AFAPL-TR-76-49

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VARIABLE CYCLE ENGINE CONTROL SYSTEM DEFINITION STUDY.
TURBINE ENGINE TECHNOLOGY DEMONSTRATOR COMPONENT DEVELOPMENT PROGRAM, PROJECT 668A.
Controls Development Program.

9 Final technical rept.
Jul 73 - Mar 76,

DETROIT DIESEL ALLISON
DIVISION OF GENERAL MOTORS CORPORATION
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12 199p.

11 MARCH 1976

14 DDA-EDR-8836

15 F33657-73-C-0618

18 19

TECHNICAL REPORT AFAPL TR-76-49

16 668A

FINAL REPORT COVERING PERIOD JULY 1973 to MARCH 1976

17 01

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This final report was submitted by Detroit Diesel Allison, Division of General Motors Corp., under Contract F33657-73-C-0618. The effort was sponsored by the Air Force Aero-Propulsion Laboratory, Air Force Systems Command, Wright-Patterson AFB, Ohio under Project 668A, Task 668A01 and Work Unit 668A0106 with Lester L. Small, AFAPL/TBC as Project Engineer. Robert C. Boyer and Jay D. Meador of Detroit Diesel Allison were technically responsible for the work.

This report has been reviewed by the Information Office, ASD/OIP, and is releasable to the National Technical Information Service (NTIS). At NTIS, it will be available to the general public, including foreign nations.

This technical report has been reviewed and is approved for publication.

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FOR THE COMMANDER

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SECURITY CLASSIFICATION OF THIS PAGE (When Data Entered)

REPORT DOCUMENTATION PAGE		READ INSTRUCTIONS BEFORE COMPLETING FORM
1. REPORT NUMBER AFAPL-TR 76-49	2. GOVT ACCESSION NO.	3. RECIPIENT'S CATALOG NUMBER
4. TITLE (and Subtitle) Variable Cycle Engine Control System Definition Study		5. TYPE OF REPORT & PERIOD COVERED Technical Report (Final) July 1973 - March 1976
		6. PERFORMING ORG. REPORT NUMBER DDA-EDR-8836
7. AUTHOR(s) Robert C. Boyer Jay D. Meador		8. CONTRACT OR GRANT NUMBER(s) F33657-73-C-0618 P00001
9. PERFORMING ORGANIZATION NAME AND ADDRESS Detroit Diesel Allison Division of General Motors Corporation Indianapolis, Indiana 46206		10. PROGRAM ELEMENT, PROJECT, TASK AREA & WORK UNIT NUMBERS Project 668A Task 668A01 Work Unit 668A0106
11. CONTROLLING OFFICE NAME AND ADDRESS Air Force Aero Propulsion Laboratory (TBC) Wright-Patterson AFB, Ohio 45433		12. REPORT DATE March 1976
		13. NUMBER OF PAGES
14. MONITORING AGENCY NAME & ADDRESS(if different from Controlling Office)		15. SECURITY CLASS. (of this report) Unclassified
		15a. DECLASSIFICATION DOWNGRADING SCHEDULE

16. DISTRIBUTION STATEMENT (of this Report)

Approved for public release; distribution unlimited

17. DISTRIBUTION STATEMENT (of the abstract entered in Block 20, if different from Report)

18. SUPPLEMENTARY NOTES

19. KEY WORDS (Continue on reverse side if necessary and identify by block number)

APSI Controls
JTD Controls
Variable Cycle Engine
Variable Geometry
Optimal Control
VCE Simulation
Digital Control System

20. ABSTRACT (Continue on reverse side if necessary and identify by block number)

The primary objective of this Air Force funded program was the development of a control mode for a Joint Technology Demonstrator (JTD) engine that achieves "optimal" steady state performance (minimum sfc and maximum thrust) with rapid transient performance over the entire flight envelope of a hypothetical transonic/supersonic aircraft. A flexible digital computer simulation of the JTD turbofan engine with a control system was developed along with computerized optimization and constraint procedures to establish optimal engine

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ABSTRACT (Continued)

operating conditions and variable geometry positions to achieve minimum sfc and maximum thrust.

Speed and turbine temperature fuel governors along with a compressor Mach Number ($\Delta P/P$) surge control loop in the fuel control were evaluated. A control mode that positioned the geometry to "scheduled" optimal geometry settings was unable to achieve steady state maximum thrust over the entire flight envelope. However, a control mode developed to force engine parameters to "scheduled" optimal parametric relationships successfully attained minimum sfc and maximum thrust at all the selected test points in the flight envelope.

Since certain parametric relationships are true for minimum sfc (i.e., maximum airflow and minimum temperature for a given thrust) and maximum thrust (i.e., maximum speed, temperature, and pressure within engine limits), the control based upon parametric relationships will yield maximum thrust and near minimum sfc for a resonable range of engine variations. Therefore, it is not necessary to tune each new engine for maximum thrust or adjust the control for engine aging.

The requirements for a backup control to meet various goals were investigated. A backup control mode was developed and a smooth transition between the primary control and backup control was demonstrated with the simulation.

A component and control system test plan was formulated for the JTD control system.

Table of Contents

List of Figures

List of Figures (Continued)

List of Figures (Continued)

List of Figures (Continued)

NOMENCLATURE LIST

A	Area
A_X	Area (X denotes station location)
AC	Alternating current
A/D	Analog to digital
AFAPL	Air Force Aero Propulsion Laboratory
APSI	Advanced Propulsion Sub-System Integration
ATEGG	Advanced Technology Engine Gas Generator
Avg	Average
CDP	Compressor discharge pressure
CY	Calendar year
DC	Direct current
DDA	Detroit Diesel Allison
EMC	Electromagnetic compatibility
ERR	Error
F_N	Thrust
FVT	Flapper Valve travel
g	Gravity constant
G(s)	Transfer function
H(s)	Transfer function
HIA	Homopolar induction alternator
HP	High pressure
HPC	High pressure compressor
HPT	High pressure turbine
I.C.	Initial condition
I_c	Command current
IOC	Initial Operational Capability
IGV	Inlet guide vane
JTD	Joint technology demonstrator
K	Gain
LP	Low pressure
LPT	Low pressure turbine
LVDT	Linear variable differential transformer

M	Mass
MAX	Maximum
MIN	Minimum
MN	Mach Number
NH	High pressure rotor speed
NHC1	High pressure rotor speed (corrected to engine inlet)
NHC2	High pressure rotor speed (corrected to compressor inlet)
NL	Low pressure rotor speed
NLC1	Low pressure rotor speed (corrected to engine inlet)
Opt	Optimal
P	Pressure (total)
P_x	Pressure (x denotes station location)
P_s	Static pressure
P_t	Total pressure
PMG	Permanent magnet generator
Q	Flow
R	Pressure ratio
REF	Reference
REQ	Request
R & D	Research and Development
RF	Radio frequency
RPR	Ram pressure ratio
S	Laplace variable
SCH	Schedule
Sfc	Specific fuel consumption
T	Temperature
T_x	Temperature (x denotes station location)
TBT	Turbine blade temperature
TIT	Turbine inlet temperature
V	Velocity
V/M	Volts per meter
V/STOL	Vertical/short take off and landing
Wa	Air Flow
WF	Fuel flow

α	General engine parameter
β	Geometry position
ΔP	Pressure Differential
$\Delta P/P$	Measure of air flow Mach number
δ_x	P_x/standard pressure
θ_x	T_x/standard temperature
τ	Time constant

1.0 INTRODUCTION

This final report covers the work performed in a Control System Definition Study under Contract F33657-73-C-0618, Amendment P00001. This control system definition study was performed from July, 1973 through March, 1976. Two other controls component development programs were also performed under this contract amendment and were previously reported. These two programs were the Evaluation of a Fluidic Temperature Sensor/Air Ejector Assembly, reported in AFAPL Technical Report TR-74-58, November, 1974, and a Fuel Pump and Metering Assembly Development, reported in AFAPL Technical Report TR-75-85, July, 1975.

These three controls development programs were part of an overall plan of advanced controls development for the DDA Joint Technology Demonstrator (JTD) program. The DDA Joint Technology Demonstrator program will provide test verified scalable technology for transonic/supersonic tactical fighter and interceptor aircraft variable cycle propulsion systems for IOC in the 1980's.

The potential benefits of the variable-geometry engine, such as increased transient stability margin, faster thrust response, propulsion system flow matching, and improved installed thrust and fuel consumption performance, are directly related to the ability to effectively control the various geometry elements. Multiple control modes are required to achieve optimum engine performance for each phase in a multimission aircraft. Effective control of an engine with a large number of variable-geometry functions requires an integrated control system to ensure proper scheduling and fail-safe interlocking features. The number of control parameters to be sensed, the multiple control modes to be established, the large number of control functions to be accomplished, and the requirements to effectively interface with the aircraft control system all point to a complex engine control system. For the variable-geometry engine design to be cost effective, practical multiple control requirements must be established early in the engine design study program through close coordination between the engine/component designer and control design engineer.

The objective of this control system definition study was to define a control system for the JTD engine compatible with the environmental and operational requirements of projected applications in transonic/supersonic aircraft. Specific objectives of this control system definition effort were:

- To identify specific control functional requirements and unique control problems related to the variable-geometry engine
- To determine the best control mode for each variable which must be regulated on the engine
- To determine design definition of the control system using the maximum degree of functional integration
- To develop a dynamic digital computer simulation of the engine propulsion system and control system
- To conduct simulation studies of potential control systems for trade-off evaluations to identify the most suitable system
- To identify high payoff advanced technology control development programs related to the Technology Demonstrator control system
- To define the control system and test plan for the Technology Demonstrator engine for verifying the feasibility and capability of controlling advanced variable-geometry engines

This report presents a description of the work performed during this study and describes the selected control mode and control system implementation concepts.

2.0 SUMMARY

The flight envelope, environmental requirements, and functional requirements for a JTD control system are established for a hypothetical transonic, supersonic aircraft which would utilize a JTD derivative engine envisioned for the 1980's. A control system goal of "optimal" steady state performance (minimum sfc and maximum thrust) with rapid transient performance "over the entire flight envelope" is established as the foundation for the control mode study.

A flexible digital computer simulation of the JTD turbofan engine with a control system is developed. A computerized optimization and constraint procedure is added to the simulation to compute optimal engine operating conditions and variable geometry positions to achieve minimum sfc and maximum thrust.

Speed and turbine temperature fuel governors along with a compressor exit Mach Number($\Delta P/P$) surge control loop in the fuel control are evaluated. Two types of variable geometry controls are examined in detail. The first positions the geometry to "scheduled" optimal geometry settings. In this mode, the errors in the geometry control over the flight envelope generate stability problems in the fuel control and make it difficult to achieve steady state maximum thrust. The second type uses the geometry to control engine parameters to "scheduled" optimal parameter relationship to successfully attain minimum sfc and maximum thrust. Several combinations of geometry positions and engine parameters are studied to develop a control mode that meets the requirements over the flight envelope with good transient response during a wide range of accelerations and decelerations.

The requirements for a backup control to meet various goals are investigated. A backup control mode is developed and the transition between the primary control and backup control is demonstrated with the simulation.

A conceptual design of the control system is formulated and component development work required to implement the selected control mode for the JTD is identified. A component and control system test plan is presented for the JTD control system.

3.0 JOINT TECHNOLOGY DEMONSTRATOR (JTD) ENGINE DESCRIPTION

The JTD being developed by DDA under contract (USAF F83657-76-C-0021 and Navy N00019-76-C-094) is conceived as a turbofan powerplant to demonstrate the integration of advanced component technology as it becomes available from individual component R&D efforts. The primary objective of the technology development is to demonstrate the performance, structural, and operating characteristics leading to an engine that is significantly advanced in terms of stage loading, material application, and component variability for transonic/supersonic applications.

The APSI design cost study clearly identified significant reductions in acquisition and life cycle costs of JTD derivative engines incorporating high performance variable flow capacity components currently in development. These benefits were described in report AFAPL-75-90 "Turbine Engine Technology Demonstrator Component Development Program - Project 668A - Effects of JTD Advanced Technology on Life Cycle Costs".

A brief description of each of the JTD components technology is presented in the following sections.

<u>Fan</u>

The fan is a high tip speed, two stage system being developed under APSI contract. The full fixed geometry fan performance has been demonstrated in a previous development program. The present effort of redesign and testing has significantly upgraded performance over the original design. Inlet distortion testing is scheduled.

<u>Compressor</u>

The compressor has inlet guide vanes and six-stages of compression and incorporates variable vanes in all stages. A primary design requirement of the compressor was to provide variable flow capacity at near constant speed and pressure ratio. Two separate tests of this compressor have been conducted and a third is planned for late 1977 during which inlet distortion testing will be accomplished.

The compressor assembly incorporates advanced titanium materials and design features resulting in a lightweight structure meeting the design requirements imposed by the varying temperature and stress conditions.

Combustor

The combustor development objective was to design and demonstrate high performance over a wide range of fuel/air ratios necessary for incorporation in a variable flow engine. A turn down ratio over twice current technology combustors was required. Other objectives included low pressure drop and a very low T max/T average outlet profile. The diffuser/combustor system is a triple passage diffusion system which includes inner and outer boundary layer bleed slots to produce a diffusion system insensitive to compressor discharge profile variation. The high-temperature combustor liner utilizes a combination of convection and film cooling. This design provides the capability to operate at extremely high temperatures with minimum cooling airflow, thus permitting a larger percentage of total airflow to be used to tailor the temperature pattern. A staged fuel concept allows efficient operation over an expanded turndown ratio range.

Rig testing of the combustor has resulted in the demonstration of these design objectives.

H.P. Turbine

The high-pressure turbine is a high work, variable flow capacity maximum temperature single stage assembly. The turbine nozzle is designed to be mechanically variable to accept a wide range of flows from the compressor and operate at maximum temperatures. Rig and cascade testing has been performed which demonstrated near goal performance. Testing in the gas generator has further demonstrated the ability of the mechanically variable vanes to be moved satisfactorily while operating at high temperature. Predicted performance was also demonstrated.

L.P. Turbine

The low-pressure turbine is an air-cooled, variable flow capacity, single-stage assembly. A unique design feature is the integration of the vane cooling and the variable flow capacity achieved aerodynamically by use of a jet flap vane. Modulated compressor discharge air is routed through the vane for cooling and discharged from the jet flap slot approximately at right angle to the gas stream flow. The amount of cooling air flowing into the gas stream varies the effective flow area (capacity) of the turbine. Rig testing of this component has demonstrated performance levels near the original design objectives in both efficiencies and flow variation.

Exhaust Nozzles

The primary and secondary exhaust nozzles for the JTD have been designed. The designs provide for both nozzle areas to be varied on the test stand over the full range for demonstration of the total potential of all the variable components. Additional testing of the JTD will thus demonstrate the full potential of the advanced variable flow components.

Figure 3-1 shows the external view of the JTD engine with these variable component features.

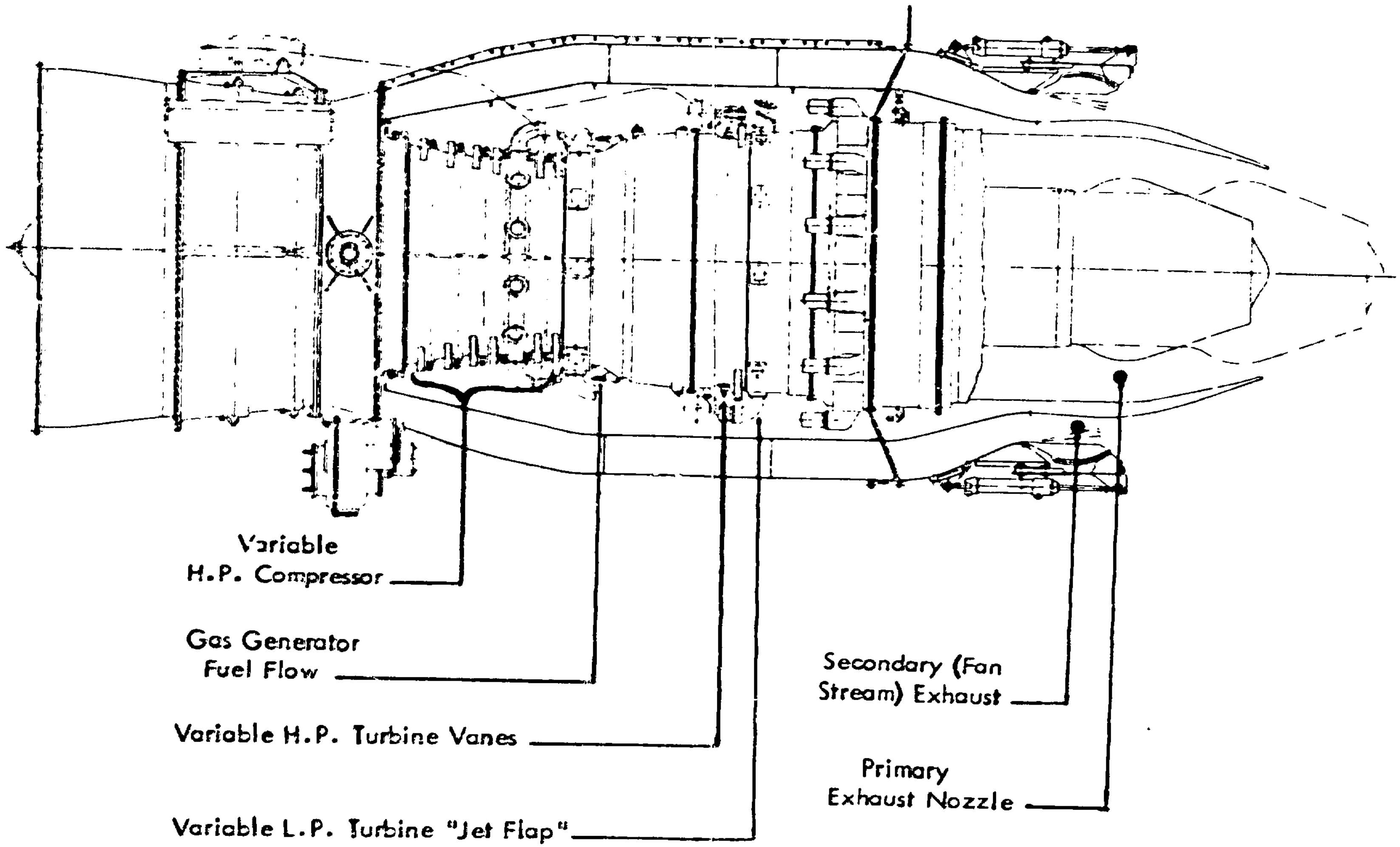


Figure 3-1 JTD External View

4.0 CONTROL SYSTEM REQUIREMENTS

This control system definition study addresses the control of the JTD engine as described in Section 3.0. It further considers the operational and environmental requirements for a projected transonic/supersonic application of a JTD derivative engine in which an augmentor and possibly a variable geometry fan would be added. The potential aircraft applications considered include missions of:

- Interdiction
- Combat air patrol
- Low altitude intercept
- Deck launch intercept
- Subsonic surface surveillance

A composite flight envelope to include the mission capabilities was formulated as a basis for establishing the control system requirements. This control system design flight envelope is shown in Figure 4-1.

The control system environmental requirements were established from considerations of a typical transonic/supersonic mission profile as shown in Figure 4-2.

The increased number of variable geometry components points toward an advanced control system which must be capable of handling multiple inputs and outputs, multiple control modes, and effective interfacing with other systems. The additional complexity of the engine, the increased functions of the control systems, and low cost goals present new challenges, unique problems, and expanded requirements for the control system.

The definition of the JTD control system must address the requirements of the actuation systems, the requirement for total system integration, the hostile environment, and the reliability, maintainability, and cost of the system. These factors are discussed in some detail to indicate the problems they present in defining the control system.

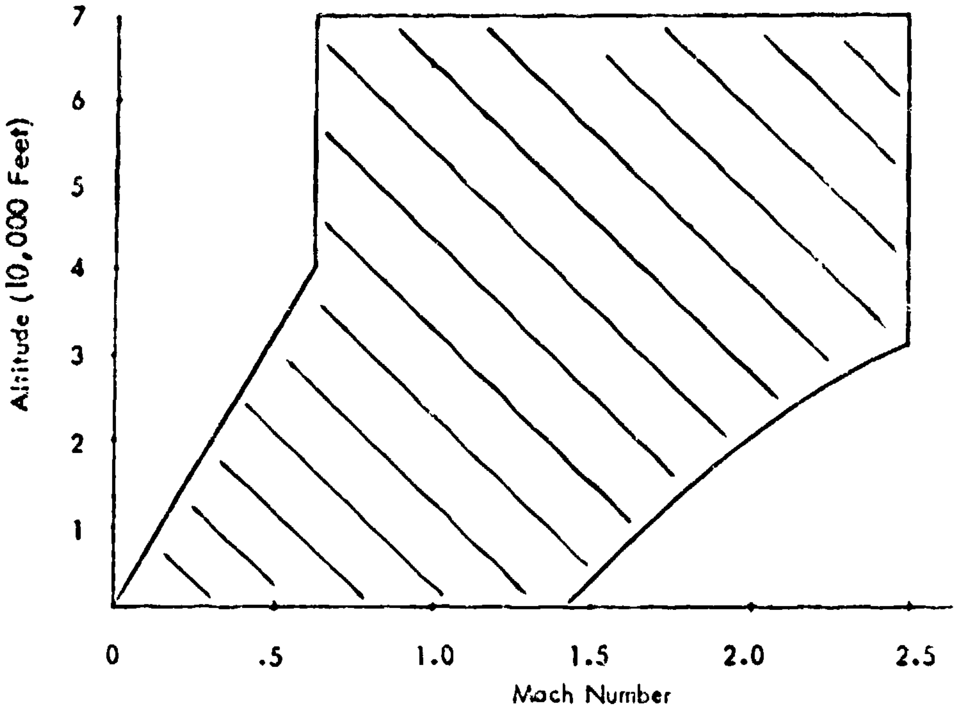


Figure 4-1 Transonic/Supersonic Flight Envelope

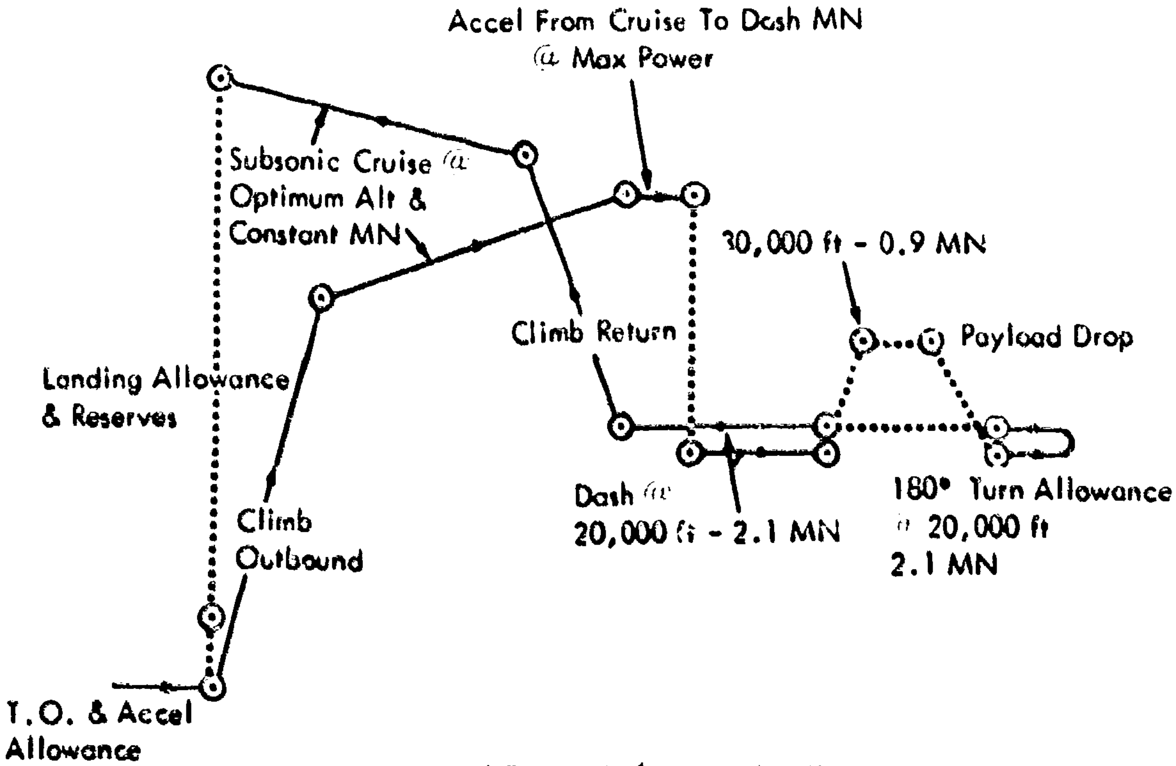


Figure 4-2 Typical Transonic/Supersonic Mission

Unique Functional Requirements

The system must control five variable geometry components (six independent control functions) in addition to the gas generator fuel flow. Although the overall functional requirements are similar to other aircraft engines, the actuation systems for the variable geometry rotating components present new challenges. The variable geometry components that provide the most unique control system problems are the compressor (having two variable functions), the HP turbine, and the LP turbine. These requirements include the wide range of geometry movement, the increased component performance sensitivity to geometry movement, the higher force levels needed to control the component, and the hostile temperature environment where the actuators are located.

Compressor

The HP compressor has one stage of variable inlet guide vanes and six stages of variable stator vanes to be controlled. The basic purpose of the compressor geometry is to provide optimum engine performance and surge free operation over the complete flight envelope. The present compressor design indicates that two independent control functions are necessary to control the compressor. The vanes are scheduled to vary as a function of corrected high-pressure rotor speed to provide maximum efficiency and surge margin. In addition, these schedules are shifted and altered to provide variable compressor flow capacity at a speed, as illustrated by the open, nominal, and closed vane schedules shown on Figure 4-3.

The design of an actuation system to provide the two independent compressor functions is a unique problem. It is impractical and undesirable to vary each stage independently with individual actuators. The practical approach is to provide two actuators, one for each of the two independent functions. This approach requires a mechanization concept that utilizes a unique combination of linkages and cams.

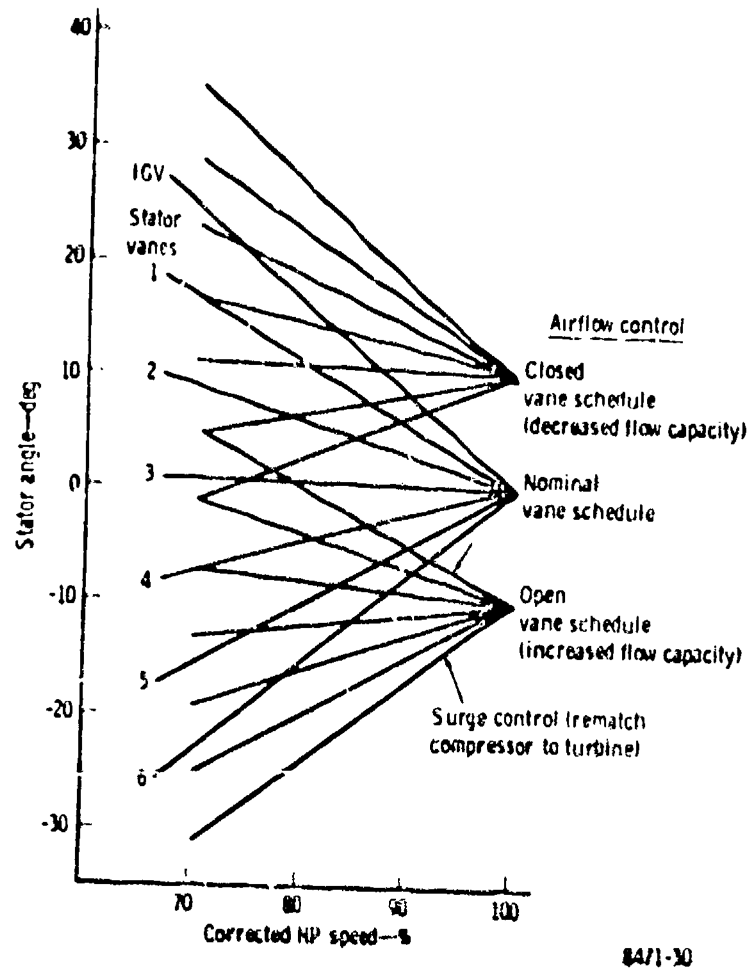


Figure 4-3 Present HP Compressor Stator Schedule

High-Pressure Turbine

The HP turbine has one stage of mechanically variable stator vanes to be controlled. The HP variable vane turbine concept requires a fully modulating, precise positioning, and fast responding actuation system that can operate in a hot ambient temperature environment.

