rapidly, dropping to 25 per cent thrust in about 1.25 s (sea level, static). For the Tay Engine, with (0.9 x CASC 211), Fig. 2, the corresponding time is about 1.45 s (and 2.5 s with (1.1 x CASC 211), Fig. 11).

5. CHANGE OF PREDICTIONS AT SEA LEVEL, MACH NO. 0.2 cf SEA LEVEL, STATIC

Transient predictions for the Tay, at sea level, Mach No. 0.2, using Schedule (0.9 x CASC 211) have been carried out. Results are shown in Figs. 16 to 18.

For practical purposes, these results are virtually identical to those at the sea level, static case – certainly for the critical predictions of surge margin usage in the I.P. compressor during decelerations. (Fig. 18 cf. Fig. 6).

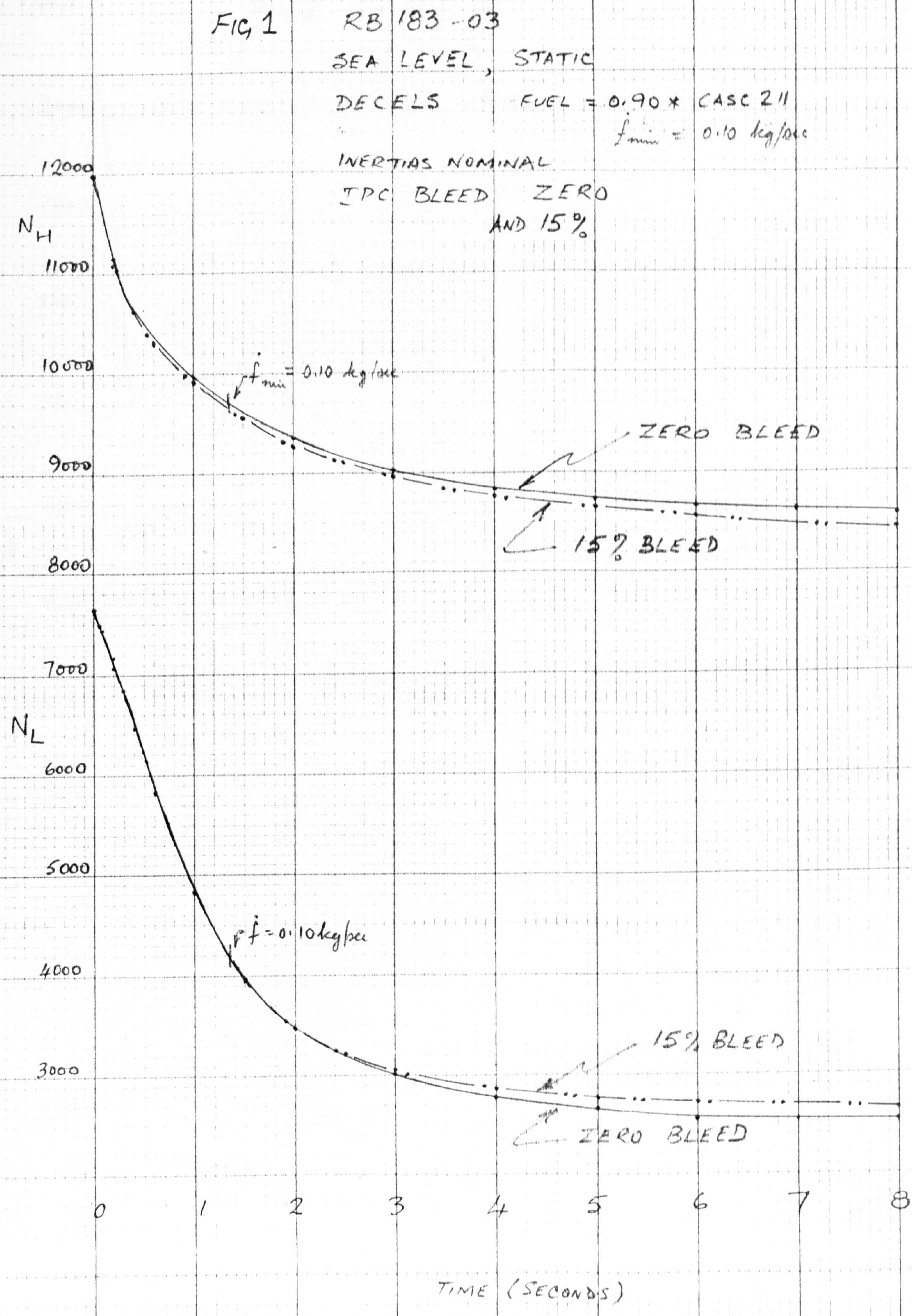


FIG 1
RB 183-03
SEA LEVEL, STATIC
DECELS
FUEL = 0.90 * CASC 211
$\dot{f}_{min}$ = 0.10 kg/sec
INERTIAS NOMINAL
IPC BLEED ZERO
AND 15%
N_H
N_L
12000
11000
10000
9000
8000
7000
6000
5000
4000
3000
$\dot{f}_{min}$ = 0.10 kg/sec
ZERO BLEED
15% BLEED
$\dot{f}$ = 0.10 kg/sec
15% BLEED
ZERO BLEED
0
1
2
3
4
5
6
7
8
TIME (SECONDS)

21 MAY 83

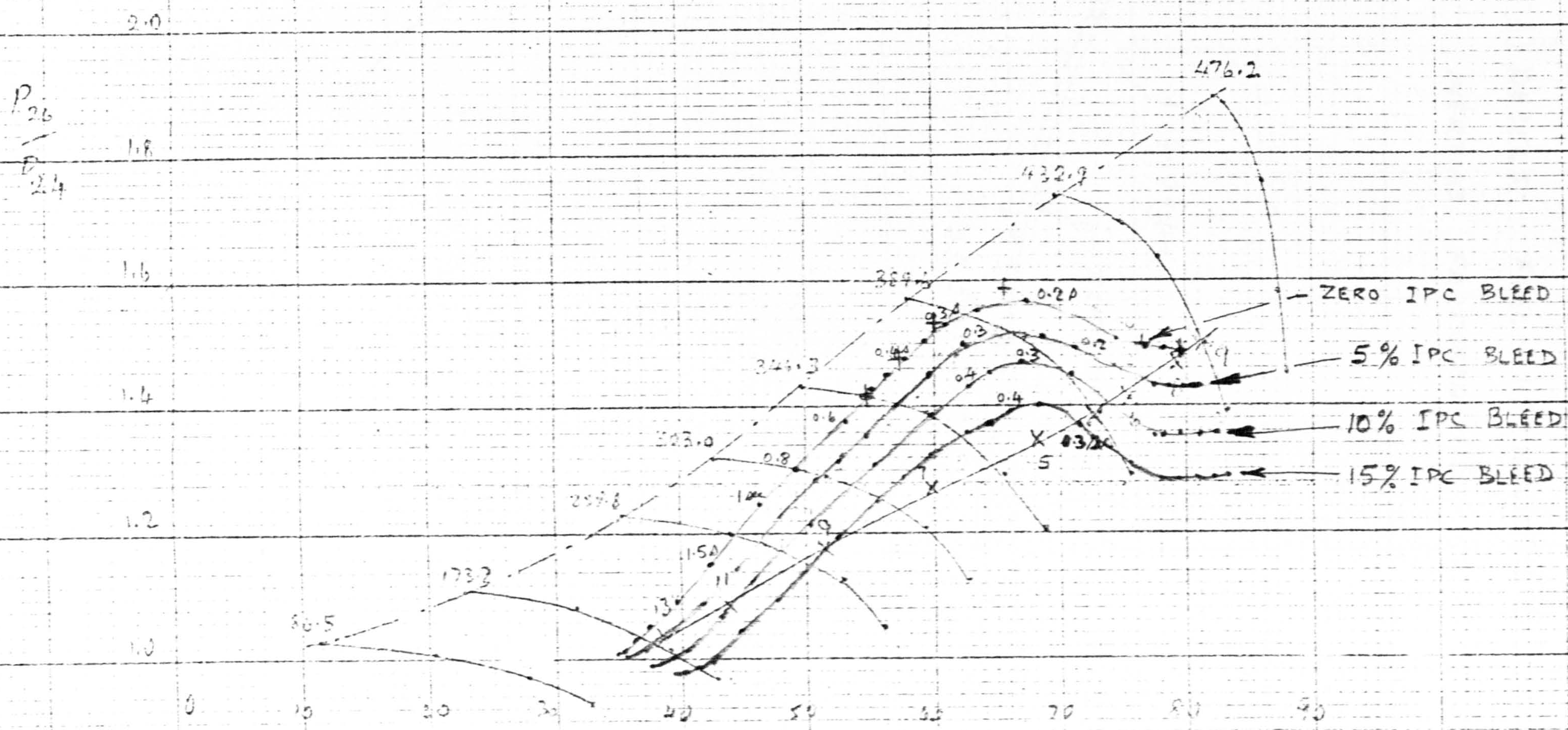
Fig 2 RB 183-03
SEA LEVEL STATIC
DECEL, FUEL = 0.90 * CASC211
TO $\dot{f}_{min}$ = 0.10 kg/sec
INERTIAS NOMINAL
X_{NET}
13000
12000
11000
10000
9000
8000
7000
6000
5000
4000
3000
2000
1000
0
$\dot{f}$ = 0.10 kg/sec
0
1
2
3
4
5
6
7
8
TIME (SECONDS)

FIG 3 — 03 SEA LEVEL, STATIC DECELS FROM $N_L = 7635$

$N_H = 11995$

I P Compressor NOMINAL INERTIAS $FUEL = 0.90 * CASC\ 211$

$To\ FUEL_{MIN} = 0.10\ kg/sec$



21 MAY 8.

FIG 4 RB 183-03

SEA LEVEL, STATIC

DECELS FUEL = 0.90 * CASC 211

INERTIAS FACTORED 1.3

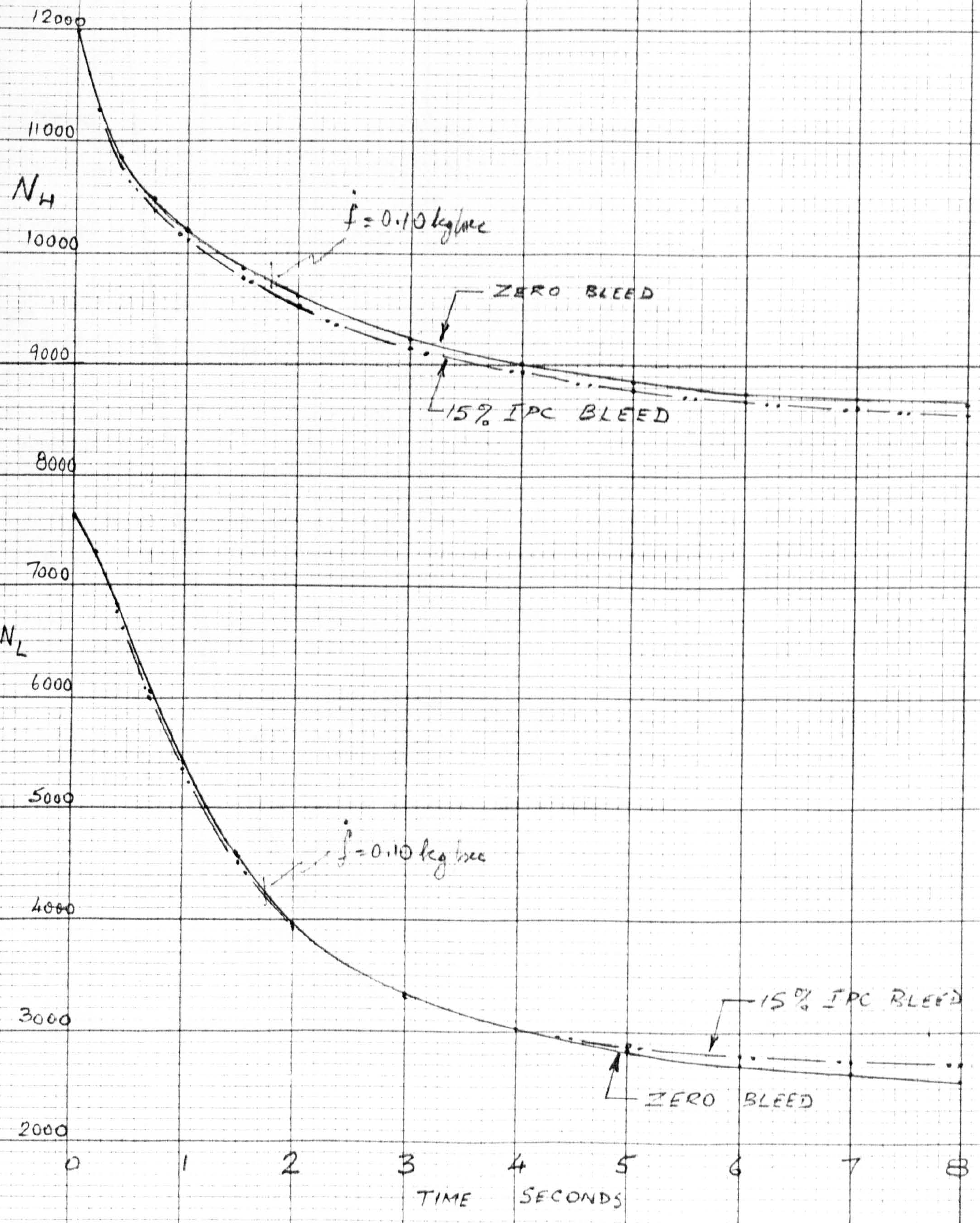


Fig 5
RB183-03
SEA LEVEL, STATIC
DECEL FUEL = 0.90 * CASC 211
TO $\dot{f}_{MIN}$ = 0.10 kg/sec
INERTIAS FACTORED 1.3
ZERO BLEED
$\dot{f}$ = 0.10 kg/sec
15% IPC BLEED
X_{NET} (LBF)
13000
12000
11000
10000
9000
8000
7000
6000
5000
4000
3000
2000
1000
0
0
1
2
3
4
5
6
7
8
TIME (SECONDS)

FIG 6 - 03

SEA LEVEL, STATIC DECELS FROM $N_L = 7635$ $N_H = 11995$ to $\dot{f}_{min} = 0.10$ kg/sec

I P Compressor INERTIAS FACTORED 1·3 FUEL = 0.90 * CASE 211

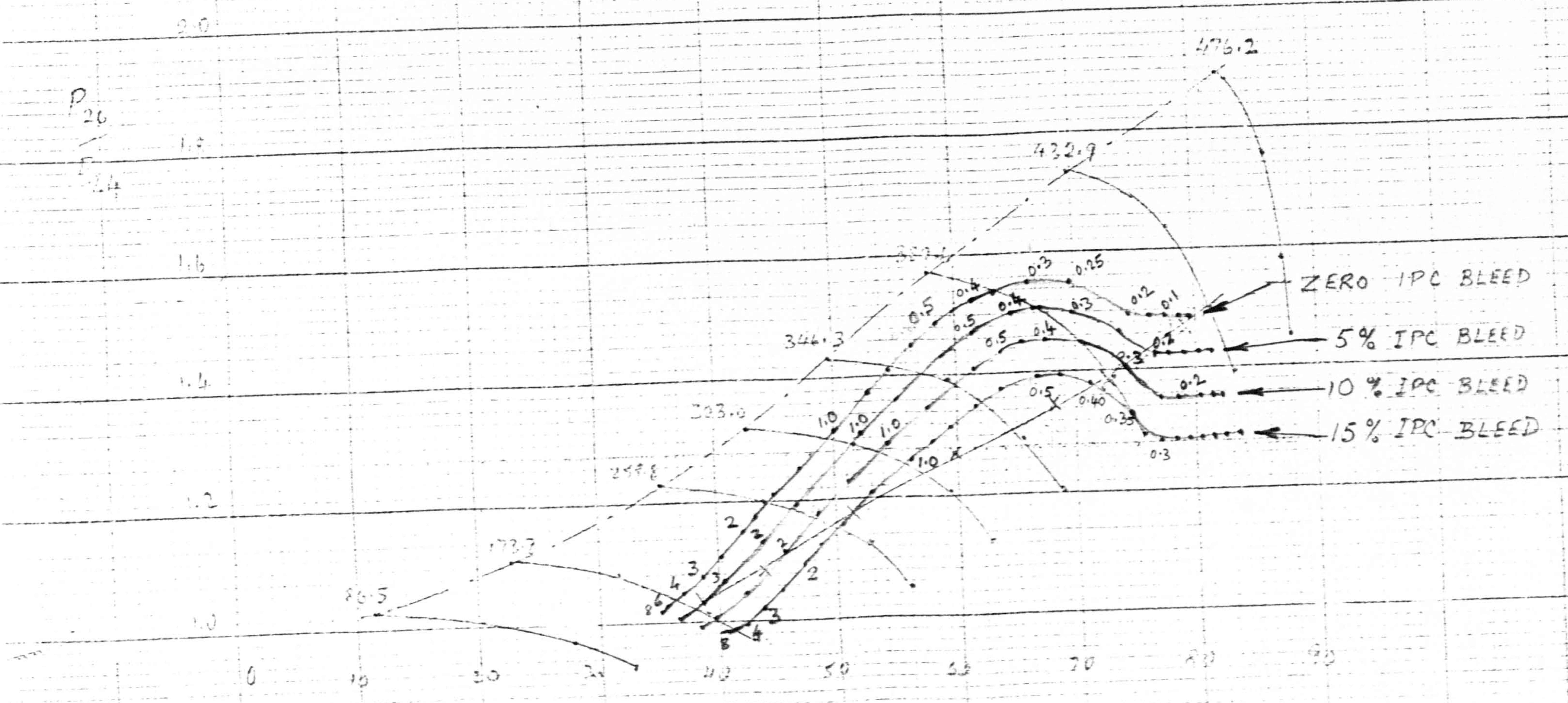



FIG. 7 RB 183 - 03
SEA LEVEL - STATIC
DECEL FUEL = 0.90 * CASC 211
To f_{min} = 0.10 kg/sec
INERTIAS NOMINAL
TRANSIENT DELTAS ON TURBINE EFFICIENCIES
+ 0.01 HP TURBINE
+ 0.01 LP TURBINE
+ 0.03 HP TURBINE
+ 0.03 LP TURBINE
× with 10% BLEED (IPC)
$\dot{f}$ = 0.10 kg/sec
N_H
N_L
$\dot{f}$ = 0.10 kg/sec
12000
11000
10000
9000
8000
7000
6000
5000
4000
3000
2000
0
1
2
3
4
5
6
7
8
TIME SECONDS

FIG 8 RB 183-03

SEA LEVEL, STATIC

DECEL, FUEL = 0.90 × CASC 211 TO $\dot{f}_{min}$ = 0.10 kg/sec

FIG 9 — 03

SEA LEVEL, STATIC DECELS FROM $N_L = 7635$

$N_H = 11995$

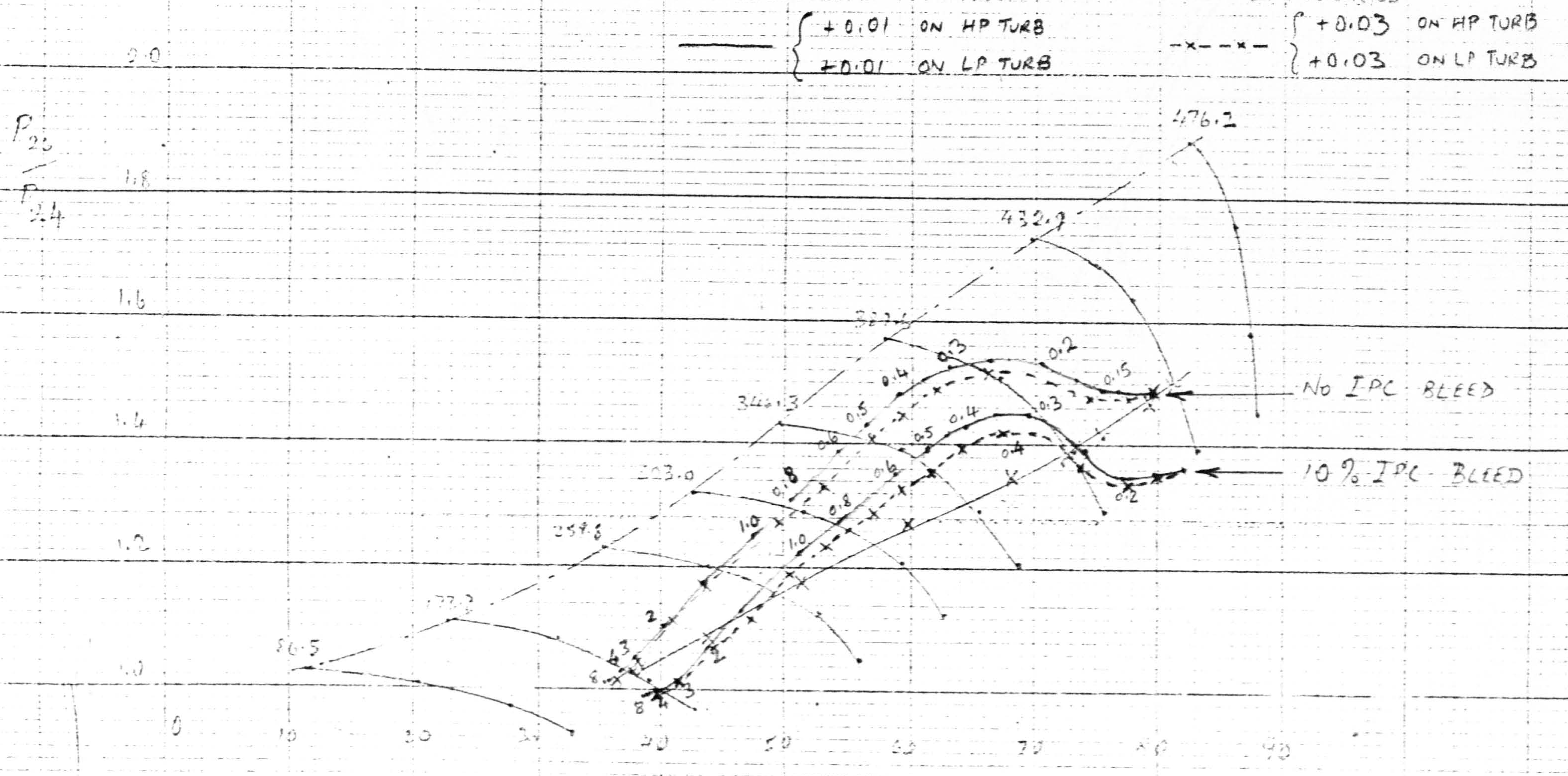
I P Compressor

τ_0 $\dot{f}_{mm} = 0.10$ kg/sec

INERTIAS NOMINAL

FUEL = 0.90 * CASC 211

TRANSIENT DELTAS ON TURBINE EFFICIENCIES



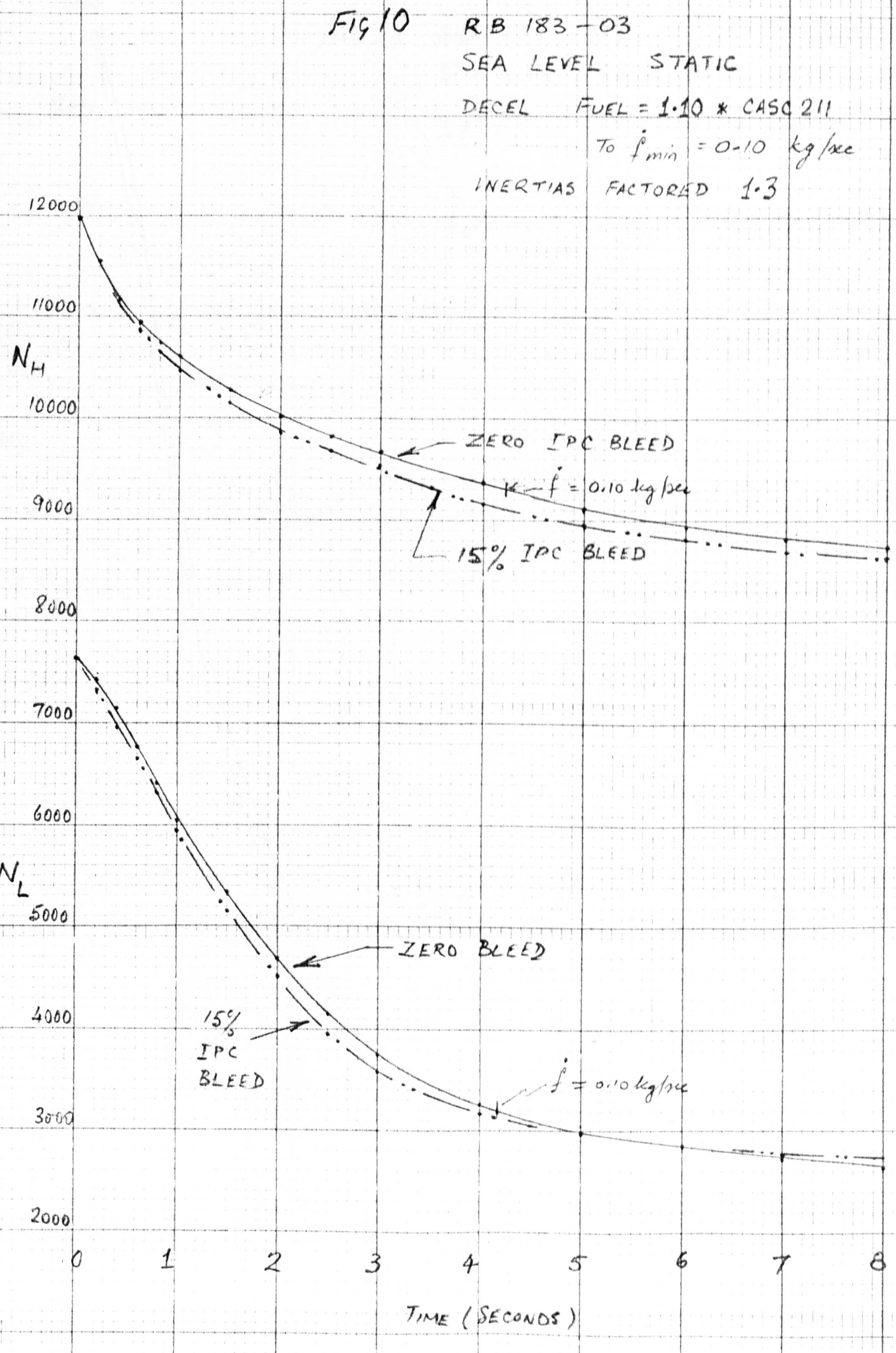


Fig 10
RB 183-03
SEA LEVEL STATIC
DECEL FUEL = 1·10 * CASC 211
To $\dot{f}_{min}$ = 0-10 kg/sec
INERTIAS FACTORED 1·3
N_H
N_L
12000
11000
10000
9000
8000
7000
6000
5000
4000
3000
2000
ZERO IPC BLEED
$\dot{f}$ = 0.10 kg/sec
15% IPC BLEED
ZERO BLEED
15% IPC BLEED
$\dot{f}$ = 0.10 kg/sec
0
1
2
3
4
5
6
7
8
TIME (SECONDS)

FIG 11 RB 183 - 03
SEA LEVEL STATIC
DECEL FUEL = 1·10 * CASC 211
TO $\dot{f}_{MIN}$ = 0·10 kg/sec
INERTIAS FACTORED 1·3
X_{NET}
LBF
13000
12000
11000
10000
9000
8000
7000
6000
5000
4000
3000
2000
1000
0
ZERO IPC BLEED
15% IPC BLEED
$\dot{f}$ = 0·10 kg/sec
0
1
2
3
4
5
6
7
8
TIME SECONDS

Fig 12 - 03

SEA LEVEL, STATIC

I P Compressor

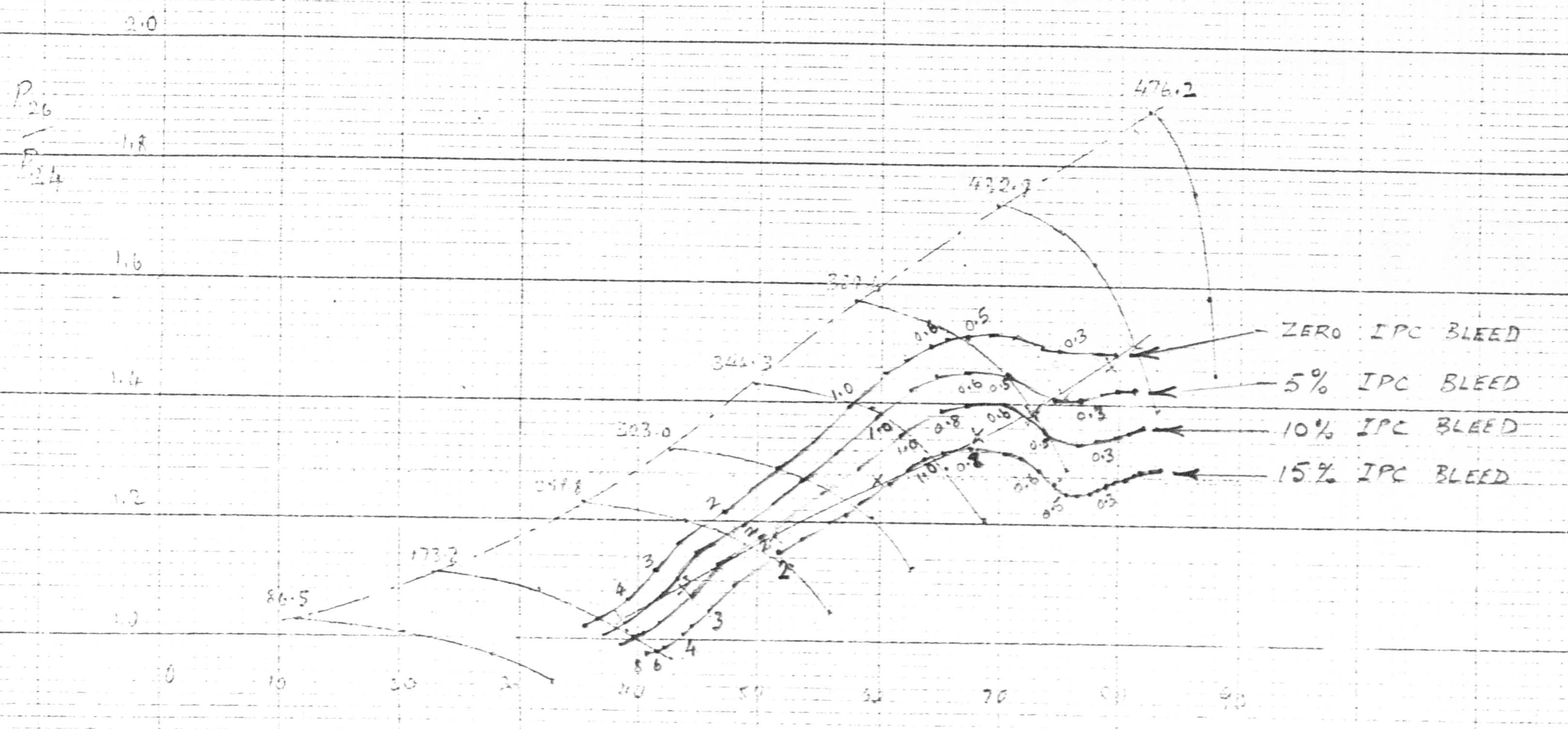
DECELS FROM $N_L = 7635$

$N_H = 11995$

To $\dot{f}_{min} = 0.110$ kg/sec

INERTIAS FACTORED 1.3

FUEL = 1.10 * CASC 211



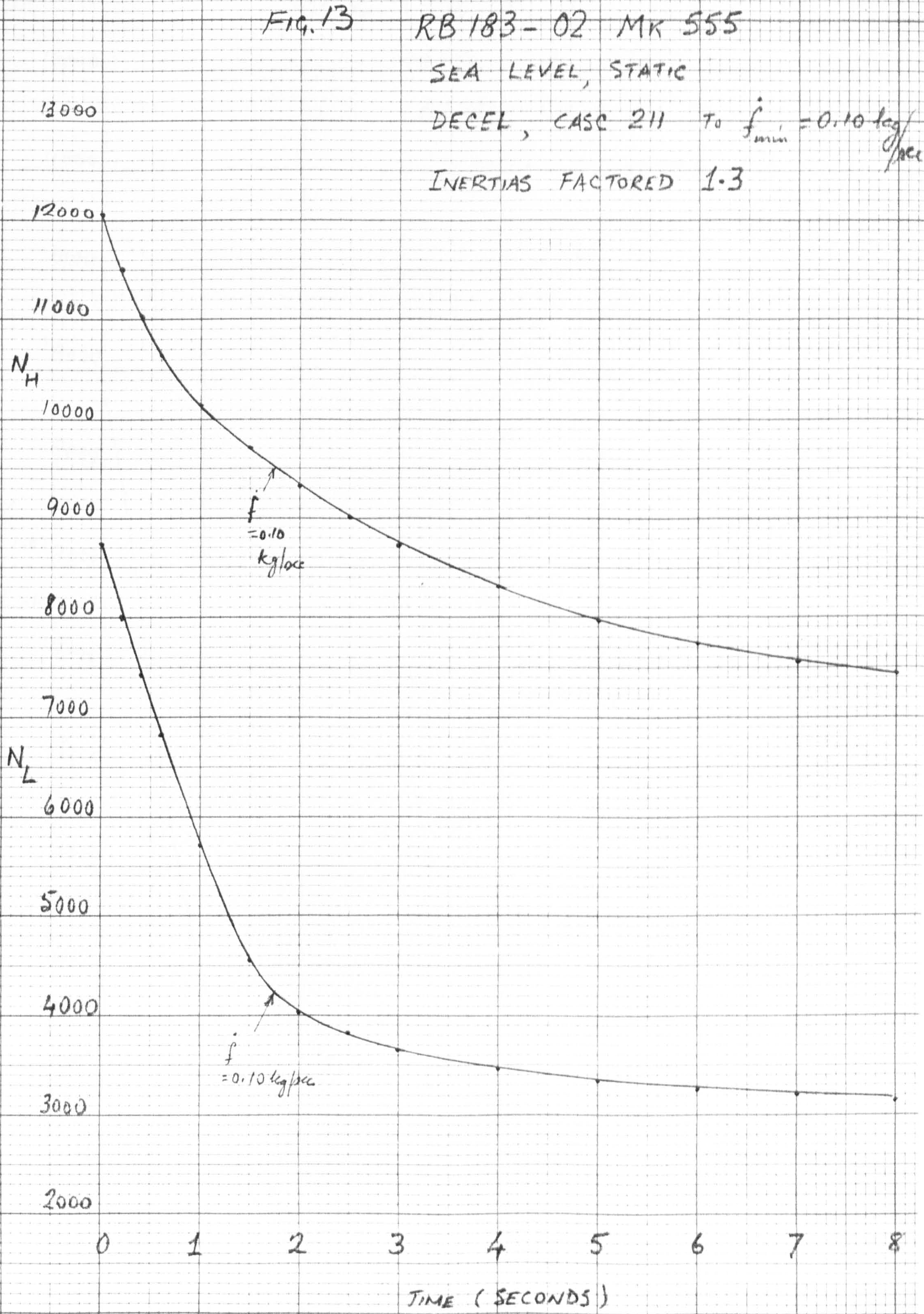


Fig. 13
RB 183-02 Mk 555
SEA LEVEL, STATIC
DECEL, CASC 211 To $\dot{f}_{min}$ = 0.10 kg/sec
INERTIAS FACTORED 1.3
13000
12000
11000
10000
9000
8000
7000
6000
5000
4000
3000
2000
N_H
N_L
$\dot{f}$ = 0.10 kg/sec
$\dot{f}$ = 0.10 kg/sec
0
1
2
3
4
5
6
7
8
TIME (SECONDS)


FIG 14
RB 183 - 02 MK 555
SEA LEVEL, STATIC
DECEL, CASC 211 TO $\dot{f}_{min}$ = 0.10 kg/sec
INERTIAS FACTORED 1.3
INIT. SPEEDS N_L =
N_H =
12000
11000
1000
9000
8000
7000
6000
5000
4000
3000
2000
1000
0
X_{NET}
LBF
$\dot{f}$ = 0.10 kg/m
0
1
2
3
4
5
6
7
8
TIME SECONDS

FIG 15 RB 183 - 02 Mk 555

SEA LEVEL MACH 0·2

THRUST RESPONSES

FUEL SCHED. CASC 211

INERTIAS FACTORED 1·3

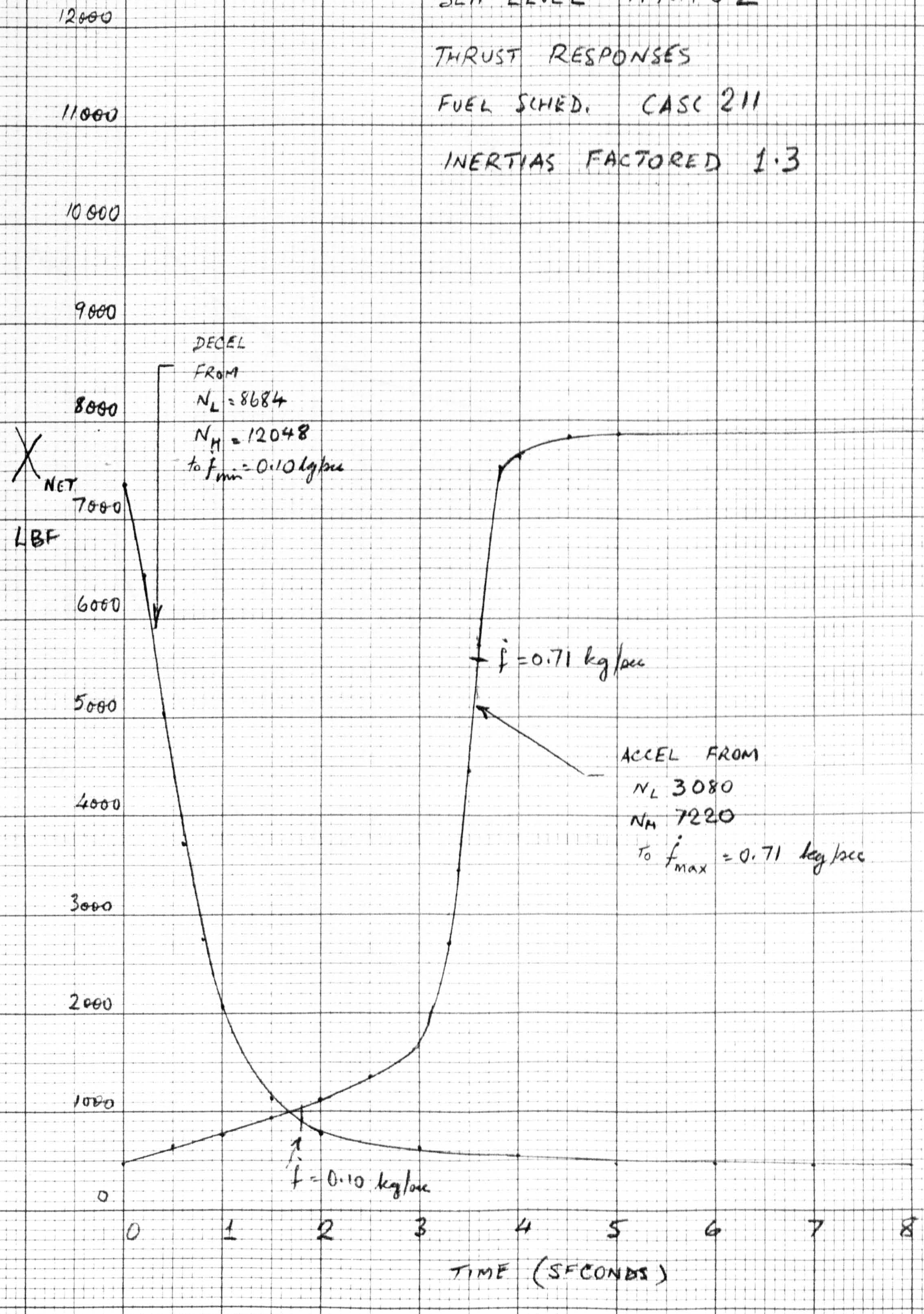


FIG 16 RB 183 - 03

SEA LEVEL, MACH NO 0·2

RECOVERY 0·995

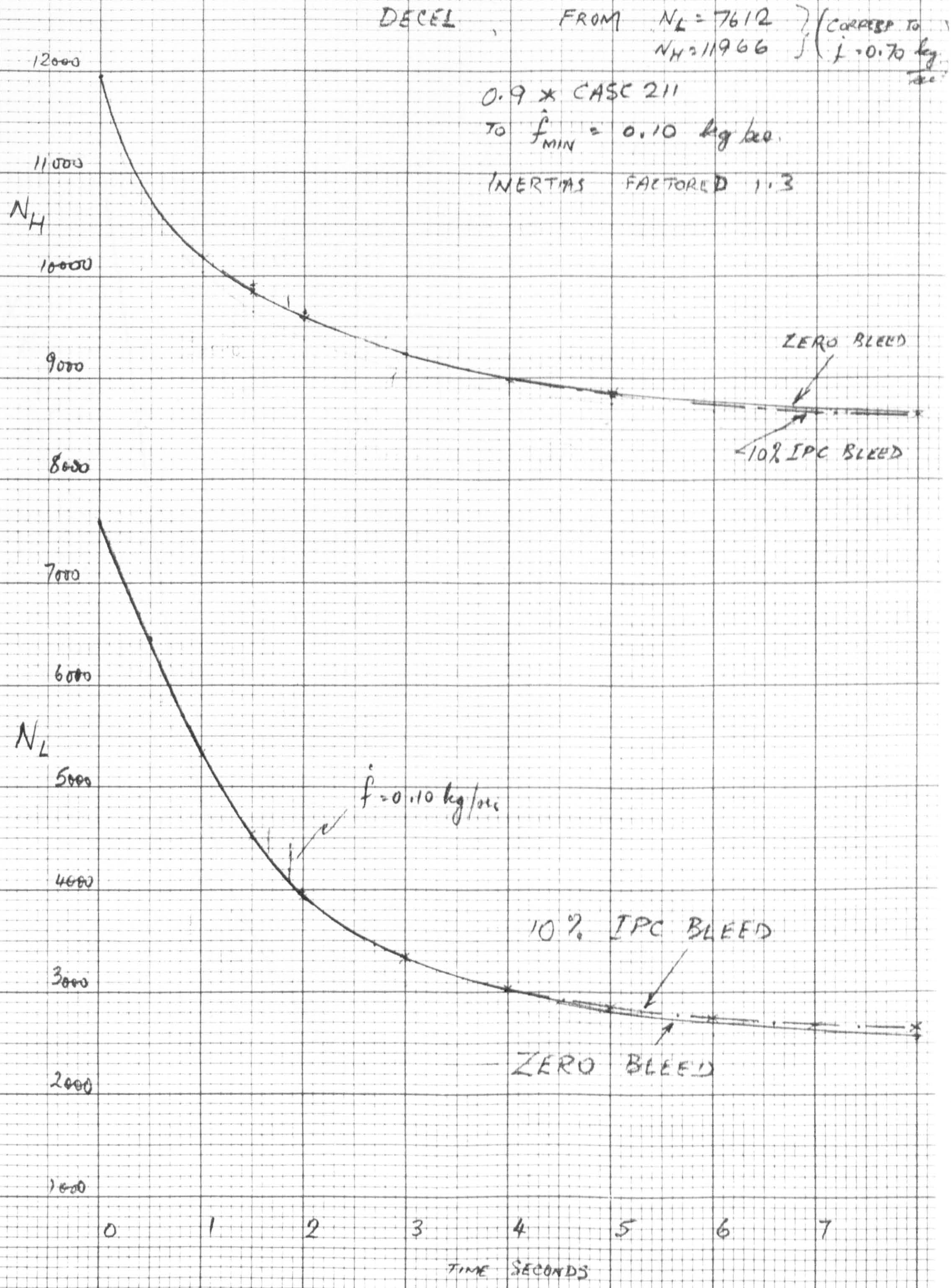


FIG 17 RB 183-03

Fig. 18 - 03

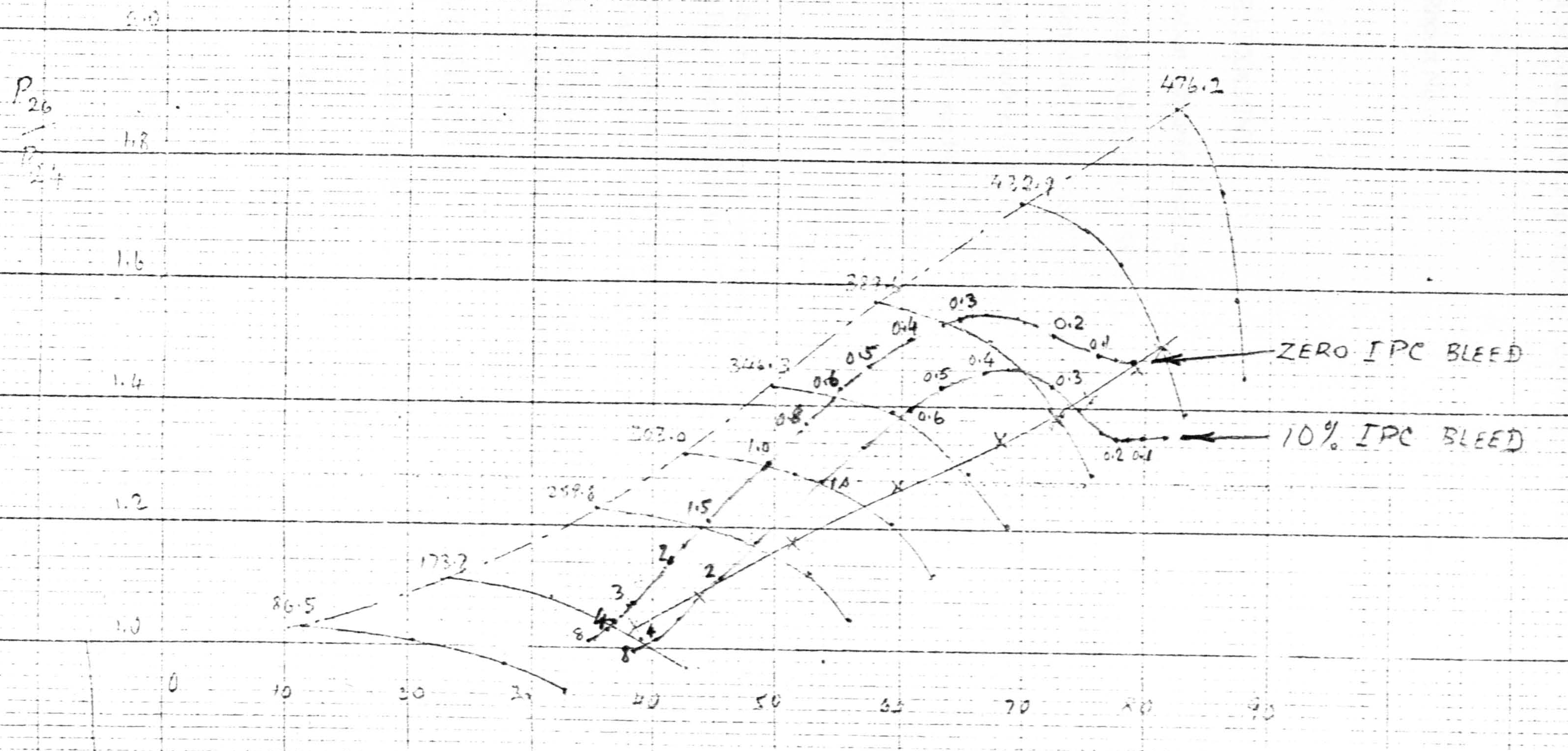
SEA LEVEL MACH 0.2

DECELS FROM N_L = 7612
N_H = 11966

I P Compressor

INERTIAS FACTORED
1.3

FUEL = 0.90 * CASC 211
to $\dot{f}_{min}$ = 0.10 kg/sec



Adjustments to Fuel Schedules to relieve surges in Decelerations – RB183-03 Tay

TAY PROJECT – Report No.6 9 June 1983

N.R.L. Maccallum

1. SCHEDULES EXAMINED

Five Deceleration Schedules were tested:

"A" reference schedule (0.9 x CASC 211)

"B" slower schedule (1.1 x CASC 211)

"C" very slow schedule (1.3 x CASC 211)

"D" altered profile – see Fig.1

"E" altered profile – see Fig.1

2. RESULTS

Thrust responses in Decelerations (sea level, Mach No. 0.2) for Schedules A, B and C are given in Fig. 2 and for Schedules D and E in Fig. 3.

Predicted deceleration trajectories in the I.P. Compressor, for Schedules A, B and C are given in Fig. 4, and for Schedules D and E in Fig. 5.

Predicted deceleration trajectories in the H.P. compressor for Schedules A, B and C are given in Fig. 6, and for Schedules D and E in Fig. 7.