Figure 4-4 indicates that the 23% variation in turbine flow capacity is accomplished with a 5.5 degree angular movement of the vanes. This results in a sensitivity of

4.18 percent flow change per angular degree movement. This sensitivity indicates the need to select a control mode that closes the loop on an engine parameter rather than actuator position only. A highly precise actuation system with a minimum amount of hysteresis or backlash is required to maintain accurate position control.

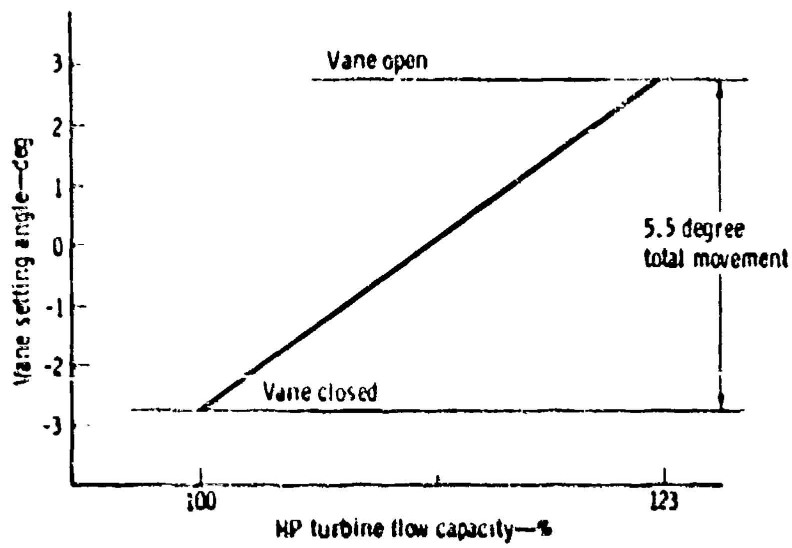


Figure 4-4 HP Turbine Flow Capacity

Low-Pressure Turbine

The LP turbine is an air-cooled variable flow capacity single-stage turbine. The LP turbine aerodynamically varies the turbine area with a jet flap design. This is accomplished by controlling the amount of air forced into the jet flap stator vanes which exhausts perpendicularly into the gas stream, thus aerodynamically varying the flow area. Because of this unique turbine design, the air being supplied to the turbine not only varies the area but also serves as the cooling flow required by the turbine. Consequently, the control mode selected for the LP turbine must provide two functions (1) variable area control and (2) adequate cooling throughout the entire flight envelope.

Interface Considerations

Although it is not intended to demonstrate actual integration of aircraft inlet and flight control functions into the engine control during the JTD program, the interfaces between aircraft engine control, and diagnostic equipment must be considered during the control system conceptual design. The system must accommodate the following interfaces:

- Aircraft Inlet Control - provide proper airflow matching capability between the inlet and engine to avoid high levels of turbulence and distortion due to augmentor light-offs or blow-outs, inlet unstart, buzz, or supercritical operation and reduce spillage drag.
- Aircraft Flight Control - provide proper thrust management to accommodate automatic controls including attitude and altitude hold, engine trim, and autopilot landings.
- Engine Condition Monitoring/Diagnostics - provide for continuous monitoring of engine and control component performance to detect malfunctions or degradation in performance.
- Aircraft Cockpit Displays - provide the pilot with information of the control system in addition to the required aircraft/engine instrumentation, digital controller failure level indicator, and engine condition monitoring flags.

Back-Up Control

In the event of a failure in the primary controller, it is desirable that the engine control automatically or manually revert to a secondary or back-up control. For economy, it is desirable to keep the back-up control as simple as possible. Two different criteria were considered. The simplest would only provide safe operation for a fly-home capability with reduced performance with emphasis on low SFC during cruise. The second would provide for 90% maximum thrust capability in accordance with Military Specification MIL-E-5007D.

The back-up system is simplified by reducing the number of controlled geometry variables to a minimum and fixing the remaining geometry in a fail-safe position. The electrical/mechanical design of such geometry would automatically provide fail-safe operation by automatically positioning the geometry in the desired position when the primary controller fails. Potential failures other than the primary controller must also be considered in accordance with the probability of such a failure.

The back-up control mode must provide for a smooth transfer from primary to back-up and also have compatible interface features with the primary control components.

Environmental Requirements

The transonic/supersonic applications envisioned for the JTD derivative engine impose severe and hostile requirements on the control system. Specific environmental requirements for control system design criteria have been established for the thermal, vibration and electromagnetic compatibility levels.

Thermal

A typical mission thermal environment profile is shown in Figure 4-5. This shows the range of expected thermal environment to be -65°F to 470°F for control system components mounted on the engine fan case or accessory housing.

A thermal environment of 470°F air ambient has a significant impact on the design of the control system components. The reliability and maintainability of an electronics package are adversely affected by the thermal environment since reliability falls as temperature rises. If fuel is used as the only heat sink source (210°F), it is necessary to minimize the thermal gradients, which may preclude the use of plug-in-modules and may require piping fuel through the electronics package.

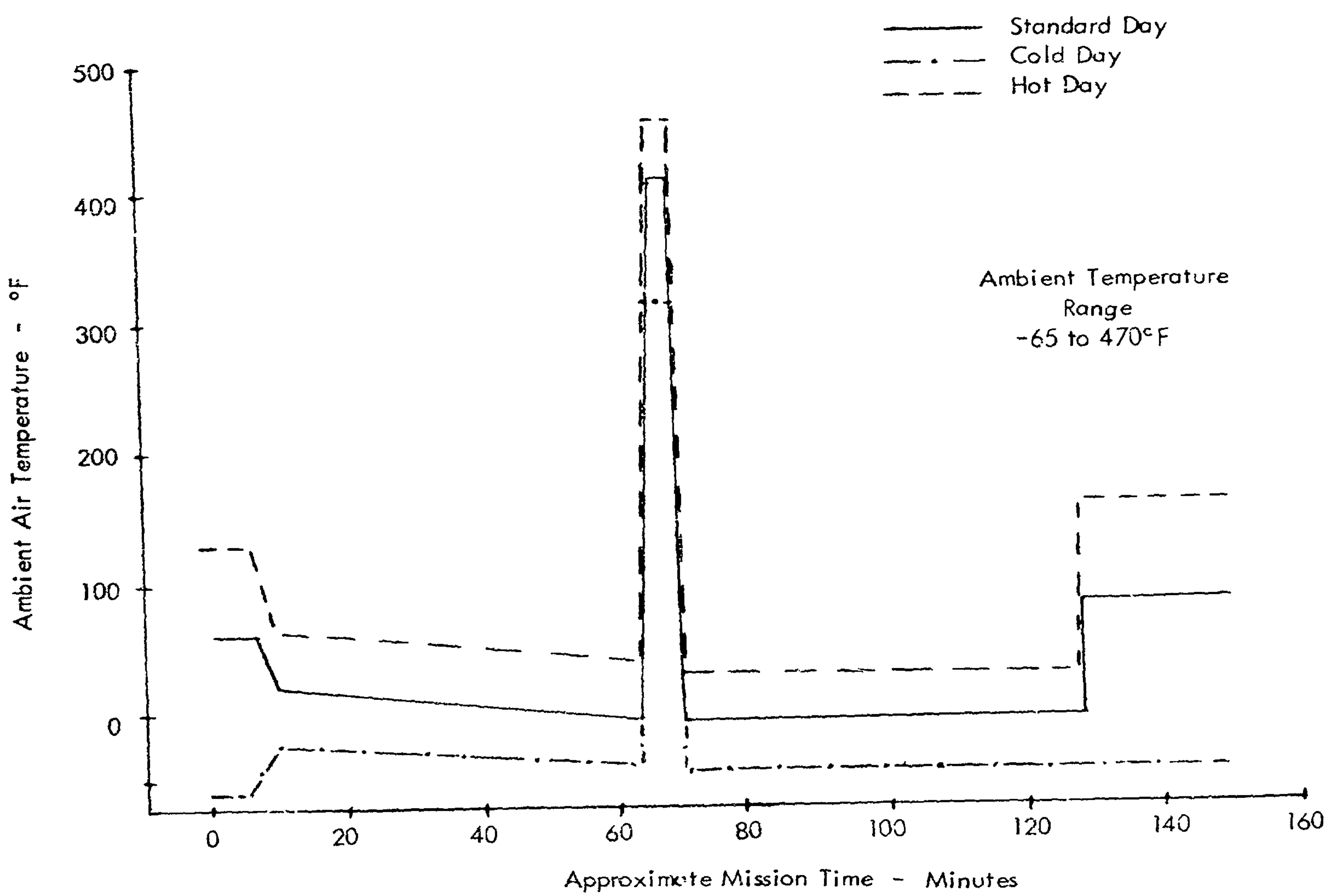
The thermal environment for actuator components on the hot section of the engine has been estimated to be 1000°F or greater. For example, the HP turbine vane synchronization ring - actuator components are situated on the turbine case and surrounded by the secondary fan stream augmentor inner duct wall as shown in Figure 3-1. This area would be cooled by fan bypass airflow to achieve acceptable thermal levels for the selected actuators. Similar temperature levels are assumed for the LP turbine jet flap bleed and exhaust nozzle actuator components.

Vibration

The control components will be engine mounted and, therefore, subjected to a continuous engine vibration. The vibration environment shown in Figure 4-6 is based upon extrapolated engine data which will be updated as JTD testing progresses.

Electromagnetic Compatibility (EMC)

The increased use of electronics and electromagnetic systems has resulted in a radio frequency polluted environment. The control system will be designed to meet the EMC requirements for RF susceptibility specified in MIL-STD-461 and tested to MIL-STD-462. Unfortunately, meeting these specifications does not always ensure reliable RF susceptibility-free operation. Some airborne systems in the real world experience RF levels of 100V/M or greater. Aboard military ships, it is not uncommon for equipment to be subjected to RF levels in excess of 185 V/M. MIL-Handbook 235 will be used for a definition of frequencies and levels in the real world. During the design of the control system, cables, connectors, shields, filters, and grounds must be used effectively to minimize the EMC problems.



Figur 4-5 Thermal Environment for Contiol Components During a Typical Mission

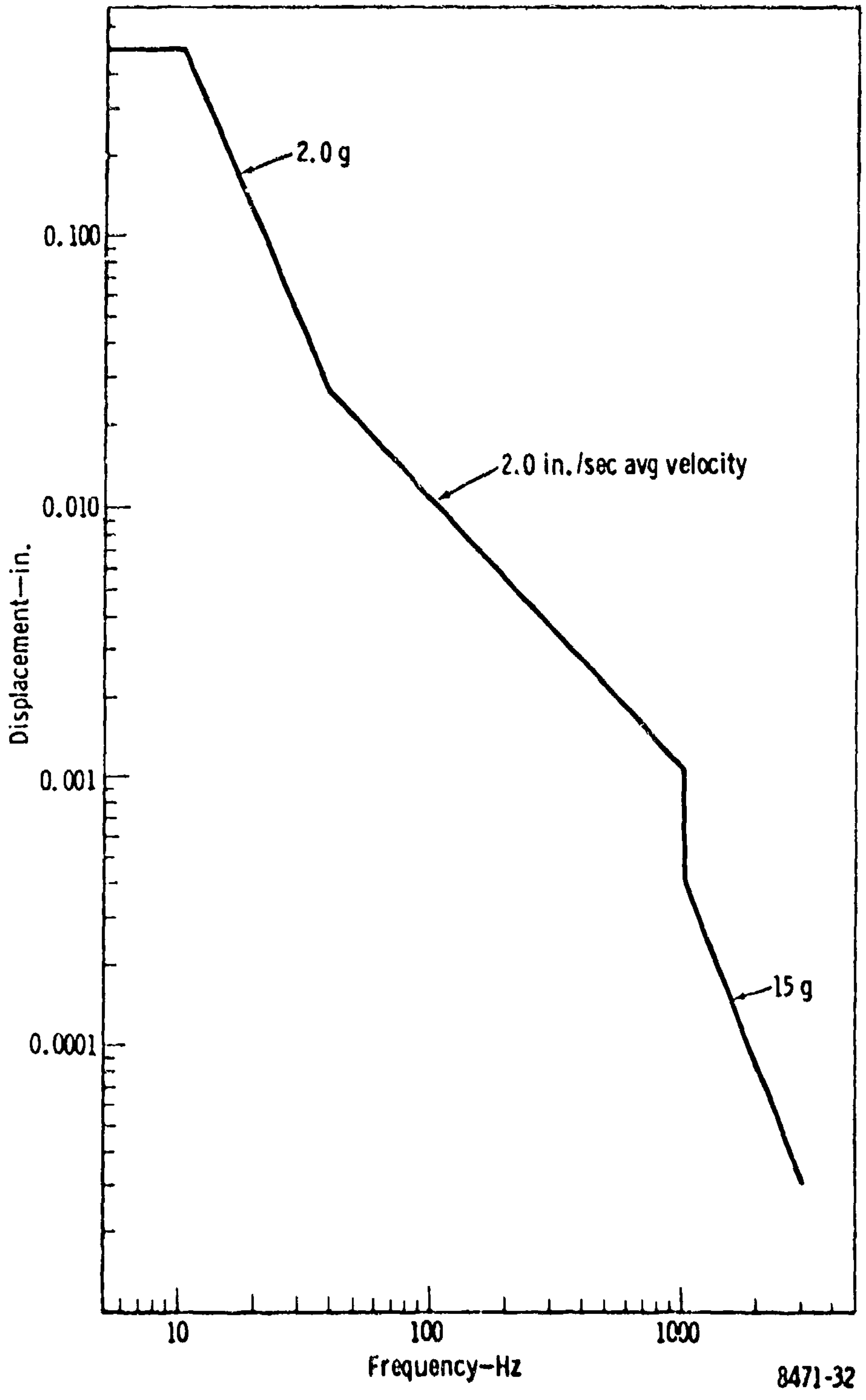


Figure 4-6 Proposed JTD component vibration specification

5.0 DIGITAL COMPUTER DYNAMIC SIMULATION

Engine Simulation

A highly sophisticated digital computerized dynamic engine and controls simulations and special procedures were developed to support the controls design and evaluations required by this project. This controls design and analysis tool utilizes many of the mathematical modeling and system simulation skills developed under IR&D Item 75-659, Engine System Integration (Dynamic Modeling). The simulation provides a general design, off-design, steady state, and transient engine model with variable geometry components.

DDA computerized steady-state performance programs are designed on the building block concept and consist of a controlling logic routine which links a system of generalized component subroutines into any desired type of engine configuration defined by inputs (see Figure 5-1). Additional specialized calculations can be easily and efficiently incorporated with only minor modification which allows maximum flexibility in studying a variety of cycle arrangements. The system also features rapid cycle matching procedures and direct transfer from design point to off-design calculation modes.

Transient analysis of a system is rapidly accomplished by additional dynamic routines being interfaced with the steady-state simulation. These additional routines perform the control of time functions, rotor dynamics, and heat storage effects to produce engine response time history characteristics. Thus, the dynamic simulation is achieved with little change to the steady-state model and makes maximum use of component characteristics prepared for the steady-state analysis.

Calculation procedures have been developed and incorporated into the basis system for simulating variable geometry rotating components by a generalized approach of layered characteristics representing a range of geometry settings or

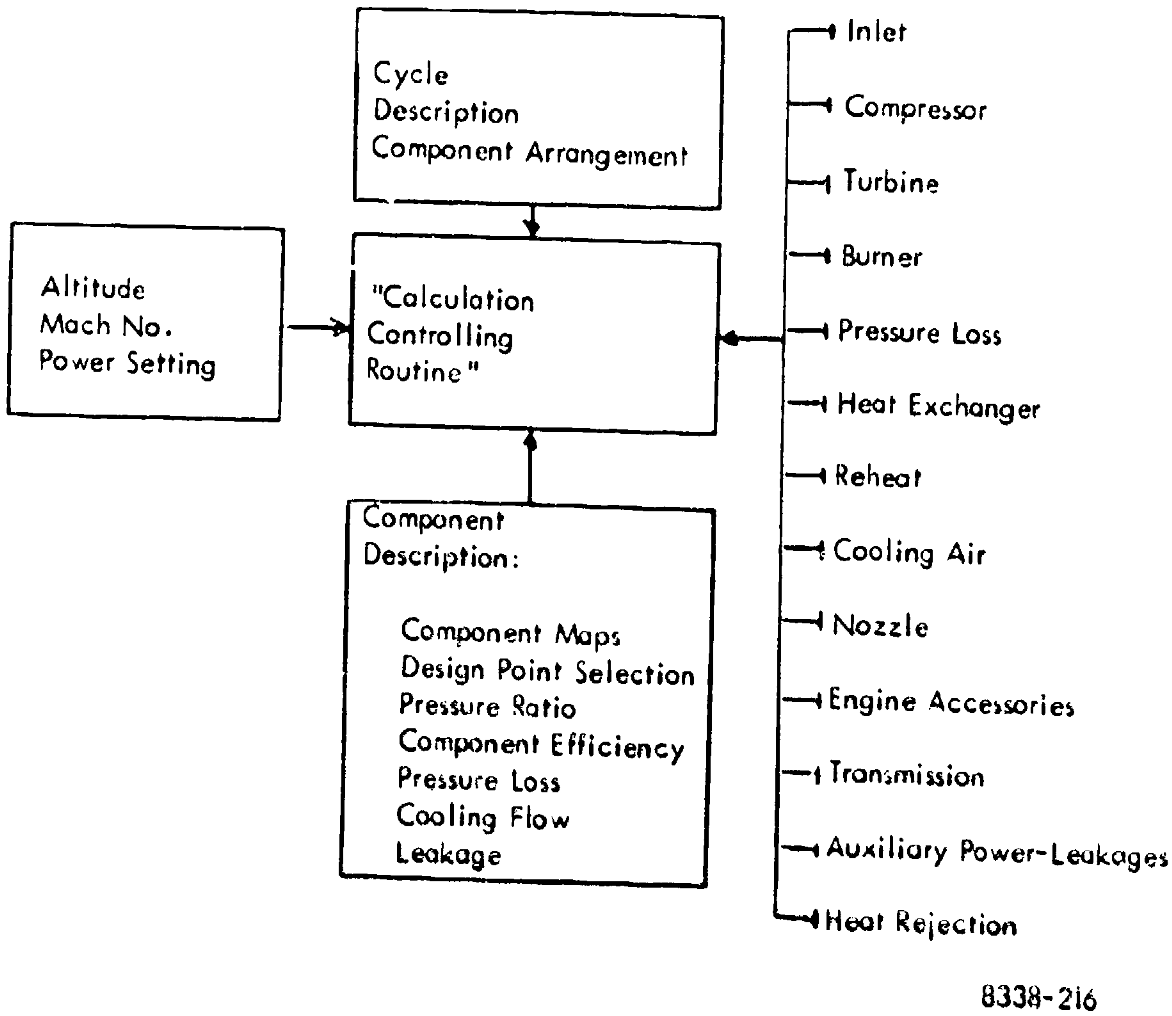
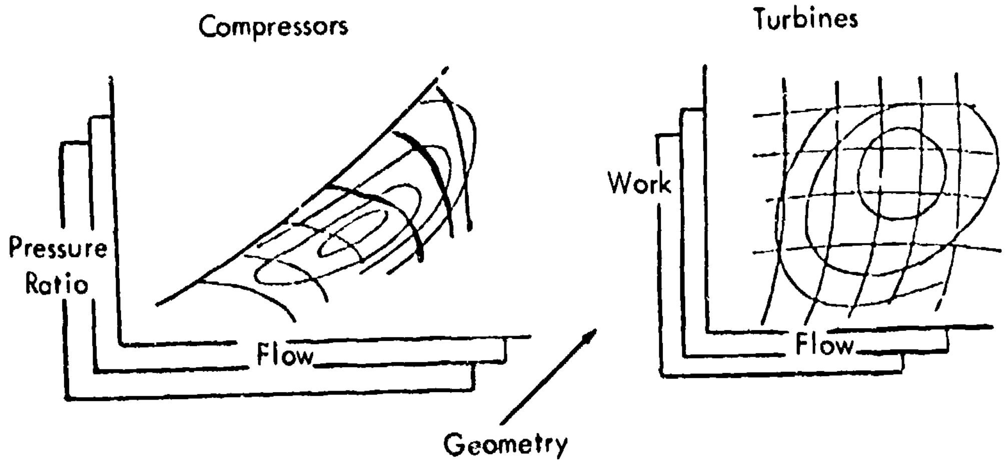


Figure 5-1 Engine Simulation Structure

schedules as illustrated in Figure 5-2. This layered map approach is applicable to a variety of variable geometry definitions without program changes. Thus, engine component performance is easily and rapidly modified to reflect changes in component design or test results. This approach is used for a variable area turbine, a high pressure compressor with variable scheduling of all stators and a controlled cavity pressure ratio in an aerodynamically variable flow capacity, low-pressure turbine employing compressor bleed. All component maps, cycle constants, control schedules and other necessary model data are compiled into the program



Multiple-Layered Maps Represent Each Variable Geometry Component

Figure 5-2 Variable Geometry Component Performance Map

with provision for overriding by input data.

Figure 5-3 shows the gas path for the JTD simulation used in this study along with the corresponding station numbers. These station numbers are used as subscripts on engine parameters to denote the location of the temperatures, pressures, flows, etc. through the report.

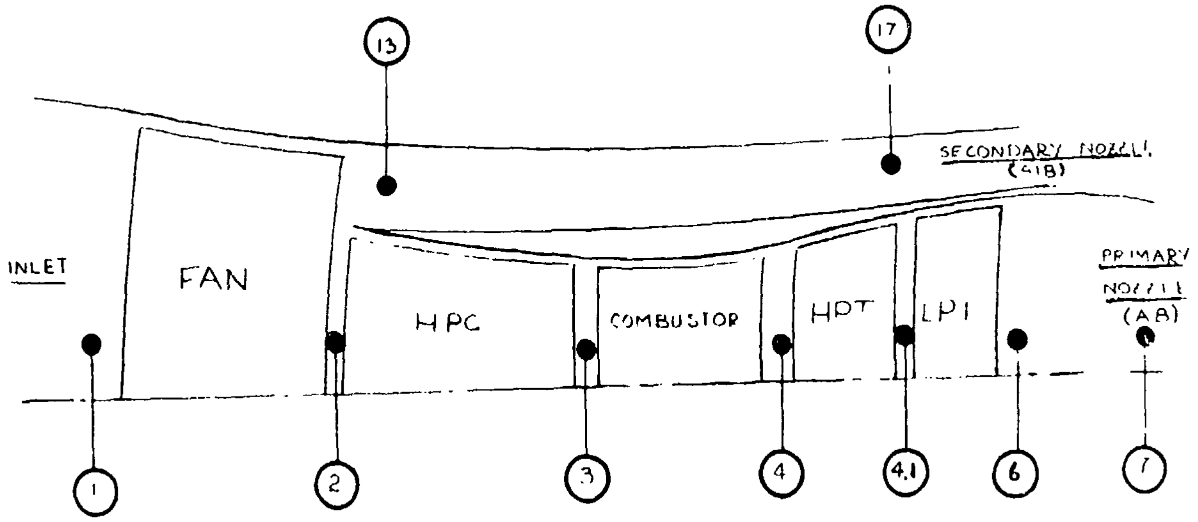


Figure 5-3 Engine Flow Diagram

Control System Simulation

The control system included in the dynamic computer simulation is capable of evaluating various control modes (laws) for controlling the engine. The control system simulation can also be used to evaluate requirements for sensor response and accuracy, actuator characteristics, control loop capability, system stability, failure modes, and control loop solution rates. The control of the HPC geometry position for maximum surge margin and efficiency is built into the open, nominal and closed compressor maps used in the engine simulation. Thus, the simulation cannot evaluate this control loop.

The complex control system is simulated by subsystems (computer subroutines). As shown in Figure 5-4, each of the control loops is a separate subsystem. In addition, each control loop is broken down into two subelements:

o Control law - the calculations done inside the controller to determine the requested output value.

o Actuator hardware - the calculations done to determine the component position based upon the dynamic characteristics of the actuator.

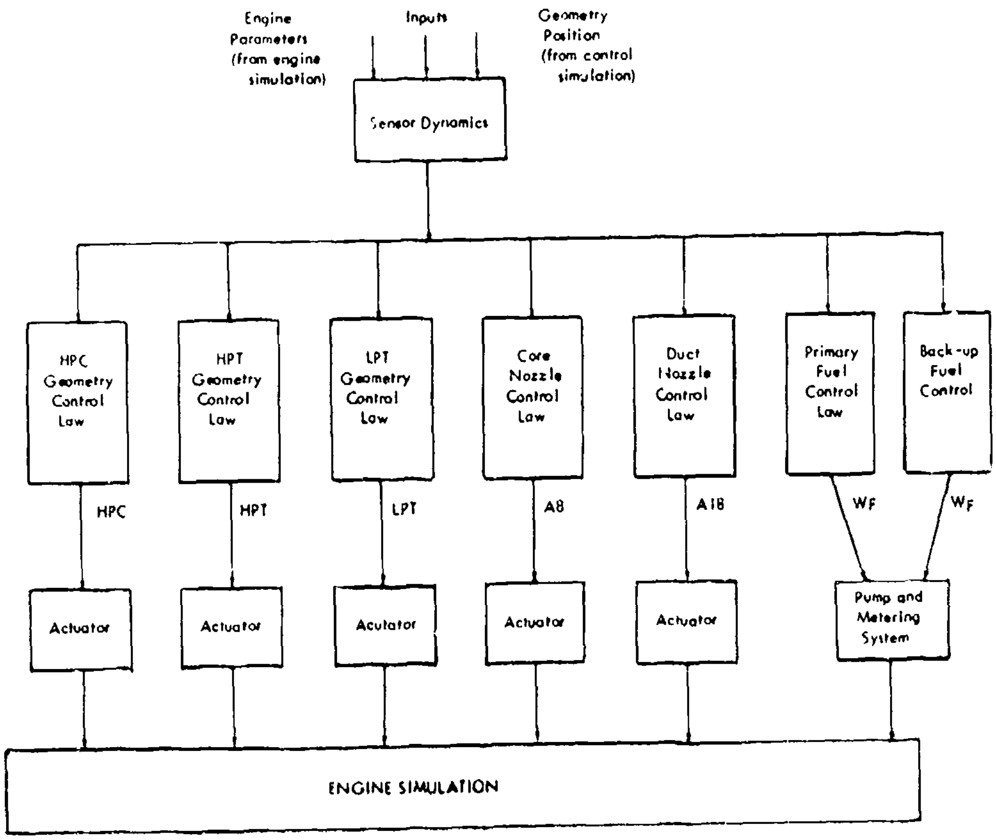


Figure 5-4 JTD Control Simulation Structure

This approach allows the flexibility to interchange the control modes with minimum computer programming changes. Also, as test data becomes available, the dynamic characteristics of the hardware components (pump/metering, and actuation systems) can be simulated in more detail without any major programming changes. In addition, each control loop subsystem or subelement can be isolated and checked out by an input/output analysis. This is analogous to testing a hardware component to a written specification on a test bench. The control logic or control laws are tailored to the control modes. The sensor and actuator models are discussed next.