3. DISCUSSION OF RESULTS

Reference case Schedule A gives usage of surge margin in I.P. compressor of 75 to 80 per cent.

Under Schedule B the surge margin usage in the I.P. compressor is 60 per cent (a previous result), and thrust reduction is about 40 to 50 per cent slower.

Under Schedule C the thrust response is too slow to be acceptable.

Schedule D gives poor performance, with surge margin usage in the I.P. compressor still high (at 70 to 75 per cent) but thrust reduction rate being about 50 per cent slower than reference case (A).

Schedule E gives better performance, surge margin usage in the I.P. compressor being about midway between cases A and B, and thrust reduction rate also lying about midway between cases A and B.

The conclusion drawn is that a factored CASC 211 schedule provides as good results as any, although further tuning on "E" might be helpful.

4. H.P. COMPRESSOR AND I.G.V. SCHEDULE

In the decelerations, the trajectories move very rapidly across the mass flow range when the Inlet Guide Vanes are turning. The rate of change of the non-dimensional mass flow into the H.P. compressor with time is very high. In the deceleration this tends to throw the I.P. compressor towards surge.

Can we extend the Inlet Guide Vane rotation range over a wider span of non-dimensional speed ($N/\sqrt{T}$)? Suggest we keep the same limits on minimum and maximum angles, but lower the "closed" I.G.V. ($N_H/\sqrt{T}$), while keeping the same "open" I.G.V. ($N_H/\sqrt{T}$).

FIG 1

C.A.S.C. 211 (20.2.74)

DECELERATION FUEL SCHEDULES

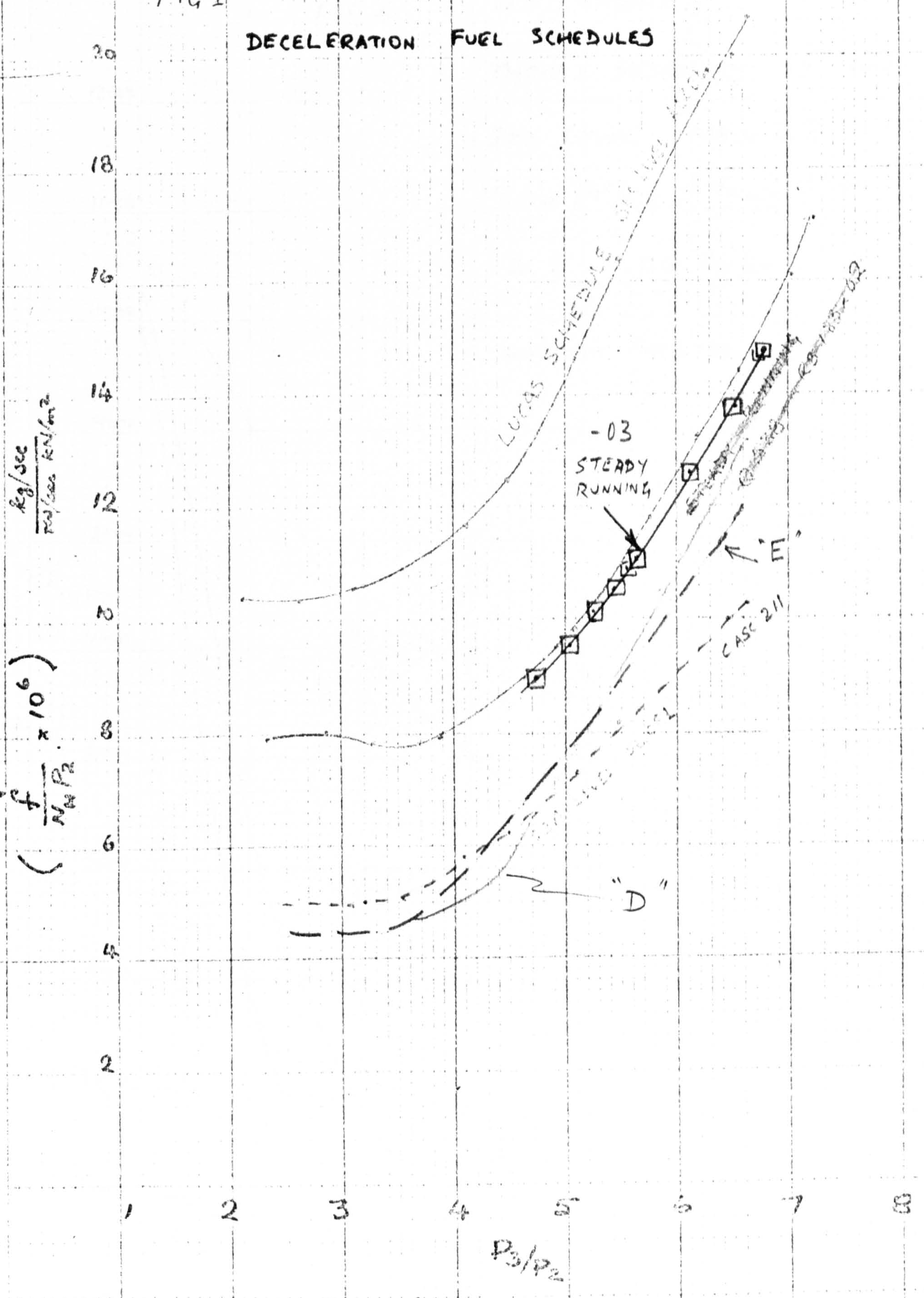



Fig 2
03 ENGINE
THRUST RESPONSES TO DECELS.
SEA LEVEL, MACH 0.2
$N_{L\,init}$ 7612, $N_{H\,init}$ 11966
to $\dot{f}_{min}$ = 0.10 kg/sec.
INERTIAS FACTORED 1.3
12000
11000
10000
9000
8000
X_{NET}
LBF
7000
6000
5000
4000
3000
2000
1000
0
SCHEDULE "C"
1.3 * CASC 211
SCHEDULE "B"
1.1 * CASC 211
SCHEDULE "A"
= 0.9 * CASC 211
0
1
2
3
4
5
6
7
8
TIME SECONDS


FIG 3 O3 ENGINE
THRUST RESPONSES TO DECELS
SEA LEVEL MACH 0.2
$N_{L\,init}$ 7612 $N_{H\,init}$ 11966
to f_{min} = 0.10 kg/sec
INERTIAS FACTORED 1.3
12000
11000
10000
9000
8000
7000
6000
5000
4000
3000
2000
1000
0
X_{NET}
LBF
SCHEDULE "D"
SCHEDULE "E"
0
1
2
3
4
5
6
7
8
TIME (SECS)

FIG 4 - 03 ENGINE

I P Compressor

DECELS SEA LEVEL MACH 0.2

N_L init 7612, N_H init 11966

To f_{min} = 0.10 kg/sec

INERTIAS FACTORED 1.3

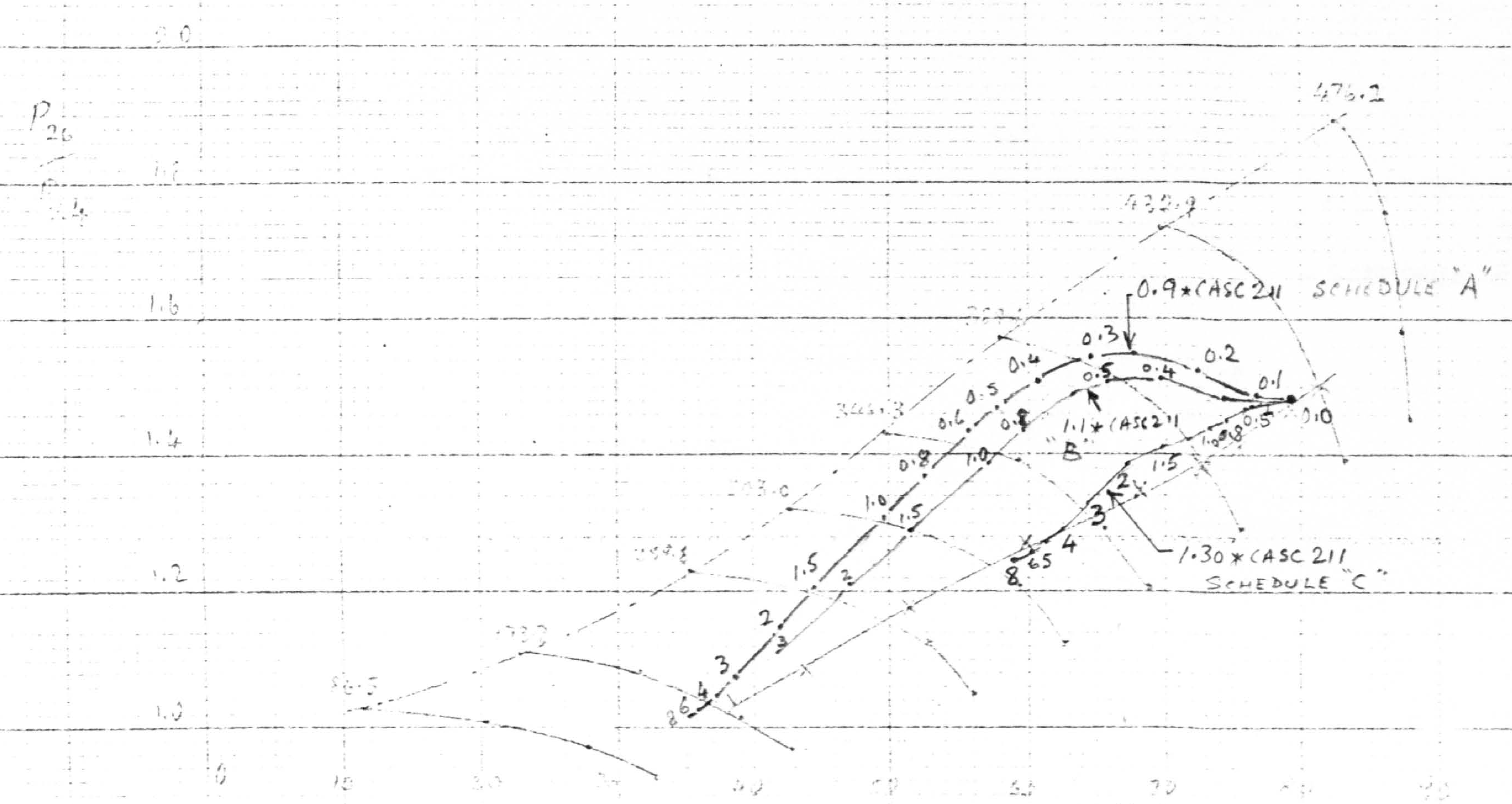


FIG 5 - 03 ENGINE

I.P. Compressor

SEA LEVEL, MACH 0.2

DECELS, FROM $N_L = 7612$

$N_H = 11966$

TO $\dot{f}_{min}$ 0.10 kg/sec

INERTIAS FACTORED 1.3

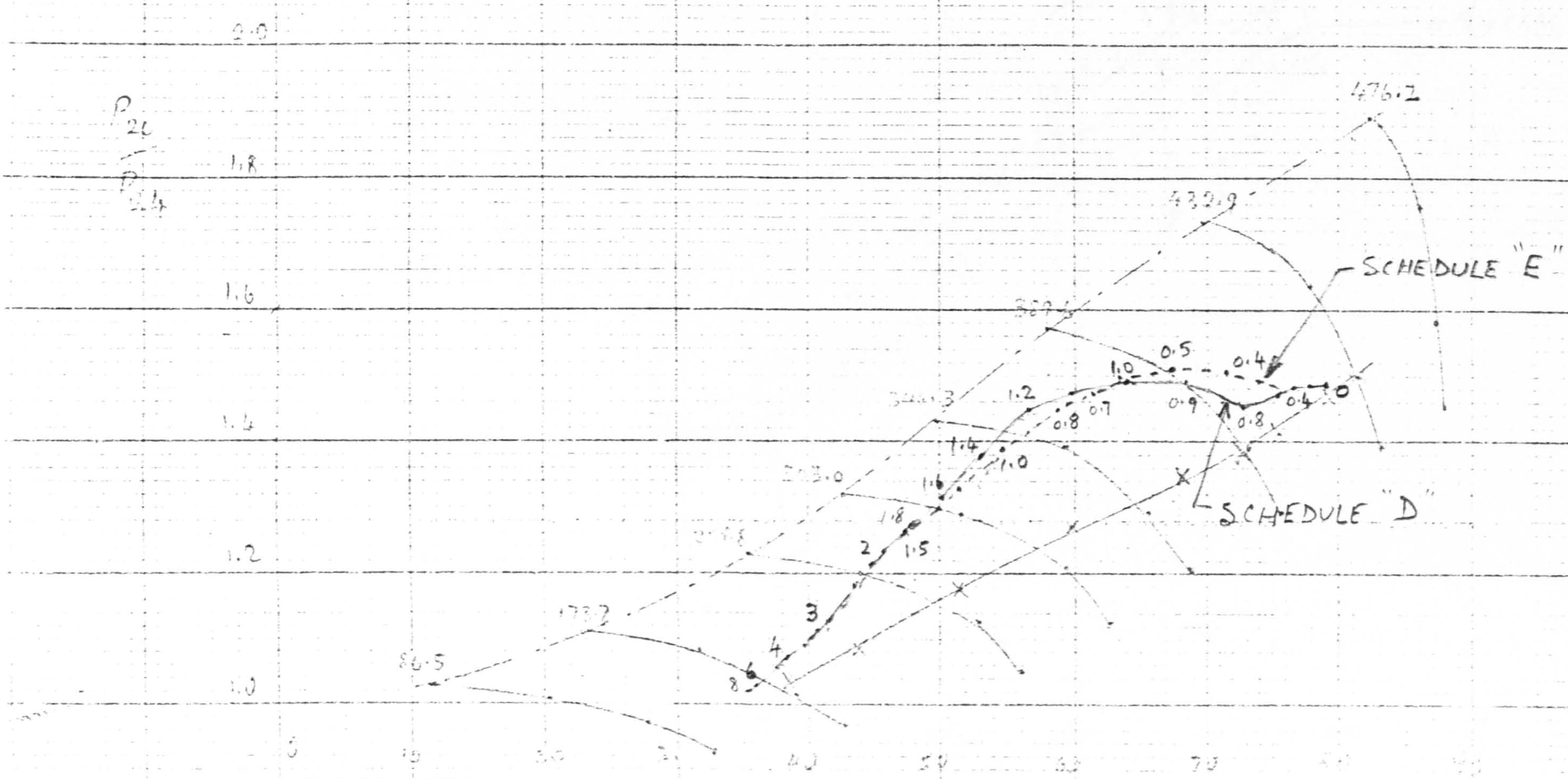


FIG 6


SPEY RB 183-02 MK 555-15P
TAY RB 183-03
H.P. COMPRESSOR
R-R SYNTHESIS Q 538 (2 FEB 1983) -02 MK555
SEA LEVEL, MACH NO 0.2
DECEL FROM 7612
11966
FUEL SCHEDULE
FACTOR * CASC 211
ZERO IPC BLEED
INERTIAS FACTORED 1.3
1.3 * CASC 211 SCHEDULE "C"
1.1 * CASC 211 SCHEDULE "B"
0.9 * CASC 211 SCHEDULE "A"
644
650
663
635
614
595
584
576
567
560
549
500
447
300
8.0
7.0
6.0
5.0
4.0
3.0
2.0
1.0
10
15
20
25
30
35
40
45
50
55
60


FIG 7
SPEY RB 183.02 MK 555-15P
TAY RB 183-03
H.P. COMPRESSOR
SEA LEVEL MACH 0.2
DECELS FROM N_L = 7612
N_H = 11966
To $\dot{f}_{min}$ = 0.10 kg/sec.
INERTIAS FACTORED 1.3
SCHEDULE "D"
SCHEDULE "E"
644
650
663
635
614
595
584
576
567
560
549
500
300
0.0
0.5
0.2
0.8
0.4
0.9
0.6
1.0
0.8
1.1
1.0
1.2
1.5
1.5
2
3
4
6
8
8.0
7.0
6.0
5.0
4.0
3.0
2.0
1.0
15
20
25
30
35
40
45
50
55
60

Predicted Transient Response of Engine RB 183-03 Tay

Effects of Gross Changes of Shaft Inertias

TAY PROJECT – Report No.7 10 June 1983

N.R.L. Maccallum

1. TAY ENGINE – CHANGE TO INERTIA OF L.P. SHAFT TO SPEY ENGINE VALUE

The effects have been examined of reducing the inertia of the Tay engine L.P. shaft from the design value of 415 lb ft^2 to the inertia value of the L.P. shaft of the Spey Engine – 95 lb ft^2. This reduction is of course not practicable, but the theoretical effects should be understood. The deceleration schedule used has been (0.9 x CASC 211), and the shaft inertias have all been factored by 1.3 to allow for thermal effects.

The reference deceleration trajectory in the I.P. compressor of the Tay engine with nominal inertias is given in Fig. 1 (sea level, Mach No. 0.2). With the L.P. shaft inertia reduced to 95 lb ft^2 (a factor of 4.3), the deceleration trajectories in the I.P. and H.P. compressors are given in Figs. 2 and 3 respectively. The transient excursion in the I.P. compressor is largely suppressed. The excursion in the H.P. compressor is not significantly affected (but this is not a critical area).

2. SPEY ENGINE – CHANGE TO INERTIA OF L.P. SHAFT TO TAY ENGINE VALUE

For further background, the effect on deceleration trajectories in compressors of the Spey engine, when the L.P. shaft inertia is increased from 95 to 415 lb ft^2, has been examined. The predictions with this change implemented are given in Figs. 4 and 5. The trajectory in the L.P. compressor is somewhat raised, but not seriously, compared with that in the nominal Spey engine (Report No.1). The trajectory in the H.P. compressor is not significantly altered.

3. DISCUSSION

The movements in the trajectories in the I.P. and L.P. compressors are as expected. For the Tay engine this demonstrates the advantage of lowering the inertia of the L.P. spool. However this order of reduction is certainly not feasible.

The trajectories in the H.P. compressor are not significantly affected as the H.P. compressor is operating in front of usually a choked turbine and the fuel flow is controlled by a schedule which uses H.P. compressor pressure ratio as input.

Fig 1 - 03 ENGINE

SEA LEVEL 0.2 MACH NO
0.995 RECOVERY

IP Compressor

HP INERTIA AS NOMINAL
LP INERTIA FACTORED 1.3

DECEL FROM 7612
11966

SCHED = 0.9 * CASC 211
To $\dot{f}_{min}$ = 0.10 kg/sec

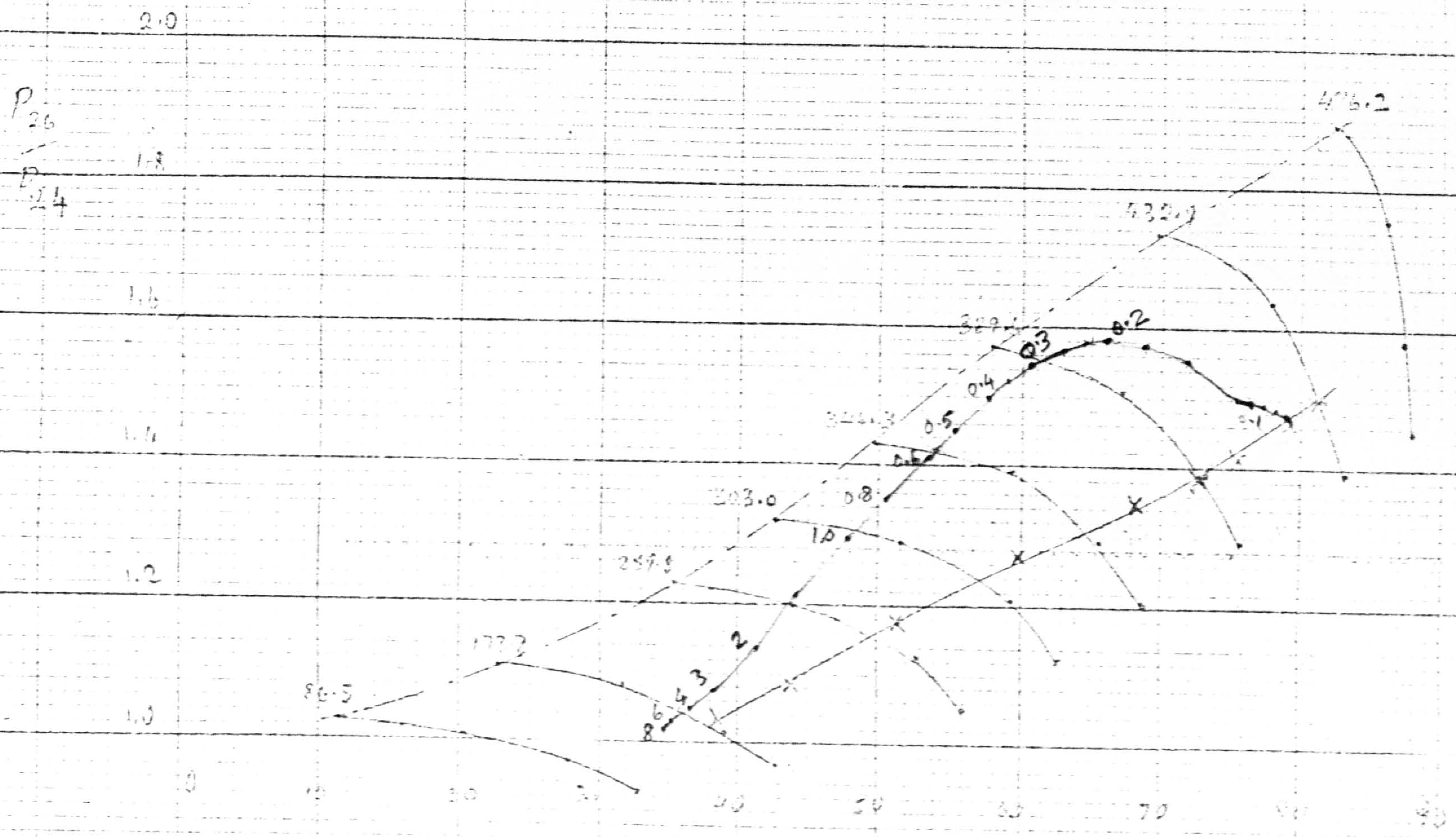


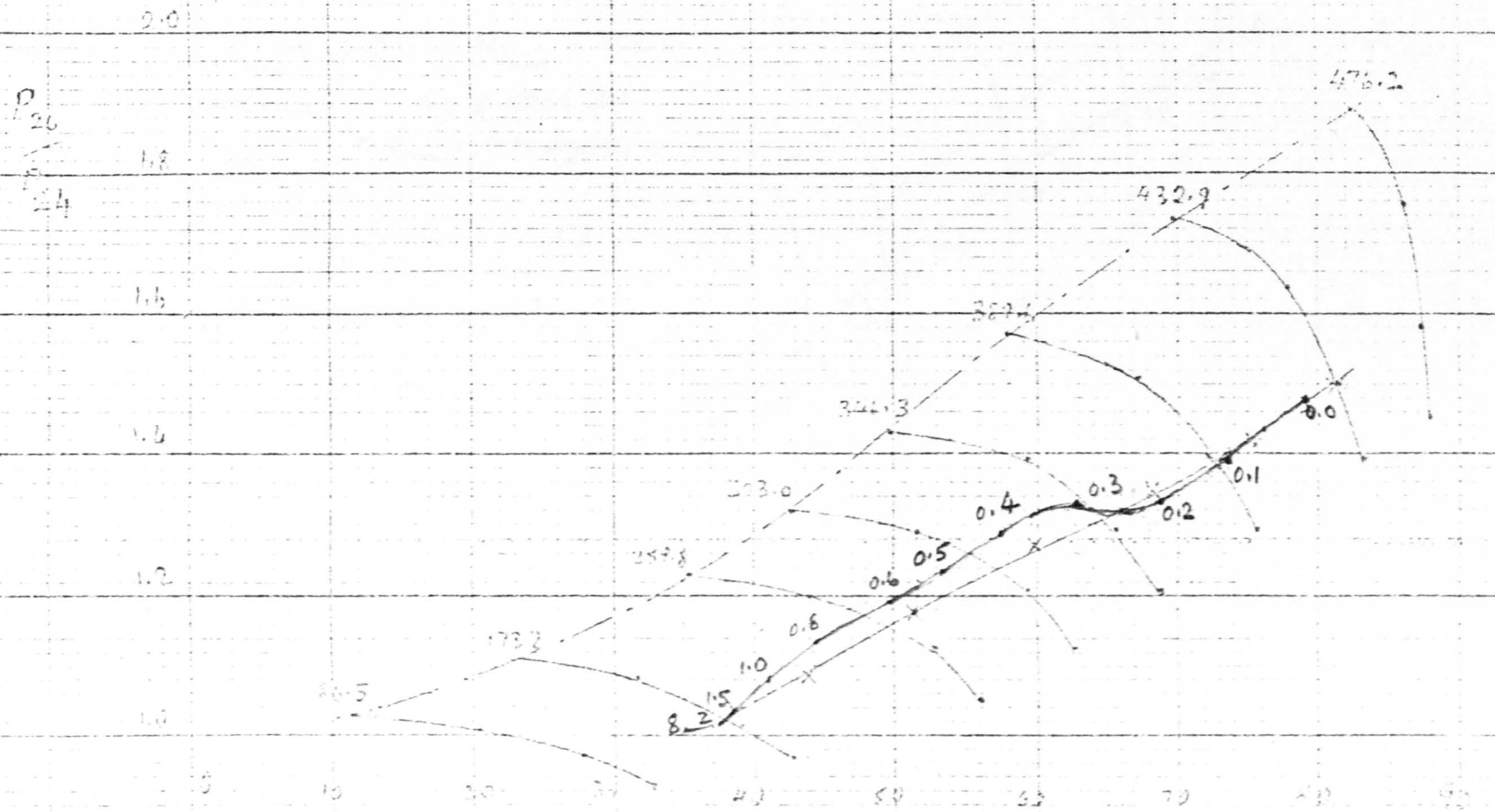
Fig 2 DECEL — 03 ENGINE FROM 7612 N_L FUEL = 0.90 * CASE 2"