Math Models

The modeling of the sensors and actuators is important to the control design since they represent the greatest share of dynamics in the control system.

The math model for the sensors, actuators, and fuel metering system used in the JTD simulation are typical of the hardware expected in the final JTD control. These models will be updated as additional design and test data are available. Parameters have been chosen to represent the capability of existing hardware or hardware under design if at all possible.

Sensor Models

For simplicity, all pressure transducers were simulated as a single lag although many transducers actually are better characterized by an overdamped second order lag. This simplification is justified by the fact that the lags introduced by pressure transducers are usually not within the bandwidth of the control loop in question. Thus, the input-output relationship of the pressure transducers is given by

$$\text{Sensed Pressure} = \frac{1}{1 + .02S} \text{ Actual Pressure}$$

The lags introduced by speed sensors are even more negligible than pressure transducers so that a simple first order lag is adequate. Thus, the speed sensor is represented by

$$\text{Sensed Speed} = \frac{1}{1 + .01S} \text{ Actual Speed}$$

for both spool speeds. For any accuracy analysis, the actual mechanization scheme must be considered along with the expected "quantization noise" generated with a digital signal.

The temperature sensors are also represented by a first order lag which is adequate for the type of sensors (thermocouples) projected for the JTD. However, the time constant of the thermocouple is usually a function of the airflow in the gas path being measured. The input-output relationship used for the temperature sensors is

$$\text{Sensed Temperature} = \frac{1}{1 + \tau_{Temp} S} \text{ Actual Temperature}$$

$$\tau_{Temp} = \tau_{sensor} \sqrt{(W_{standard} / W_{actual})}$$

τ_{sensor} = sensor time constant at an airflow of $W_{standard}$

W_{actual} = actual airflow

An important factor in parameter sensing impractical to simulate is the errors created by pressure and temperature "distribution" and the sensor location. Engineering judgment must be exercised in this area when assessing the "error" contribution of the sensors.

Actuator Models

Although several different actuators and linkages are projected to drive the various component geometries, the math models of all the actuators are similar when the models are reduced to the level practical for the JTD simulation. A typical actuator (possibly a torque motor and servo valve controlling a hydraulic cylinder actuator which drives a mechanical linkage and load) is shown in Figure 5-5. Generally, the gains in the servo valve are high enough that the servo valve dynamics can be neglected. However, the flow rate limits cannot be ignored. For control studies, the compressibility of the fluid (fuel) can be ignored so that the actuator dynamics (except the free integrator) can be neglected. This simplification results in the general actuator model used in the JTD simulation shown in Figure 5-6. A rate command from the control is multiplied by a gain to produce a rate motion of the actuator. This rate is limited in the actuator and

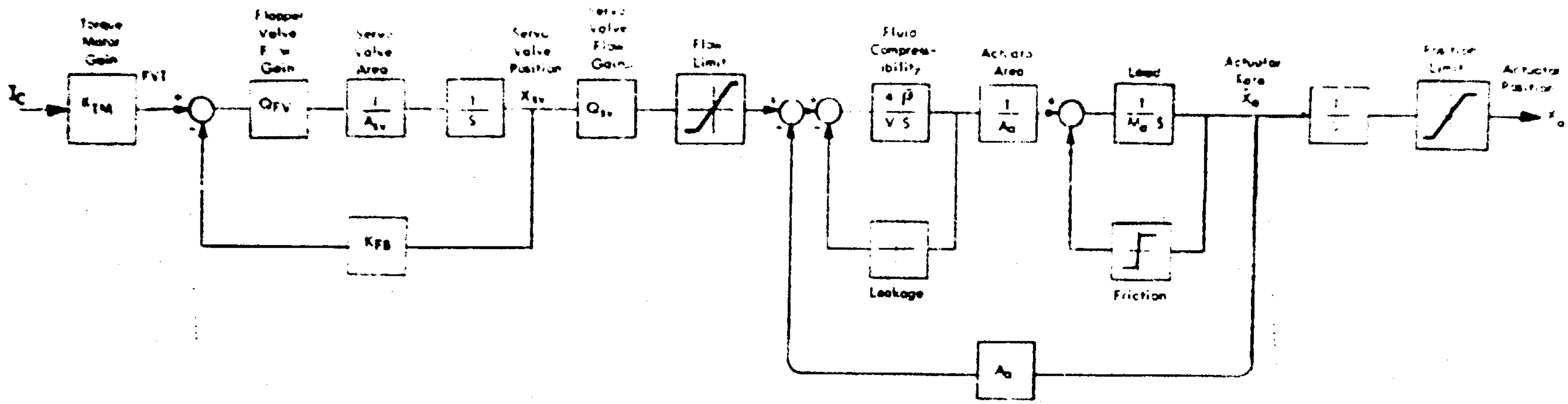


Figure 5-5 General Actuator Model

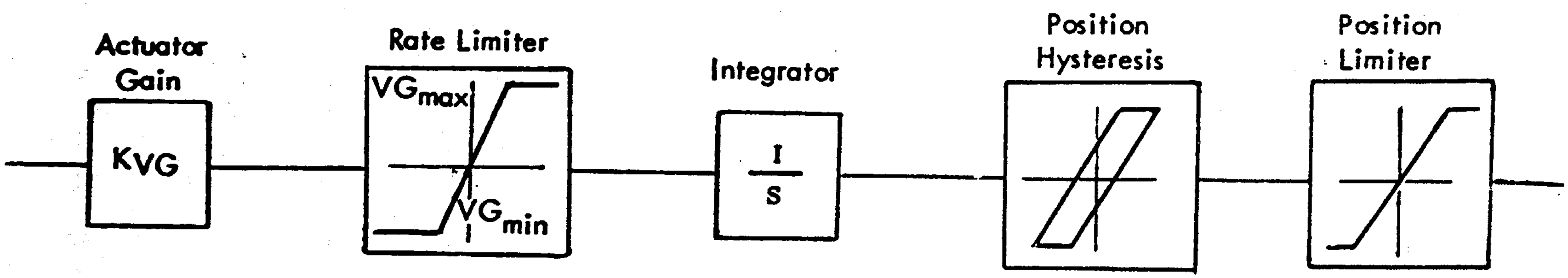


Figure 5-6 Simplified Actuator Model Used in Simulation

integrated to produce the actuator position. The actuator position also experiences hysteresis* and position limiting.

Fuel Pump and Metering Systems

Several pump and metering systems were considered including the APSI "vapor core" pump and the TF41 pump with both torquemotor and stepper motor driven metering systems. There is sufficient experimental data on the TF41 pump and metering system to justify a simple model that consists of two first order lags and a fuel flow limit as shown in Figure 5-7.

Since there is not enough "measured" data available on the APSI vapor core pump to mathematically evaluate the pump and metering system from a detailed math model, experimental data from previous testing on similar vapor core pumps was used to model the vapor core pump and metering system. The data shows that the pump dynamics can be represented by two or three first order lags. However, only one lag need be considered since the other two have no effect below 5 Hz and the engine response is basically between 1 and 2 Hz and no control loops are expected to have a response in excess of 5 Hz. The dominate time constant can vary from 0.01 to 0.25 as a function of supply pressure, delivery pressure, and flow rate. A typical value of 0.05 was used during most of the study although comparisons were made at other values. The pump time constant was held constant for any given transient response since a mathematical relationship between the time constant and the related pressures and flow was not available. Both torque-motor and stepper motor driven metering systems were investigated. In each case, sufficient feedback gain can be provided to approximate the metering system with a single lag with a time constant between .005 and .025 seconds. Thus, the model given earlier for the TF41 pump and metering system is a good approximation for the vapor core pump and fuel metering system although the vapor core pump may have a more variable time constant.

* A method of reducing the degrading effects of actuator nonlinearities is presented later.

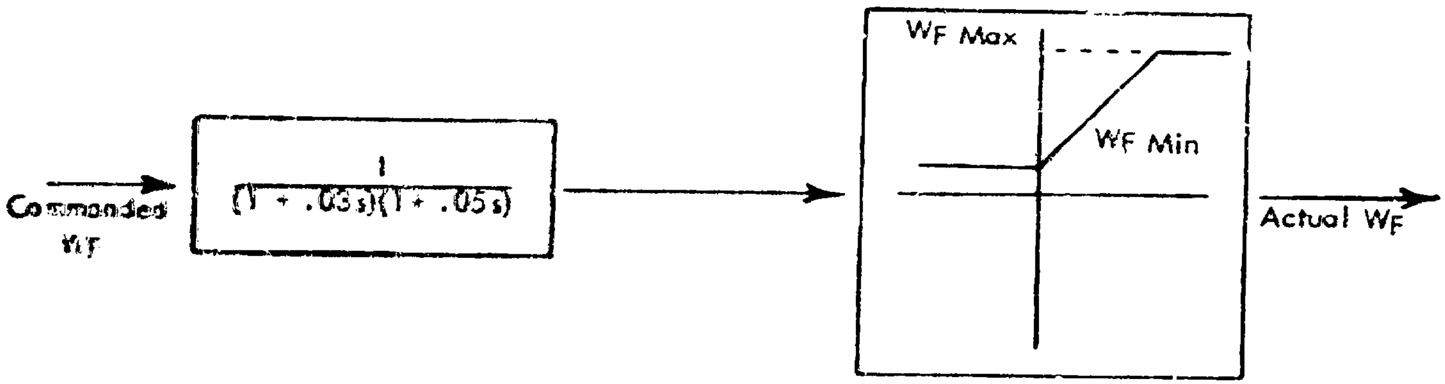


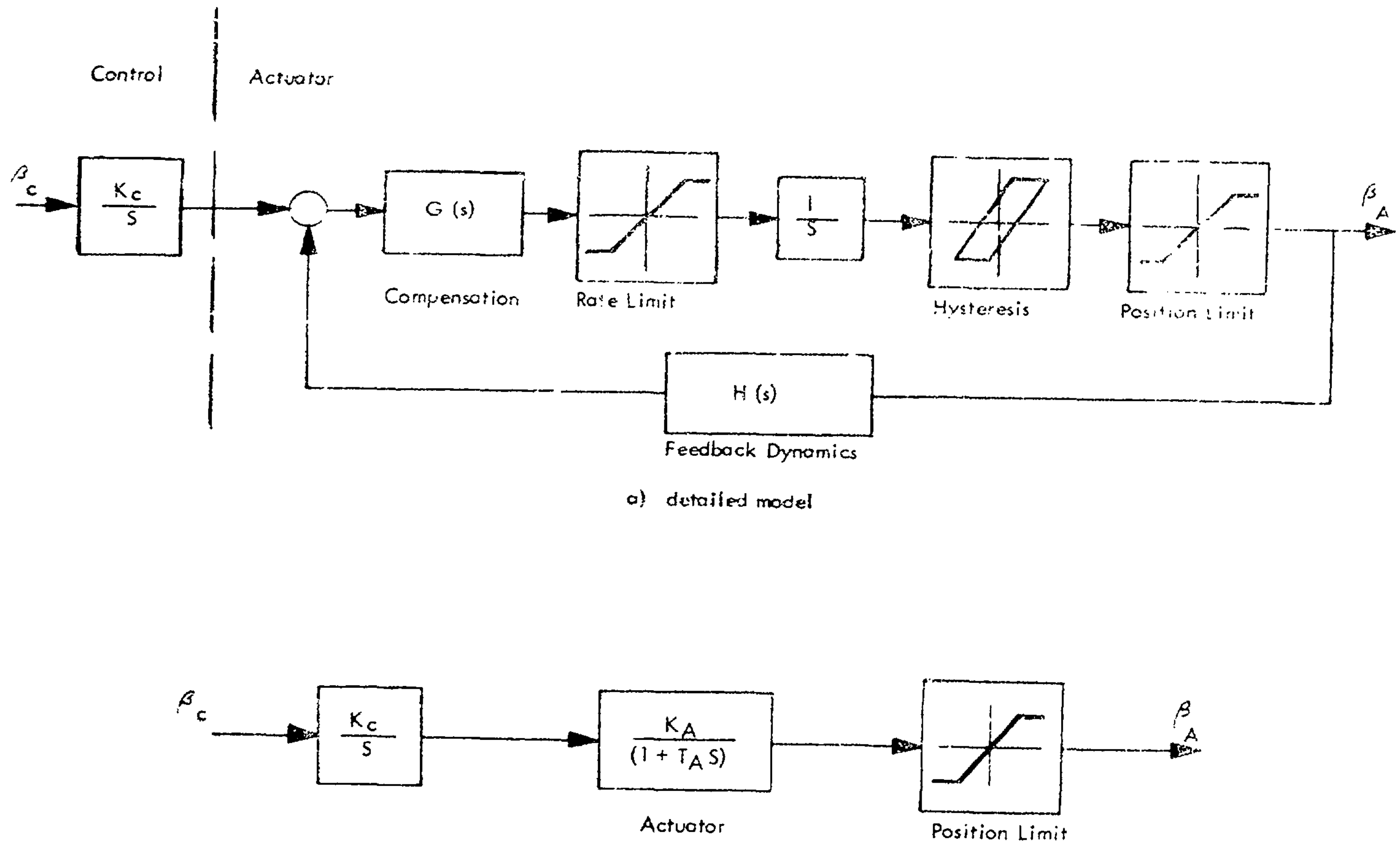
Figure 5-7 Pump and Metering Valve Simulation Model

Actuator Non-Linearities

The actuators and their respective loads (geometry position and fuel metering) display several undesirable characteristics other than the limiting included in the models presented above. These nonlinearities include hysteresis, deadband, and backlash which degrade the accuracy of a control loop and can create instabilities. However, the effect of these nonlinearities can be minimized by the use of feedback as shown in Figure 5-8. In this case, the output position is fed back to the input to the servo-valve to create a position loop which eliminates or reduces the effective hysteresis, deadband, and backlash. However, the open loop integrator feature of the actuator is lost and an integrator must be added to the control which changes the control output from a rate command to a position command. In addition, the control complexity is increased by an additional transducer, feedback path, and integrator. The transducer will be useful in failure detection. The quality of the transducer will be dictated by the mode selected and the degree of nonlinearities that is tolerable since the sensor becomes a major source of nonlinearity with position feedback. The use of position feedback will be avoided if possible to reduce control system complexity and cost.

Computerized Optimization and Constraint Procedures

The large number of variables involved in the JTD require a computerized method to efficiently and accurately optimize combinations of geometry settings for either maximum thrust or minimum specific fuel consumption. Without an optimization computer tool, each component would have to be varied independently to construct multi-layered plots. Time consuming multiple computer passes involving a significant number of points would be required for each flight condition. These plots become extremely complex when dealing with more than two or three variables -- with six control variables it is a near impossible task for hand optimization.



a) detailed model

b) simplified model

Figure 5-8 Actuator with position feedback

A system was developed that will, with a single computer run, simultaneously optimize each of the controlled variables to give either a maximum installed thrust or minimum installed specific fuel consumption within engine and geometry limitations. As a byproduct of this type of analysis, individual sensitivities are established for each variable. Thus, the relative contribution of each component to the overall performance can easily be determined.

A separate system has been developed that will adjust the variable geometry to satisfy an imposed set of constraints simultaneously. It can be used to maintain several engine limits simultaneously, such as rotor speeds and turbine temperature. It may also include as constraints parameters not limited in a real engine, such as surge margins and bypass ratio. These two systems added to the computer simulation of the JTD were developed to aid performance and control design studies. The application of these tools will be presented in the control mode section.

Variable Geometry Optimization System

The optimization system is designed to maximize net thrust or minimize specific fuel consumption by automatic manipulation of one or more variable geometry components simultaneously in one pass on the computer. This may be done for any flight condition for either uninstalled or installed engine performance. All defined engine limiters can be maintained for engine matching, and maximum and minimum setting limitations can be imposed on each variable geometry component.

The method used involves the search for zero slope on a generated curve of net thrust or specific fuel consumption versus each variable geometry component position. The iteration procedure varies all the geometry settings individually on each pass to generate the curves, then produces new settings for the components simultaneously for the next iterative pass based on zero slope estimates derived from those curves. The iteration process is satisfied when each variable geometry component setting meets any one of three criteria: (1) its slope versus F_n or sfc is within a tolerance of zero; (2) it has reached its maximum or minimum limited setting; (3) its variation plus and minus did not significantly affect the parameter being optimized, F_n or sfc.

Variable Geometry Constraint System

This system forces selected geometry variables to positions which satisfy an equal number of compatible constraints. The system generates a set of simultaneous linear differential equations to relate the selected variables with their effect on matching the selected constraints. Through an iterative process, these equations reposition the geometry settings to satisfy these constraints similar to the iteration method used for optimization.

The constraints available are:

- H. P. turbine inlet temperature
- Engine net thrust
- Fan surge margin
- H.P. compressor surge margin
- H.P. rotor speed (rpm)
- L.P. rotor speed (rpm)
- Percent corrected H.P. compressor speed (map value)
- Primary nozzle area
- Bypass nozzle area
- H.P. turbine flow capacity (% of design setting)
- L.P. turbine cavity pressure ratio
- H.P. compressor variable geometry schedule
- L.P. turbine jet flap flow (% of HPC inlet flow)
- Percent corrected fan speed (map value)
- Bypass ratio

- L.P. turbine inlet temperature (°R)
- Engine inlet airflow (lb/sec)
- Engine inlet corrected airflow (lb/sec)
- Compressor discharge pressure (PSIA)

6.0 CONTROL MODE STUDY

The foundation of the control mode study is the definition of the control goal or control mode design criteria. With the great variability of the JTD, the ability to optimize the engine operation to some criteria is obvious. The ability to provide optimal transient response in the true sense of "optimal control" or "modern control theory" was beyond the scope of this project. A "pseudo" optimal transient capability based upon maximum rotor speeds over a large thrust range was investigated. In this mode, the rotor speeds are maximized at each thrust level. Thus, the basic lags of the engine (rotor dynamics) are minimized and faster acceleration of the engine are possible. This criteria has many potential applications such as a special combat mode, carrier landings, V/STOL, etc., however, optimization of steady state performance is a more reasonable criteria since it does not place additional high dynamic requirements on the engine or control system. Optimization of sfc will increase the cruise range of an aircraft while maximizing the thrust over the flight envelope increases the combat effectiveness of the aircraft. In addition, the ability to optimize steady state performance of the engine over the entire flight envelope demonstrates a capability of variable geometry engines not attainable with a fixed geometry engine.

The design goal of the control mode for the JTD is to provide optimum steady state performance and rapid transient response. Specifically, the objectives are

- minimum sfc at all thrust levels
- maximum thrust over the flight envelope
- safe engine transient operation (do not violate engine parameter limits)
- rapid transient response

Engine Optimization

Detailed Uninstalled Optimization

The first step of the design was to optimize the engine (no control system) within the design constraints. To assure a design that is valid over the flight envelope, the following four representative flight conditions were chosen

- o sea level static
- o Mach 1.2 at 500' (maximum CDP pressure)
- o Mach .75 at 36089'(low airflow and low inlet temperature)
- o Mach 2.2 at 36089'(high inlet ram)

The optimization of sfc and maximum thrust for an uninstalled engine was carried out at many thrust levels for these four flight conditions with the computerized programs presented in the previous section. The results are not completely as one might expect since the JTD is a combination of development components to demonstrate component technology rather than a matched engine. For a matched engine, minimum sfc generally requires maximum airflow, maximum bypass ratio, and minimum temperature until engine limits are reached at which time the temperature is increased to achieve maximum thrust. Thus, the fan speed should be brought up as fast as possible for maximum airflow and the bypass ratio maximized by minimizing the core flow through lowering the core spool speed and keeping the turbine and compressors closed. However, for minimum sfc with the JTD, the HPC remains open and the HPT remains closed at the lower and mid-thrust region until an engine limiter is reached (speed or pressure). In the mid-thrust range, both nozzles remain open. The sfc is not affected greatly by the LPT geometry. However, in the high thrust range and for maximum thrust, all the geometry moves to allow the engine to attain more than one limiting state. The final engine limiter attained is usually temperature. Specific data from the optimization is presented in the mode development as it is used to develop the various modes investigated.

Comparison of Uninstalled vs. Installed Optimization

The optimization for the control design was computed with an uninstalled engine configuration since it represents the test stand configuration which is the initial application of the control. To keep the study as general as possible, the mode design is based upon an uninstalled engine.

However, the final mode should be able to fly throughout the flight envelope which requires the consideration of the installation effects. The optimization procedure was repeated for three corrected thrust levels and a large range of corrected inlet conditions for both installed and uninstalled conditions. Although no significant differences effecting the control mode were seen in this investigation, a more detailed study with an actual aircraft and inlet is expected to impose additional control requirements (i.e. inlet flow matching and augmentation).

Uninstalled performance was computed for the JTD engine using MIL-E-5008B inlet recovery, unity for nozzle throat flow coefficients and 0.985 for the nozzle gross thrust coefficients. Ranges of ram pressure ratio and engine inlet temperatures were investigated over a range of power levels. Ram pressure ratios from 1.0 to 8.0 and inlet theta values from 0.8 to 1.5 were studied at power levels of 3000, 6000 and 9000 pounds thrust (F_n/δ_1) as well as maximum non-augmented and maximum augmented thrust levels. The three lower thrust levels were optimized for minimum specific fuel consumption and the two higher thrust levels were optimized for maximum thrust by coordinating the geometry positions of the high pressure compressor, high pressure turbine, low pressure turbine and both exhaust nozzles within their physical capabilities.

Installed performance was computed for the same ram pressure ratio, engine inlet theta and power level ranges as for the uninstalled data to determine the degree of effect on control mode and scheduling requirements. A simulation of a fixed-schedule variable inlet was used for inlet recovery and inlet drag characteristics with the capture area sized by the guidelines developed for the APSI cycle studies. Flight nozzle characteristics were used for gross thrust and flow coefficient characteristics. No aircraft bleeds or power extractions were considered, but this consideration should have little impact on the results.

The results of the uninstalled and installed performance data were generalized with check runs made to assure that smoothing and generalizing of schedules maintained performance within one and one-half percent of optimum. The following table compares the resulting generalized scheduling trends.

Table 6-1

JTD Optimized Non-Dimensional Data Comparison

Operation	Geometry	Uninstalled High Flow Rate	Installed High Flow Rate
3000# F_n/δ_1	HPC	120 (Open)	120 (Open)
Opt SFC	HPT	Closed	Closed
	LPT	Min Jet Flap Flow	Min Jet Flap Flow
	A8	Avg 244 $in.^2$	Avg 280 $in.^2$
	A18	High RPR - 150 $in.^2$	High RPR - 280 $in.^2$
		Low RPR - 290 $in.^2$	Low RPR - 314 $in.^2$
6000# F_n/δ_1	HPC	115	116
Opt SFC	HPT	Closed	Closed
	LPT	Min Jet Flap Flow	Min Jet Flap Flow
	A8	Avg 272 $in.^2$	Avg 295 $in.^2$
	A18	High RPR - 120 $in.^2$	High RPR - 220 $in.^2$
		Low RPR - 309 $in.^2$	Low RPR - 310 $in.^2$
9000# F_n/δ_1	HPC	112	115
Opt SFC	HPT	Closed	Closed
	LPT	Min Jet Flap Flow	Min Jet Flap Flow
	A8	Avg 290 $in.^2$	Avg 300 $in.^2$
	A18	High RPR - 120 $in.^2$	High RPR - 150 $in.^2$
		Low RPR - 286 $in.^2$	Low RPR - 290 $in.^2$

Table 6-1 (continued)

Operation	Geometry	Uninstalled High Flow Rate	Installed High Flow Rate
Max Dry	HPC	Open	Open
	HPT	Schedule vs 1	Same Schedule vs 1
	LPT	Min Jet Flap Flow	Min Jet Flap Flow
	A8	Schedule vs 1	Same Schedule vs 1
	A18	Schedule vs 1	Diff Schedule vs 1

Sensitivity Study

In most of the control modes studied, the sensitivity or gain of the engine is an important part of the loop design. To make a "tight" control loop, it is desirable to make the loop gain as high as possible. Thus, it is desirable that the sensitivity be as large as possible. It is also desirable that the sensitivity be a constant over the entire flight envelope to minimize the nonlinearities and provide constant response at all conditions. A list of potential engine parameters to be used in the various control modes was developed from a survey of existing engine controls. This list is given in Table 6-2. A sensitivity study was made at several flight conditions by varying the geometry one at a time about the optimum position and observing the engine parameter changes with the steady state engine simulation. Both corrected and uncorrected engine parameters from Table 6-2 were studied. Typical sensitivity values are given in Table 6-3.