SEA LEVEL MACH 0.2 11966 N_H TO $FUEL_{MIN}$ = 0.10 kg/sec

I P Compressor

INERTIAS : LP 95 LB FT² * 1.3 FACTOR
(re-02 ENGINE)

HP 85 LB FT² * 1.3 FACTOR



~~SPEY RB 183-04 MK 555 15P~~
FIG. 3: TAY RB 183 - 03
H.P. COMPRESSOR
R-R SYNTHESIS Q 538 (2 FEB 1983) -02 MK555
DECEL OF -03 ENGINE
SEA LEVEL, MACH 0.2
$N_{L_{min}} = 7612$ $N_{H_{min}} = 11966$
SCHEDULE : 0.9 * CASC 211
To $\dot{f}_{min}$: 0.10 kg/sec
INERTIAS:-
LP 95 LB FT² *1.3
ie -02 ENGINE VALUE
HP 85 LB FT² * 1.3
644
650
663
635
614
595
584
576
567
560
549
530
447
302
0.0
0.1
0.2
0.3
0.4
0.5
0.6
0.7
0.8
1.0
2.0
8.6 4 3
8.0
7.0
6.0
5.0
4.0
3.0
2.0
1.0
P_3/P_2
15
20
25
30
35
40
45
50
55
60
$\dot{m}_2\sqrt{T_2}/P_2$

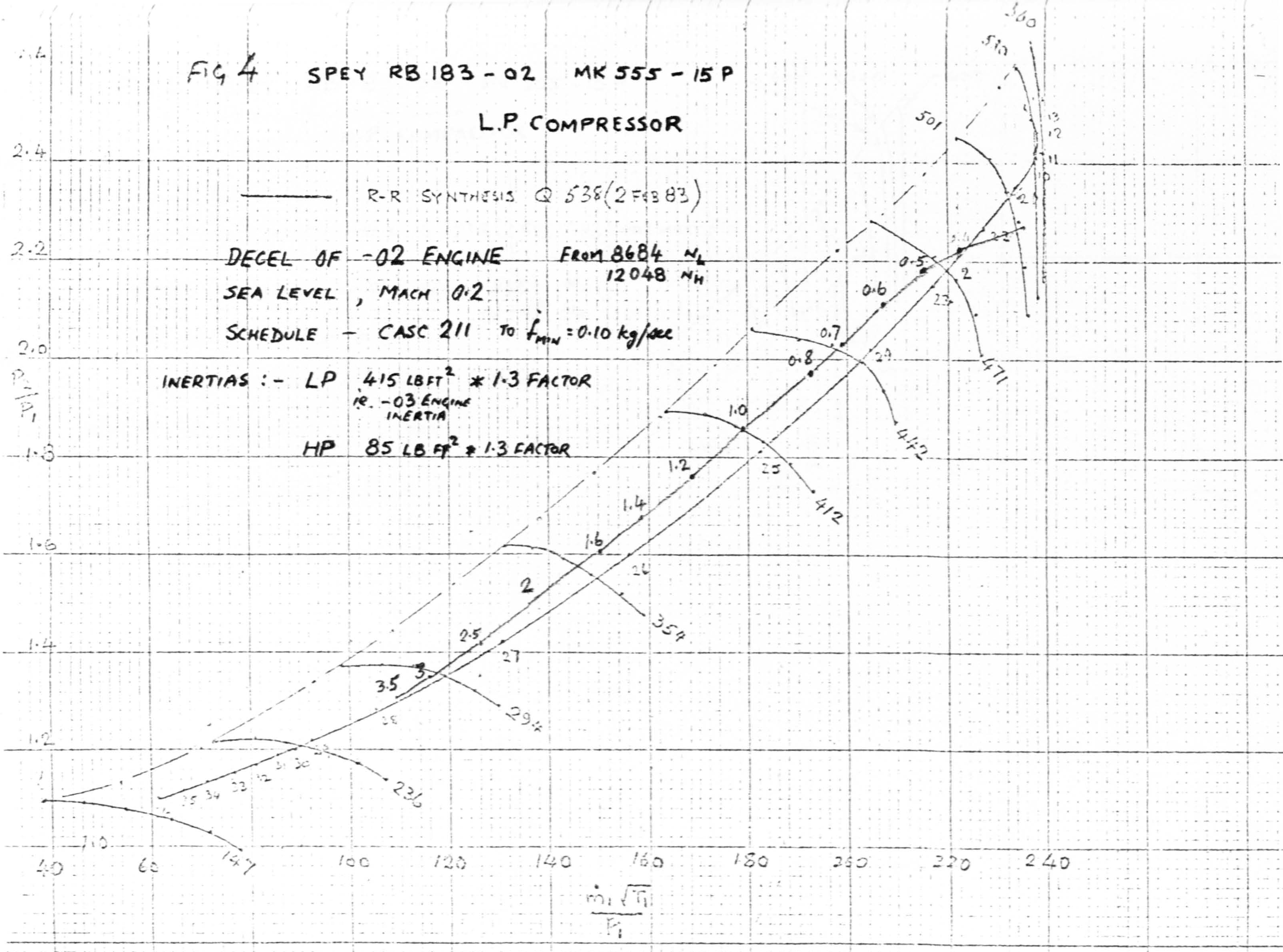


FIG 4 SPEY RB 183 - 02 MK 555 - 15 P
L.P. COMPRESSOR
R-R SYNTHESIS Q 538 (2 FEB 83)
DECEL OF -02 ENGINE FROM 8684 N_L
12048 N_H
SEA LEVEL, MACH 0.2
SCHEDULE - CASC 211 TO $\dot{f}_{MIN}$ = 0.10 kg/sec
INERTIAS :- LP 415 LB FT^2 * 1.3 FACTOR
ie. -03 ENGINE INERTIA
HP 85 LB FT^2 * 1.3 FACTOR
P_2/P_1
$\dot{m}_1\sqrt{T_1}/P_1$
40
60
100
120
140
160
180
200
220
240
1.2
1.4
1.6
1.8
2.0
2.2
2.4
501
510
360
471
442
412
354
294
236
147
0.5
0.6
0.7
0.8
1.0
1.2
1.4
1.6
2
2.5
3
3.5

Fig 5 SPEY RB183-02 MK 555-15P

H.P. COMPRESSOR

Predicted Transient Response of Engine RB 183-03 Tay

1. With Revised L.P. Shaft Inertia

2. With Proposed Extended I.G.V. Schedule

TAY PROJECT – Report No. 8 13 June 1983

N.R.L. Maccallum

1. EFFECT OF REVISION OF L.P. SHAFT INERTIA

The mechanical design of the L.P. Shaft system has been developed and the expected inertia is now 260 lb ft^2, as compared with the previously expected value of 415 lb ft^2.

Performance predictions with this revised inertia have been carried out, attention being focused on the deceleration at sea level, Mach 0.2 (the abandoned take-off).

Thrust responses are given in Fig. 1 for the two deceleration schedules of (0.9 x CASC 211) and (1.1 x CASC 211) with the revised L.P. Shaft Inertia. These thrust responses are then compared with the thrust response of the original inertia engine under deceleration schedule (0.9 x CASC 211). The lower inertia engine under equivalent decelerations obviously decelerates more rapidly.

Trajectories in the critical deceleration in the I.P. compressor must also be considered (ref. condition – Sea Level, Mach No. 0.2). With this revised L.P. shaft inertia, the trajectory in the I.P. compressor is very safe (surge margin usage 55 to 60 per cent) – Fig. 3, to be compared with Schedule "A" plot on Fig. 4 of Report No. 6 (surge margin usage 75 to 80 per cent).

2. PROPOSED SCHEDULE FOR I.G.V.s OF H.P. COMPRESSOR

A previous proposal (Report No. 6) had suggested that the rate of opening and closing of the I.G.V.s of the H.P. Compressor might be slowed down. Results are presented for a case where the H.P. Compressor I.G.V. turning range of 550 to 587 ($N_H/\sqrt{T}$) – the existing design – is modified to 515 to 587 ($N_H/\sqrt{T}$) i.e. the range is doubled and the rate of turning of the I.G.V.s halved. The method of establishing the equivalent H.P. compressor characteristic with the revised I.G.V. schedule is illustrated in Fig. 2. The predicted transient response of thrust (under (0.9 x CASC 211)) is shown in Fig. 1. The predicted trajectory in the I.P. compressor is shown in Fig. 3. It is to be noted that with this modified I.G.V. schedule, the thrust response (Fig. 1) scarcely departs from the previous thrust response. Importantly, the deceleration trajectory in the I.P. compressor is noticeably relieved (Fig. 3).

3. DISCUSSION

(i) The lowering of the L.P. shaft inertia is significantly beneficial.

(ii) The proposal presented of extending the $(N_H/\sqrt{T})$ turning range of the I.G.V.s. in the H.P. Compressor is worthy of consideration.

FIG 1 03 ENGINE

THRUST RESPONSES TO DECELS

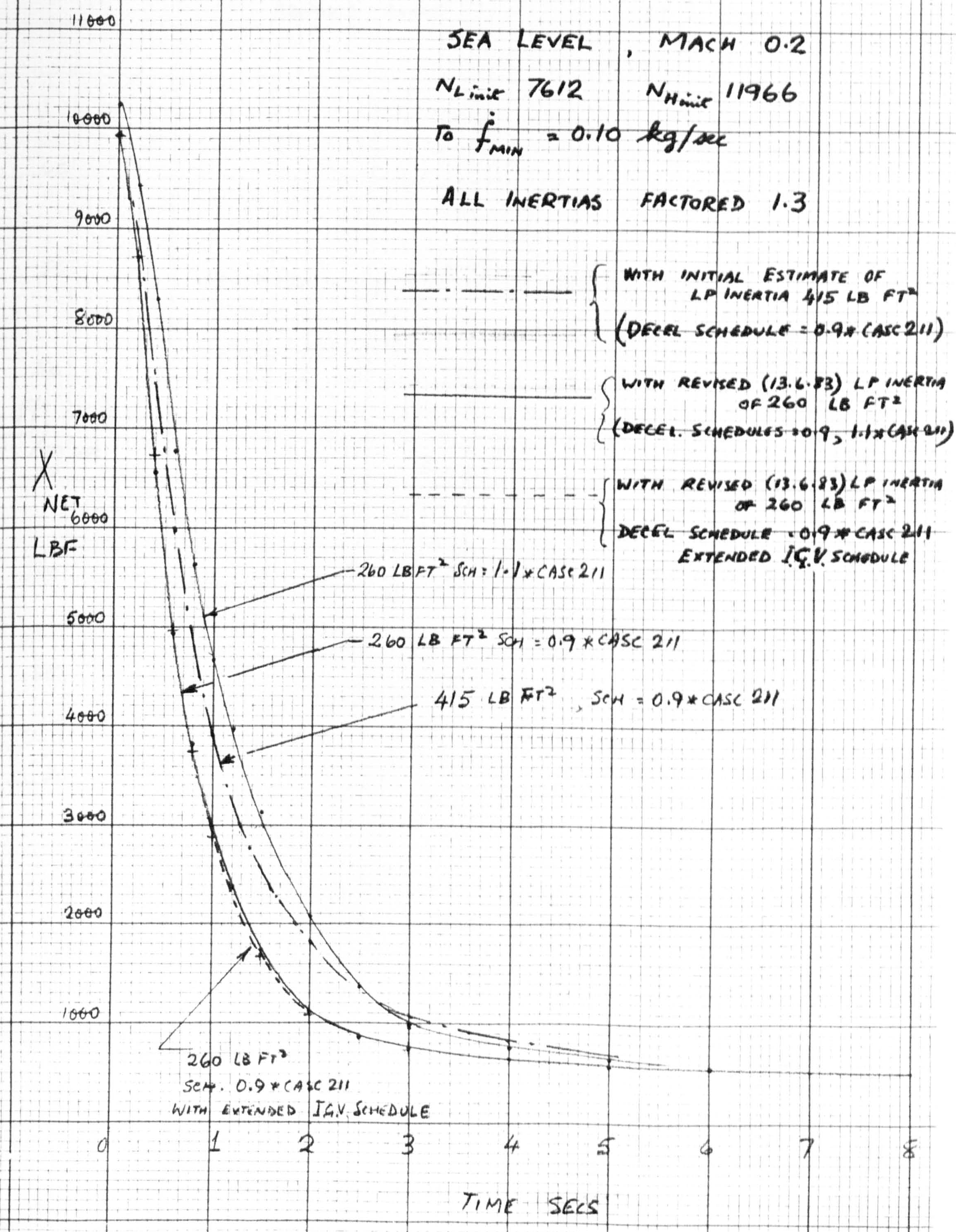
WITH REVISED L.P. SPOOL INERTIA

SEA LEVEL , MACH 0.2

$N_{L\,init}$ 7612 $N_{H\,init}$ 11966

To $\dot{f}_{MIN}$ = 0.10 kg/sec

ALL INERTIAS FACTORED 1.3



11 June 2

Fig 2 FOR DOUBLED IGV RANGE IN DECEL

ORIGINAL RANGE LOOKED LIKE 587 TO 550

PROPOSED RANGE 587 TO 513

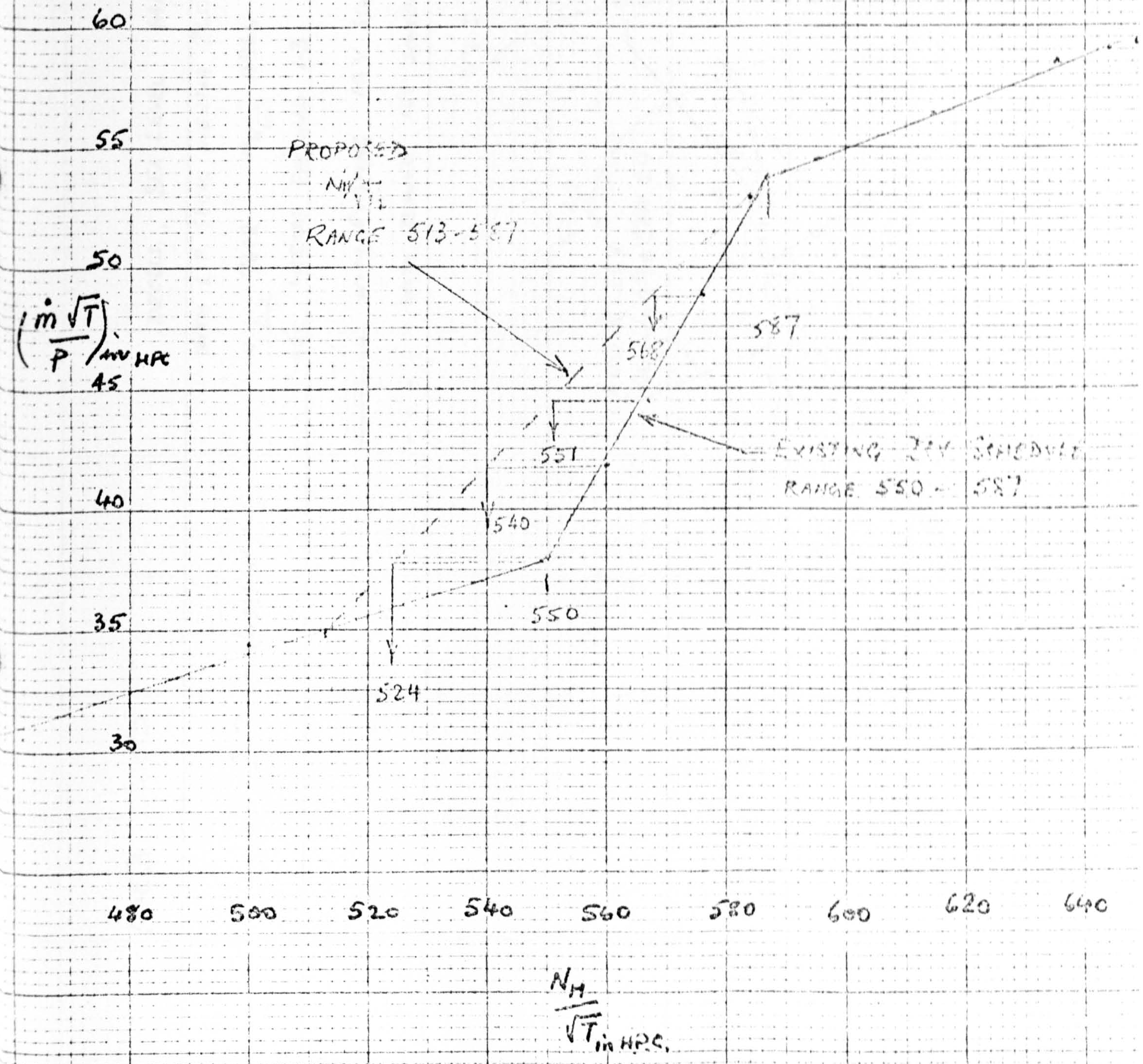


FIG 3 - 03

I P Compressor

DECELS., SEA LEVEL MACH 0.2

WITH L.P. SPOOL INERTIA 260 LB FT² ie AS REVISED 13.6.83

$N_{L_{init}}$ 7612 $N_{H_{init}}$ 11966 To $\dot{f}_{min}$ = 0.10 kg/sec

AEL INERTIAS FACTORED 1.3

——— WITH EXISTING IGV SCHEDULE

- - - - - - WITH SLOWED IGV SCHEDULE IN DECEL

Computational Models for the Transient Performance of RB183-02 (Spey) and RB183-03 (Tay) Engines.

Report RR/1 13 August 1984

N.R.L. Maccallum University of Glasgow

1. Introduction to Modelling Methods

There are two commonly used methods for modelling the transient performance of gas turbines. One is the method of "intercomponent volumes" and the other is the method of "continuity of mass flow".

In the method of "intercomponent volumes", volumes are allocated to the spaces between the components (compressors, combustion chamber, turbines, nozzles). Initial mass flows are selected in the components. Where mismatches occur, then mass accumulates or diminishes in the intercomponent volumes. The pressures there rise or fall, and from the new pressures at the end of the time interval, new mass flows in the components are then determined. This procedure, which is a "once through" calculation at each time step, requires short time intervals and considerable computing time.

In the"continuity of mass flow" method, the intercomponent volumes usually are ignored. The calculation procedure at each time step is iterative until continuity of mass flow is achieved. Difficulties have have been encountered in obtaining convergence of the computation, particularly in 2-spool and 3-spool engines. However if convergence can be achieved, longer time intervals (e.g. 0.05 sec.) can be used and less computing time is required.

The predictions by the two methods appear to be basically similar (Ref. 1), with the exception of the first time intervals of a transient.

The method of "continuity of mass flow" has been used for the procedures described in this report for modelling the transient performance of the RB 183-02 (Spey) and RB183-03 (Tay) Engines.

2. Model for RB 183-02 (SPEY Mk555) Engine

The Block Diagram for the calculation procedure for the RB 183-02 Engine is given in Fig. 1.

All the compressions and expansions have been assumed adiabatic. It has further been assumed that at the intermediate speeds encountered during the transient, the characteristics of the components are the same as those observed when the component is running steadily at those particular values of corrected speed.

Experience indicates (Ref. 2) that actual engine accelerations and decelerations are about 30% slower than are predicted using the assumptions of the preceding paragraph. A crude "rule-of-thumb" to compensate for this has been to scale up the inertias of the spools of the engine. One value of this scaling factor which has been used is 1.3. The models described in this report have the facility for incorporating a factor of this type.

Typical predictions for accelerations and decelerations are shown in Figs. 2 to 6. In this case the engine is at Sea Level, Mach 0.2. The acceleration and deceleration fuel schedules are as defined by the Combined Acceleration and Speed Control (CASC) No. 211. These Schedules are plotted in non-dimensional form in Fig. 2 where they are compared with the steady-running fuel flow values as predicted by the transient model of the engine described here (steady-running values are obtained by holding the fuel flow constant at some specified value and allowing the engine to stabilise itself). The shaft speed and thrust responses to the acceleration are shown in Fig. 3 and correspondingly for the deceleration in Fig. 4. The multiplying factor of 1.3 was applied to the polar moments of inertia of the two spools. The time increment used during the transient was 0.05 second. The transient trajectories in the LP Compressor are shown in Fig. 5 and in the HP Compressor in Fig. 6. The predicted "steady-running" working lines in these compressors are also given in these Figures and are compared with the working lines given by Rolls-Royce Synthesis Program Q538.

The agreement between the steady-running working lines as predicted by the proposed model and as given by Synthesis Program Q538 is regarded as being very satisfactory.

Considering the transient predictions, the times required for the thrust and speed responses are in broad agreement with experience. It is noted that in the LP Compressor the transient trajectories move only a small amount away from the steady-running line. However in the HP Compressor the trajectories depart significantly from the steady-running line. Indeed in the acceleration the trajectory appears to reach the surge line. The fact that experience with the engine has not thrown up surge problems in acceleration may be partially due to the beneficial displacement of the surge line in an acceleration due to heat absorptions in the metal of the compressor (Ref. 3). However a detrimental effect on the surge line arises from the higher tip clearances usually encountered in an acceleration of a cold engine. There is some evidence that this latter effect is less marked in the Spey HP Compressor than in the RB 211 HP Compressor (Ref. 4)

With regard to the choice of the time increment for the transient calculations in the model, increasing the time increment from 0.05 second to 0.1 second frequently led to computational instabilities. Reducing the time increment to 0.02 second made insignificant difference to the predictions, but of course increased the computing time required. The choice of 0.05 second appeared to be a fair compromise.

3. Model for RB 183-03 (Tay) Engine

The characteristics provided for this engine included separate representations for the inner and outer sections of the Fan. This may be a satisfactory way of treating the Fan under steady-running conditions, but it was felt that this procedure might be too rigid for transient behaviour, and some allowance should be made for interchange of mass flow between the two sections.

The allowance for interchange of flow was achieved by assuming firstly that the characteristics initially provided were based on a frontal area split in the ratio 1 to 3 between the inner and outer sections. This was quantified by the parameter GEOM which represents the fraction of the total frontal flow area allocated to the inner section in the initial characteristics. GEOM thus was assumed to have the value 0.25. Small alterations of GEOM to 0.22 or 0.28 made little difference to the predicted transient behaviour in the critical areas - namely in the IP and HP Compressors.

The second step to allow for interchange of flow between components was to assume that the axial component of velocity of the air into the Fan is constant, at any instant, over the whole annular area, and that the fraction of this area which "feeds" the inner (or core) Fan is not necessarily equal to GEOM, but is some fraction of it. This fraction was named the "fraction of split", labelled FCSP. This is a floating variable which is calculated in the program at each time interval.

The Block Diagram for the calculation procedure for the RB 183-03 Engine can now be followed. This is illustrated in Fig. 7. Two alternative forms of logic can be used, depending on whether NLOGIC is given the value 1 or 2. The procedure followed when NLOGIC has the value 1 has proved satisfactory for decelerations and for modest acceleration schedules. However for the more rapid acceleration schedules, the procedure could enter a loop with this form of logic. The alternative logic (NLOGIC with value 2) overcomes this difficulty, although computing times are lengthened. It is therefore recommended that NLOGIC be given the value 1 (in practice anything other than 2) for decelerations and the value 2 for accelerations.