For most geometry variables, a few engine parameters displayed a much higher sensitivity value than the remaining engine parameters. For example, the important sensitivity data for the primary exit nozzle is given in Table 6-4 for a large number

Table 6-2

Variable Geometry Potential Control Modes

Fan stator control

- Fan inlet temperature
- Fan inlet pressure
- Fan inlet Mach number
- [illegible]
- HPC inlet pressure

HPT stator control

- HPT inlet temperature
- HPT inlet pressure
- HPT outlet pressure
- HPC speed
- HPC pressure ratio
- HPC outlet temperature
- HPC outlet pressure
- Turbine blade tip temperature

Primary exit nozzle

- Ambient temperature
- LPT outlet pressure
- LPT pressure ratio
- Power lever position
- Fan speed
- Fan pressure ratio
- Fan inlet temperature
- Fan exit pressure

Secondary exit nozzle

- Power lever position
- Primary nozzle area
- Duct flow
- Inlet flow
- Fan pressure ratio
- Fan speed

HPC stator control

- HPC inlet temperature
- HPC inlet pressure
- HPC inlet Mach number
- HPC outlet pressure
- HPC outlet Mach number
- HPC speed

LPT jet flap control

- LPT cooling
- LPT inlet temperature
- LPT inlet pressure
- LPT outlet pressure
- Fan speed
- Fan pressure ratio
- Fan inlet temperature

Fuel control

- Fan speed
- HPC speed
- HPC discharge pressure
- HPT inlet temperature
- HPT inlet pressure
- HPT exit temperature
- HPT exit pressure
- Ambient temperature
- Ambient pressure
- Ambient Mach number
- Power lever position
- Turbine blade temperature
- Exhaust gas temperature

Table 6-3

Typical Sensitivity Matrix

ALT = 36,089 Ft.   MACH = 2.400   PAMB = 3.283 PSIA   TAMB = -69.70 DEG.F

NOMINAL CONTROL MODE SETTINGS

WFENGN - 17430 Lbs/Hr	HPCSET = 109.92	HPTSET - 114.45
AREA8 · 329.00 Sq. In.	AREA18 = 536.00 Sq. In.	LPTSET = 1.048

ENGINE PARAMETERS - NOMINAL VALUES

FNET = 37999 Lbs.	PCODE = 100.00	SMHPC = 69.835	SMLPC = 63.571
SFC = 1.855 Lbs/Hr/Lb	BPR = 1.836	NHIGH = 13633 RPM	
NLOW	CORNL = 8604	CORNH 9547 RPM	

PFANIN = 42.34 PSIA	TFANIN = 377.8 DEG F	WAFANI = 461.06 LBS/SEC
PHPCIN = 89.54 PSIA	THPCIN = 1057.5 DEG R	WAHPCI = 162.94 LBS/SEC
PHPCO = 342.37 PSIA	THPCO = 1619.3 DEG R	WAHPCO = 163.01 LBS/SEC
PHPTIN = 321.21 PSIA	THPTIN = 3655.2 DEG R	WAHPTI 137.86 LBS/SEC
PMTURB 136.82 PSIA	TMTURB = 3007.6 DEG R	WGNOZL = 167.14 LBS/SEC
PLPTO = 48.93 PSIA	TLPTO = 2410.7 DEG R	WADUCT = 298.05 LBS/SEC
PNOZL = 48.21 PSIA	TNOZL = 1951.0 DEG R	WGDUCT = 312.75 LBS/SEC

	HPTSET	LPTSET	AREA8	AREA18	HPCSET	WFENGN
FNET	-0.15	0.00	0.26	0.26	0.08	0.53
NLOW	-0.19	-0.00	0.28	0.06	0.08	0.27
NHIGH	-0.50	-0.08	-0.02	-0.00	-0.32	0.16
WAFANI	-0.36	-0.03	0.32	0.29	0.14	0.36
WAHPCI	-0.93	-0.16	0.15	-0.00	0.31	0.4[illegible]
WAHPCO	-0.92	-0.15	0.17	0.00	0.33	0.46
WGHPTI	-0.92	-0.22	0.15	-0.01	0.30	0.46
WGNOZL	-0.90	-0.15	0.15	-0.00	0.32	0.47
WADUCT	-0.05	0.02	0.40	0.44	0.04	0.31
WGDUCT	-0.06	0.02	0.40	0.46	0.05	0.30
TFANIN	0.00	0.00	0.00	0.00	0.00	0.00
THPCIN	-0.03	0.00	0.10	-0.01	0.02	0.09
THPCO	-0.44	-0.04	0.03	-0.0[illegible]	-0.06	0.15
THPTIN	0.27	0.08	-0.07	-0.01	-0.18	0.32
TMTURB	0.52	0.11	-0.07	-0.00	-0.18	0.33
TLPTO	0.52	0.07	-0.23	-0.01	-0.19	0.33
TNOZL	0.65	0.09	-0.23	-0.01	-0.24	0.41
PFANIN	0.00	0.00	0.00	0.00	0.00	0.00
PHPCIN	-0.12	0.00	0.35	-0.01	0.07	0.29
PHPCO	-1.90	-0.18	0.11	-0.01	0.20	0.61
PHPTIN	-2.00	-0.18	0.11	-0.01	0.19	0.[illegible]
PMTURB	-0.64	-0.30	0.14	-0.00	0.23	0.65
PLPTO	-0.61	-0.12	-0.96	-0.00	0.21	0.64
PNOZL	-0.61	-0.12	-0.99	-0.00	0.22	0.64
CORNL	-0.19	-0.00	0.28	0.06	0.08	0.27
CORNH	-0.50	-0.08	-0.02	-0.00	-0.32	0.16
CWHPCI	-0.93	-0.16	0.16	-0.00	0.31	0.4[illegible]
CWHPCC	-0.92	-0.15	0.17	0.00	0.33	0.46

Table 6-4

Engine parameter sensitivity to primary exit nozzle changes over the flight envelope

Case	Engine Parameters Fan Speed	Fan Airflow	Duct Airflow	HPC Inlet Pressure	LPT Outlet Pressure
1	.28	.32	.46	.38	-.92
2	.17	.20	.26	.22	-.94
3	.23	.27	.35	.29	-.88
4	.58	.44	.51	.80	-.76
5	.46	.51	.64	.65	-.77
6	.39	.47	.62	.48	-.83
7	.34	.35	.50	.44	-.98
8	.34	.35	.49	.44	-1.00
9	.34	.35	.47	.42	-.97
10	.42	.39	.51	.46	-.95
11	.45	.32	.40	.42	-.86
12	.65	.46	.45	.40	-.75
13	1.27	.47	.37	.33	-.57
14	.49	.54	.66	.68	-.74
15	.46	.40	.34	.68	-.84
16	.36	.30	.34	.49	-.90
17	.29	.28	.35	.45	-.92
18	.23	.23	.30	.28	-.57
19	.24	.15	.14	.18	-.76
20	.21	.27	.36	.30	-.91
21	.27	.26	.34	.33	-.98
22	.59	.31	.30	.38	-.76
23	.47	.57	.75	.68	-.72
24	.52	.71	.85	.73	-.72
25	.43	.59	.80	.64	-.79
26	.41	.36	.45	.65	-.90
27	.37	.29	.36	.55	-.93
28	.30	.25	.34	.46	-.93
29	.35	.33	.45	.52	-.96
30	.30	.37	.50	.42	-.92
31	.32	.31	.42	.42	-.92

$$\text{Sensitivity} = \frac{\text{Percent Change in Engine Parameter}}{\text{Percent Change in Primary Nozzle Area}}$$

of thrust levels and flight conditions. The pressure at the LPT output displays the greatest sensitivity to changes in primary nozzle area. Other engine parameters (both corrected and uncorrected) showing a promising sensitivity to primary nozzle area changes are:

- o Fan speed
- o Fan airflow
- o Duct airflow
- o HPC inlet pressure
- o LPT outlet pressure

Thus, from a loop gain criteria, these parameters represent the prime candidates for control of the primary exit nozzle area.

Fuel Control

The fuel control can be divided into three sections: 1) a governor, 2) acceleration and deceleration schedules, and 3) limiters. Ideally, the governor regulates the fuel flow during steady state and the acceleration schedule, deceleration schedule, and limiters protect the engine during transients. However, at maximum thrust, either the governor or the limiters may be in control of fuel flow in most fuel controls on today's engines. Thus, the governor's primary goal is to regulate fuel in a manner that provides minimum sfc over the thrust range. The acceleration and deceleration schedules must protect the engine during the transient operation. The limiters also must protect the engine during transient operation and may be required to maintain engine parameters during steady state at or near maximum thrust. With these requirements established, the individual section will be discussed.

Governor

Since accurate control of the engine parameter during steady state operation is required to maintain minimum sfc, only an integral plus proportional governor was considered to assure zero steady state errors. Thus, the governor will be of the form shown in Figure 6-1. The following parameters were considered as the control engine parameter based upon their criticality in achieving the optimal objectives.

- o High pressure spool speed (N_H)
- o Low pressure spool speed (N_L)
- o Turbine inlet temperature

Both corrected and uncorrected parameters were considered.

Turbine inlet temperature has the greatest appeal for achieving maximum thrust and good fuel modulation over the thrust range since it is the final limiter reached. In general, many engine parameters reach near their maximum values

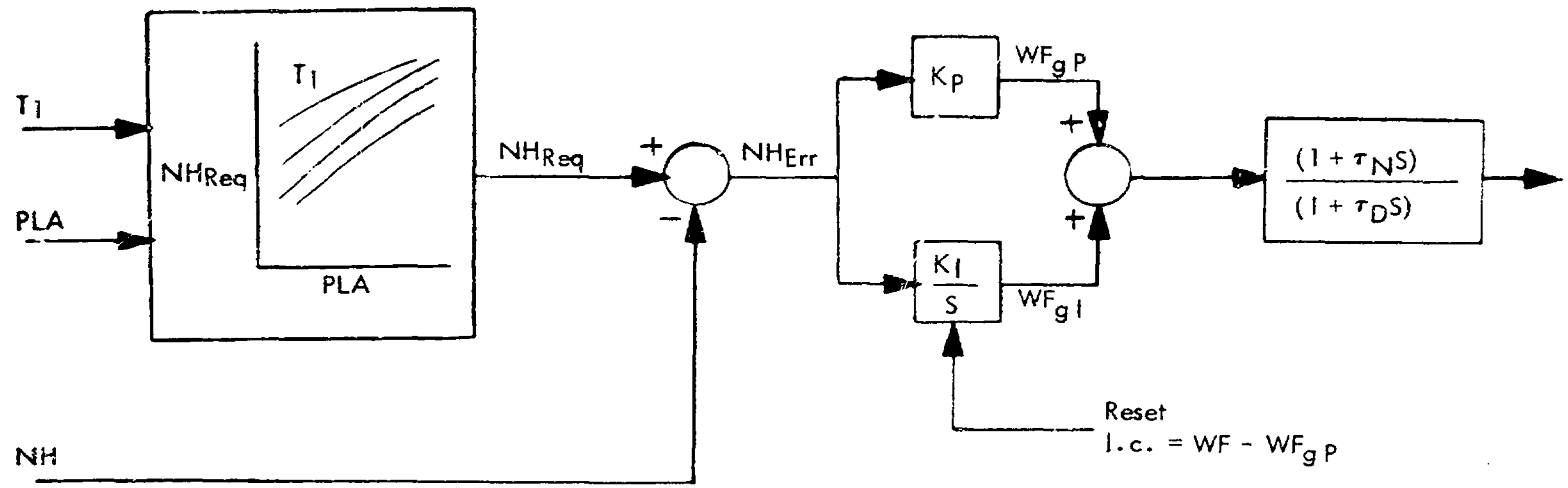


Figure 6-1 Fuel Control Governor

at the upper thrust range while the temperature is relatively low for minimum sfc. Maximum thrust is achieved by raising the turbine inlet temperature to the maximum allowable. Thus, a temperature governor with zero steady state error would theoretically achieve maximum thrust over the flight envelope.

However, dynamic problems, reliability, and inaccuracy, make the use of a temperature governor impractical. Temperature measuring devices (principally thermocouples) have slow dynamics (high time constants) which are difficult to compensate because the time constants vary greatly with mass flow. Secondly, the measurement of TIT at the temperature levels experienced in this engine are impossible with today's sensors except for special short life test equipment. Thus, the hottest cycle temperature that is practical to measure with current technology is the temperature between the two turbines. Even the measurement of the inter-turbine temperature (T4.1) requires some development work and the sensors that exist are unproven. In addition, the relationship between TIT and T4.1 is a function of the HPT index and the power extraction from the HP rotor system. Therefore, the value of T4.1 representing maximum thrust is not a constant even though TIT is a constant maximum throughout the flight envelope. Even if an accurate algorithm between TIT and T4.1 was found and a satisfactory variable compensation for the thermocouple was developed, a T4.1 governor was considered too "high-risk" because of a lack of experience and reliability data in measuring temperatures in this range.

Both NL and NH speed governors were also investigated. The primary difference between the two speeds is that the NL maximum is reached at about 80% thrust in some cases whereas true NH maximum is generally reached at maximum thrust. In some cases the modulation of high pressure rotor speed is very small above 90% thrust. Thus, with a NL governor, the throttle command (PLA) will have to perturb a second engine control (variable geometry) which must drop NL before more fuel will be commanded above 80% thrust. Although the response of such a governor will be good since the dynamics of the engine have been removed to some extent, the accuracy may be less because the errors in the geometry loop as well as the governor loop must be considered in evaluating the accuracy of fuel flow vs. throttle position.

Because of these potential problems with an NL governor, an NH governor was chosen. The speed request schedule shown in Figure 6-2 is based upon the optimization on sfc and the requirement to linearize thrust on PLA. Since the use of corrected speed did not eliminate the need for lines of constant inlet temperature, uncorrected speed is used to simplify the computations.

Acceleration and Deceleration Schedules

Acceleration Schedule

A conventional acceleration schedule of W_f/P_3 was selected to protect the engine during the transient and aid surge recovery. An interesting result of the optimization surfaced during the construction of the acceleration schedule. The acceleration and deceleration curves were estimated from the operating curve using conventional guidelines of 15% - 20% displacement between the required to run schedule and the acceleration and deceleration schedules. This allows approximately a 500°F increase in engine temperature in the case of an acceleration for a given engine speed. The operating line for the cold inlet (Mach .75 at 36089 ft.) was actually higher than the operating line for sea level static. Thus, it was difficult to accurately interpret between the temperature lines on the estimated acceleration schedule. As the acceleration schedule was adjusted to provide a minimum of 10% surge margin and a goal of 20% surge margin over the flight envelope, the static sea level acceleration schedule shifted above the cold inlet as shown in Figure 6-3. Although the present acceleration schedule is adequate, the relative distances

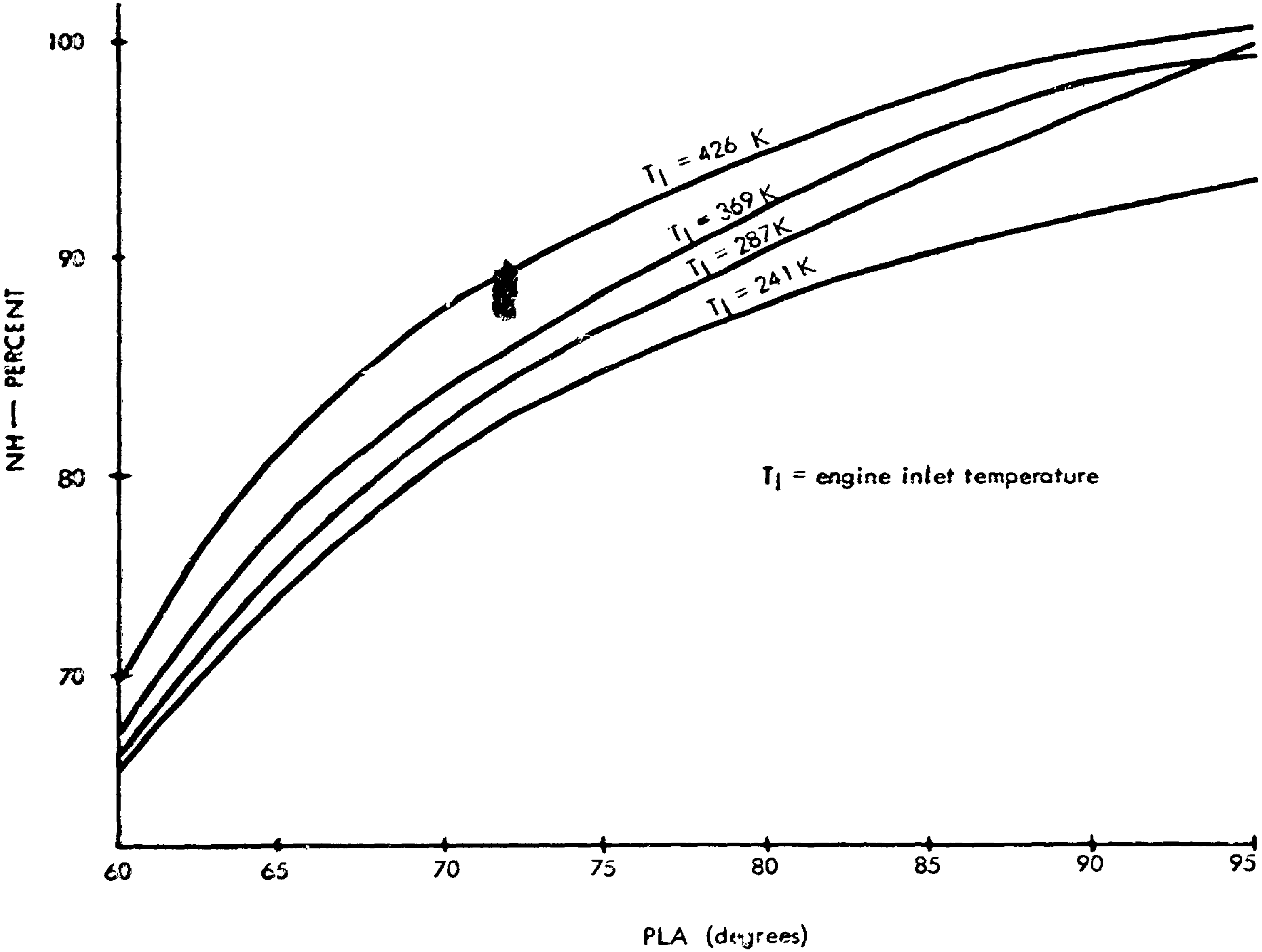


Figure 6-2 Governor Speed Request Schedule

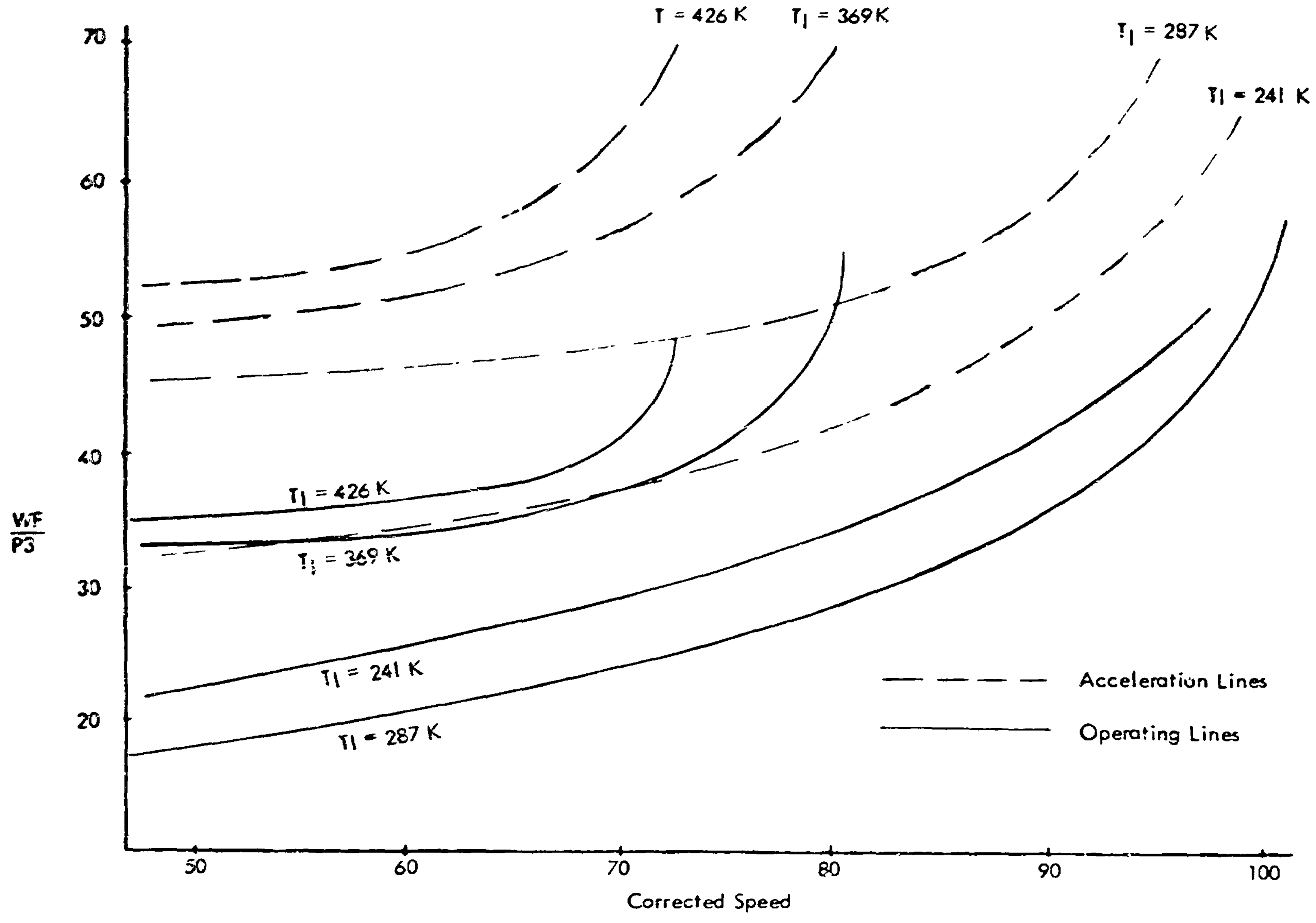


Figure 6-3 Acceleration Schedules

between the operating lines and acceleration schedules shows that surge avoidance will be much more difficult at a cold inlet than at static sea level indicating that a sacrifice of optimal steady state performance for more surge margin may be desirable for cold inlet conditions. This was the case in the adjustment of the gains for stable, safe engine transients. Safe operation with the original acceleration schedules was possible with constant gains at all the other flight conditions. The acceleration schedule isn't exercised in an accel from 40% thrust to intermediate power at static sea level. However, some gains had to be reduced and the acceleration schedule adjusted several times to provide safe engine operation at the cold inlet conditions. This phenomena requires further investigation since no trouble was encountered in running cold day conditions at static sea level.

ΔP/P Surge Control

The above acceleration schedule to avoid transient surge of the HPC are based upon an apriori knowledge of the relationship between the engine operation and HPC surge. That is, a given fuel flow or W_f/P_3 , inlet temperature, and speed combination will produce a given surge margin. Because of variations in this relationship caused by component variation between engines and component degradation, adequate surge margin must be built into the acceleration schedules. It is more desirable to tie the surge avoidance of the fuel control more directly to the compressor operation, especially since the surge line shift with HPC index adds one more variable into the determination of the acceleration schedules.

Two HPC related parameters were investigated to yield a better definition of the surge margins during transient operation. Since speed is the transient independent variable in the fuel control during an acceleration response, the remaining parameters required to specify surge margin are HPC pressure ratio, corrected flow, and HPC index. Considerable interest has been shown in the use of $\Delta P/P$, where

$$\Delta P/P = \frac{\text{total pressure} - \text{static pressure}}{\text{total pressure}}$$

as a measure of local Mach number or the corrected flow (Wa) for surge control. An engine parameter and sensor accuracy comparison has been made between $\Delta P/P$ and HPC pressure ratio to determine which one imposes the most stringent accuracy requirements for estimating surge.

The engine parameter accuracy comparison was done by an analysis of the compressor map shown on Figure 6-4. The $\Delta P/P$ lines that result at the compressor exit are superimposed on the compressor map. The more stringent accuracy requirements for either mode occurs at a relatively low flight Mach number and high altitude condition. This condition results in low compressor airflow which for this analysis corresponds with a corrected speed of 70%. The results of the engine parameter accuracy comparison are shown in Figure 6-5. As shown, the $\Delta P/P$ mode is approximately three times more accurate than the compressor pressure ratio mode for the same surge margin accuracy. To maintain +2% surge margin accuracy, the $\Delta P/P$ percent of point accuracy is 2.2% and the pressure ratio percent of point accuracy is .8%.

Based upon the engine parameter accuracy data, a sensor accuracy comparison was made to determine how accurate the sensors have to be to achieve the +2% surge margin accuracy. The pressure ratio mode uses compressor inlet ($P_{2.1}$) and discharge (P_3) total pressure probes. The $\Delta P/P$ mode uses compressor discharge ΔP ($P_{t_3} - P_{s_3}$) and P_3 total pressure probes. The results of the sensor accuracy comparison are shown in Figure 6-6. As shown, to achieve a +2% surge margin

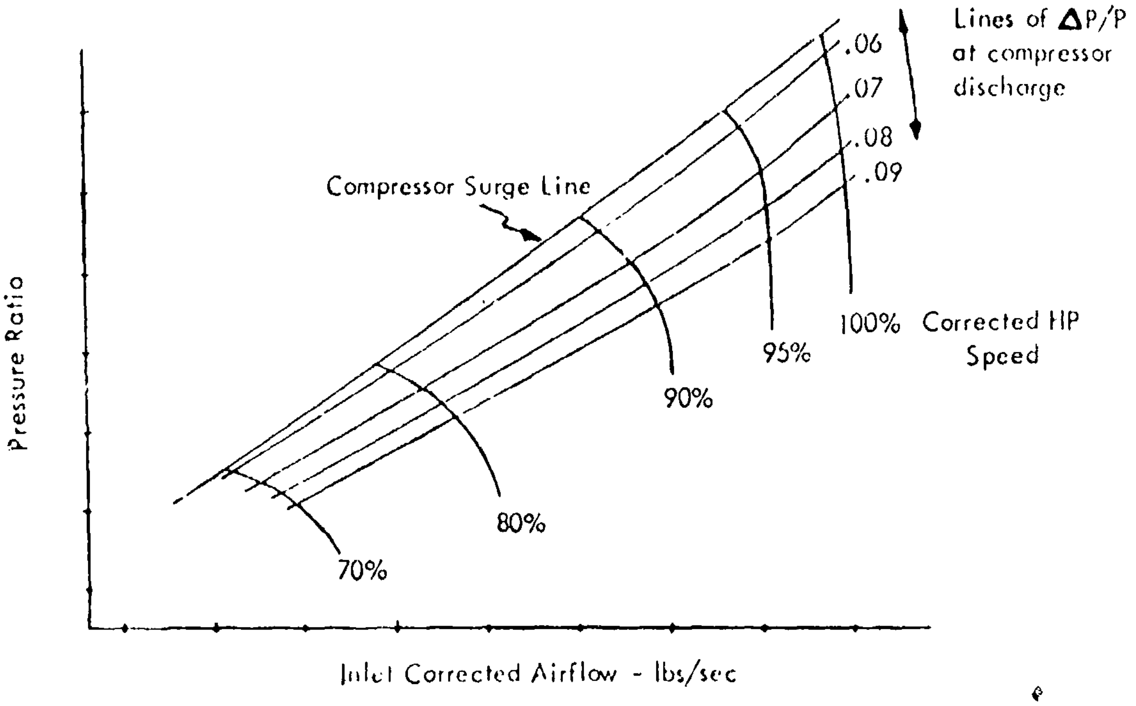


Figure 6-4 HPC Compressor Map With ΔP/P Lines

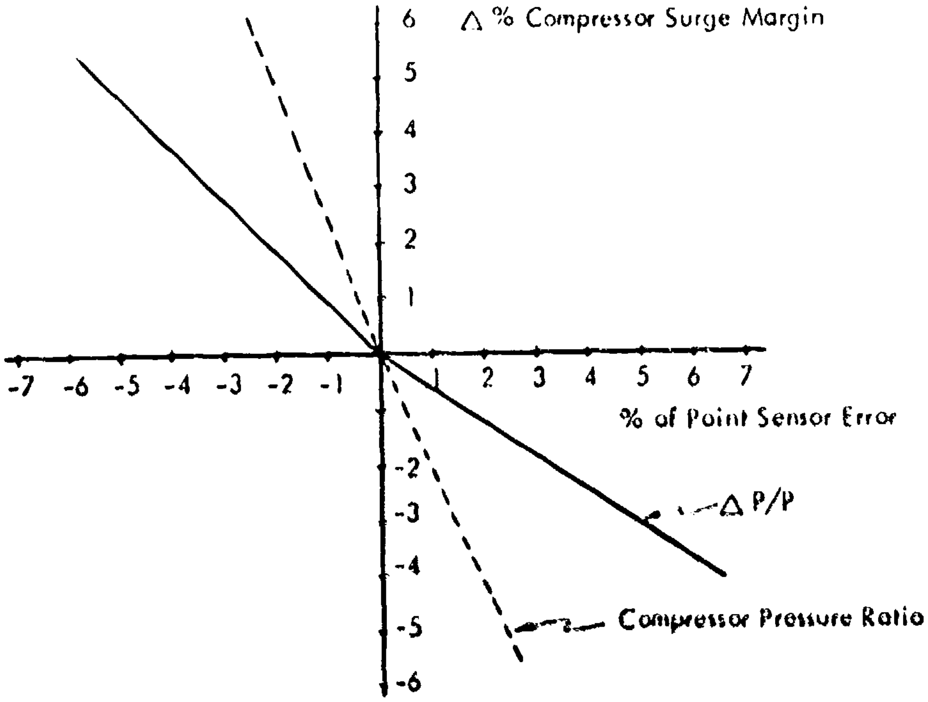


Figure 6-5 Acceleration Mode -- Parameter Accuracy Comparison

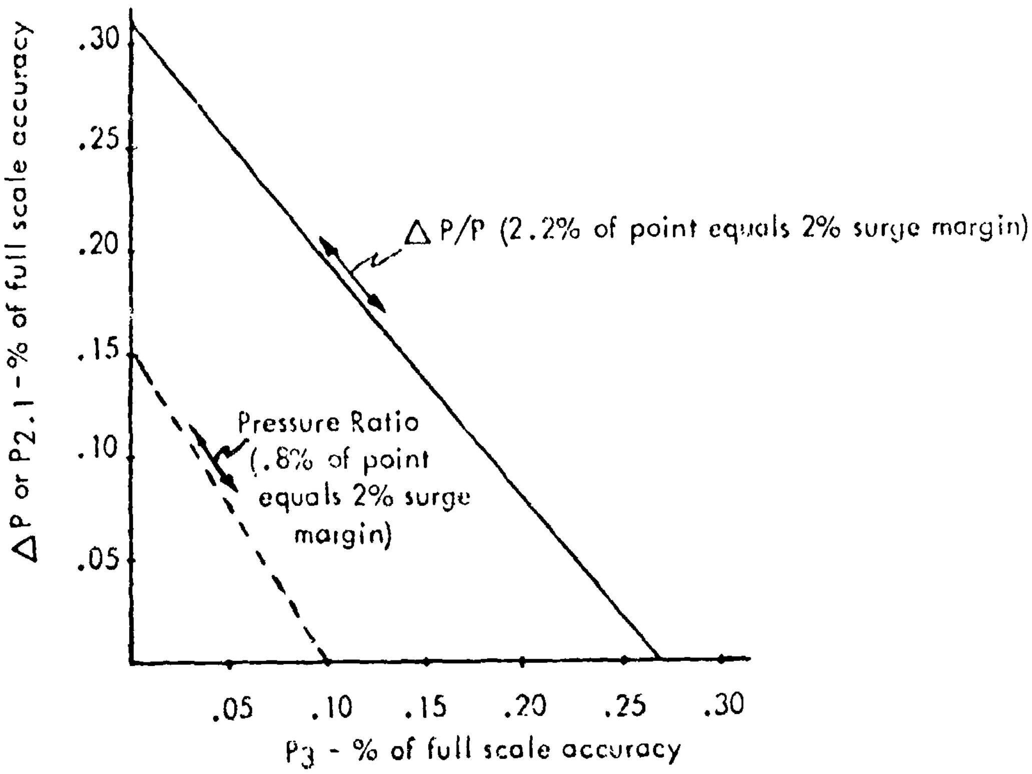


Figure 6-6 Acceleration Mode Sensor Accuracy Comparison

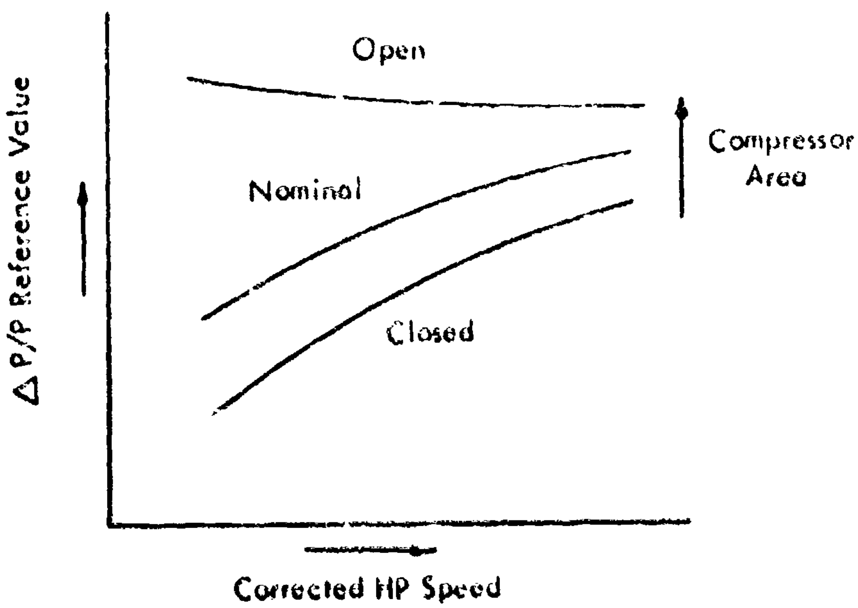


Figure 6-7 ΔP/P Surge Control Schedule

accuracy, the pressure ratio mode requires much more accurate sensors than the $\Delta P/P$ mode. As an example, to maintain +2% surge margin accuracy, the pressure ratio mode requires the $P_{2.1}$ and P_3 sensors to have a full scale accuracy of .08% and .05% respectively, while the $\Delta P/P$ mode only requires the ΔP and P_3 sensors to have a full scale accuracy requirement of .14% and .15%, respectively.

The conclusion of this accuracy study was that the compressor pressure ratio measurement has more stringent parameter and sensor accuracy requirements than the $\Delta P/P$ measurement for estimating surge.

At first, it was thought that the $\Delta P/P$ reference value could be a fixed value during engine accelerations. However, due to the fact that the variable geometry compressor area changes during engine acceleration, thus shifting the operating line of the compressor, the $\Delta P/P$ reference value must also change. Further investigation has indicated that varying the $\Delta P/P$ reference value with only the compressor index is not enough to represent a constant surge margin. The $\Delta P/P$ reference value must also be varied with corrected HP speed. Consequently, the $\Delta P/P$ reference schedule is defined by corrected HP speed and compressor area as shown in Figure 6-7.

Several different methods of incorporating this surge margin "measurement" into control algorithm were considered. One requirement was that the $\Delta P/P$ mode must compliment the acceleration schedule so that the acceleration schedule will always be able to "back-up" the $\Delta P/P$ surge control if required to protect the engine. The method that demonstrated the most promise was to use the $\Delta P/P$ reference in the same manner as a limiter which will be discussed next.

Fuel Limiters

Since the limiters are basically a transient device, only proportional limiting was used. A limiter only subtracts fuel from the governor fuel request (or acceleration schedule in some designs) when an engine parameter exceeds a reference value. The engine parameters requiring limiters were:

- o $\Delta P/P$ (surge control)
- o NHC2 (corrected NH)
- o NL
- o NLC1 (Corrected NL)
- o T4.1 (interturbine temperature)
- o TBT (turbine blade temperature - optical pyrometer)
- o CDP (compressor discharge pressure)

The reference values for NHC2, NL, NLC1, and CDP are constants as shown in Figure 6-8. The TBT limiter was not exercised since the simulation has no method of determining metal temperatures. Experimental data will be used to evaluate this loop as data becomes available from early ATEGG and JTD testing. The reference value for $\Delta P/P$ is a function of NH and HPC index as shown in Figure 6-7. The reference value for T4.1 is a function of the HPT setting since T4 is the temperature that actually requires limiting. The relationship between T4 and T4.1 was approximated with the equation (in degrees F).

$$T4 = T4.1 + T_{const} - 5\,(HPT_{index} - 120)$$

such that T4.1 is higher for a lower HPT index and same T4 (the variation is more than 100°F over the full range of the HPT). Compensation for each loop in the form of gain and lead-lag or lag-lead is provided.

There are several ways to incorporate the limiters into the logic of the fuel control. First, if more than one limiter is exceeded at any given time, either the sum of the corrections, the greatest correction, or some weighted average could be used. Generally, the most restrictive limiter (greatest correction) yields the best results since the summation of the limiters usually overcorrects and the relative gains of

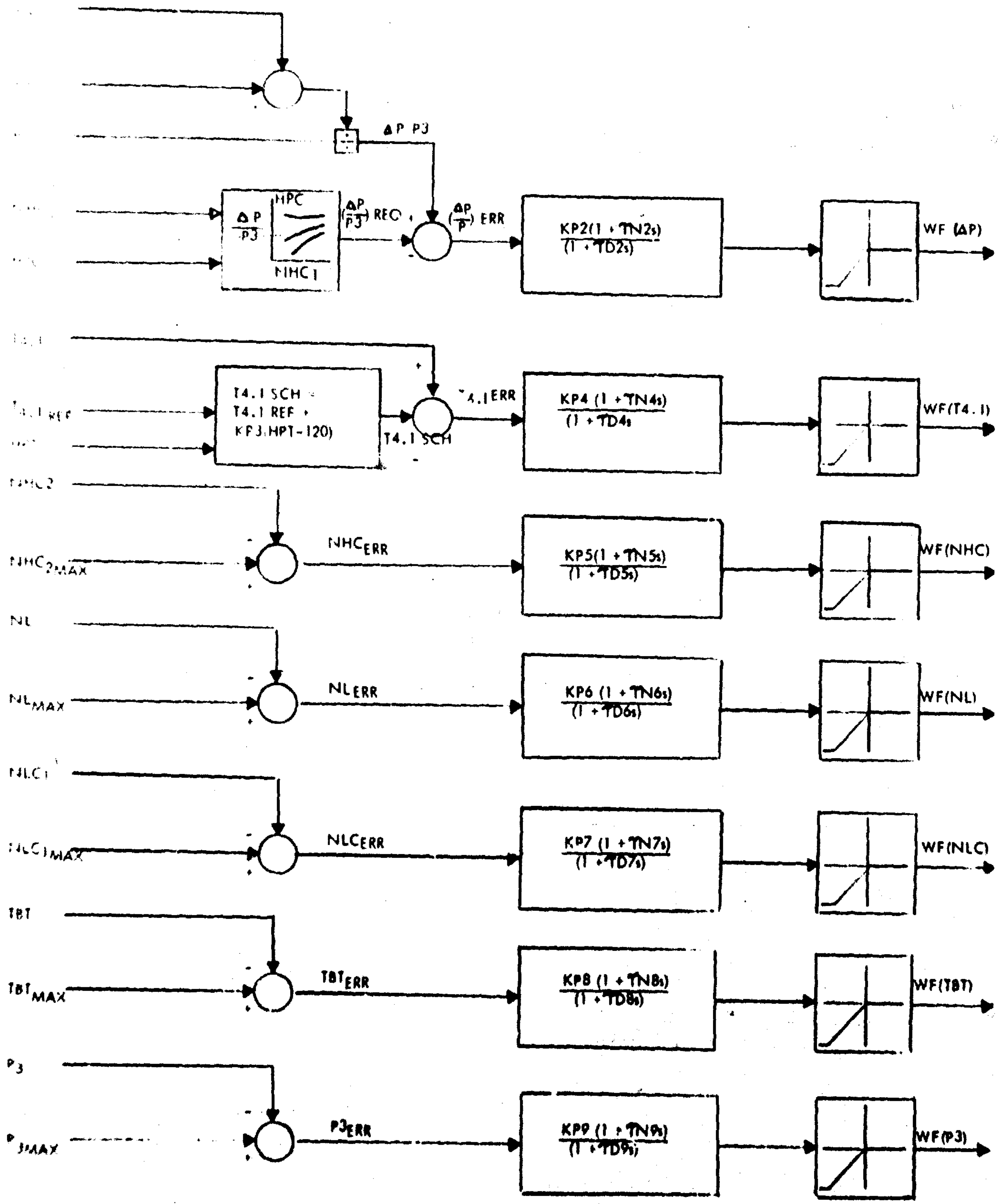


Figure 6-8 Fuel Control Limiters

the limiting loops already "weight" the importance of the various limiters to some extent. The greatest problem with using the most restrictive limiter is that the control may "bounce" from one limiter to another and back, especially at maximum thrust, and create a stability problem in the fuel control. This will be discussed later.

The second decision in the limiter logic that must be made is if the limiter correction is to be made to the governor fuel request or after the logic selecrion between the governor and the acceleration schedule. Although the acceleration schedule is a type of limiter to avoid surge, it is not expected to protect the engine from overtemperature or overspeed conditions. Both methods were applied in this study with little appreciable difference noted.

Geometry Controls

The primary objective of the geometry controls is to operate the engine with minimum sfc or at maximum thrust during steady operation. The criteria for steady state operation does not put high dynamic requirements on the geometry controls. However, care must be exercised to prevent the geometry from having an adverse effect on the transient response (i.e., geometry moving in a fashion to have an adverse effect on surge margin). Adequate steady state surge margin can be assured by constraining surge margin during the optimization for minimum sfc.

Two significantly different classes of geometry controls were studied. The first type of control involved the scheduling of the geometry positions as a function of various engine parameters. The control is based upon positioning the geometry and is generally classified as "open loop" on engine parameters. The second type of geometry control uses the geometry to position (control) engine parameters and the position of the geometry is never defined explicitly in the control. This type of control is classified as "closed loop" on engine parameters. Each type of control will be discussed in detail in the following.

Scheduled Geometry Position

The basic assumption of this type of control is that the engine will run optimally if the geometry is positioned to a predetermined optimal setting. This is certainly true as long as the optimized model is a good representation of the engine for which the control is designed. How well this assumption holds over variations in engines and throughout the degradation of the engine is not known.

A typical "open loop" geometry control mode is illustrated in Figure 6-9. The optimal geometry position is given as a function of engine input parameters (PLA, MN, TI, Pi, etc.) or engine operating parameters (NH, NL, T4.1, etc.). A geometry positioning control loop places the geometry at the optimal setting. If the optimal position is a function of engine operating parameters, a feedback

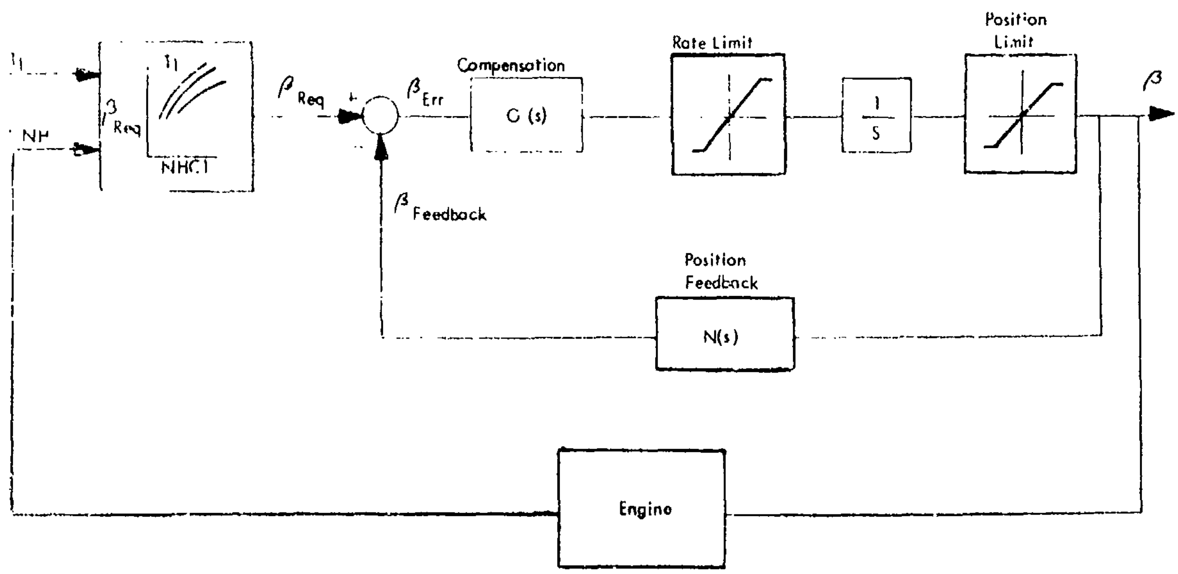


Figure 6-9 "Open Loop" Geometry Control Loop

loop involving those parameters also exists. Although this feedback can effect the control stability, the control is not considered closed loop on engine parameters since it does not attempt control of the engine parameter to a reference value.

The primary task in the design of this class of geometry controls is the choice of input and engine parameters to describe the optimal geometry position. The use of engine parameters is intuitively attractive because it allows the geometry to respond to some changes in the engine operation. Therefore, the sensitivity studies are the logical starting point for the choice of engine parameters. The design of the primary nozzle control will be discussed in detail first to be followed by a less detailed discussion of the other geometry control modes.

Primary Nozzle Control

The most likely candidates for use in the primary nozzle control from the sensitivity data were

- o Fan speed
- o Fan airflow
- o Duct airflow
- o HPC inlet pressure
- o LPT outlet pressure

The second criteria used to make a final selection of engine parameter(s) for geometry control involves "control law" or functional relationship between the optimum geometry position and the engine parameter. It is desirable to make this relationship as simple as possible over the entire flight envelope for a practical control system. The results from the geometry optimization studies for some of the above engine parameters corrected to the inlet are given in Figure 6-10.

Correction to the engine inlet was required to yield a set of curves applicable to the entire flight envelope. It should be noted that the end point of each curve represents the maximum thrust point. Any approximation of the control law should give special consideration to this point to achieve the full thrust potential of the engine.

Fan airflow was rejected as a control variable since it remains constant at the high thrust levels as A_8 varies drastically. Also, normalization to the inlet still yielded a non-unique (two values of A_8 exist for the same flow and inlet conditions) set of control laws. The duct airflow displayed characteristics similar to the fan airflow. The fan speed is not practical since it is also constant in the high thrust region and A_8 varies drastically.

This leaves only the HPC inlet and the LPT outlet pressures as acceptable candidates for controlling the primary exit nozzle. The LPT outlet pressure corrected to engine inlet pressure yielded a set of curves that, for simplicity, can be reasonably approximated by a single curve as shown in Figure 6-11. However, this curve has a very steep slope for low pressures (low thrust levels). Therefore, small errors in sensing the LPT output pressure will result in large errors in primary nozzle area. On the contrary, the HPC inlet pressure corrected to inlet pressure (see Figure 6-11) yields a control curve which is acceptable at the low pressure

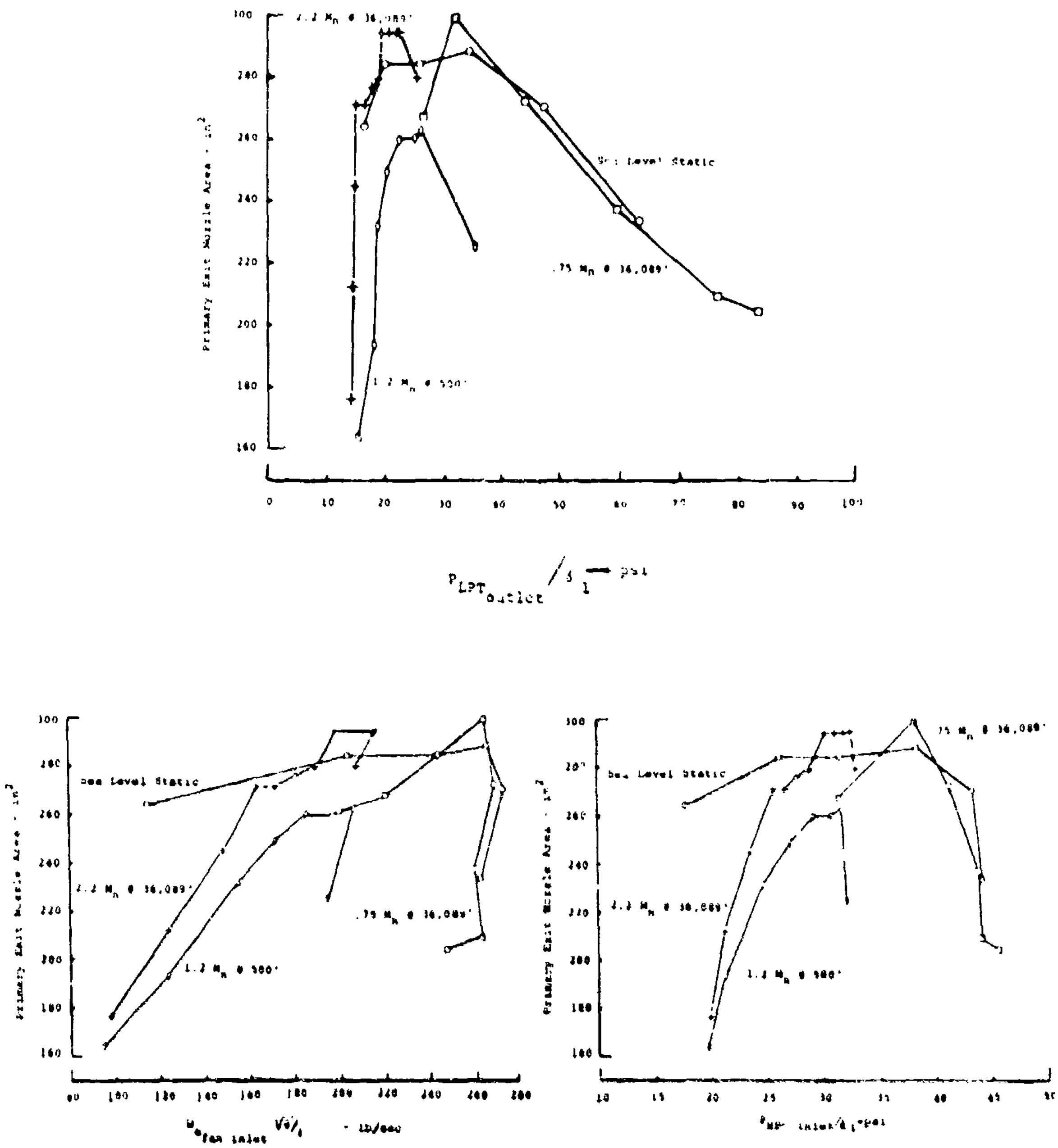


Figure 6-10 Optimal Primary Nozzle Area

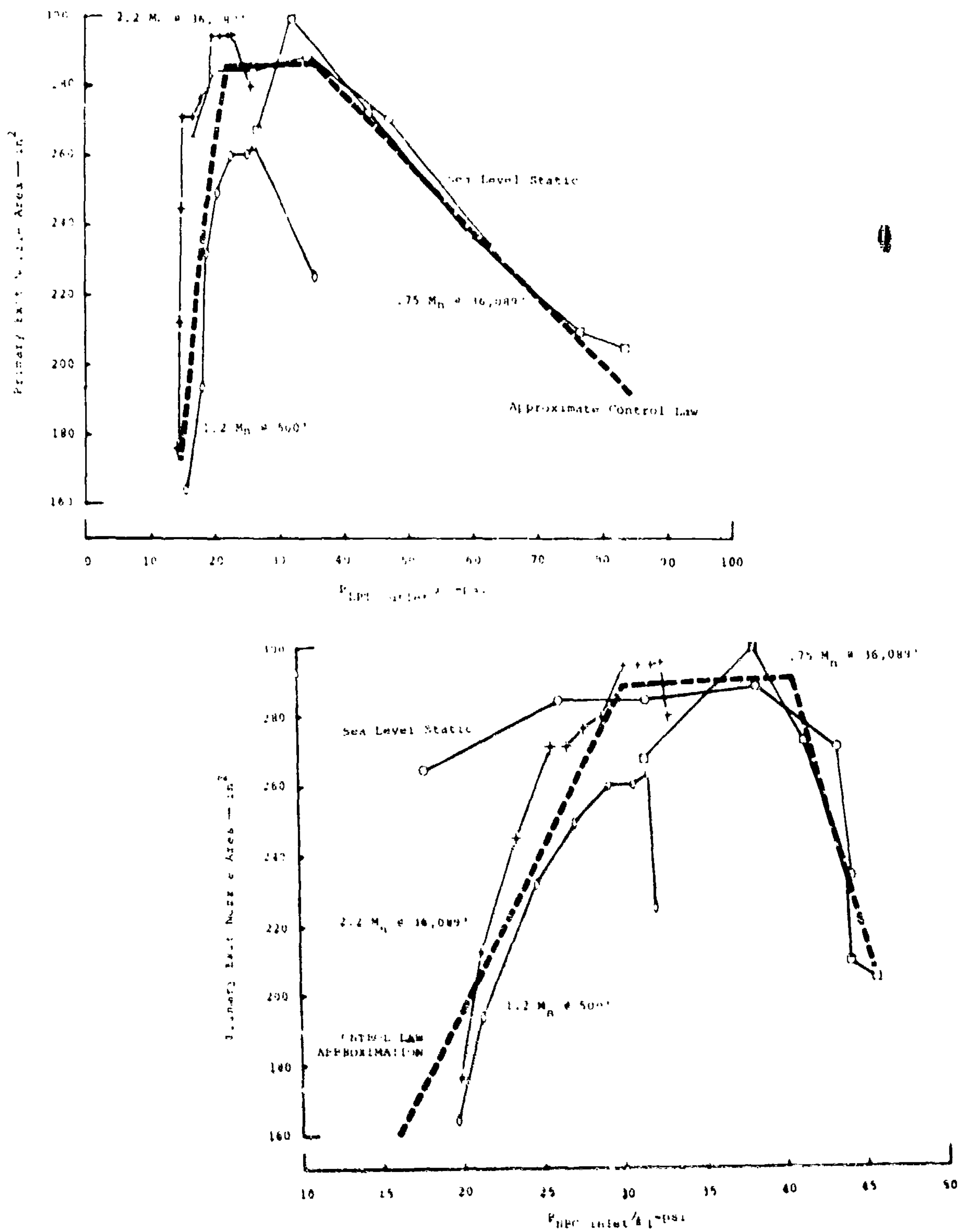


Figure 6-11 Potential Primary Nozzle Control Laws

(low thrust) side but too steep at the high pressure end. In the intermediate pressure range, the ideal control laws for both corrected pressures can be approximated by keeping the nozzle open to a maximum setting. Therefore, a control law combining both curves can be used to control the primary nozzle area as a function of corrected HPC inlet pressure and corrected LPT outlet pressure.

This control mode worked well transiently with two noted exceptions. The single curve for (A8) vs. (P2.1/ δ_1) is not accurate for low thrust at static sea level or maximum thrust for Mach 1.2 at 500 feet. The effect on the maximum thrust was negligible. The problem at static sea level was solved by adding inlet temperature lines as shown in Figure 6.12.

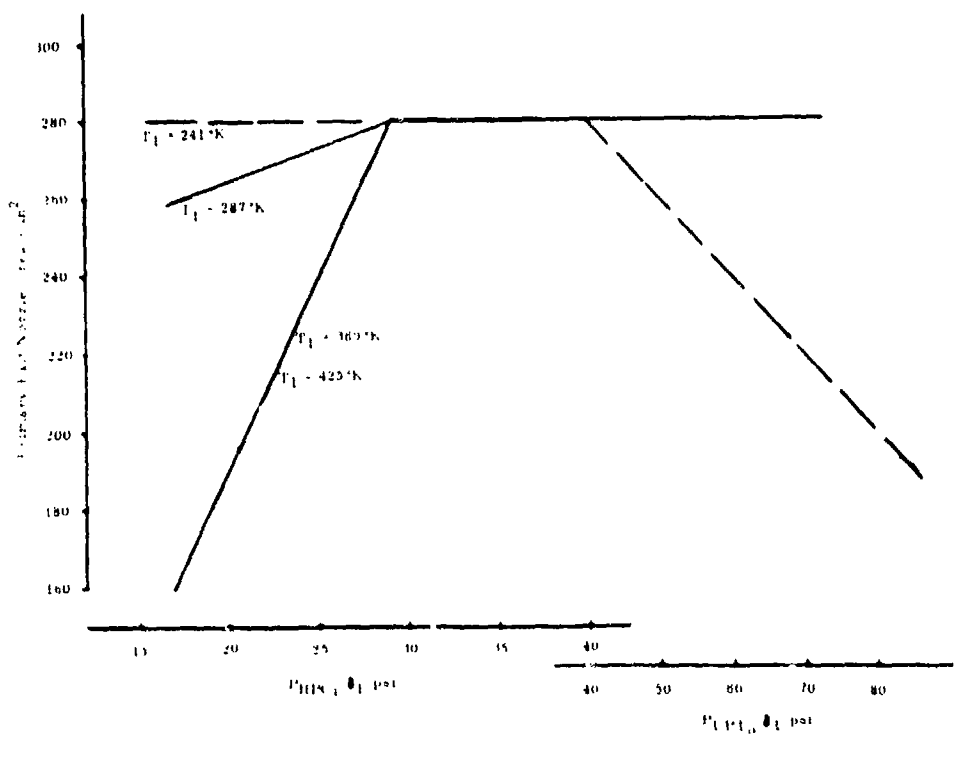


Figure 6-12 Primary Nozzle Control Law

Duct Nozzle

The secondary or fan nozzle created the greatest geometry control problem. The sensitivity studies showed that fan airflow was the best parameter for controlling the fan nozzle. Fan pressure ratio was another control candidate.