Adjustments to compressor mass flow capacities and efficiencies, due to Reynolds Number effects, are incorporated in the program. For the IP and HP Compressors the procedure uses the adjustments given in Verses 24051, 24001 (for IP) and 26051 and 26001 (for HP), updated 1 February 1982, and used in Program G519. For the Fan Sections, the procedure follows the recommendation of Compressor Research Department Internal Memorandum Ref CGB/Tms 7/KR, dated 9 June 1983, that the effect of P_1 on Tay Fan performance should be half that specified in the Spey Synthesis.

A time increment of 0.05 seconds was generally used in this model, with the exception of the first 1 second of the transient when an increment of half the normal duration was adopted. The reason for this was to overcome computational instabilities in the early stages of the transient, particularly in decelerations.

3.1 Sea Level, Mach 0.2, Zero IP Bleed

The model thus developed has been used to predict transient behaviour under a variety of conditions. The first case illustrated here is at Sea Level, Mach No. 0.2, Recovery Ratio 0.995. (Recovery Ratio being defined by P_1/P_1 ideal). As a first proposal for acceleration and deceleration fuel schedules, schedules scaled by 0.9 from the CASC 211 have been used. Those are shown in Fig. 8. The predicted steady-running points are also shown, lying reasonably between the schedules. The resulting predicted speed and thrust responses are shown in Figs. 9 and 10. It is interesting to compare the acceleration responses of the Tay Engine (Fig. 9) and the Spey Engine (Fig. 3). The Tay has been started at much lower speeds, and takes 6 seconds to develop 500 lbf of thrust, which is the starting point of the Spey acceleration. Thereafter, the Tay is predicted to take just over 6 seconds to obtain maximum thrust while the Spey reaches its maximum thrust in just over 4 seconds. It will be seen on comparing HP trajectories on Figs. 14 and 6 that the transient excursion in the Tay is less severe than in the Spey, and so probably a more rapid acceleration schedule could be tolerated by the Tay, say with multiplying factor 0.95 applied to the CASC 211. Considering the decelerations of the two engines (Figs. 10 and 4), both engines drop to 20% of their initial steady thrusts in about 1.25 seconds.

Examining now the trajectories in the various components, the results for the Inner Fan are given in Fig. 11. The mass flow value plotted on the base line of this characteristic is the mass flow the component would be handling if the same axial velocity entered the area corresponding to an FCSP value of 1.0 instead of the actual value of FCSP. It should first be noted that the steady-running conditions predicted by this program apparently deviate markedly from those predicted by R-R Program G519. This is attributed to the different logics of the two programs, and the different ways of handling the two sections of the fan. While this difference in approaches is worthy of further investigation, it is noted (on Fig. 11) that the transient points (acceleration and deceleration) predicted by the present program practically coincide with the steady-running points and therefore no additional problem should be experienced here during transients.

The predicted steady-running points and trajectories in the Outer Fan are shown in Fig. 12. The steady-running points deviate slightly from the predictions of Program G519. The reason is probably as discussed above for the Inner Fan. In the Outer Fan, as for the Inner, transient trajectories are only slightly displaced from the steady-running line and it is considered that, here too, no additional problem should be encountered in transients.

The results for the IP Compressor are shown in Fig. 13. Before discussing the transient trajectories, it should be mentioned that a slight discrepancy in predicted steady-running points arises at higher $N/\sqrt{T}$ values. This again is probably partly related to the differing fan treatments given in the two programs. In the transients, the trajectories depart significantly from the steady-running line. In the acceleration this begins after about 9.5 seconds when the 7th Stage Bleed in the HP compressor begins to close and the IGV's begin to open. The engine begins to accelerate rapidly and the air demand of the HP compressor increases more quickly than is available from the IP Compressor outlet. This causes the pressure ratio provided by the IP Compressor to reduce (at times to close to unity), thereby meeting the $(\dot{m}\sqrt{T}/P)_{26}$ requirement of the HP Compressor. The opposite situation arises in the deceleration, where the trajectory is predicted to approach the surge line (as revised Jan, 1984). These results are discussed in more detail in Section 4.

The HP Compressor results are shown in Fig. 14. The predicted steady-running points almost coincide with the Spey values. The Tay transient trajectories are as expected, with the excursions slightly less pronounced than for the Spey, but this is a function of the acceleration schedule selected, as discussed above. It should also be noted that in the acceleration there is an overshoot in the $(N_H/\sqrt{T_{26}})$ values before the engine stabilises. This is partly due to an overshoot in N_H (see Fig. 9). This subject is discussed further in Section 4.

3.2 Sea Level, Mach 0.2, 10% IP Bleed

One method of relieving the tendency of the IP Compressor to surge during decelerations is to bleed a fraction of the IP Compressor delivery air into the Bypass Duct. This facility has been incorporated in the program, and the resulting deceleration trajectories in the IP and HP Compressors are shown in Figs. 15 and 16 for the case of a 10% IP Bleed (flight conditions-sea level, Mach 0.2, as before). The trajectory in the IP Compressor is dropped significantly away from surge, about 60% of the surge margin between the revised surge line and steady-running remaining intact. The trajectory in the HP Compressor is virtually unaffected (Fig. 10). The HP speed drops to a slightly lower equilibrium speed (Fig. 10).

3.3 Altitude 41,000 ft, Mach 0.8, Zero and 10% IP Bleeds

This procedure has also been applied to the flight case of Mach 0.8 at 41,000 ft, with Recovery Ratio 0.995. The acceleration and deceleration fuel schedules were again based on CASC 211 (Altitude, with P_1=4.4 lbf/in^2). Again a multiplying factor of 0.9 was used. The schedules, and the predicted steady-running line are shown on Fig. 17. It is seen that this acceleration schedule appears to be too far above the steady-running line, and likely to cause surge in the HP Compressor, as indeed is the case (Fig. 21). The deceleration schedule is such that the engine cannot drop to a fuel flow of less than about 0.044 kg/sec. Speed and thrust responses in the acceleration (which surges in the HP Compressor) are shown in Fig. 18, and in the deceleration in Fig. 19.

The trajectories in the IP Compressor are shown in Fig. 20 (Zero IP Bleed) and Fig. 22 (10% IP Bleed). For steady-running with Zero IP Bleed, the working line is significantly raised from its sea level position (Fig. 20). The relatively modest deceleration schedule gives a trajectory which is predicted to cross the revised surge line. Provision of a 10% IP Bleed leaves about half the surge margin (relative to the sea level working line) intact.

As seen in Fig. 21, the acceleration schedule is too rapid and the predicted trajectory in the HP Compressor crosses into surge. It is also seen that as at sea level, there is a considerable overshoot in $(N_H/\sqrt{T_{26}})$ before the engine stabilises. The trajectory in the HP Compressor during the deceleration is virtually unaffected by a 10% IP Bleed.

4. Discussion

The trajectories etc in the HP Compressor of the Tay are very much as expected and similar to those in the Spey. Little further comment is required at this stage.

The trajectories in the IP Compressor of the Tay and in the LP Compressor of the Spey are very different. A major factor obviously is that the bypass air is drawn from behind the Spey LP Compressor but from in front of the Tay IP Compressor. Other factors are suggested by examining the inter-relations of the shaft speeds during transients and at steady states. These are shown in Fig. 23 for the Spey (at sea level) and in Figs. 24 and 25 for the Tay at sea level and altitude respectively. Looking at the Spey engine, in the acceleration the LP spool "leads" the HP spool until the IGV turning range of the HP Compressor is reached (Point X, see also Fig. 26). For the range where the IGV's are turning (Points X to Y), the transient and steady conditions coincide. It therefore appears that, for the Spey, the relative spool inertias are well "matched" with the rate of change with speed of the air breathing requirements of the HP Compressor when in the IGV turning range, but not in the speed range below this. By comparison, for the Tay, the sea level results (Fig. 24) indicate that the "matching" is good at the low speed range below point X (note the first 9.7 sec. of acceleration), but poor where the IGV's are turning, and probably also at the high speed end.

A word of explanation might also be given as to why, in the decelerations the point at time zero in the LP Compressor of the Spey lies well below the steady-running line while in the IP Compressor of the Tay it lies virtually on the steady-running line (in this analysis the fuel flow is assumed to drop instantaneously to the Deceleration Schedule line, and the point marked as time zero is at this reduced fuel flow). In both engines the pressure at the final nozzle drops because of the reduced turbine exit temperature, the total engine airflow remaining almost constant because of the almost vertical pressure characteristics at high speed of the Spey LP Compressor and of the outer fan of the Tay. Hence in the Spey the LP Compressor delivery pressure drops, because of the bypass duct connection. However in the Tay the final nozzle pressure reduction merely drops the pressure delivered by the outer fan. The/

The inner fan has almost horizontal characteristics, so the delivery pressure from it is almost unaltered. The IP characteristics are almost vertical so there is almost no change in the flow conditions of the core air up to the inlet to the HP Compressor, hence no movement of the working point in the IP Compressor.

To summarise for the Tay engine, the factors which have significant bearing on the trajectory in the IP Compressor are probably:-

1. Relative shaft inertias. The problem in the deceleration would be eased by reducing LP shaft inertia.

2. The rate of change of air breathing capacity of the HP Compressor with speed. At present, in the IGV turning range, this is on the high side.

3. The rate of deceleration that is wished. The less rapid this is, the easier the trajectory.

4. The aerodynamic loadings of the compressors on the two shafts.

The movement towards surge in the IP Compressor during decelerations is greatly relieved by bleeding some air from the IP Compressor delivery into the bypass duct. A bleed of 10% appears more than adequate.

5. Further Work.

Further investigation is required of the treatment of the inner and outer sections of the fan, and of whether the flow distribution is as implied by the characteristics currently used in G519, or if there is some transient redistribution .

Investigation should be made of the thermal effects which alter surge lines and working lines. These have been ignored up till now, but an approximate allowance for the expected detrimental effects has been made by adding 30% to both shaft inertias.

The relative aerodynamic loading of shafts, and associated inertias, should be examined to determine if a better "split" of compression can be achieved.

6. Conclusions

Computational models have been developed for representing the transient performances of the Spey (RB183-02) and Tay (RB183-03) Engines. The predictions for the Spey engine are in line with experience and this encourages confidence in the modelling technique.

The predictions for the Tay engine show similar behaviour in the HP Compressor to that in the Spey. In the IP Compressor, trajectories in accelerations and decelerations move significantly away from the steady-running line. In the deceleration this movement is towards surge. At sea level the predicted trajectory just touches surge, and at altitude the situation is worse. This situation can be relieved by bleeding some air from the IP compressor delivery into the bypass duct. A bleed of 10% appears more than adequate.

Factors which influence the IP Compressor trajectory are discussed.

Suggestions for further work are made.

7. References

1. Fawke, A.J., and Saravanamutto, H.I.H. "Digital Computer Methods for Prediction of Gas Turbine Dynamic Response", SAE Paper 710550 (1971).

2. Thomson, B. "Basic Transient Effects of Aero Gas Turbines", AGARD Conference Proceedings 151 (Feb. 1975) and Yarker, A.(Rolls-Royce), Private Communication (1977).

3. Maccallum, N.R.L. "Axial Compressor Characteristics during Transients", AGARD Conference Proceeding 324 (Oct. 1982).

4. Pilidis, P., and Maccallum, N.R.L., "Models for Predicting Tip Clearance Changes in Gas Turbines", ibid.

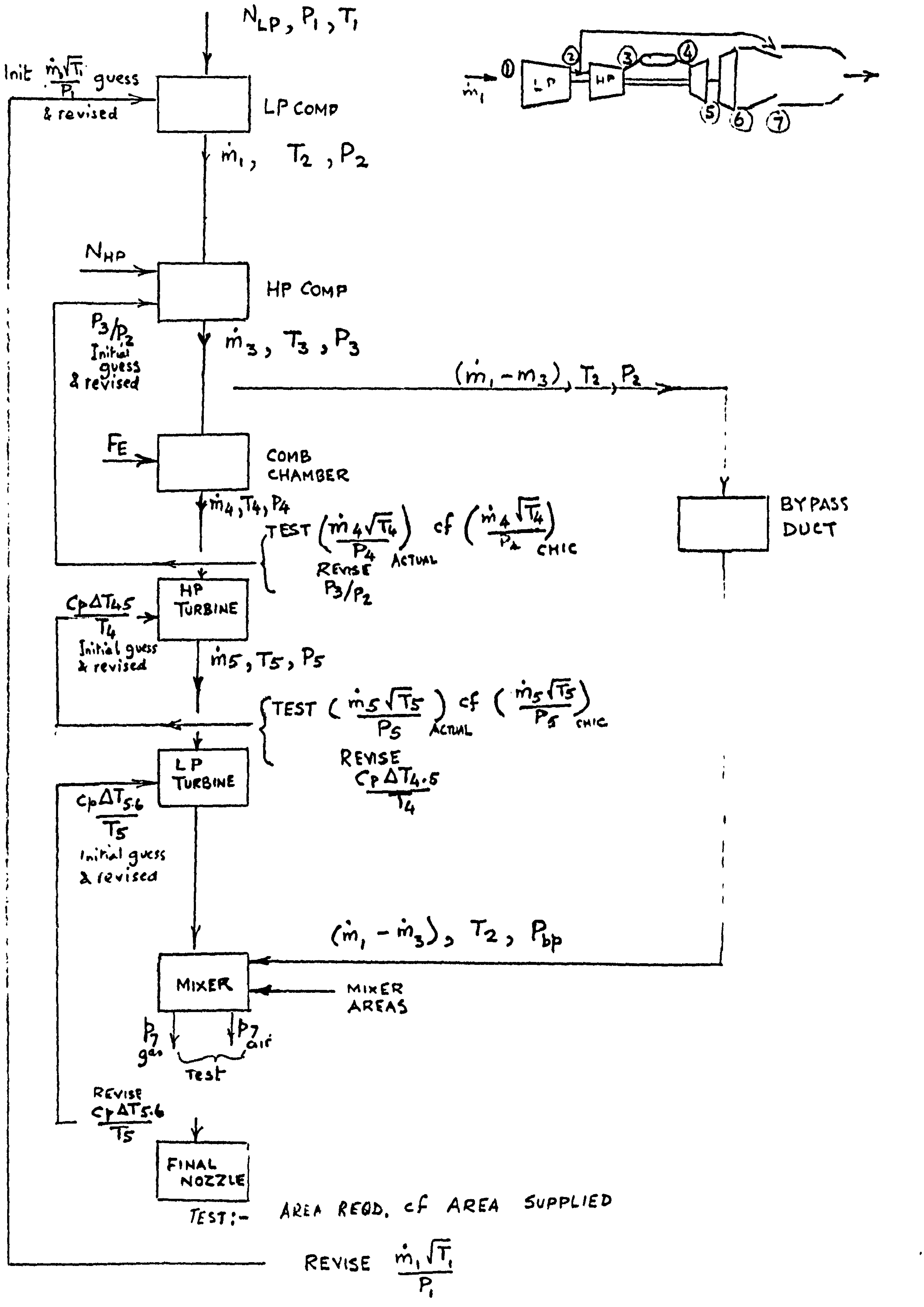


FIG. 1 LOGIC FOR SPEY RB 183-02

FIG. 2 SPEY RB 183-02 MK 555 - 15P

SEA LEVEL

ACCELERATION AND DECELERATION FUEL SCHEDULES
DEFINED BY CASC 211

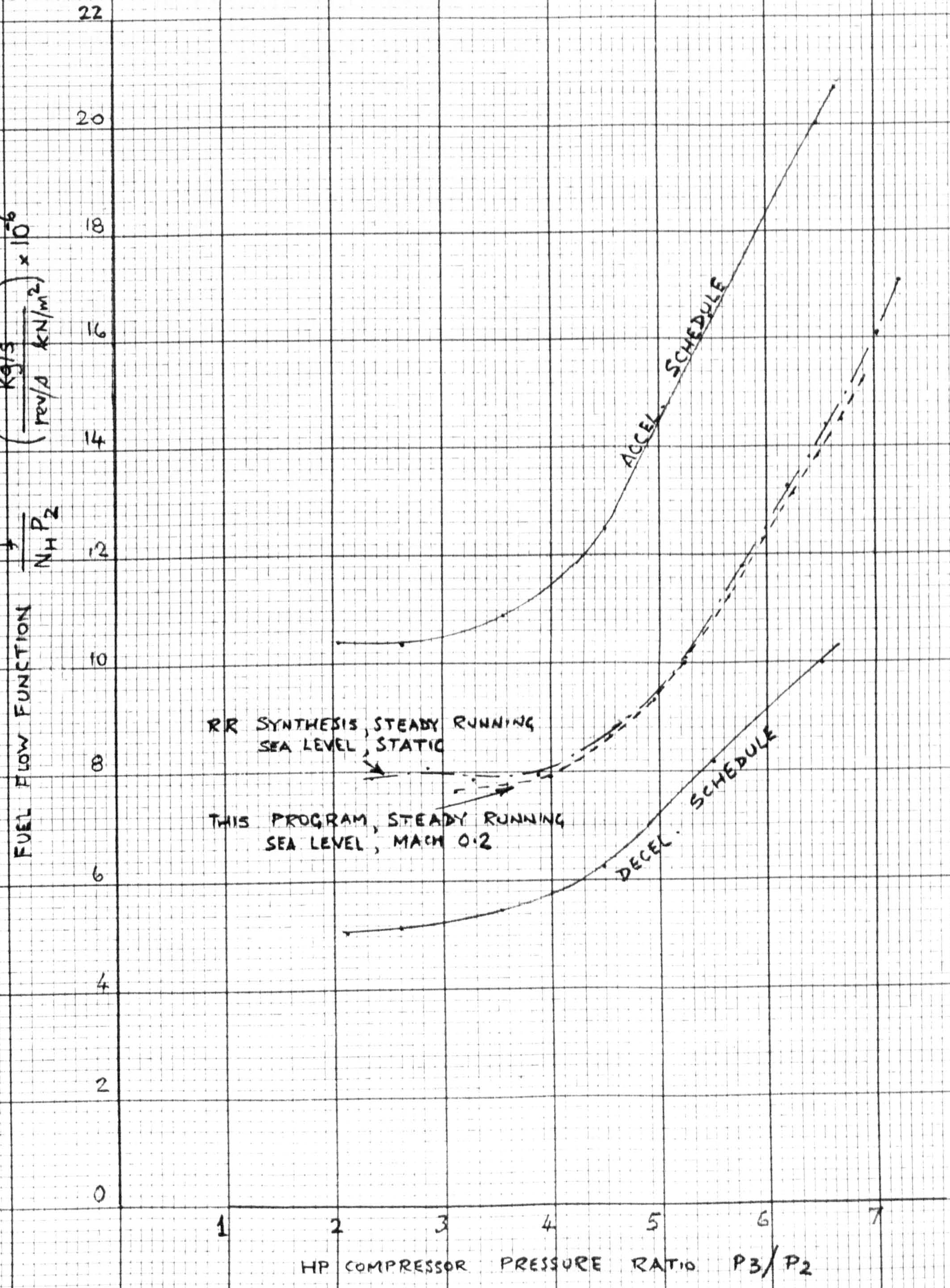


FIG 2

FIG. 3 SPEY RB 183-02 MK 555-15P

PREDICTED SPEED AND THRUST RESPONSES

SEA LEVEL 0.2 MACH NO

ACCEL. BY CASC 211 FROM 3080, 7220 To $\dot{f}_{max}$ = 0.71 KG/S

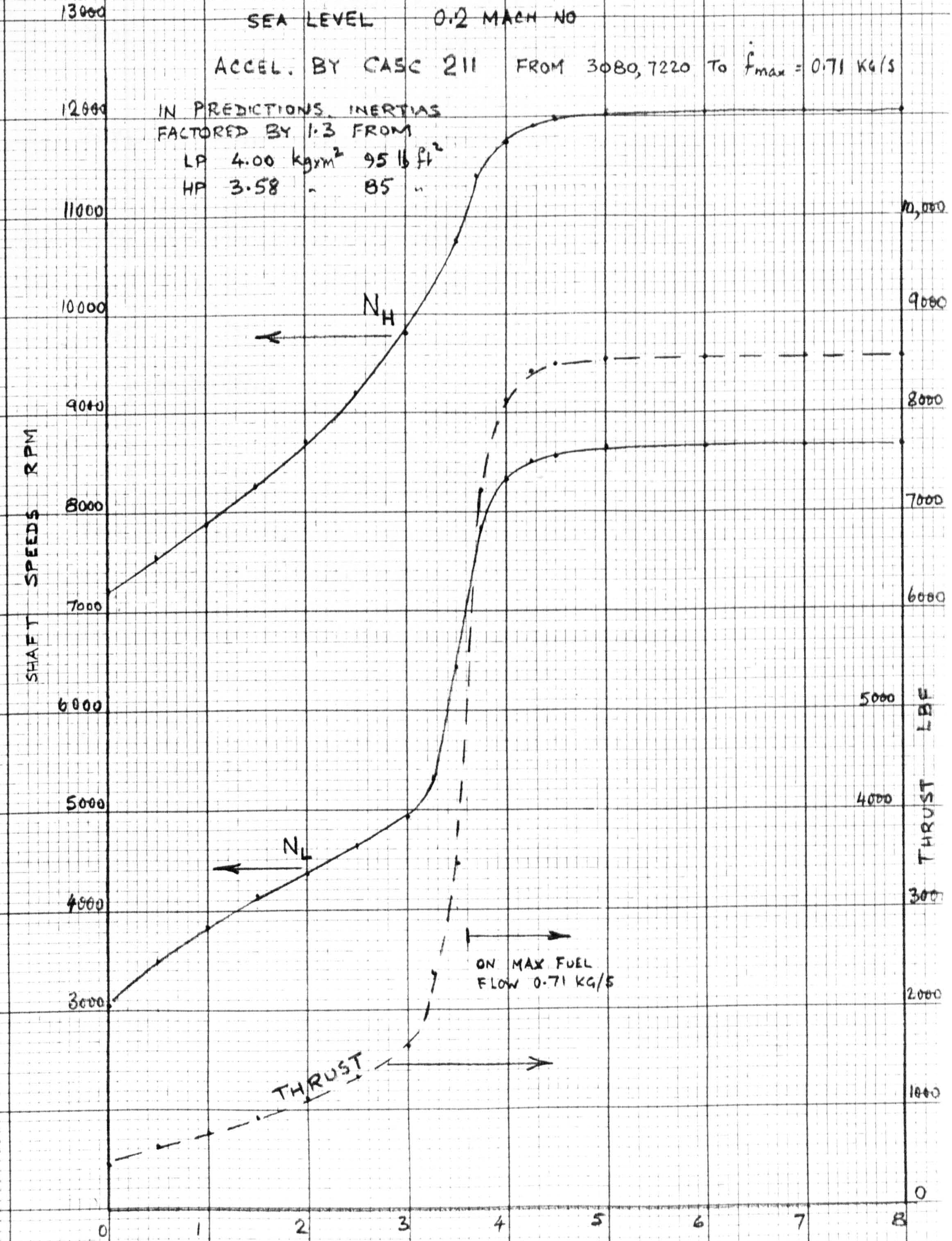


FIG 3

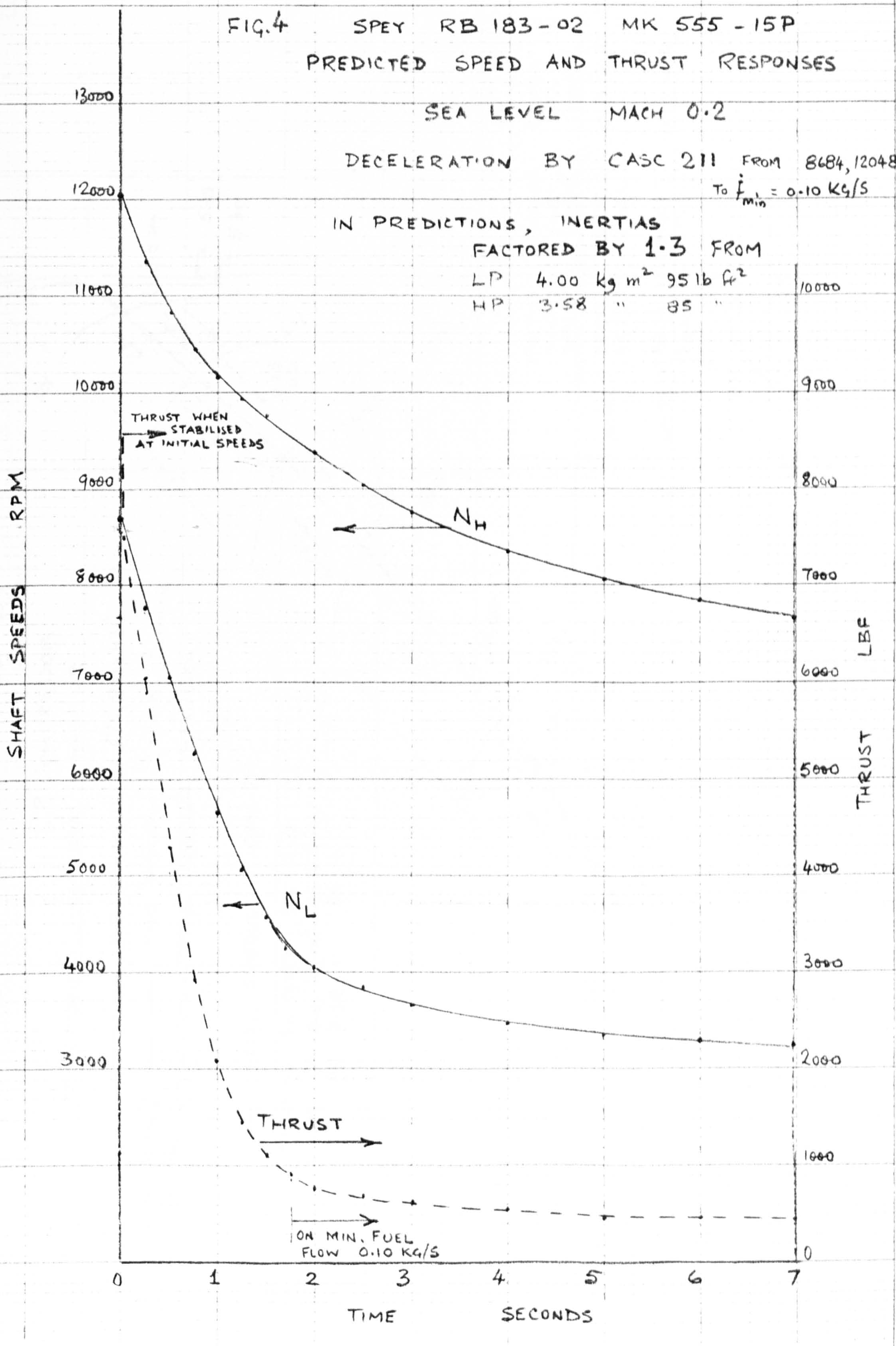


FIG.4 SPEY RB 183-02 MK 555-15P
PREDICTED SPEED AND THRUST RESPONSES
SEA LEVEL MACH 0·2
DECELERATION BY CASC 211 FROM 8684, 12048
To $\dot{f}_{min}$ = 0·10 KG/S
IN PREDICTIONS, INERTIAS FACTORED BY 1·3 FROM
LP 4.00 kg m² 95 lb ft²
HP 3·58 " 85 "
THRUST WHEN STABILISED AT INITIAL SPEEDS
N_H
N_L
THRUST
ON MIN. FUEL FLOW 0.10 KG/S
SHAFT SPEEDS RPM
THRUST LBF
TIME SECONDS
13000
12000
11000
10000
9000
8000
7000
6000
5000
4000
3000
10000
9000
8000
7000
6000
5000
4000
3000
2000
1000
0
0
1
2
3
4
5
6
7