However, airflow and a combination of airflow and pressure ratio produced unstable control systems. Simple compensation would not stabilize the loops and other parameters were sought. The use of corrected low pressure rotor speed ($N_L/\sqrt{\theta_I}$) was inadequate for low thrust at static sea level and control of the duct nozzle on ($N_L/\sqrt{\theta_I}$) in the high thrust region is impossible since the engine usually reaches maximum (N_L) or ($N_L/\sqrt{\theta_I}$) before maximum thrust.

This latter problem can be avoided by "scheduling" (A18) on the turbine inlet temperature T.4 which reaches a maximum only at maximum thrust. However, the practical mechanization of this mode is to utilize the interstage turbine temperature (T4.1) which can be physically measured. The low thrust (low temperature) at static sea level problem still exists in this mode. In this condition, the duct is unchoked which generates a different A18 demand than when the duct is choked at low temperature. This problem can be cured by measuring the fan pressure ratio and adjusting the schedule for the unchoked condition as shown in Figure 6-13. This scheme has worked well except near the transition zone between choked and unchoked conditions where a degraded sfc has been seen at static sea level in the mid-thrust range due to errors in determining true choked conditions. This mode will put stringent accuracy requirements on the transducers required to measure the fan pressure ratio if minimum sfc is to be obtained.

Geometry Index

For the compressor and turbines, the relationship between geometry position and component performance is a function of mechanical linkages which have not been defined yet. Therefore, the term geometry index is introduced for a better understanding of the effect of geometry movement on component performance. For the HPC and HPT, the geometry index is directly proportional to flow. With the HPC, 80 represents minimum flow, 100 represents nominal flow, and 120 represents maximum flow capacity for a given speed and pressure ratio. An index of 100 denotes minimum HPT flow capacity with 123 denoting maximum HPT flow capacity.

The LPT index is the pressure ratio between the pressure in front of the vane and the stator "jet flap" cavity which represents the closest measurement of the "jet flap" orifice pressure drop. Thus, the index is an indirect measure of the "jet flap" flow and the LPT flow capacity. An index of 0.8 denotes maximum LPT flow capacity and 1.6 represents minimum flow capacity (flow is not proportional for other indexes).

High Pressure Compressor

The sensitivity studies showed that the HP compressor variable geometry was most sensitive to the high pressure spool speed and compressor airflow. The compressor variable geometry design already assumes modulation of the stators with corrected spool speed to achieve good compressor surge margin and compressor efficiency over a wide operating range. Since the use of corrected HP rotor speed simplifies the control system considerably, this parameter was chosen

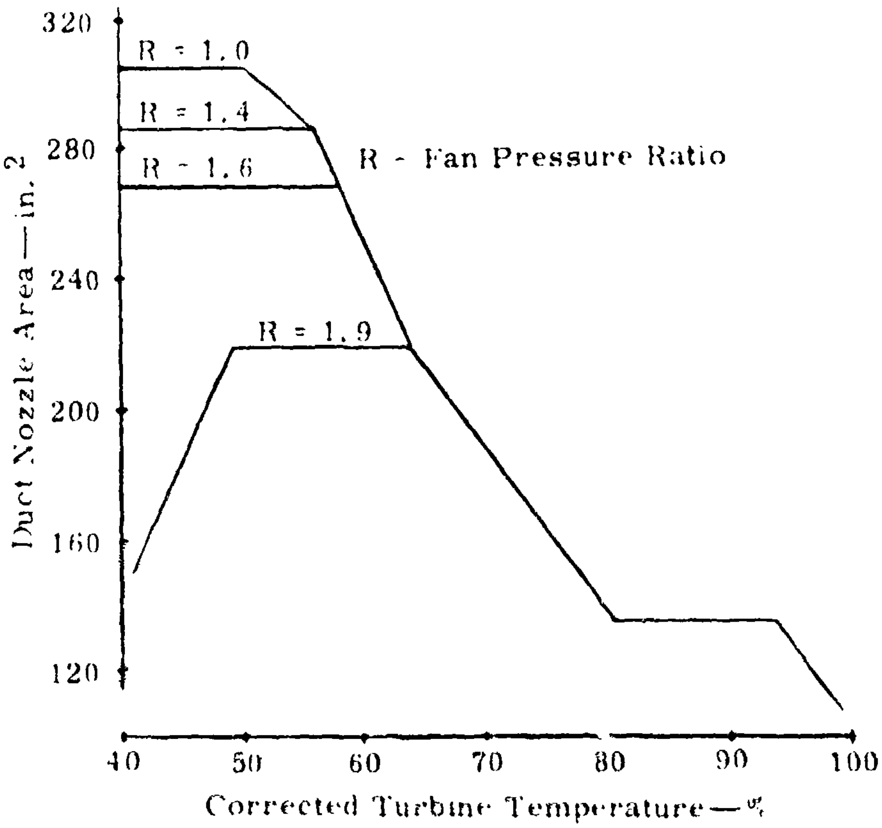


Figure 6-13 Duct Nozzle Area Schedule

High Pressure Compressor

The sensitivity studies showed that the HP compressor variable geometry was most sensitive to the high pressure spool speed and compressor airflow. The compressor variable geometry design already assumes modulation of the stators with corrected spool speed to achieve good compressor surge margin and compressor efficiency over a wide operating range. Since the use of corrected HP rotor speed simplifies the control system considerably, this parameter was chosen to control HP compressor geometry. The requirement for a back-up control in lieu of a fail-safe position for the geometry control tends to dictate a simpler control system.

High Pressure Turbine

The HPT generally remains closed until one of the engine limiters (NL, correct NL, CDP, etc.) is reached. Thus, it was possible to make the HPT index a function of the throttle position. However, this mode did not work transiently in an acceleration because the HPT reached an open position before the correct HPT rotor speed was attained (NH lags PLA because of the rotor inertia). Increasing the flow capacity of the HPT reduces the horsepower available for accelerating the rotor. If the flow capacity is increased too fast during an acceleration, the rotor may never achieve the proper speed due to a lack of horsepower and the interaction of other geometries. Thus, the HPT geometry schedule was changed to a function of correct speed ($NH/\sqrt{\theta_2}$) by transforming PLA to $NH/\sqrt{\theta_2}$ through the fuel control governor schedule.

Low Pressure Turbine

The LP turbine minimum sfc geometry settings showed very little variation over the entire thrust range for a given flight condition. For the two subsonic cases, the geometry index remained at approximately 0.8 for the entire thrust range. The geometry index for supersonic flight shows a small variation about 1.2 over the thrust range for each flight condition studied. A simple schedule on flight Mach number only, as shown in Figure 6-14 was investigated. It is possible that a simple on-off control based upon Mach number or inlet temperature may be adequate for steady state performance.

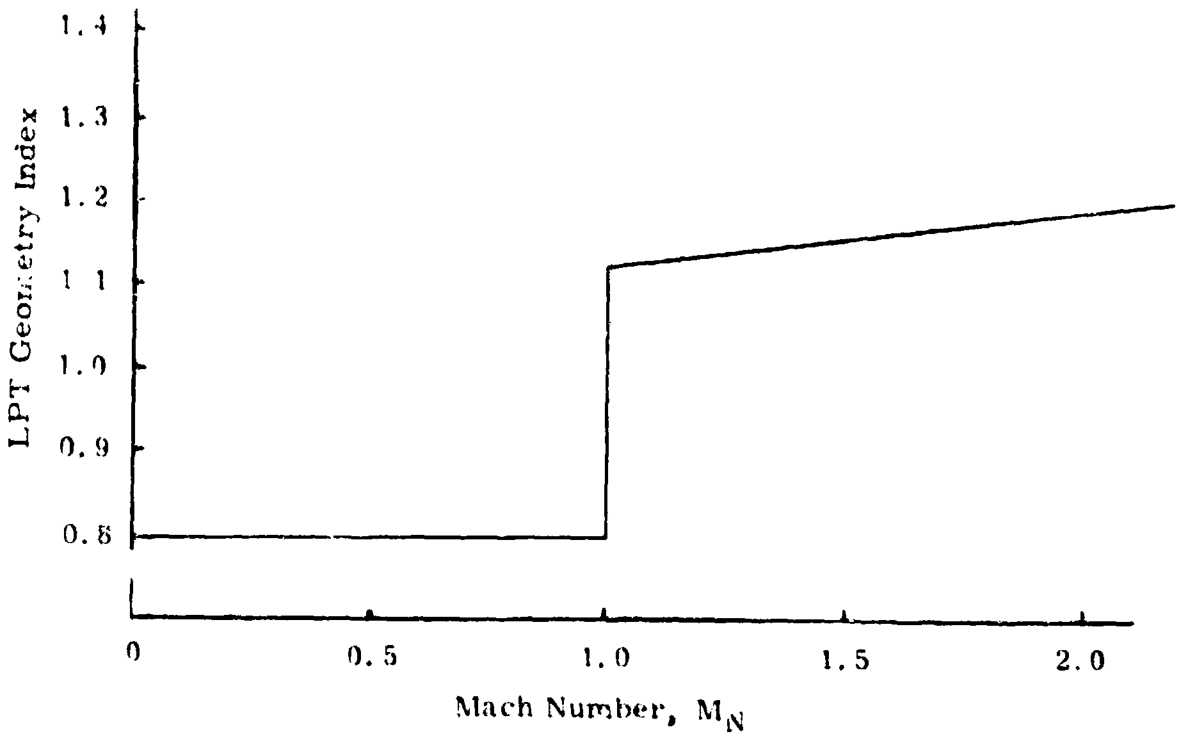


Figure 6-14 LPT Geometry Modulation for Minimum sfc

Complete "Open Loop" Geometry Control

The most successful "open loop" geometry control from a transient viewpoint is shown in Figure 6-15. This control was exercised throughout the flight envelope including standard, hot, and cold conditions with only one significant problem that essentially lead to the termination of work on this control mode. The problem occurs at maximum thrust at all the flight conditions. At this point, the engine is operating at several limiting parameters including

- o maximum burner temperature
- o maximum low pressure spool speed (corrected and/or uncorrected
- o maximum high pressure spool speed (corrected and/or uncorrected)

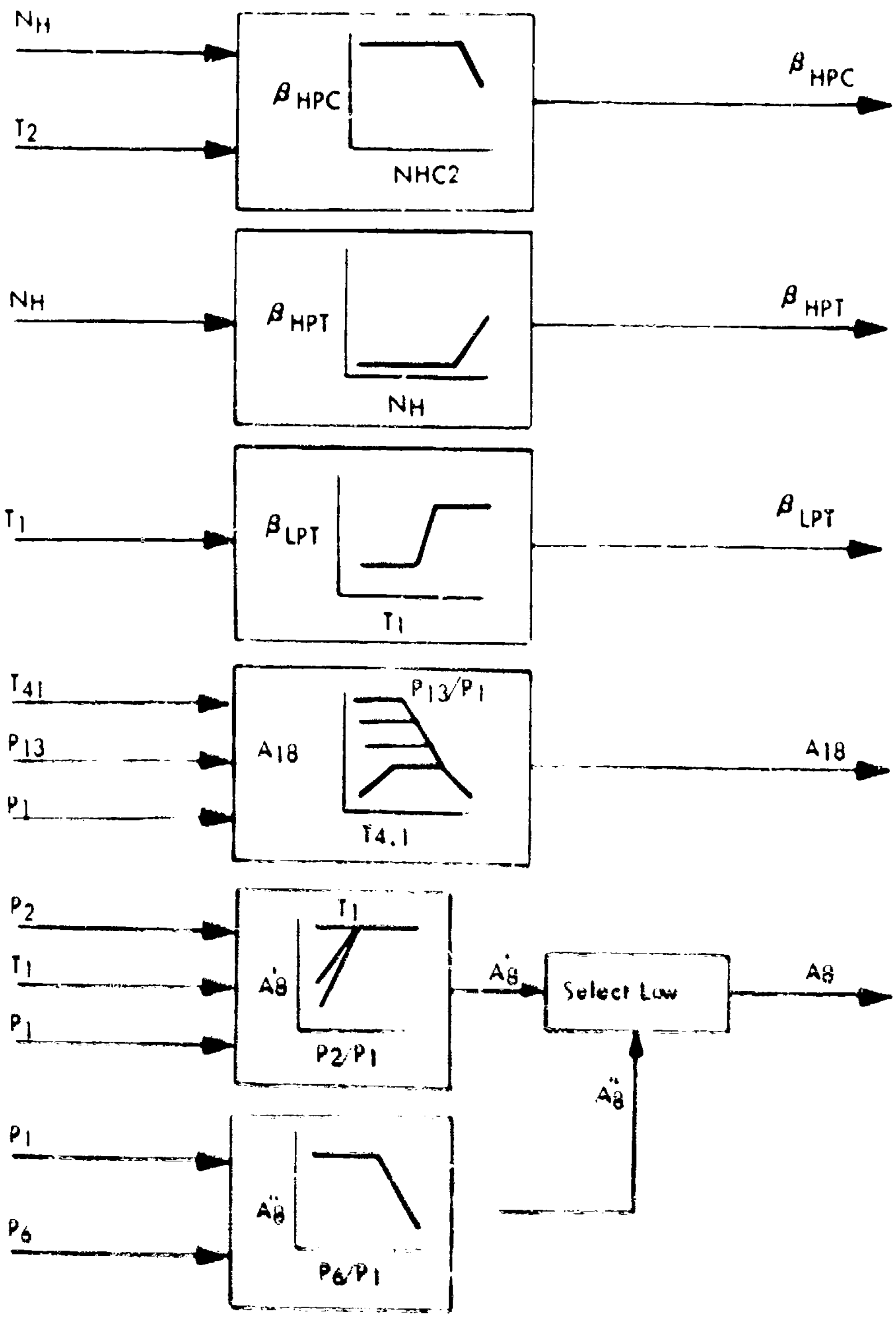


Figure 6-15 "Open Loop" Variable Geometry Control Logic

- maximum CDP (low altitude/high speed generally)

To achieve this state, the geometry must be set very accurately. Errors in the geometry will cause these limits to be exceeded. Under these conditions, the fuel control has great difficulty in satisfying the demands of all the fuel control limiters and the speed governor simultaneously with one or more of the following consequences

- unstable operation
- limiters exceeded in steady state
- less than maximum thrust

With "open loop" control, it is difficult if not practically impossible to achieve maximum thrust over the entire flight envelope. This conclusion led to the development of the "closed loop" control modes presented next.

Geometry Control of Engine Parameters

Philosophy

The "open loop" geometry controls presented above had two major deficiencies

- There is no assurance that optimal performance will be maintained for engine variations.
- Fuel control stability is difficult to achieve at maximum thrust because of the multiple limiters.

However, it is possible to guarantee stable operation at maximum thrust by utilizing the geometry to control those parameters required to assure maximum thrust as shown in the following example.

Assume maximum thrust is achieved when NH, NL, and T4.1 (T4) reach their limits and

- NH is controlled by the fuel control
- NL is controlled by the primary nozzle
- T4.1 is controlled by the HPT setting

With the "open loop" control, the HPT index setting was made a function of NH. This control and a non-nominal HPT would produce either too low a T4.1 (lost thrust) or excessive T4.1 (possible stability problems in the fuel control). However, with the HPT geometry controlling the inter-turbine temperature, maximum thrust can be attained. If the temperature is too low, the HP turbine index will increase to raise T4.1 to its limit. If the HPT index reaches its maximum value before T4.1 reaches its limit, the resulting thrust is the new maximum for the degraded engine (assuming the NL and NH limits are attained). As long as the NL limit is maintained, the fuel control will not exhibit potential stability problems if the HPT reaches its minimum value while T4.1 is still above its limit with a "super" engine. In this case, the fuel control still is only required to operate on one limiter (T4.1) although maximum NH may not be obtained. Similar arguments can be made for the primary nozzle and fan speed. Thus, the engine will achieve the maximum available thrust within its physical constraints without trimming.

Unfortunately, the case for maintaining minimum sfc with engine variations by using the geometry to control engine parameters is not as clear cut. However, the proper choice of engine parameters will enhance the probability that near optimal sfc will be obtained. Minimum sfc for a given thrust is generally obtained by attaining the following conditions:

- o Maximum airflow
- o Maximum bypass ratio
- o Minimum burner temperature

If the nominal optimal values of these parameters are maintained for a given thrust, near minimum sfc operation should be obtained even with a non-nominal engine without trimming.

With the fixed fan, fan speed and fan pressure ratio will control the total engine airflow. (It should be noted that total airflow control is also important for inlet matching, especially under supersonic conditions.) The total airflow and bypass ratio can be controlled by controlling the duct and core flows individually. Limited control of the bypass ratio can be achieved by controlling the duct and core pressure ratios. Pseudo control of the burner temperature is achieved through control of the inter-turbine temperature.

Therefore, it is desirable to control some combination of the following parameters to assure maximum thrust and near minimum sfc while minimizing the stability problems in the fuel control a variation in engine parameters.

NH	- high pressure rotor speed
NHC2	- high pressure rotor speed - corrected
NL	- low pressure rotor speed
NLC1	- low pressure rotor speed - corrected
T4.1	- HPT exit temperature
Wa_{13}	- duct airflow
Wa_3	- HPC airflow
P_{13}/P_1	- duct or fan pressure ratio
P_7/P_1	- core pressure ratio
P_3	- compressor discharge pressure

To provide the desired steady state operation, the following desirable features for the geometry control were identified:

- Control engine parameters which are critical to minimum sfc and maximum thrust operation over the entire flight envelope.
- Provide a relationship between the engine parameters to be controlled and the control system input (PLA and inlet

conditions). This may be either directly or indirectly through other parameters (NH assuming an NH governor) controlled directly by PLA.

- Steady state control of the parameters normally limited in the fuel control especially at maximum power to allow the fuel control to operate on the governor.
- Control to zero steady state error.

General Configuration

The general configuration of the geometry control loop is shown in Figure 6-16. A required value for the engine parameter is established from a schedule which satisfies the second item above. A rate command for the geometry actuator is computed from the required and measured engine parameters. The actuator then integrates the command to move the geometry to a position that forces the engine to the desired operating condition (zero error on the engine parameter). Generally, the geometry control is a two loop system as shown. However, geometry position feedback around the actuator may be required if the actuator is too "sloppy" (i.e., excessive hysteresis). In this case an integrator will be inserted in the control to replace the actuator integration. A study of the data over several flight conditions showed that most of the data corrected best to engine inlet conditions rather than component conditions.

The following factors were considered in matching the engine parameters and variable geometry for the control -- a) control sensitivity, b) loop gain variations, c) effect on transient performance, d) control authority, and e) the ability to control the parameter at maximum power. The results were

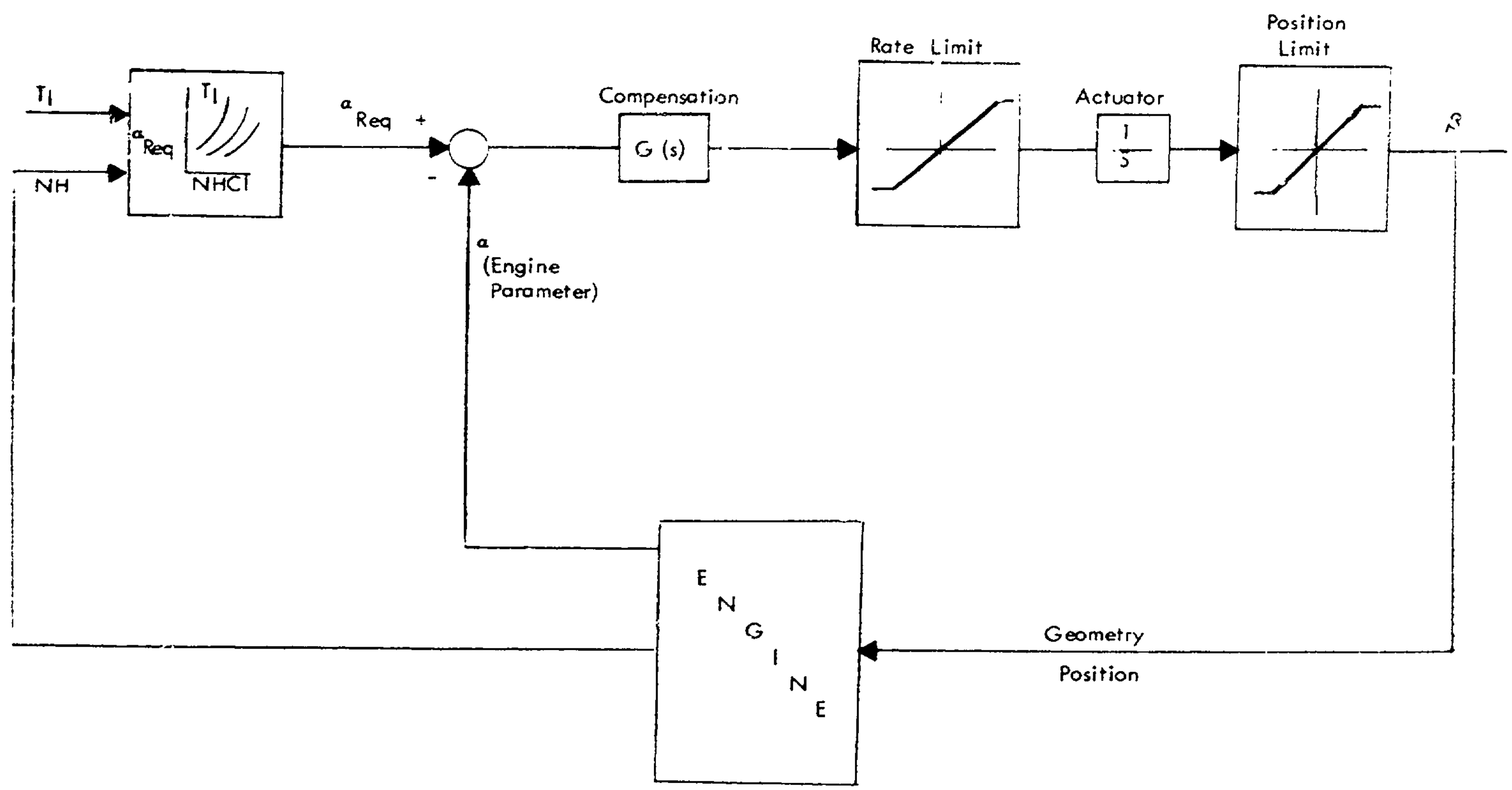


Figure 6-16 General "Closed Loop" Geometry Control

NL	- best controlled by primary nozzle (A8)
Wa_3	- best controlled by high pressure compressor (HPC) or high pressure turbine (HPT)
Wa_{13}	- best controlled by duct nozzle (A18)
P_3	- best controlled by HPC or HPT
P_7	- best controlled by A8
P_{13}	- best controlled by A18
T4	- cannot be measured, must use T4.1
T4.1	- best controlled by HPT or low pressure turbine (LPT)

which allows considerable flexibility in constructing the geometry control modes.