FIG 4

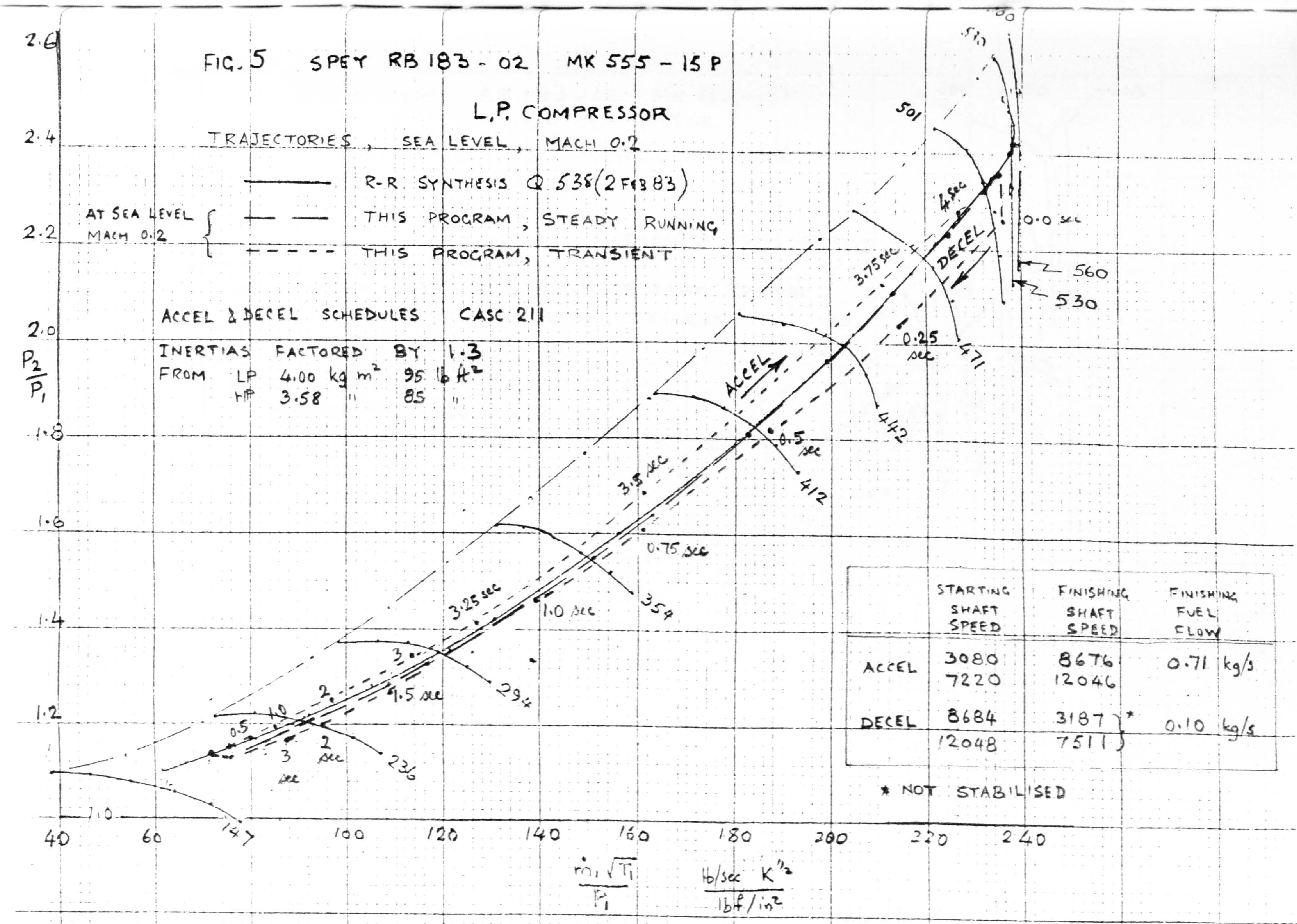


FIG. 5 SPEY RB 183 - 02 MK 555 - 15 P
L.P. COMPRESSOR
TRAJECTORIES, SEA LEVEL, MACH 0.2
R-R SYNTHESIS Q 538 (2 FEB 83)
AT SEA LEVEL MACH 0.2
THIS PROGRAM, STEADY RUNNING
THIS PROGRAM, TRANSIENT
ACCEL & DECEL SCHEDULES CASC 211
INERTIAS FACTORED BY 1.3
FROM LP 4.00 kg m² 95 lb ft²
HP 3.58 " 85 "
STARTING SHAFT SPEED
FINISHING SHAFT SPEED
FINISHING FUEL FLOW
ACCEL
3080
7220
8676
12046
0.71 kg/s
DECEL
8684
12048
3187
7511
*
0.10 kg/s
* NOT STABILISED
ACCEL
DECEL
0.0 sec
0.25 sec
0.5 sec
0.75 sec
1.0 sec
1.5 sec
2 sec
3 sec
3.25 sec
3.5 sec
3.75 sec
4 sec
560
530
501
471
442
412
354
294
236
147
P2/P1
2.6
2.4
2.2
2.0
1.8
1.6
1.4
1.2
1.0
40
60
100
120
140
160
180
200
220
240
ṁ1 √T1 / P1
lb/sec K½ / lbf/in²

FIG 5

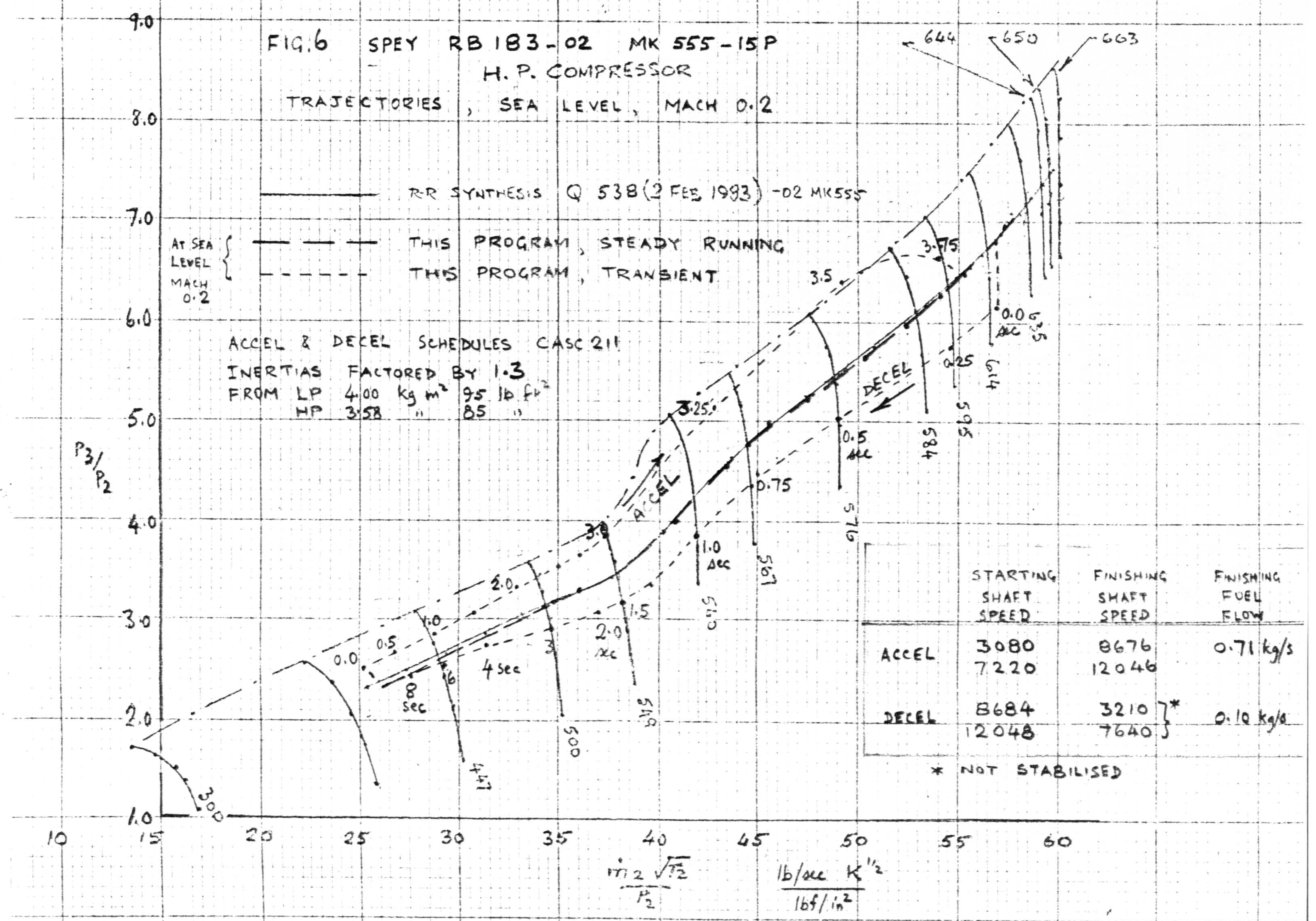


FIG.6 SPEY RB 183-02 MK 555-15P
H. P. COMPRESSOR
TRAJECTORIES, SEA LEVEL, MACH 0.2
R-R SYNTHESIS Q 538 (2 FEB 1983) -02 MK555
AT SEA LEVEL MACH 0.2
THIS PROGRAM, STEADY RUNNING
THIS PROGRAM, TRANSIENT
ACCEL & DECEL SCHEDULES CASC 211
INERTIAS FACTORED BY 1.3
FROM LP 4.00 kg m² 95 lb ft²
HP 3.58 " 85 "
STARTING SHAFT SPEED
FINISHING SHAFT SPEED
FINISHING FUEL FLOW
ACCEL 3080 8676 0.71 kg/s
7220 12046
DECEL 8684 3210 * 0.10 kg/s
12048 7640
* NOT STABILISED
ACCEL
DECEL
P3/P2
ṁ2 √T2 / P2 lb/sec K^½ / lbf/in²

FIG 6

FIG. 7 LOGIC FOR 2-SPOOL TURBOFAN (TAY) WITH (FAN + IP) ARRANGEMENT

FIG. 8 TAY RB 183-03

SEA LEVEL

ACCELERATION AND DECELERATION FUEL SCHEDULES DEFINED AS FRACTIONS OF CASC 211

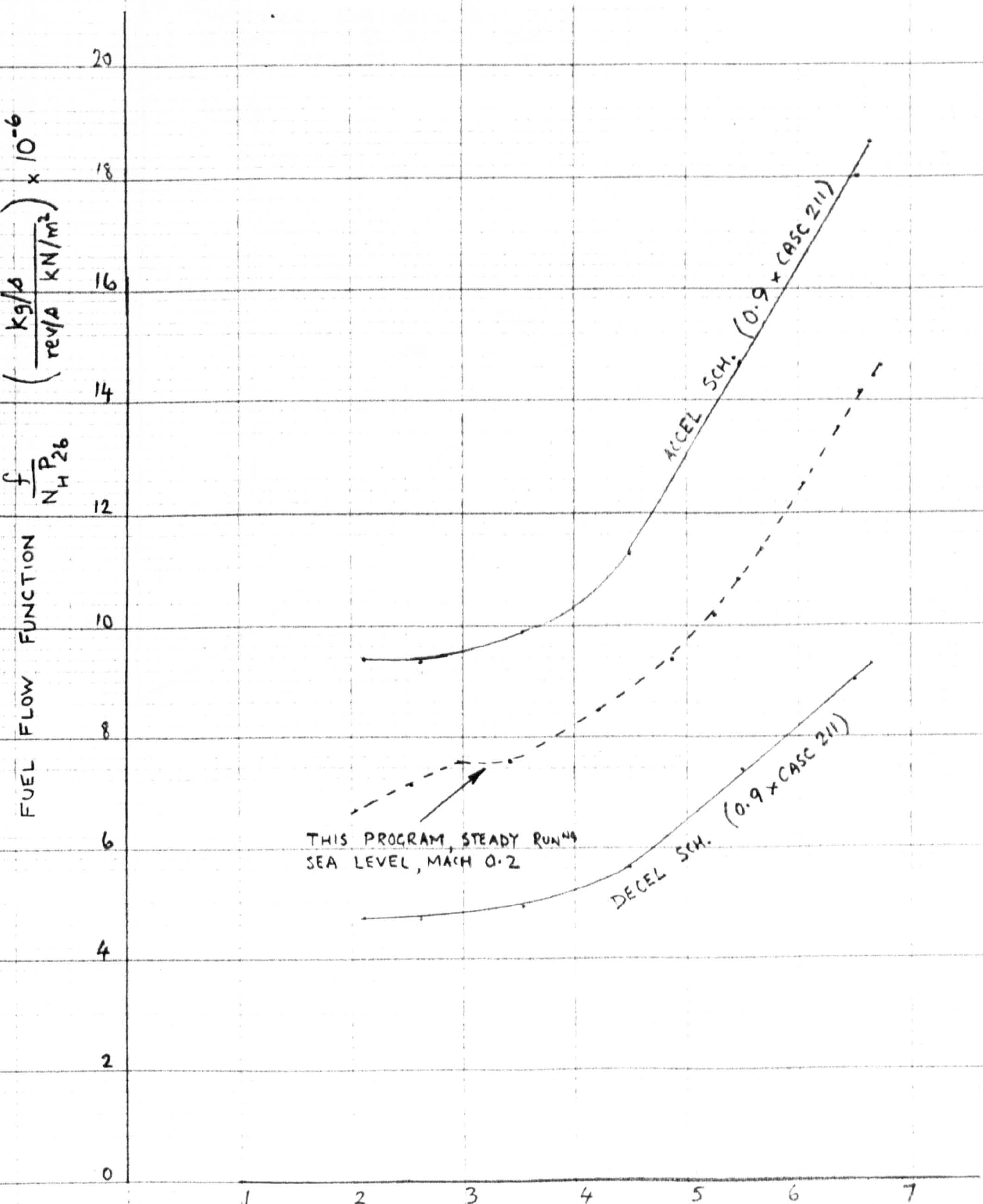


FIG 8

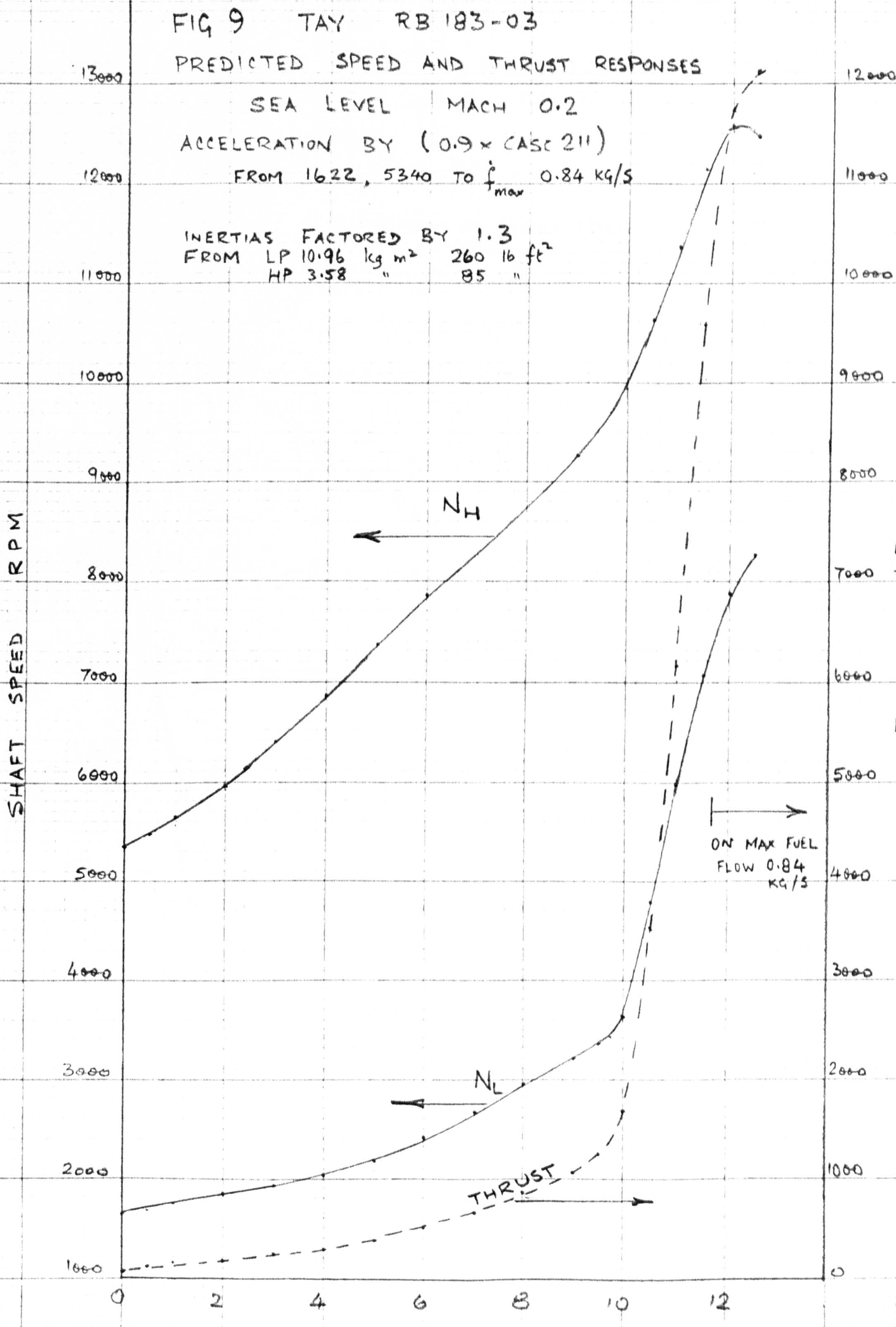


FIG 9 TAY RB 183-03
PREDICTED SPEED AND THRUST RESPONSES
SEA LEVEL MACH 0.2
ACCELERATION BY (0.9 × CASE 211)
FROM 1622, 5340 TO $\dot{f}_{max}$ 0.84 KG/S
INERTIAS FACTORED BY 1.3
FROM LP 10.96 kg m² 260 lb ft²
HP 3.58 " 85 "
N_H
N_L
THRUST
ON MAX FUEL FLOW 0.84 KG/S
SHAFT SPEED RPM
THRUST LBF
TIME SECONDS
1000
2000
3000
4000
5000
6000
7000
8000
9000
10000
11000
12000
13000
0
1000
2000
3000
4000
5000
6000
7000
8000
9000
10000
11000
12000
0
2
4
6
8
10
12

FIG 9


FIG 10 TAY RB 183 -03
PREDICTED SPEED AND THRUST RESPONSES
SEA LEVEL MACH 0·2
DECEL. BY (0·9 × CASC 211)
FROM 8547, 12186 TO f min 0·06 KG/S
INERTIAS FACTORED BY 1·3
FROM LP 10·96 kg m² 260 lb ft²
HP 3·58 " 85 "
THRUST WHEN STABILISED AT INITIAL SPEEDS
SHAFT SPEEDS RPM, THRUST LBF
13000
12000
11000
10000
9000
8000
7000
6000
5000
4000
3000
2000
1000
0
N_H
ZERO IP BLEED
10% IP BLEED
N_L
HARDLY AFFECTED BY 10% IP BLEED
THRUST
ON MIN. FUEL 0·06 KG/S
0
1
2
3
4
5
6
7
8
TIME SECONDS


FIG 11 RB183-03 TAY ENGINE
INNER FAN TRAJECTORIES IN ACCEL & DECEL WITH ZERO IP BLEED
[TRAJECTORY IN DECEL WITH 10% BLEED
VIRTUALLY IDENTICAL TO DECEL WITH ZERO BLEED]
SEA LEVEL , MACH 0.2
RR SYNTHESIS C519 (4 FEB 1983) SEA LEVEL, STATIC
SEA LEVEL MACH 0.2
THIS PROGRAM , STEADY RUNNING, ZERO IP BLEED
A A A A " " ACCEL , ZERO IP BLEED
D D D D " " DECEL , " " "
ACCEL AND DECEL SCHEDULES (0.9 x CASC 2H)
INERTIAS FACTORED BY 1.3
FROM LP 10.96 kg m² 260 lb ft²
HP 3.58 " 85 "
P24 / P1
STARTING SHAFT SPEED
FINISHING SHAFT SPEED
FINISHING FUEL FLOW
ACCEL 1622 5340 8278 12482 * 0.84 kg/s
DECEL 8547 12186 1856 6461 * 0.06 kg/s
* NOT STABILISED
465.9
449.3
372.7
326.2
279.6
186.4
11.5 sec
11 sec
10 sec
10.5
12.5
12
0.25
0.5
0.75
1.0
1.5
2 sec
1.8
1.7
1.6
1.5
1.4
1.3
1.2
1.1
10 20 30 40 50 60 70 80 90 100 110 120 130 140
ṁin √Tin / Pin lb/sec K^½ / lbf/in²
FIG 11

FIG.12 RB 183 - 03 TAY ENGINE

OUTER FAN TRAJECTORIES IN ACCEL & DECEL WITH ZERO IP BLEED

[TRAJECTORY IN DECEL WITH 10% IP BLEED IS VIRTUALLY IDENTICAL TO DECEL WITH ZERO BLEED]

SEA LEVEL, MACH 0.2

	STARTING SHAFT SPEED	FINISHING SHAFT SPEED	FINISHING FUEL FLOW
ACCEL	1622 5340	8278 * 12482	0.84 kg/s
DECEL	8547 12186	1856 * 6461	0.06 kg/s