A study of the steady state data reveals that the engine will operate at the fan spool speed limit (either mechanical or aerodynamic) over the upper thrust range at most flight conditions. Thus, it is important to control the fan speed and the primary nozzle is the only geometry that provides adequate control of this parameter. The secondary nozzle can be used to control either duct flow or duct pressure ratio. The duct pressure ratio was simpler than the relationship between flow and speed over the flight envelope. However, for an installed supersonic engine, the flow control may be more desirable to provide inlet flow matching. Therefore, for all the mode studies, the nozzle controls were the same with the

- o primary nozzle controlling $NL/\sqrt{\theta_1}$ described as a function of $NH/\sqrt{\theta_1}$ and T_1 as shown in Figure 6-17.
- o Duct nozzle controlling the fan pressure ratio P_{13}/P_1 described as a function of $NH/\sqrt{\theta_1}$ and T_1 as shown in Figure 6-18

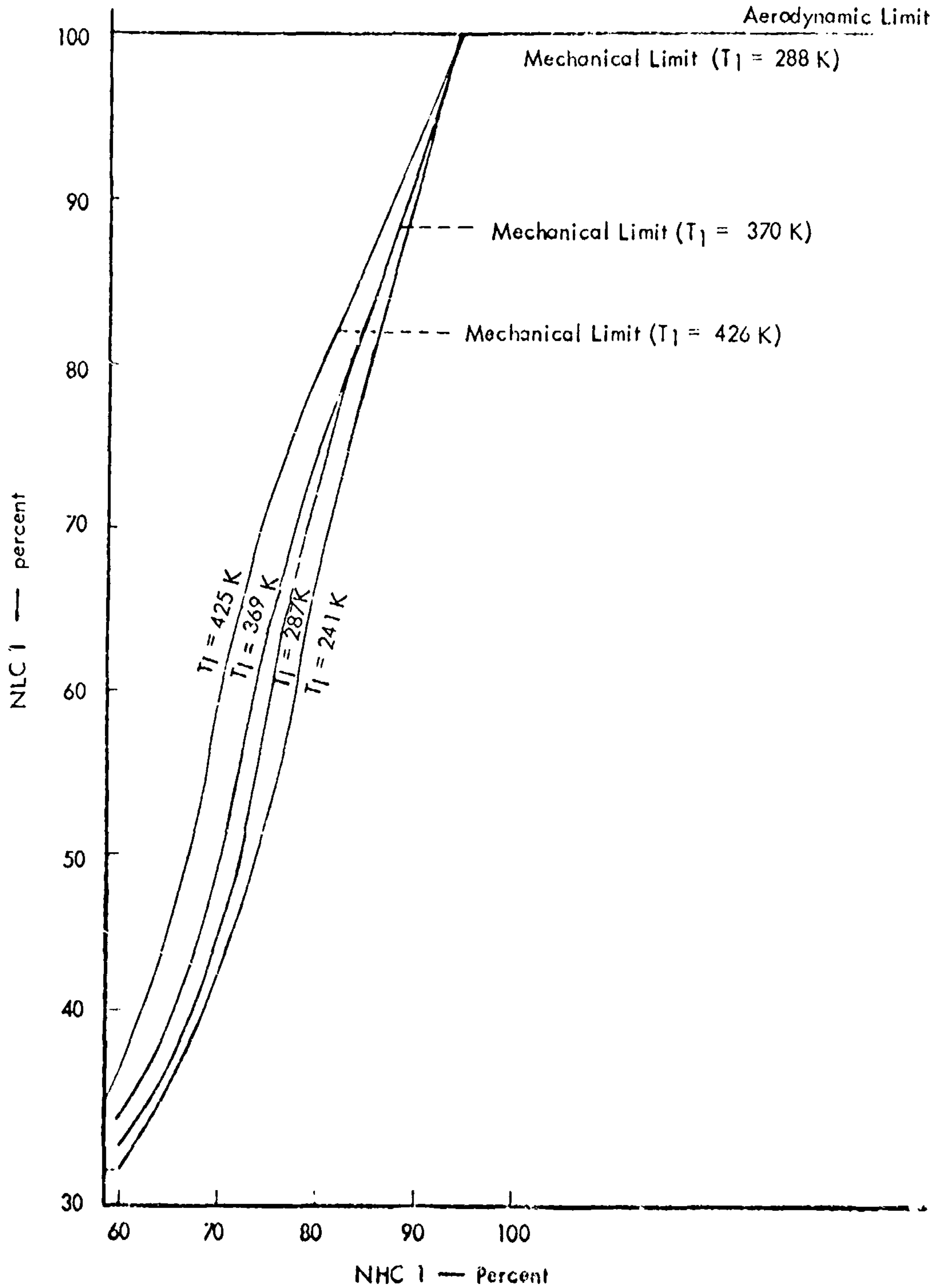


Figure 6-17 Relationships Between Spool Speeds

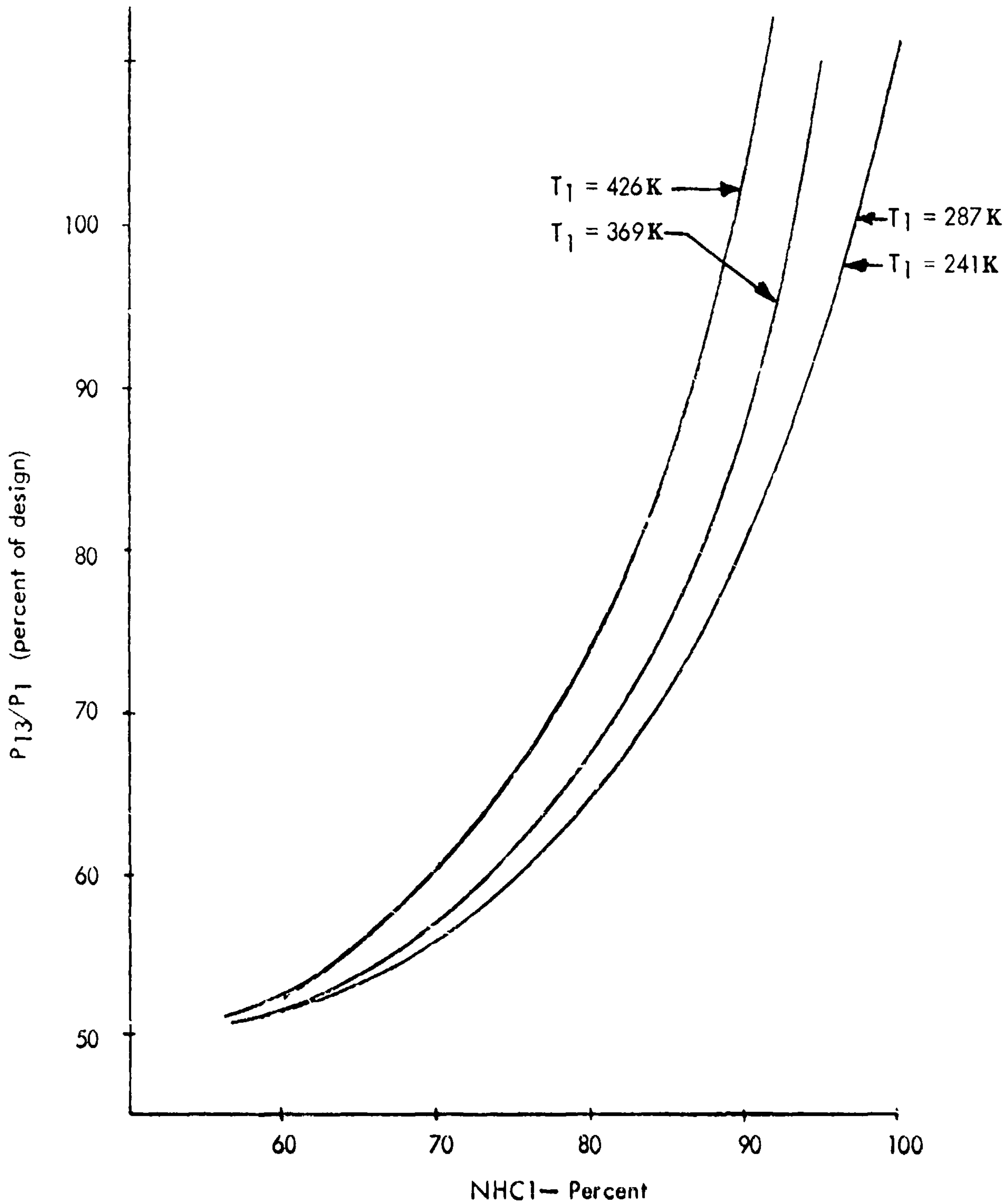


Figure 6-18 Fan Pressure Ratio Schedule

However, a greater flexibility exists in the application of the HPC, HPT, and LPT. Several different complete geometry control modes will be discussed to illustrate the care that must be taken to select an overall geometry control mode that yields near optimum performance over the flight envelope.

Control Mode #1

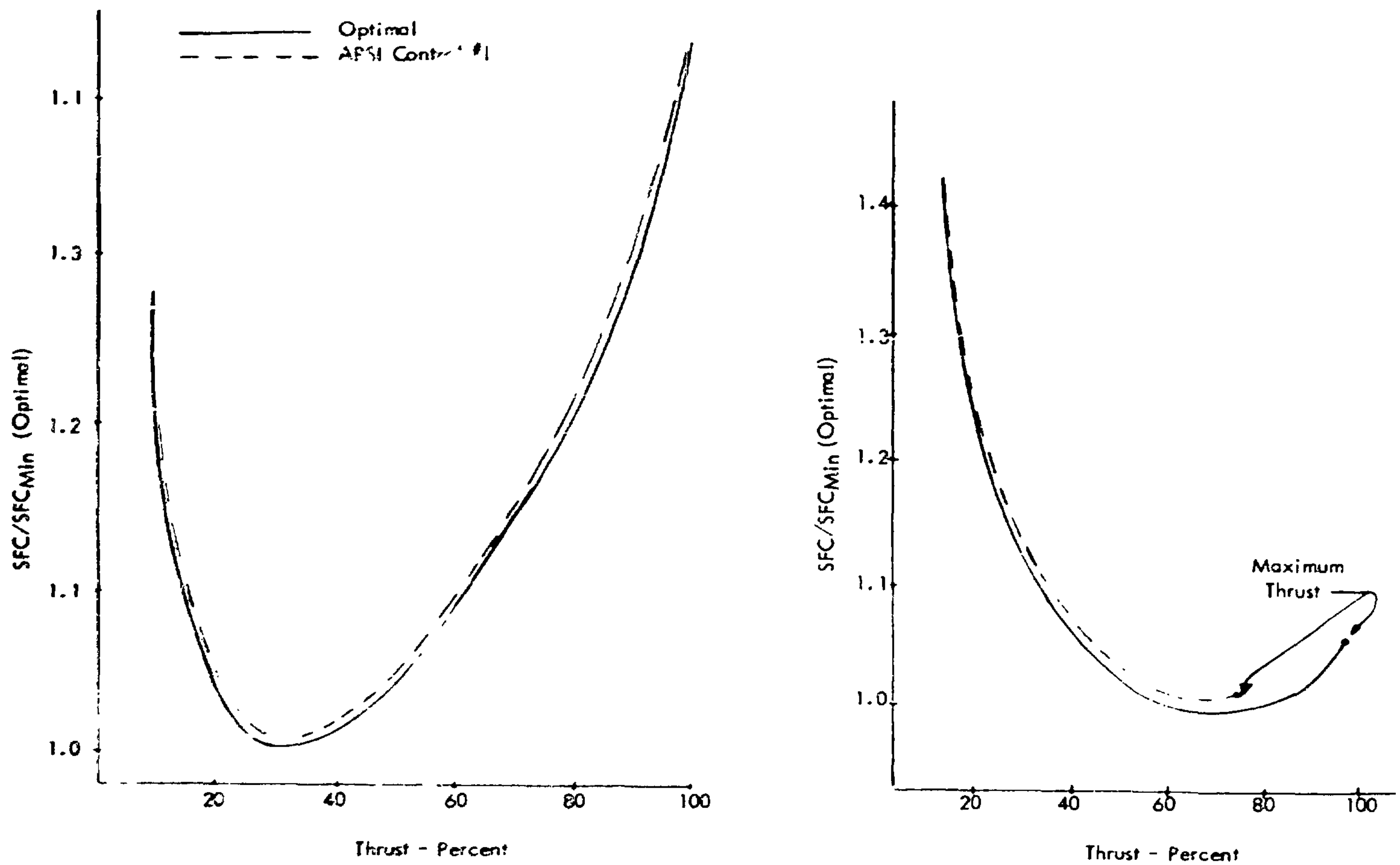
The first control mode studied in detail consisted of

HPC controlling P_3/P_1 as a function of $NH/\sqrt{\theta_1}$ and T_1
HPT controlling $T_{4.1}$ as a function of $NH/\sqrt{\theta_1}$ and T_1
LPT controlling $P_{4.1}/P_1$ as a function of $NH/\sqrt{\theta_1}$ and T_1
A8 controlling $NL/\sqrt{\theta_1}$ as a function of $NH/\sqrt{\theta_1}$ and T_1
A18 controlling P_{13}/P_1 as a function of $NH/\sqrt{\theta_1}$ and T_1

(It was found that $P4.1/P_1$ worked better as a control parameter than P_7/P_1).

This control mode worked excellently at static sea level and yielded very near optimal sfc's as shown in Figure 6-19. However, it was not possible to achieve maximum thrust at the supersonic conditions because of problems in attaining maximum temperature.

The control of T4 is a very difficult task because it cannot be measured directly. The correlation between the HPT inlet temperature (T4) and exit temperature (T4.1 which is measurable) is a function of the HPT geometry setting. With the HPT inlet temperature at its maximum, the exit temperature can vary by more than 120°F over the range of variability of the HPT. Also, the relationship of T4.1 corrected to the inlet with respect to corrected compressor speed produces large temperature errors for small speed errors due to a nearly infinite slope at some flight conditions near maximum power as shown in Figure 6-20. Thus, it was necessary to describe the required T4.1 as a function of PLA and inlet temperature which yields Control Mode #2.



a) static sea level

b) Mach 1.2 at 500 feet

Figure 6-19 SFC With Control No. 1

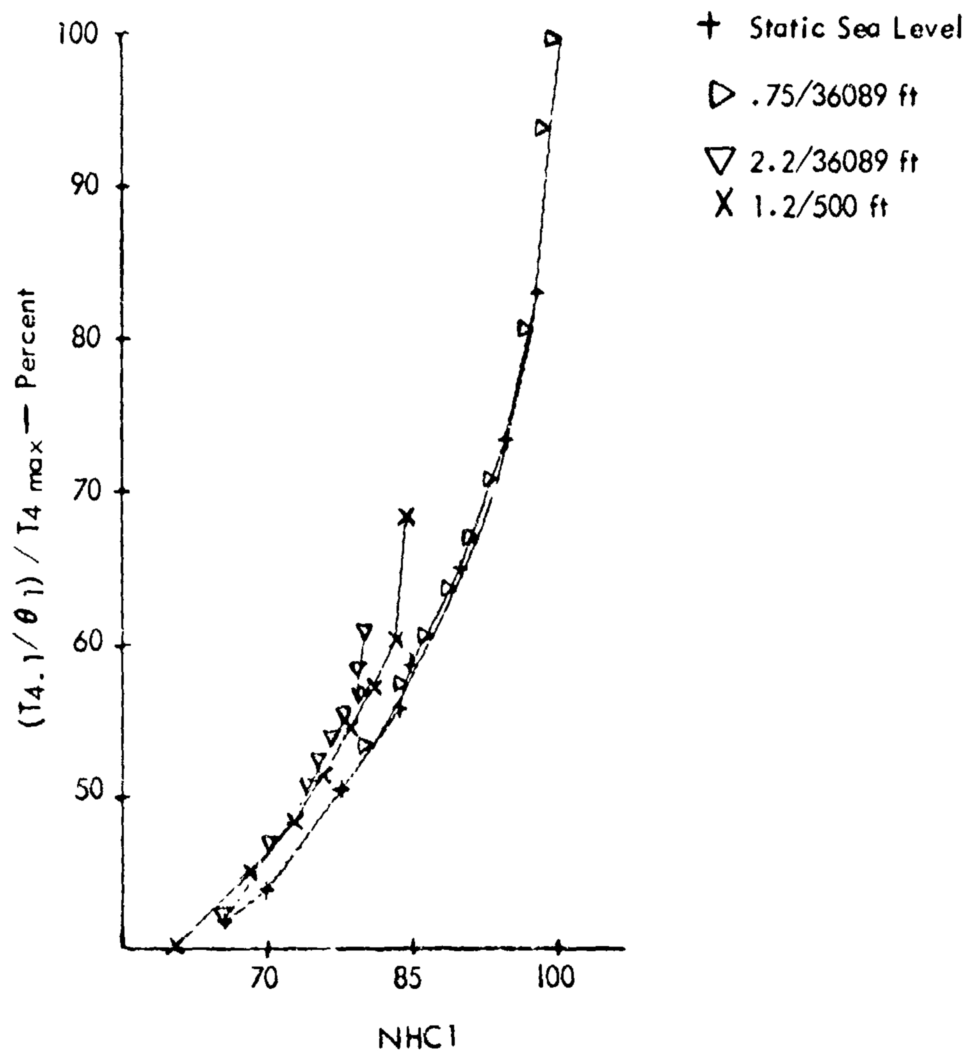


Figure 6-20 Optimal T4.1 vs. NHC1

Control Mode #2

Only the HPT control was changed to create this control mode. The translation from a T4.1 vs. NH to a T4.1 vs. PLA schedule is important to preserve a linear steady state thrust relationship to PLA position. In addition, it was found that this relationship was critical in establishing minimum sfc and maximum thrust operation. The results of several attempts to adjust this schedule at static sea level are shown in Figure 6-21. In light of the good sfc's obtained in Mode #1, it is felt that the same results could be obtained with this mode with proper "tuning". A more detailed description of the perfc nance of this mode is given in Section 7.0.

Control Mode #3

The individual geometry controls in this mode were

HPC	controls $P_3/P_{2.1}$	scheduled on NHCl
HPT	controls W_{a3}	scheduled on NHCl
LPT	controls T4.1	scheduled on PLA and T_1 plus cooling
A8	controls NLCl	scheduled on NHCl
A18	controls P_{13}/P_1	scheduled on NHCl and T_1

This mode failed to work at maximum thrust because there was insufficient control in the LPT to maintain the proper T4.1. The effective flow area of the turbine is controlled by varying the amount of cooling air to the "jet flap" controlled LPT. An increase in cooling air reduces the flow capacity and therefore the maximum flow capacity decreases (for a given speed and pressure ratio) at the higher temperatures (high power). Thus, the amount of variability and control authority of the LPT decreases as maximum power is approached. The LPT was required to go to a low index to maintain the proper T4.1 but the minimum setting dictated by the cooling requirement for the blades prevented the LPT from controlling T4.1 to the desired temperature. In this case, T4.1

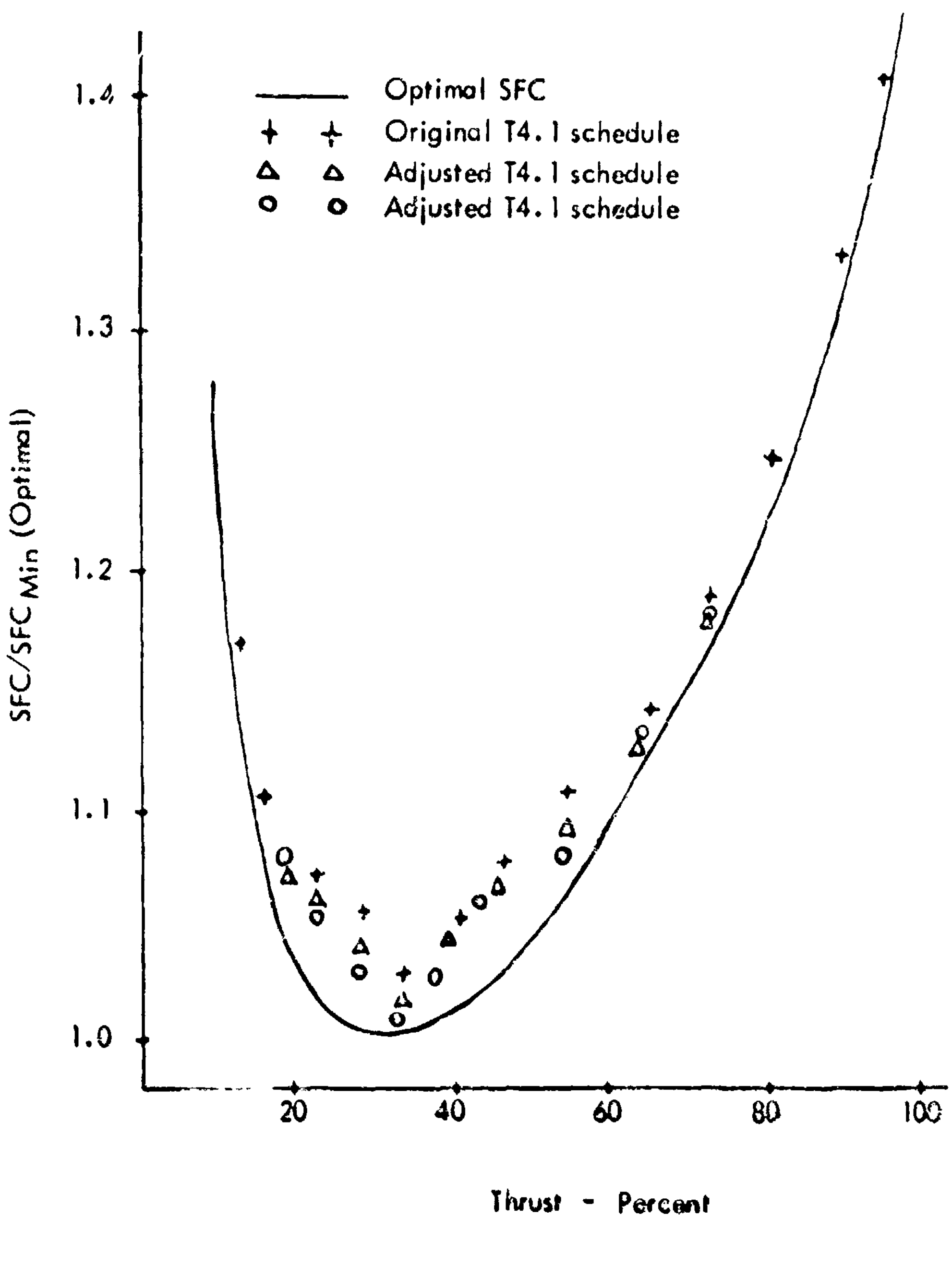


Figure 6-21 Adjustment of sfc with T4.1 (HPT) Schedule Changes

exceeded the maximum and the fuel control limiter is required to control T4.1 so that the proper NH is not attained. Thus, maximum thrust cannot be obtained with this mode. In fact, the decreased control authority of the LPT at high power makes the LPT less desirable as a means to control one of the limiting parameters.

Control Mode #4

The control mode variations have now been greatly reduced since only the choice of control for the HPC and LPT remain assuming that the HPT must control T4.1 and the nozzle controls have already been chosen (A18 can be on either pressure or flow). The sensitivity data for any of the remaining control parameters mentioned above did not look very favorable. However, the functional relationship and sensitivity data for the ratio of LPT inlet pressure to engine inlet pressure (P4.1/P1) displays the desired characteristics and this parameter was chosen for the LPT control. (It must be noted that the minimum cooling air requirement often overrides the pressure ratio control above 70% thrust.) Considering the earlier results, the only remaining mode left for consideration utilized the HPC to control the core or compressor flow. This mode works fine at static sea level. However, at Mach 1.2 and 500 ft., maximum thrust cannot be obtained since the fuel control will operate on the P3 limiter and the proper NH will not be attained.

For installed supersonic applications, the control of core flow may be more desirable than maximum thrust. In this case, this mode is more desirable than Mode #3 since the P_3 limiter is exceeded less often than the T4.1 limiter.

7.0 CONTROL MODE SELECTED FOR JTD

This section presents the selected control mode and presents the performance of the control system and engine over a wide range of operating conditions. Also, some of the key control parameters were varied to check potential problems during the testing of the JTD at sea level static conditions. Most of the information in this section has been normalized with respect to design parameters to preserve the unclassified status of this report.

Selected Control Mode

The control mode for the JTD depicted in Figure 7-1 can be summarized as follows:

Fuel Control

- o NH Governor
- o Acceleration Schedule (W_f/P_3 vs NH)
- o Acceleration Control ($\Delta P_3/P_3$)
- o Deceleration Schedule (W_f/P_3 vs NH)
- o Limiters
  - (1) NH_c (corrected NH)
  - (2) NL
  - (3) NL_c (corrected NL)
  - (4) T4.1 (inter turbine temperature)
  - (5) TBT (Turbine blade temperature-optical pyrometer)
  - (6) CDP (compressor discharge pressure)

Geometry Control

- o HPC controls P3/P1 scheduled on NH_{c1}
- o HPT controls T4.1 scheduled on PLA and T_1
- o LPT controls P4.1/P1 scheduled on NH_{c1} plus cooling requirements

JTD CONTROL MODE

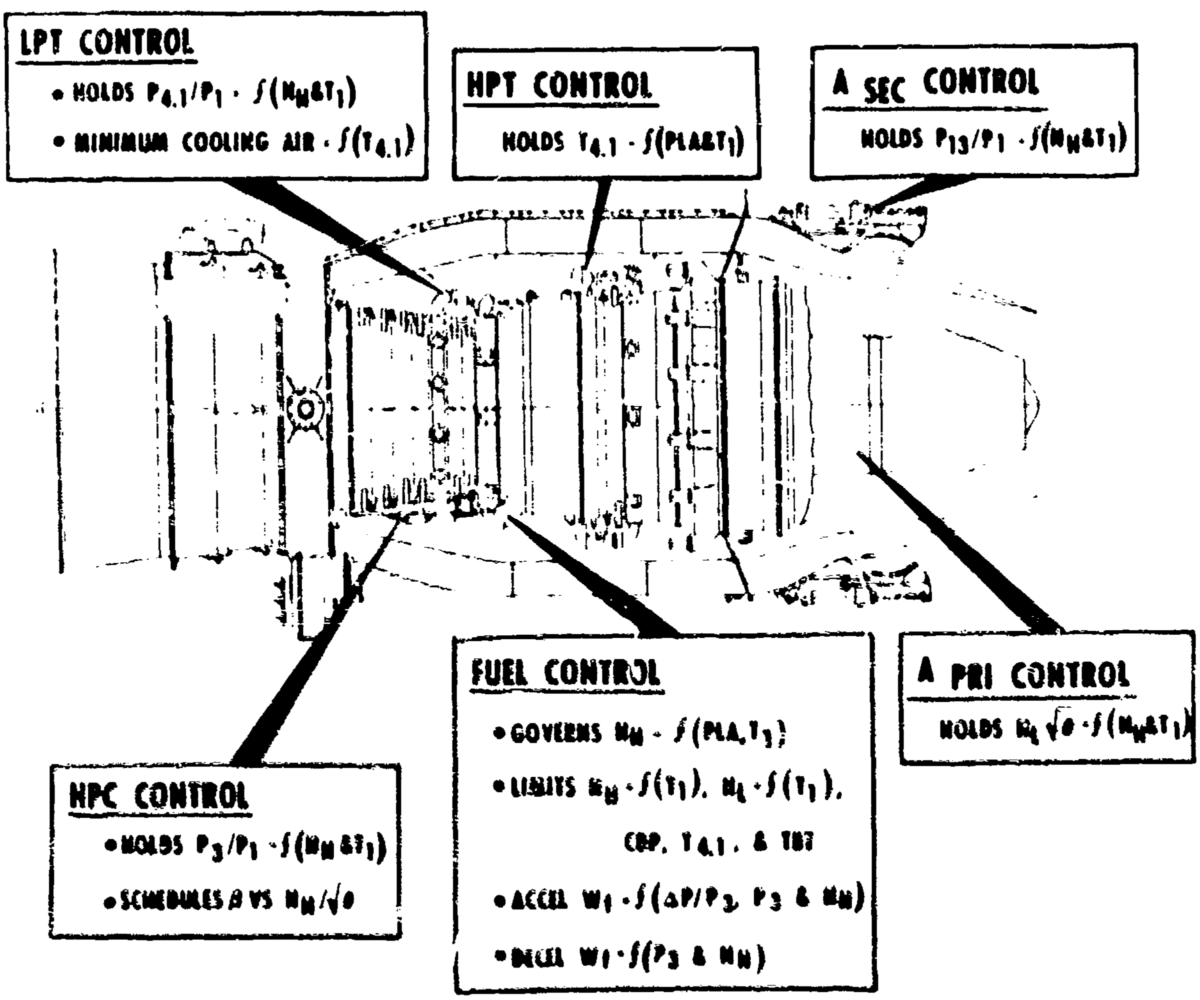


Figure 7-1 JTD Control Mode

- A8 controls NL_{c1} scheduled on NH_{c1}
- A18 controls P13/P1 scheduled on NH_{c1} and T_1

The fuel control logic is shown in Figure 7-2 and the various schedules are presented in Figure 7-3. The following limiter reference values have been established

NH_c	-	105 percent of design
NL	-	103 percent of design
NL_c	-	105 percent of design
CDP	-	105 percent of design
TBT	-	(this mode was never exercised since the simulation does not model metal temperature)
T4.1	-	102.1 percent of design point

The geometry control logic is shown in Figure 7-4 and the various schedules are presented in Figure 7-5.

Performance

The goal for the control was to provide optimal steady state performance and "good" dynamic performance. Thus, considerable effort was expended to develop a mode that achieves maximum thrust and minimum SFC over the flight envelope. The gains of the control loop were then adjusted to give good dynamic response.

The steady state and dynamic response of the control at the following flight conditions have been investigated.