FIG.12

FIG 13. RB 183-03 TAY ENGINE

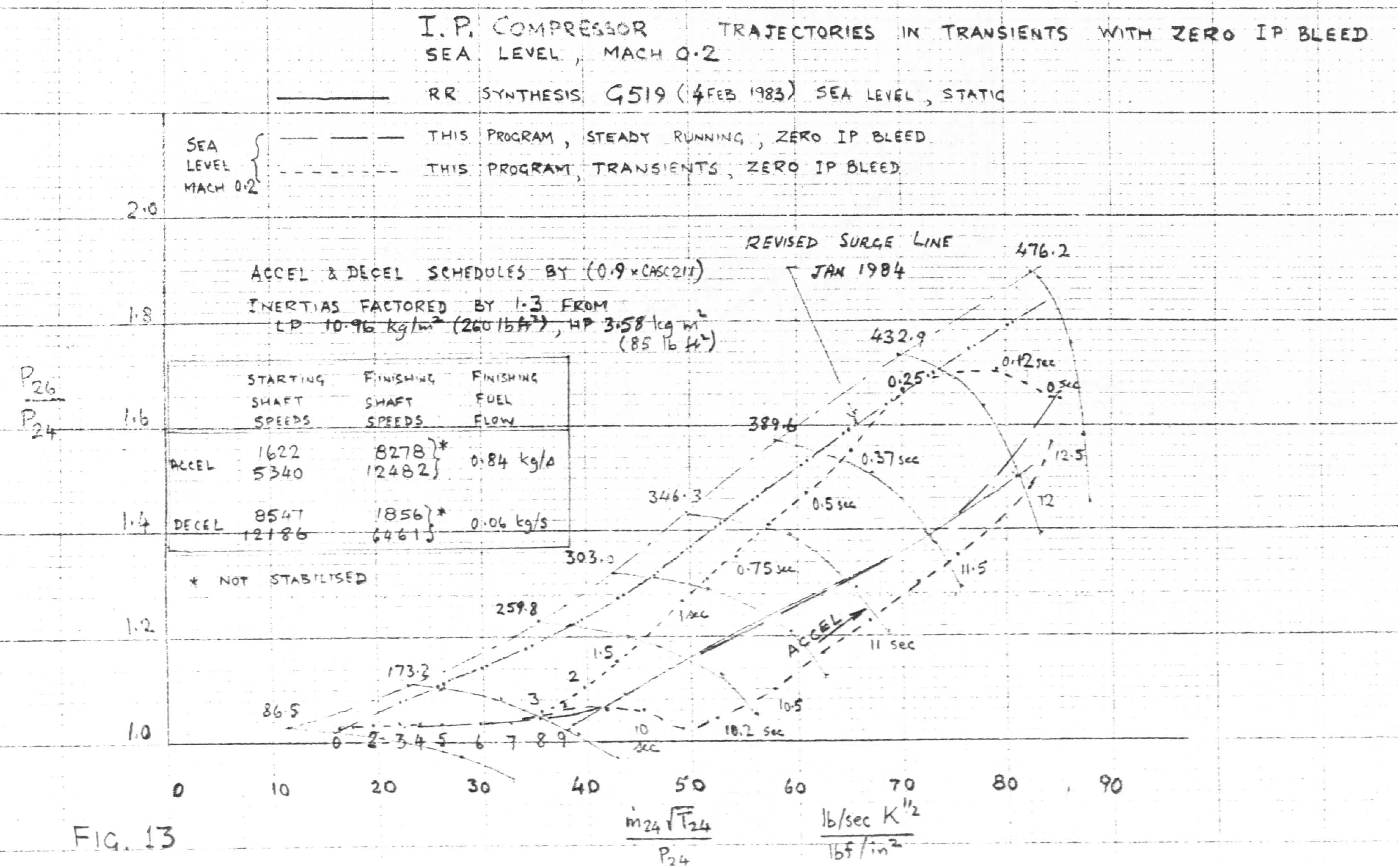


FIG. 13


FIG 14
RB 183-03 TAY ENGINE
H.P. COMPRESSOR
TRAJECTORIES IN TRANSIENTS WITH ZERO IP BLEED
SEA LEVEL, MACH 0.2
R-R SYNTHESIS SEA LEVEL, STATIC
SEA LEVEL
MACH 0.2
THIS PROGRAM, STEADY RUNNING, ZERO IP BLEED
THIS PROGRAM, TRANSIENTS, ZERO I.P. BLEED
ACCEL & DECEL SCHEDULES (0.9 x CASE 211)
INERTIAS FACTORED BY 1.3
FROM LP 10.96 kg m² 260 lb ft²
HP 3.58 " 85 "
STARTING SHAFT SPEEDS
FINISHING SHAFT SPEEDS
FINISHING FUEL FLOW
ACCEL 1622 5340 8278 12482 * 0.84 kg/s
DECEL 8547 12186 1856 6461 * 0.06 kg/s
* NOT STABILISED
ACCEL
DECEL
P_3/P_{26}
$\dot{m}_{26}\sqrt{T_{26}}/P_{26}$ lb/sec K^½ / lbf/in²
300
447
500
549
560
567
576
584
595
614
635
644
650
663
8 sec
0.0 sec
0.12 sec
0.25 sec
0.5 sec
0.75 sec
1.0 sec
9.0
8.0
7.0
6.0
5.0
4.0
3.0
2.0
1.0
10
15
20
25
30
35
40
45
50
55
60

FIG. 14

FIG. 15 RB 183-03 TAY ENGINE

I. P. COMPRESSOR TRAJECTORY IN DECEL WITH 10% IP BLEED
SEA LEVEL, MACH 0.2

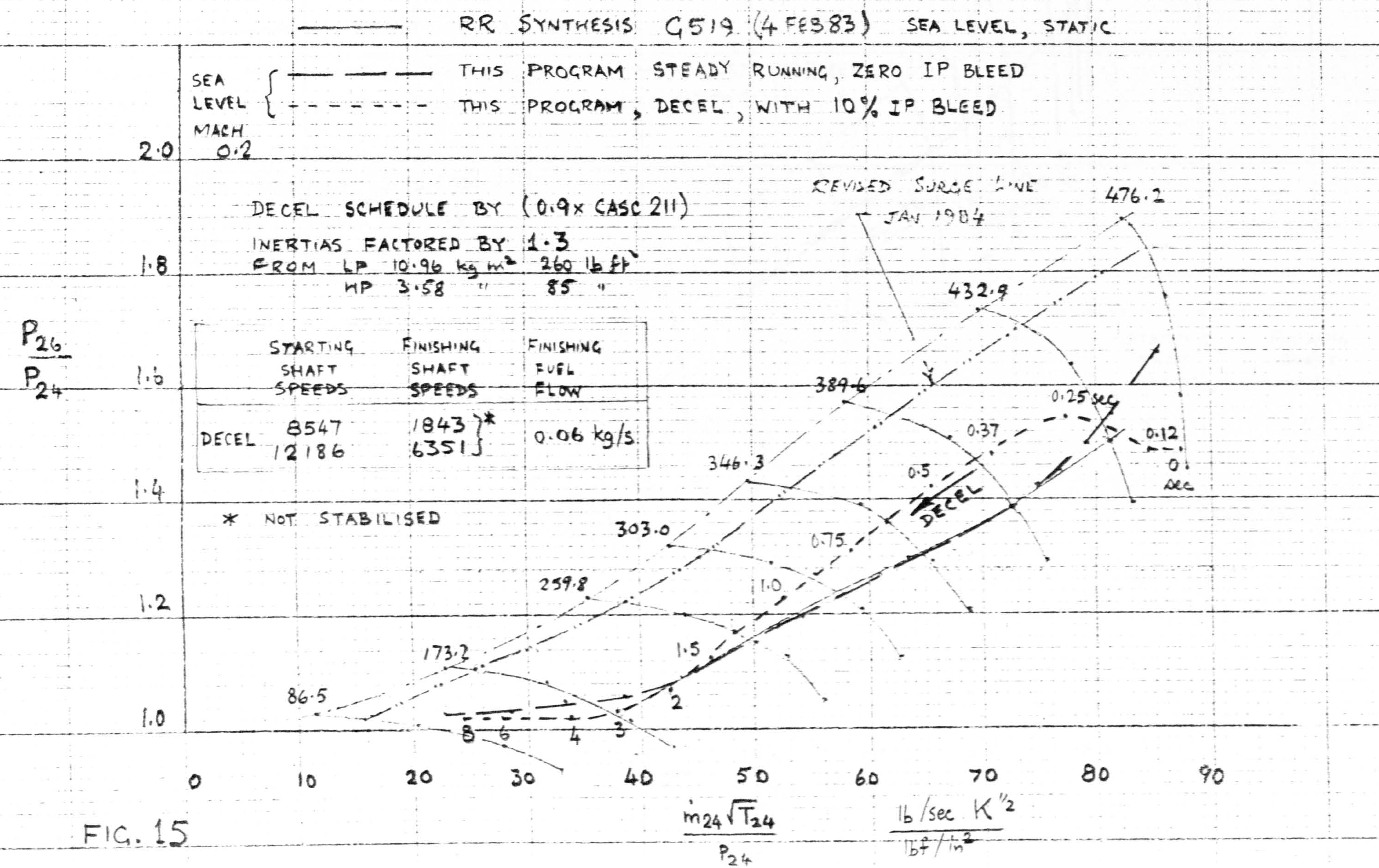


FIG. 15

FIG 16 RB 183-03 TAY ENGINE
H.P. COMPRESSOR
TRAJECTORY IN DECEL WITH 10% I.P. BLEED
SEA LEVEL, MACH 0.2
R-R SYNTHESIS, SEA LEVEL, STATIC
SEA LEVEL MACH 0.2
THIS PROGRAM, STEADY RUNNING, ZERO I.P. BLEED
THIS PROGRAM, TRANSIENT DECEL, 10% I.P. BLEED
DECEL SCHEDULE (0.9x CASC 21)
INERTIAS FACTORED BY 1.3
FROM LP 10.96 kg m² 260 lb ft²
HP 3.58 " 85 "
STARTING SHAFT SPEEDS
FINISHING SHAFT SPEEDS
FINISHING FUEL FLOW
DECEL
8547
12186
1843
6351
0.06 kg/s
* NOT STABILISED
644
650
663
635
614
595
584
576
567
560
549
500
447
300
0.0 sec
0.12
0.25 sec
0.5 sec
0.75
1.0 sec
2
3
4
5
6
8 sec
DECEL
P_3/P_{26}
9.0
8.0
7.0
6.0
5.0
4.0
3.0
2.0
1.0
10
15
20
25
30
35
40
45
50
55
60
$\frac{\dot{m}_{26}\sqrt{T_{26}}}{P_{26}}$ $\frac{lb/sec\ K^{1/2}}{lbf/in^2}$

FIG. 16

FIG 17 TAY RB 183-03

ALTITUDE 41,000 FT

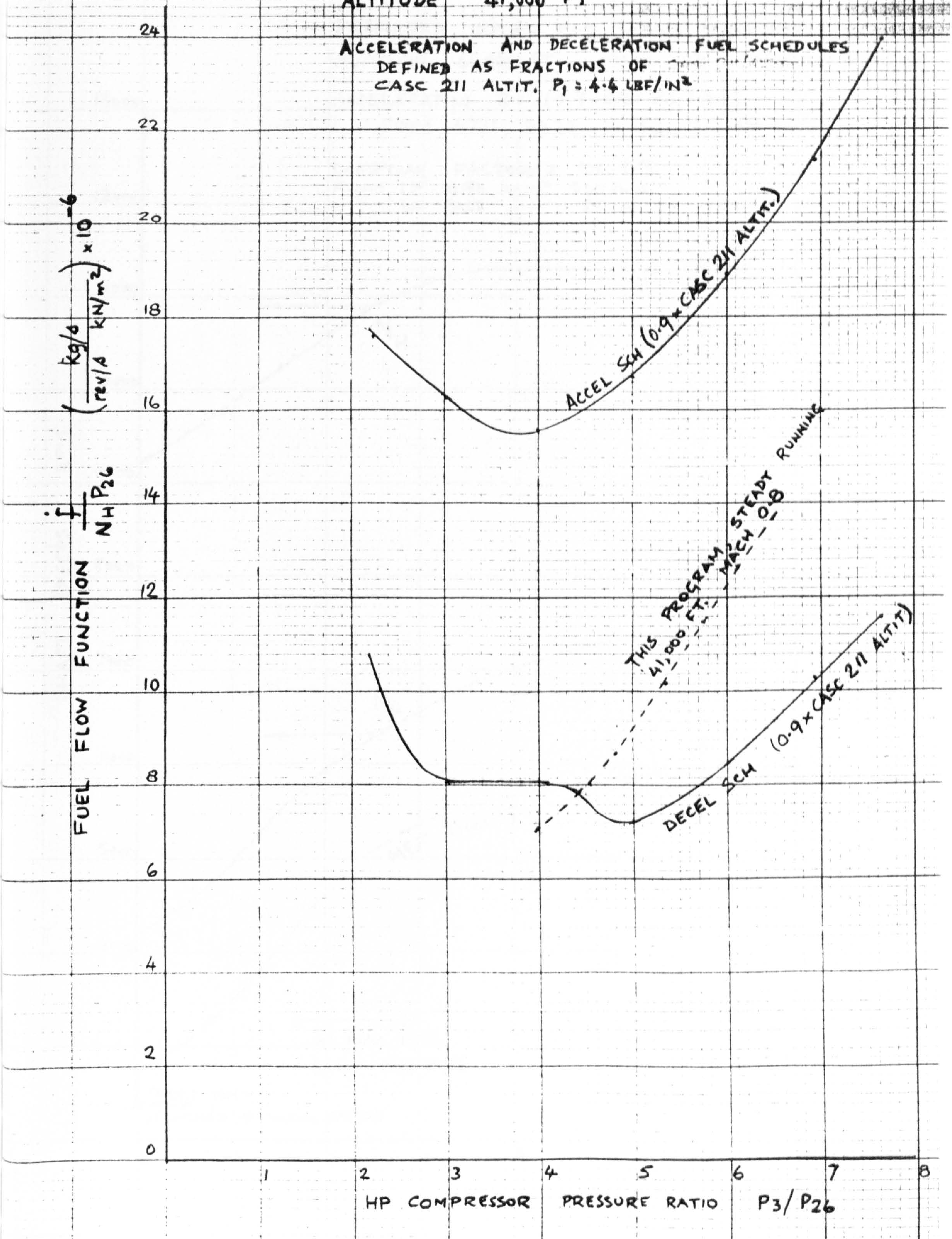


FIG 17

FIG. 18 TAY RB 183-03

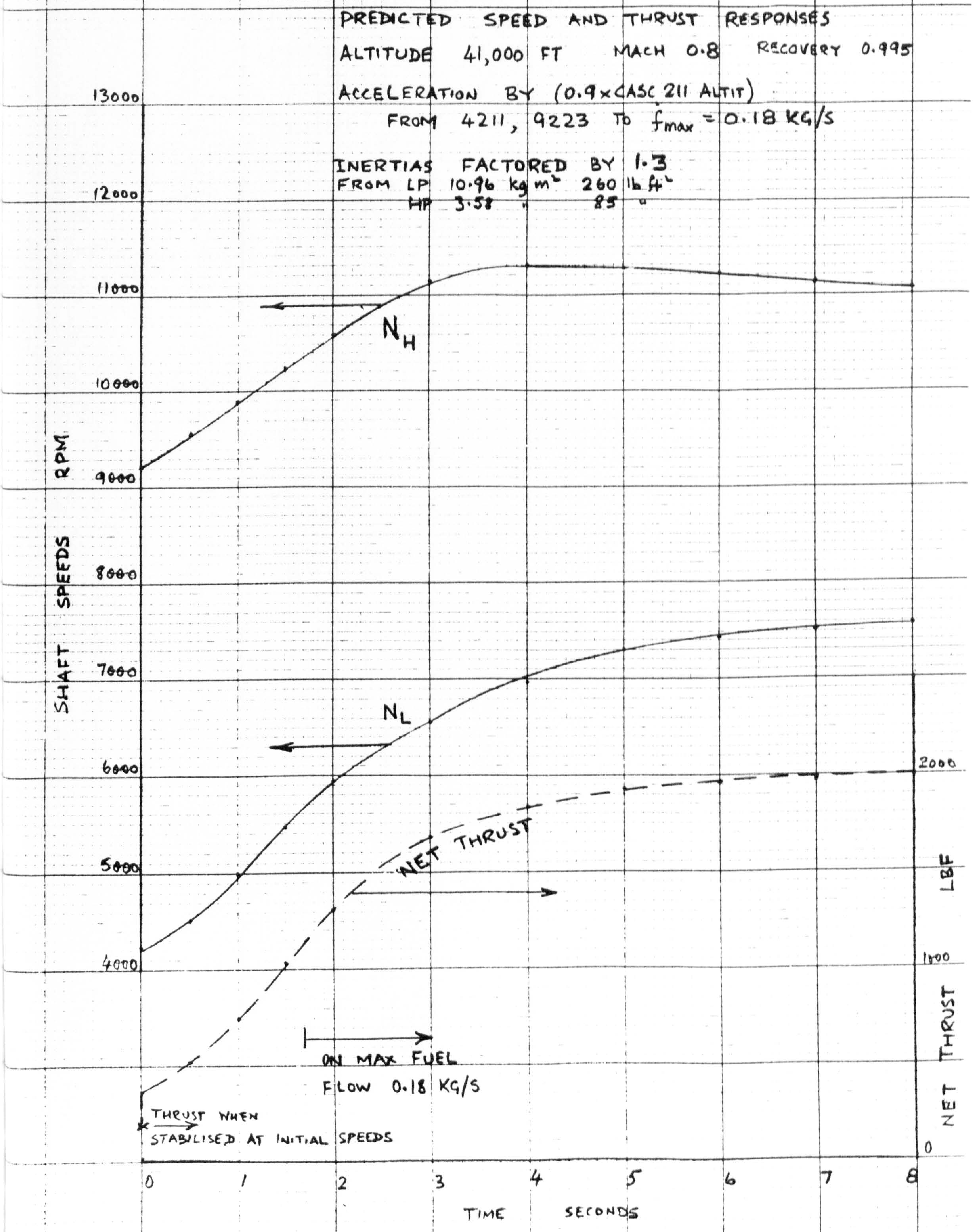


FIG 18

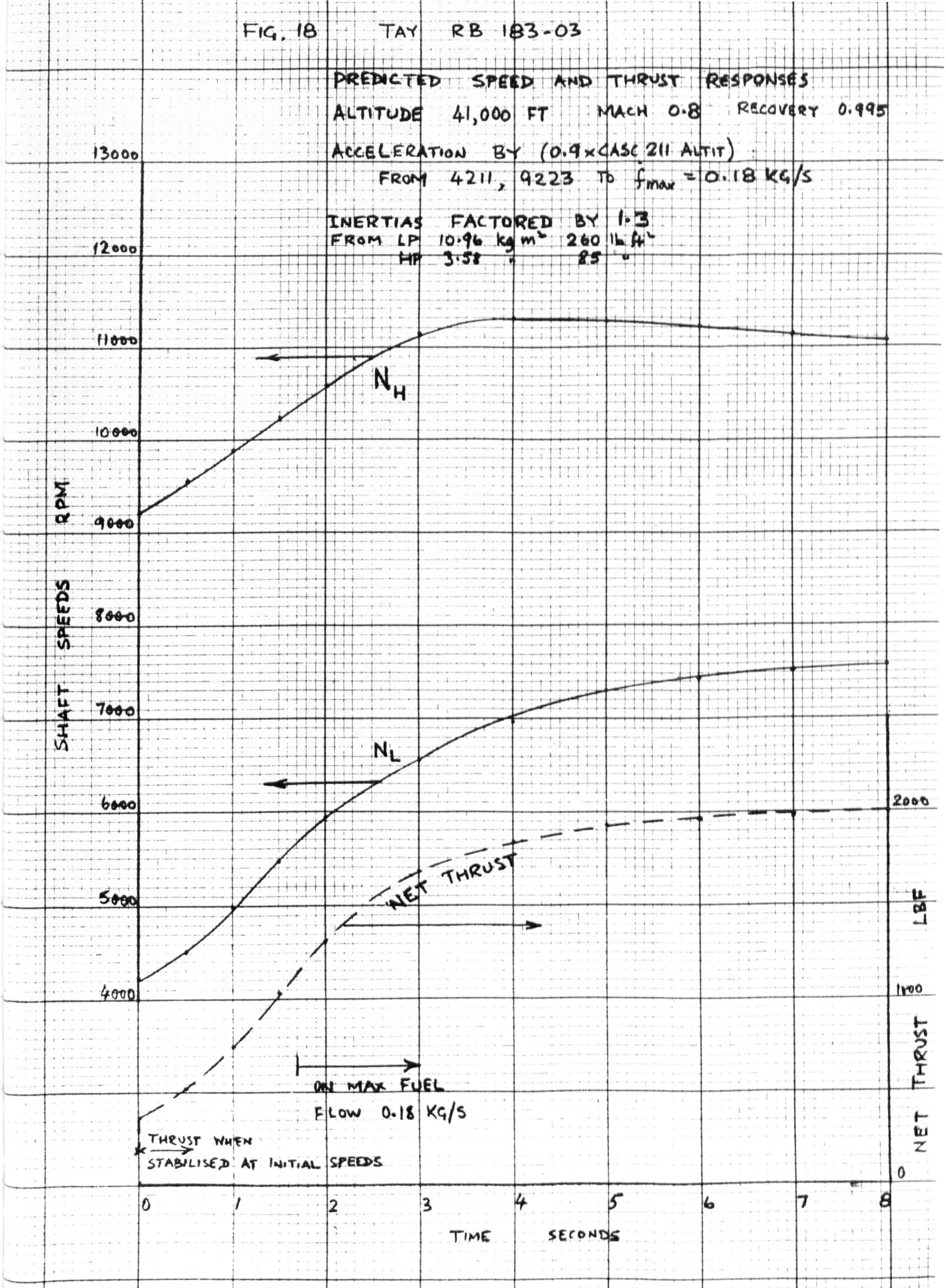


FIG. 18 TAY RB 183-03
PREDICTED SPEED AND THRUST RESPONSES
ALTITUDE 41,000 FT MACH 0.8 RECOVERY 0.995
ACCELERATION BY (0.9×CASC 211 ALTIT)
FROM 4211, 9223 TO f_{max} = 0.18 KG/S
INERTIAS FACTORED BY 1.3
FROM LP 10.96 kg m² 260 lb ft²
HP 3.58 " 85 "
13000
12000
11000
10000
9000
8000
7000
6000
5000
4000
SHAFT SPEEDS RPM
N_H
N_L
NET THRUST
2000
1000
0
NET THRUST LBF
ON MAX FUEL
FLOW 0.18 KG/S
THRUST WHEN
STABILISED AT INITIAL SPEEDS
0
1
2
3
4
5
6
7
8
TIME SECONDS

FIG 18

FIG 19 TAY RB 183 - 03

PREDICTED SPEED AND THRUST RESPONSES

ALTITUDE 41,000 FT MACH 0.8 RECOVERY 0.995.

DECELERATION BY (0.9 × CASC 211 ALTIT)

FROM 7706, 10946

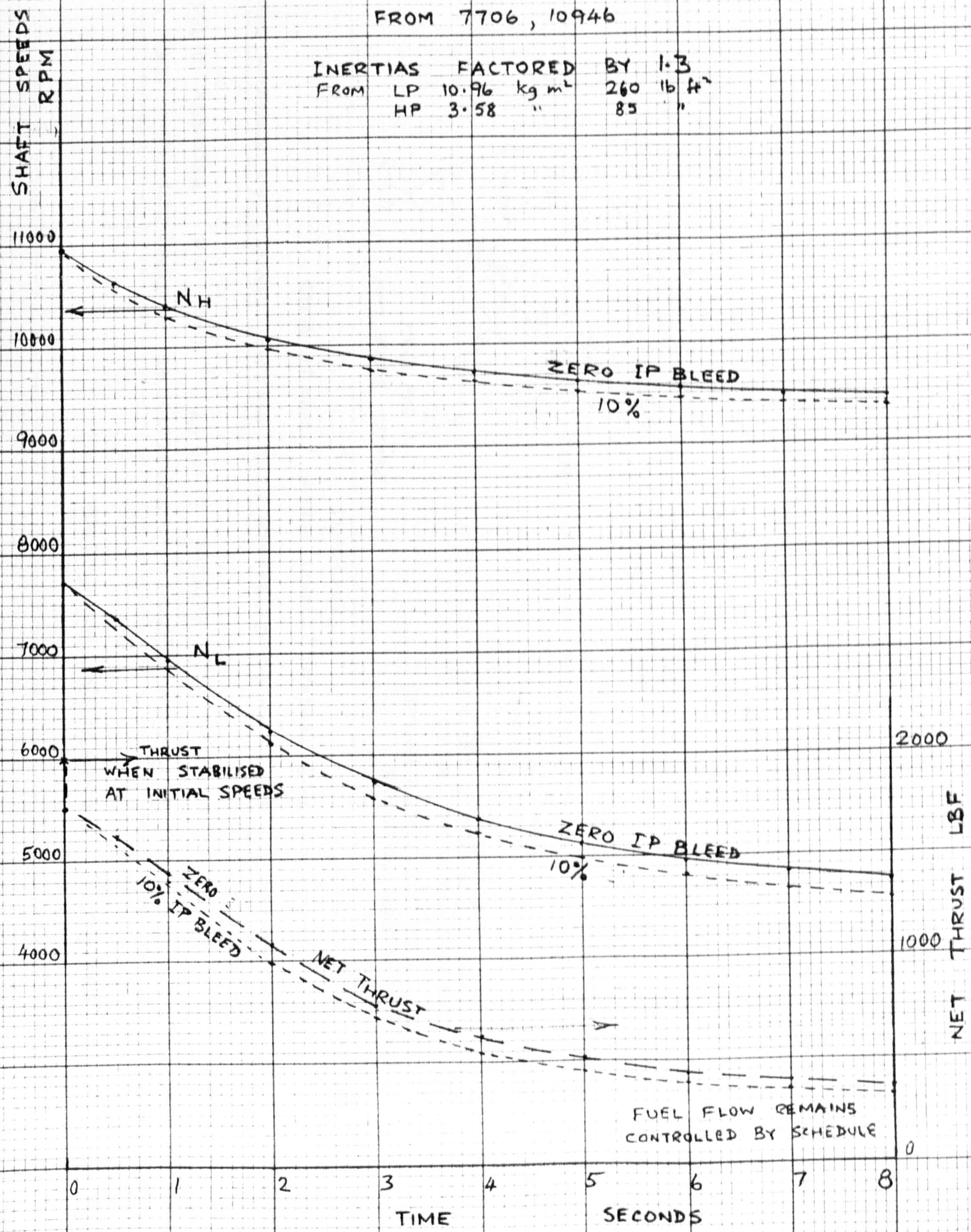


FIG 19

FIG.20 RB 183-03 TAY ENGINE

I.P. COMPRESSOR TRAJECTORIES IN TRANSIENTS WITH ZERO IP BLEED
ALTITUDE 41,000 FT, MACH 0.8, RECOVERY 0.995.

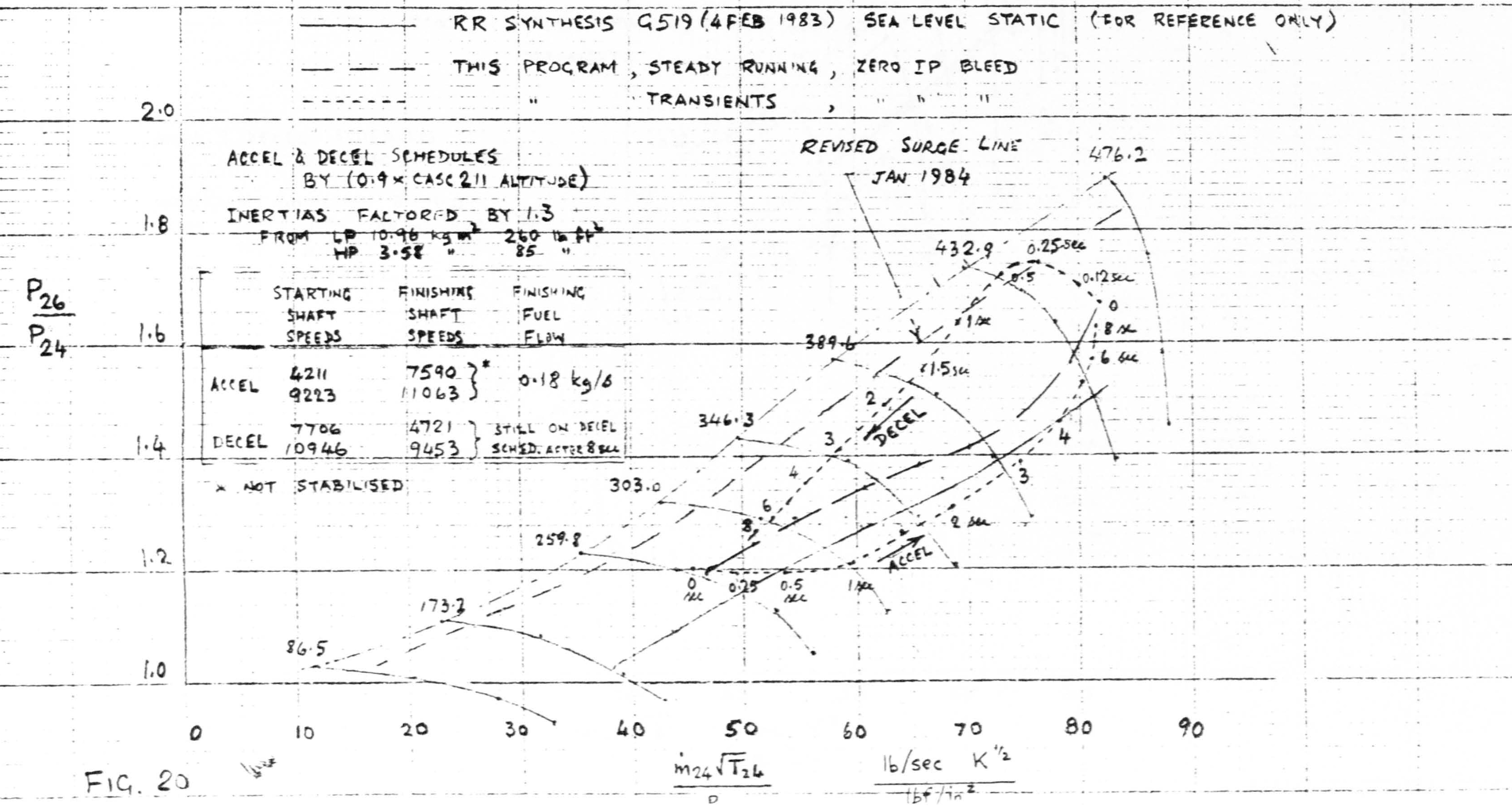


FIG. 20


FIG. 21 RB183-03 TAY ENGINE
H.P. COMPRESSOR
TRAJECTORIES IN TRANSIENTS WITH ZERO IP BLEED
(EFFECT OF IP BLEED ON TRAJECTORIES IN H.P. COMPR. IS SMALL)
ALTITUDE 41,000 FT, MACH 0.8, RECOVERY 0.995
R-R SYNTHESIS, SEA LEVEL STATIC, (FOR REF)
THIS PROGRAM, STEADY RUNNING, ZERO IP BLEED
" " , TRANSIENTS, " " "
ACCEL & DECEL SCHEDULES
BY (0.9 * CASE 211 ALTITUDE)
INERTIAS FACTORED BY 1.3
FROM LP 10.96 kg m² 260 lb ft²
HP 3.58 " 85 "
STARTING SHAFT SPEEDS
FINISHING SHAFT SPEEDS
FINISHING FUEL FLOW
ACCEL 4211 9223 7590 11063 * 0.18 kg/s
DECEL 7706 10946 4721 9453 STILL ON DECEL SCHED. AFTER 8 SEC
ACCEL
DECEL
663
650
664
635
614
595
584
576
567
560
549
500
447
300
P3/P26
8.0
7.0
6.0
5.0
4.0
3.0
2.0
1.0
15
20
25
30
35
40
45
50
55
60
ṁ26√T26 / P26 lb/sec K½ / lbf/in²

FIG. 21


FIG 22 RB 183-03 TAY ENGINE
I.P. COMPRESSOR TRAJECTORY IN DECEL WITH 10% IP BLEED
ALTITUDE 41,000 FT, MACH 0.8, RECOVERY 0.995
RR SYNTHESIS G519 (4 FEB 1983) SEA LEVEL, STATIC (FOR REFERENCE ONLY)
THIS PROGRAM, STEADY RUNNING, ZERO IP BLEED
" " , DECEL, 10% IP BLEED
REVISED SURGE LINE JAN 1984
DECEL SCHEDULE (0.9 * CASE 211 ALTITUDE)
INERTIAS FACTORED BY 1.3 FROM LP 10.96 kgm² 260 lb ft²
HP 3.58 85
STARTING SHAFT SPEEDS
FINISHING SHAFT SPEEDS
FINISHING FUEL FLOW
DECEL 7706 10946
4577 9536
STILL ON DECEL SCHED, AFTER 8 sec
f = 0.045 kg/sec
476.2
432.9
389.6
346.3
303.0
259.8
173.2
86.5
0 sec
0.25 sec
0.5 sec
1 sec
1.5
2
3
4
6
8
DECEL
P26/P24
2.0
1.8
1.6
1.4
1.2
1.0
0
10
20
30
40
50
60
70
80
90
$\frac{\dot{m}_{24}\sqrt{T_{24}}}{P_{24}}$

FIG. 22

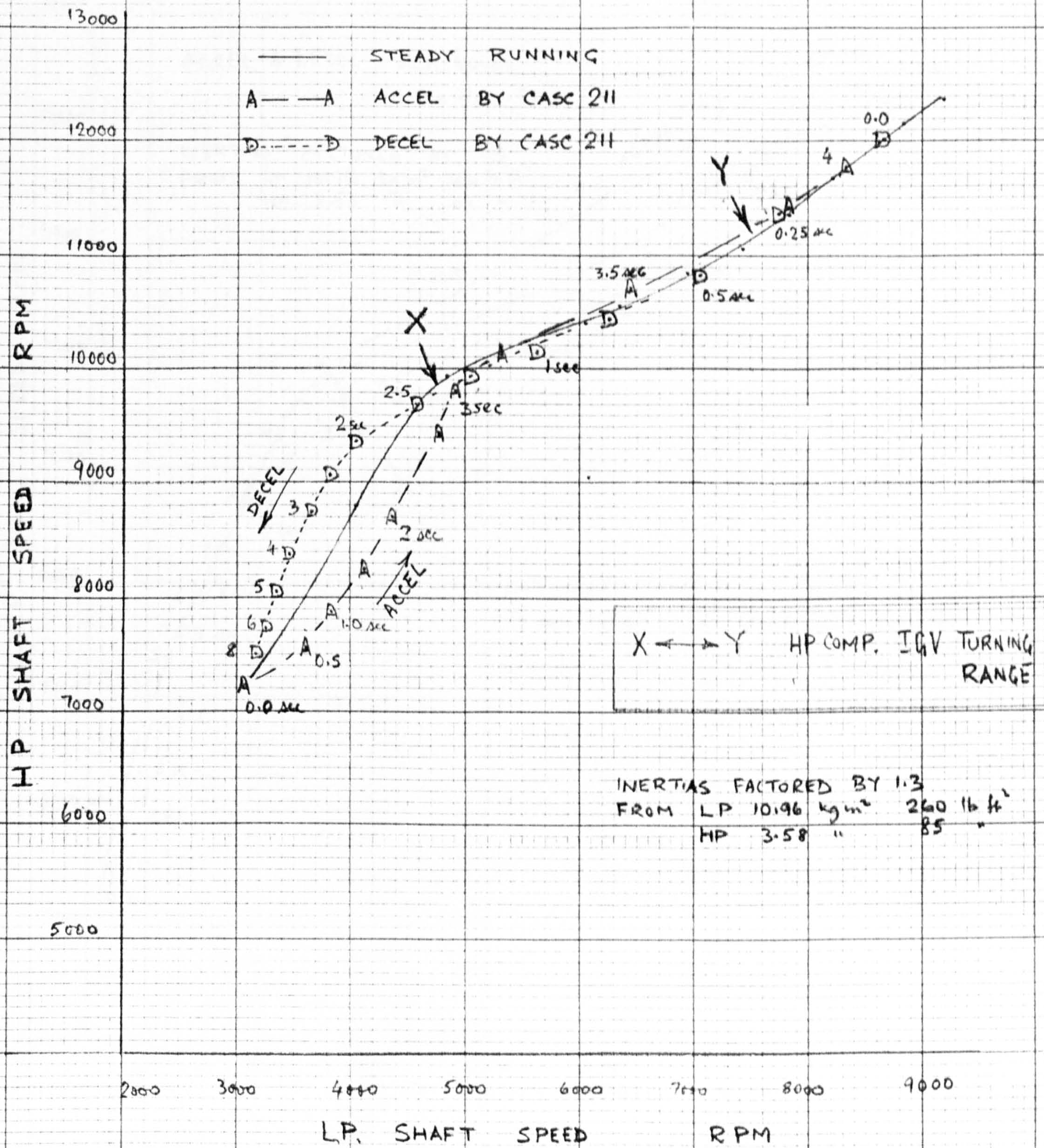


Fig 23 SPEY RB 183-02 MK 555-15P

PREDICTED LP-HP SHAFT SPEED RELATIONSHIPS

SEA LEVEL, MACH 0.2

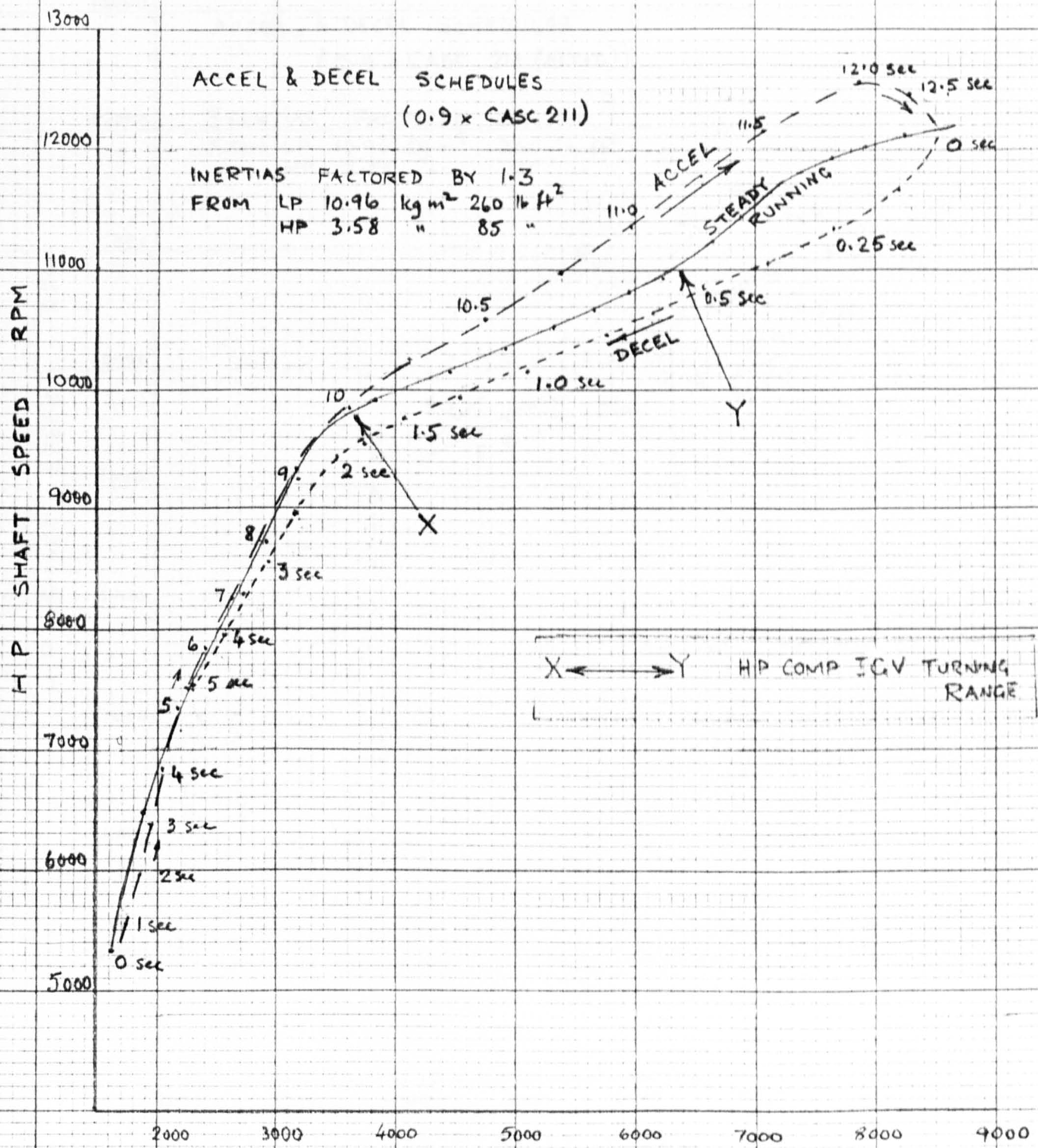


FIG 24 TAY RB 183 - 03

PREDICTED LP - HP SHAFT SPEED RELATIONSHIPS

SEA LEVEL, MACH 0.2

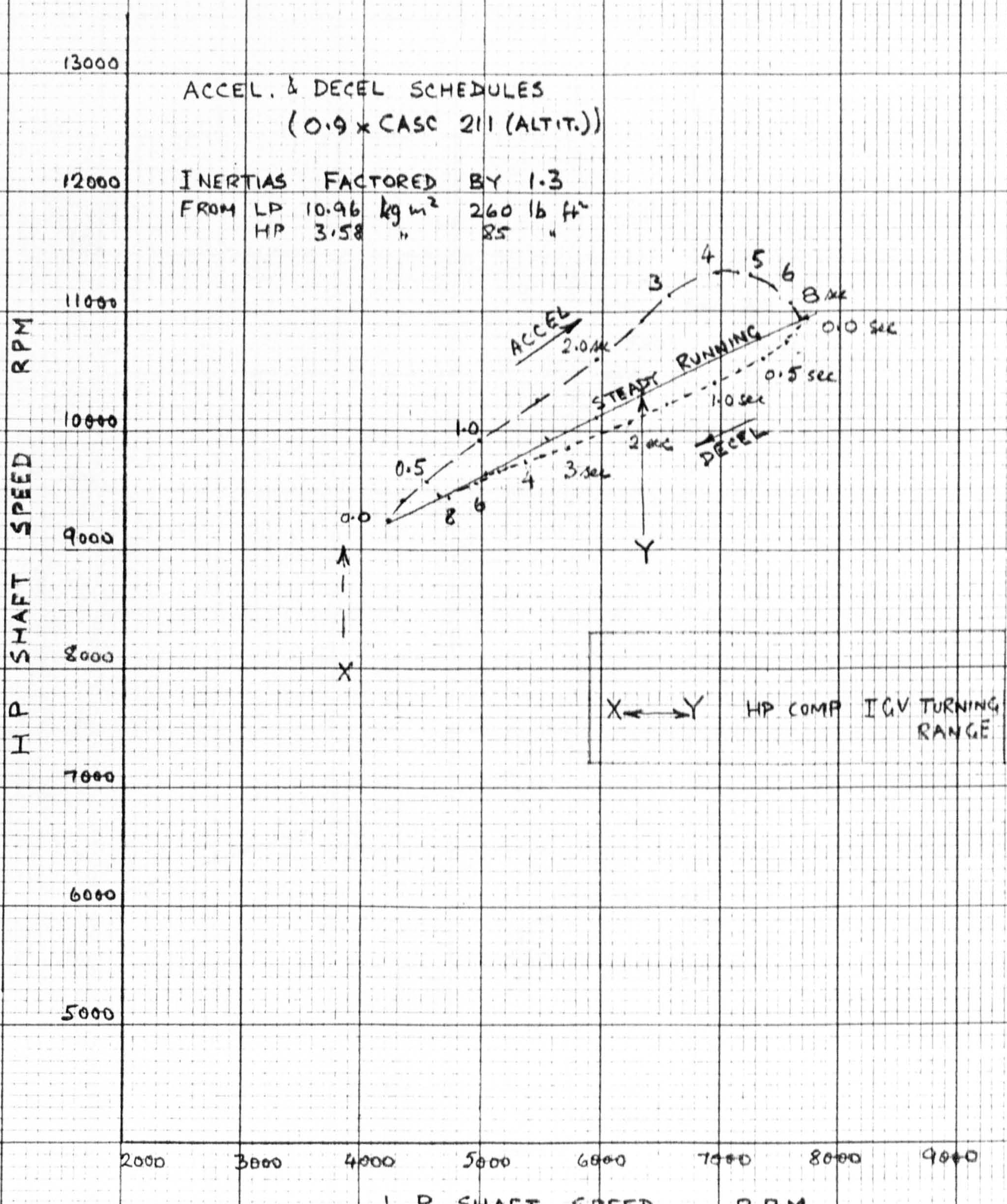


FIG 25 TAY RB 183-03

PREDICTED LP - HP SHAFT SPEED RELATIONSHIPS

ALTITUDE 41,000 FT, MACH 0.8

RECOVERY 0.995

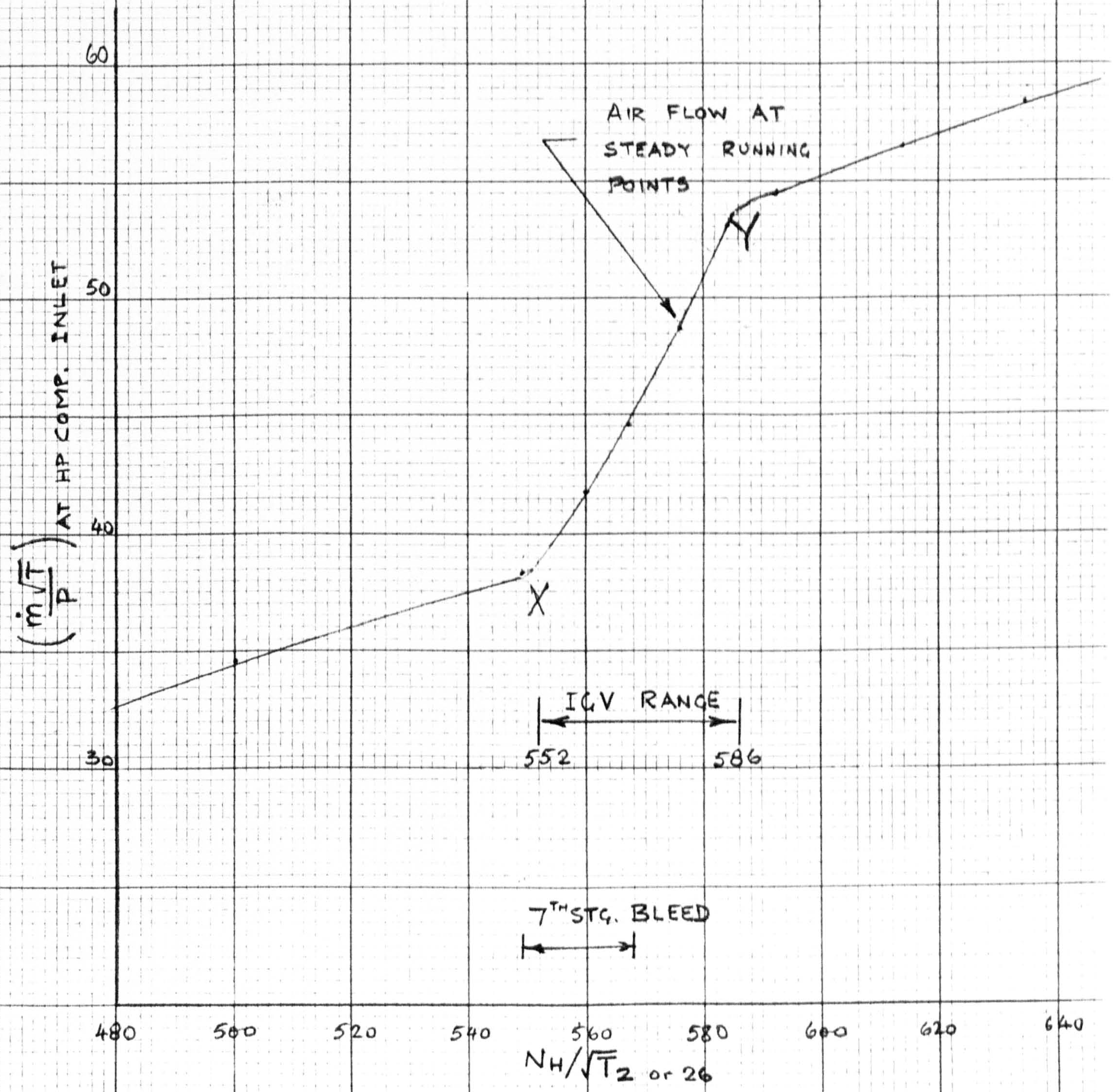


FIG 26 AIR FLOW CHARACTERISTICS OF H.P. COMPRESSOR SPEY RB183-02 & TAY RB 183-03