- Static sea level
- Mach 1.2 at 500 ft. (152.4 meters)
- Mach .75 at 36,089 ft. (11,000 meters)
- Mach 2.2 at 36,089 ft. (11,000 meters)

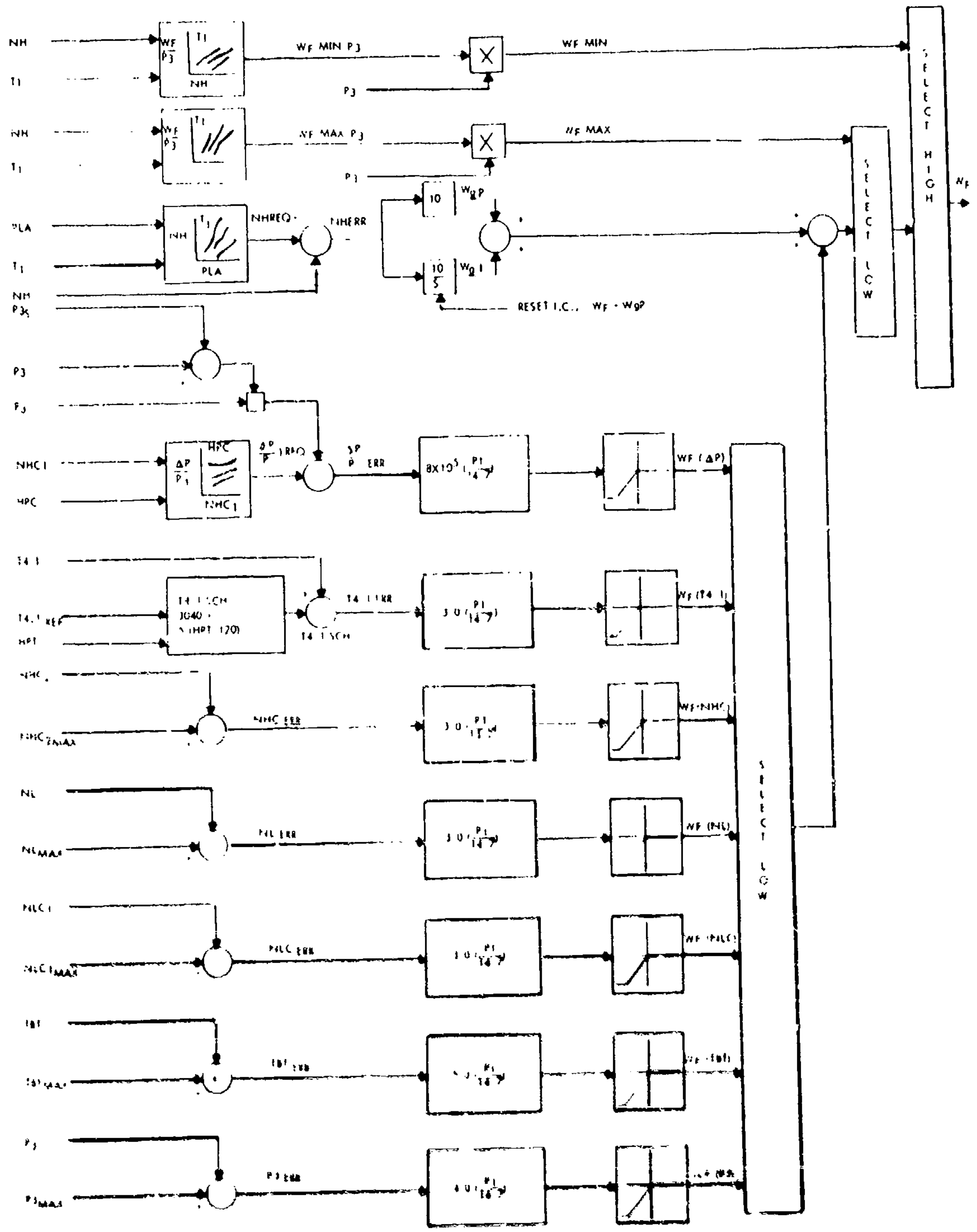
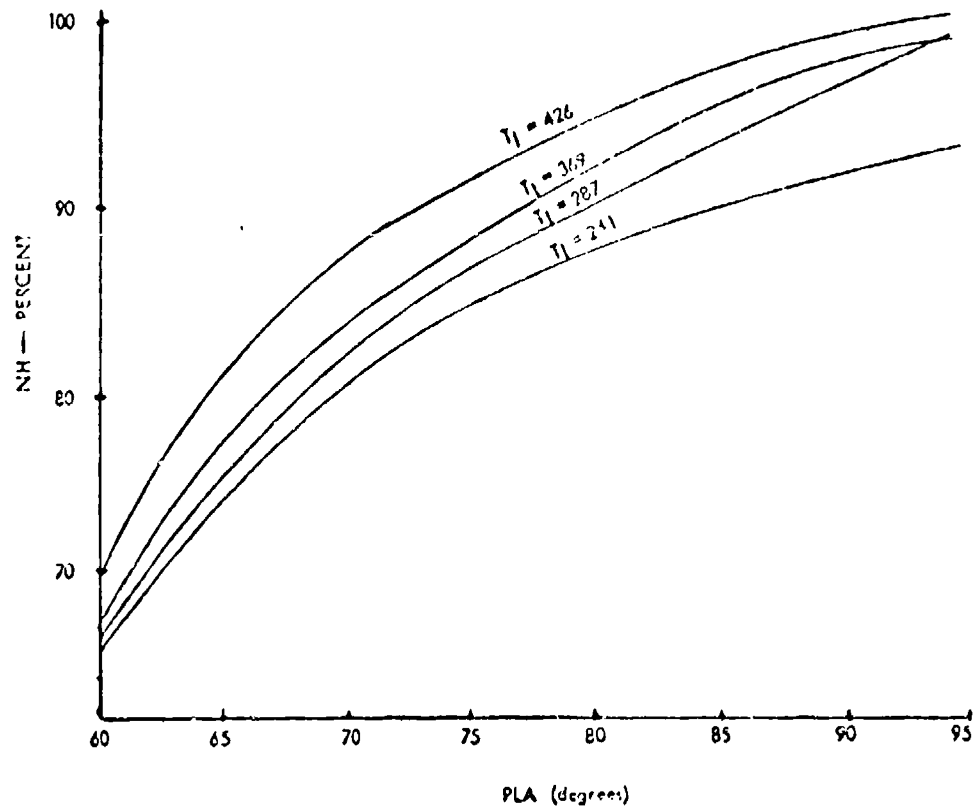
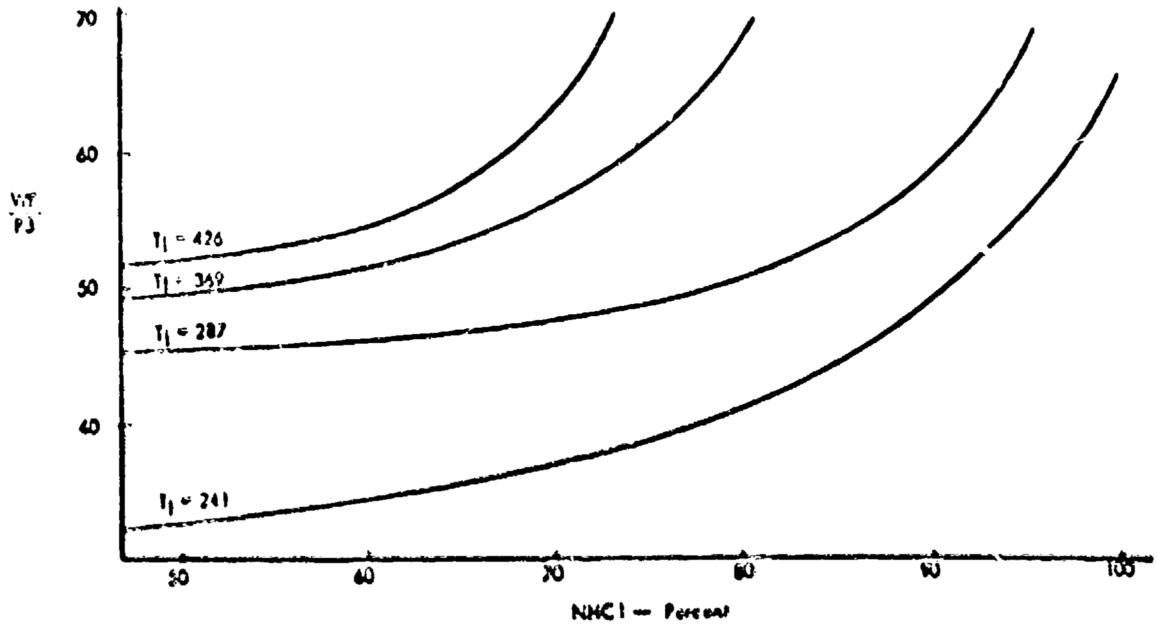


Figure 7-2 Fuel Control Schematic



a) Governor Schedule



b) Acceleration Schedule

Figure 7-3 Fuel Control Schedules

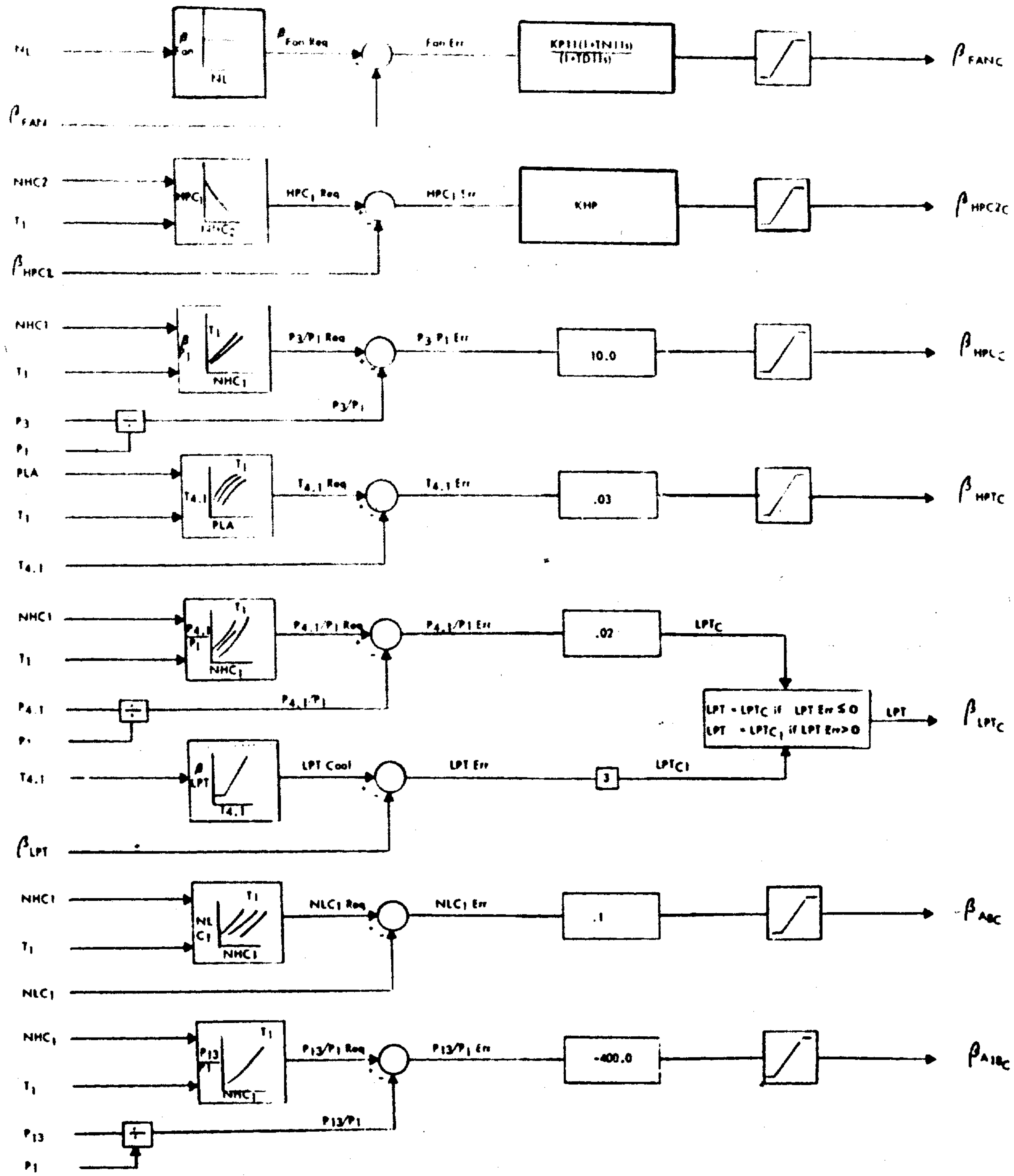
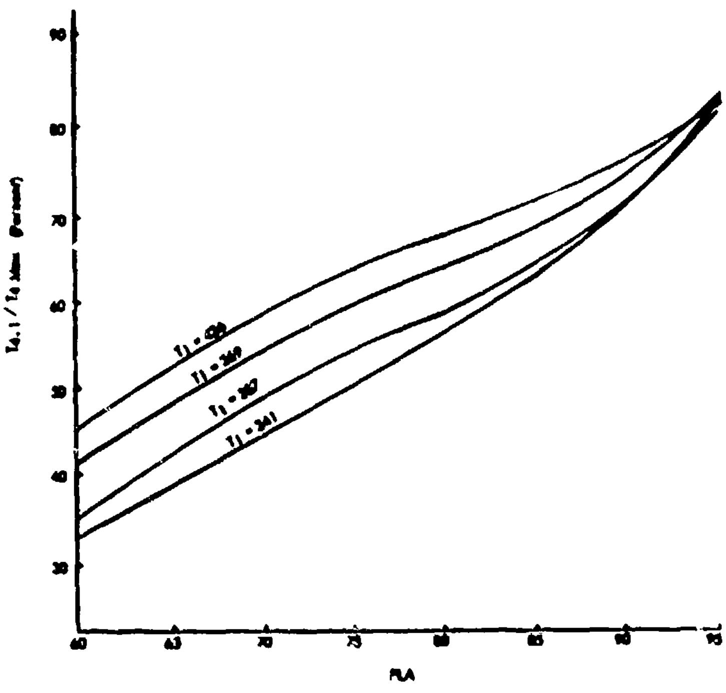
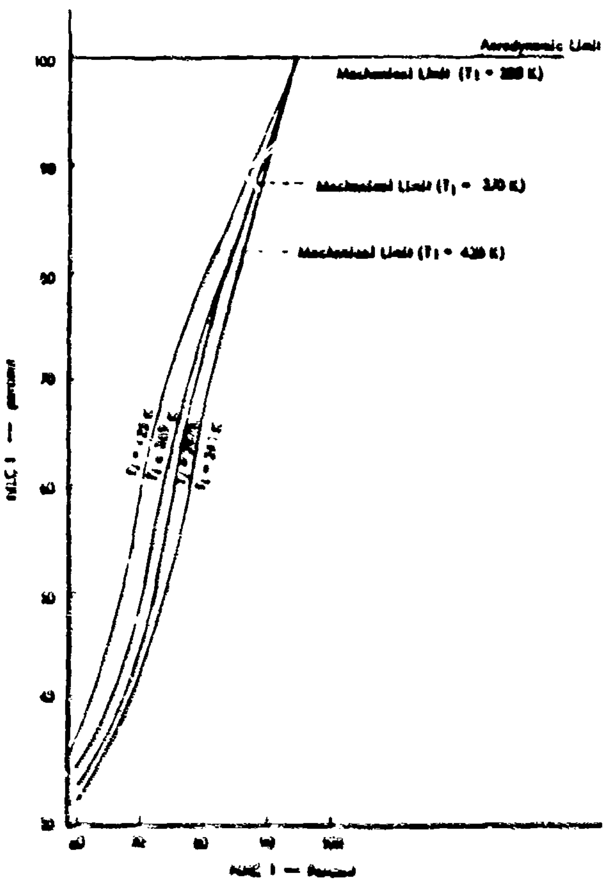
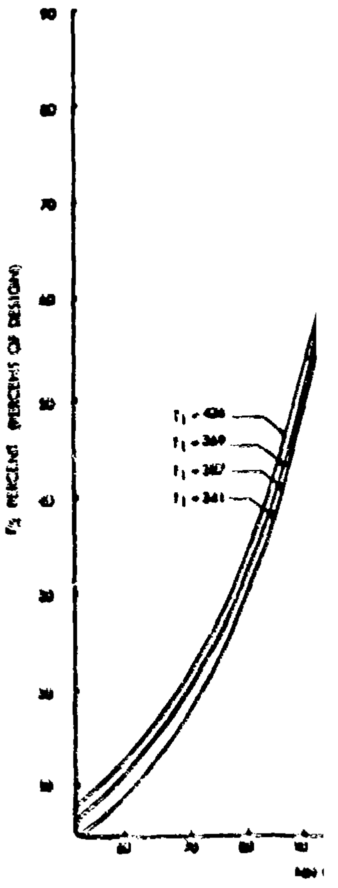


Figure 7-4 Geometry Control Schematic


PLA


Aerodynamic Limit
Mechanical Limit (T1 = 288 K)
Mechanical Limit (T1 = 370 K)
Mechanical Limit (T1 = 426 K)


PERCENT (PERCENT OF DESIGN)

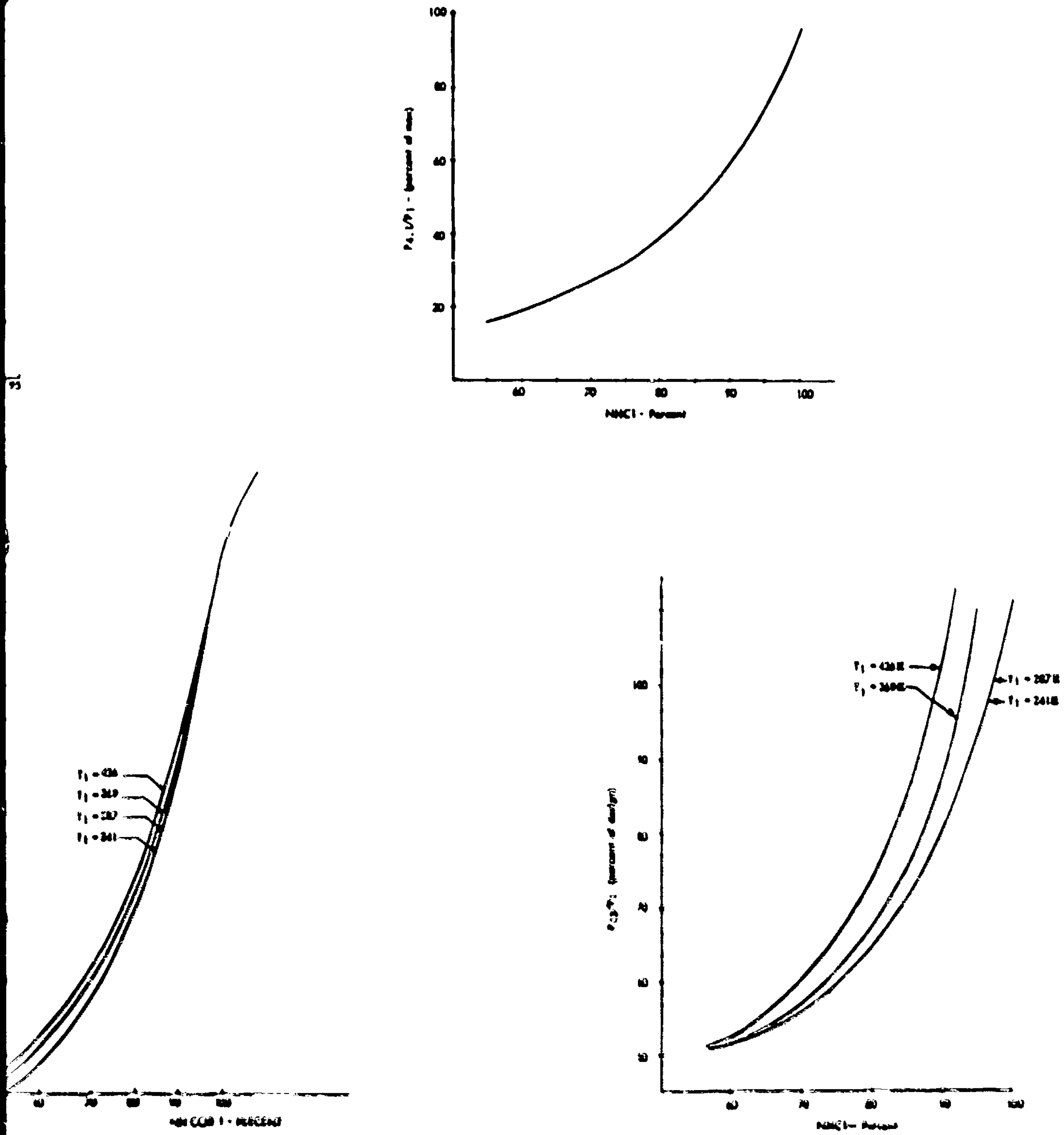


Figure 7-5 Geometry Control Scheules

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Maximum Thrust

The maximum thrust points for the four flight conditions were established with the optimization procedure described in Section 5.0. The optimization on thrust was accomplished without two constraints later imposed during the control mode development. To avoid fan surge during accelerations with hot inlet temperatures, the duct nozzle minimum area was increased from 100 to 135 square inches. In addition, evaluation of the low pressure turbine cooling showed that the minimum LPT setting of .8 would not provide sufficient cooling at the upper end of the temperature operating range. Generally, the minimum setting at maximum thrust will be 1.2 which yields a 3% to 5% reduction in the maximum thrust when compared to the 0.8 minimum. A comparison of the maximum steady state thrusts for the selected control mode and the optimal engine set-up for maximum thrust is given in Table 7-1.

In the two cases where the "optimal" maximum thrust does not violate the new constraints on the duct nozzle or LPT setting, the proposed control mode provides more than 99% of the optimal thrust. As expected, the controlled parameters (speed, temperature and pressure) were very close to the optimal and it was gratifying to note that the control variables (geometry positions) also were close to their optimal settings which indicates the optimal engine condition is unique and can be specified in terms of geometry position or engine parameters.

At static sea level, the thrust achieved is almost within 1% of the "optimal" even though the optimal LPT setting of 0.8 cannot be achieved because the cooling requirement necessitates a higher index. The high LPT setting increases the inter-turbine temperature and creates an error in the HPT. The HPT closes down (less airflow) to lower the temperature and to compensate for the higher turbine temperature created by the higher LPT setting. One should note that the controlled parameters are near optimal even though the HPT and LPT had to assume non-optimal settings and near maximum thrust was achieved.

Flight Condition	Mode	Thrust (Percent)	Fuel Flow (lb/hr)	N_L (Percent)	N_H (Percent)	T_4 (Percent)	HPC Index	HPT Index	LPT Index	A8 In^2	A18 In^2
Static Sea Level	Opt	100.0	17,362	100	100	100	120	105	.8	233	135
	APSI	98.9	16,745	100	100	100	112	101	1.17	229	141
Mach .75 Alt = 36,089 Ft	Opt	100.0	6,946	91	94	100	120	116	.8	201	108
	APSI	97.8	6,907	92	94	100	111	108	1.36	193	135
Mach 1.2 Alt 500 Ft	Opt	100.0	21,812	98	93	100	109	111	1.2	245	219
	APSI	99.9	21,763	97	98	100	108	112	1.2	255	225
Mach 2.2 Alt = 36,089 Ft	Opt	100.0	16,919	100	100	100	120	108	1.12	279	188
	APSI	99.2	16,770	100	100	100	120	106	1.20	283	208

Table 7-1 Maximum Thrust Comparison

At .75/36K, the cooling requirement does not allow the optimal .8 LPT index and the optimal duct area of 108 in.^{2} cannot be obtained because the minimum area was raised to 135 in.^{2} to prevent fan surge. Therefore the maximum thrust is 2.3% less than the optimal. As in the previous case, the HPT reduced the HPT airflow capability to offset the higher temperature created by the LPT setting. However, there is no other geometry to make up for the limits on the duct nozzle and thus the optimal duct pressure was not obtained.

Minimum sfc

Normalized sfc vs. thrust for the four flight conditions is plotted in Figure 7-6 along with the "optimal" values. As discussed earlier, a critical step in achieving the optimal sfc is the establishment of the T4.1 temperature vs. PLA schedule for the HPT. Some "tuning" of the HPT schedule was done at static sea level, but no adjustment in the initial schedule was made at the other flight conditions. The ability to attain near optimal sfc over a wide range of flight conditions has been demonstrated by this control.

Dynamic Response

The dynamic response presented here demonstrates that a properly chosen control mode designed to give optimum steady state performance can also yield good dynamic response. No attempt has been made to optimize the transient behavior of the control and only gain changes have been made to provide stability. It is felt that more complex frequency domain compensation (filtering) could improve the dynamic response of the system. However, this "tuning" of the control should be attempted after a better definition of the hardware is

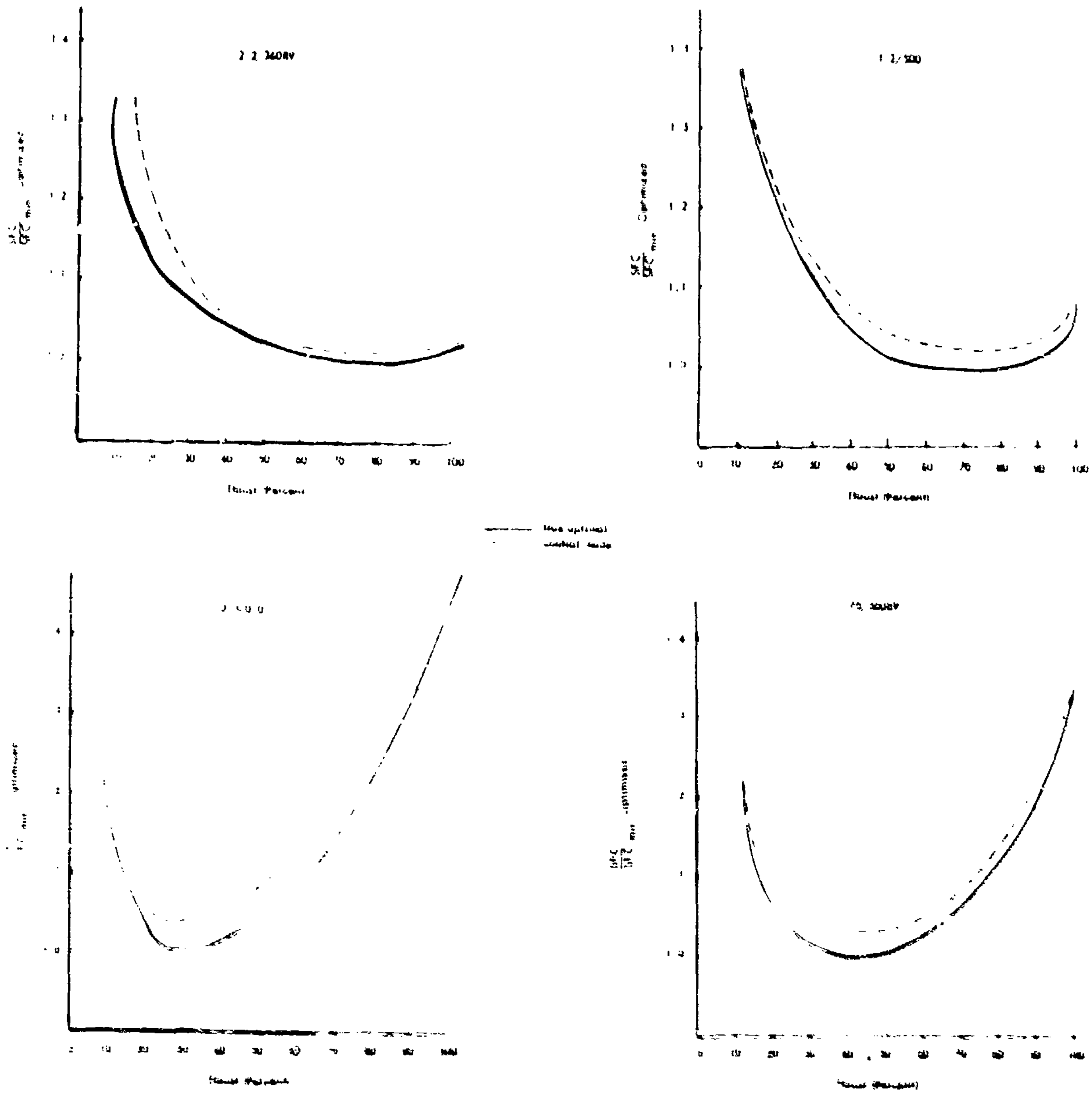


Figure 7-6 SFC Performance

incorporated into the digital engine simulation.

The following transients are presented in Figures 7-7, 7-8 and 7-16 through 7-24 for the four flight conditions.

Accelerations

- o Idle to Intermediate power
- o Idle to part power (85%)
- o Part power (30%)to part power (85%)
- o Part power (30%) to Intermediate

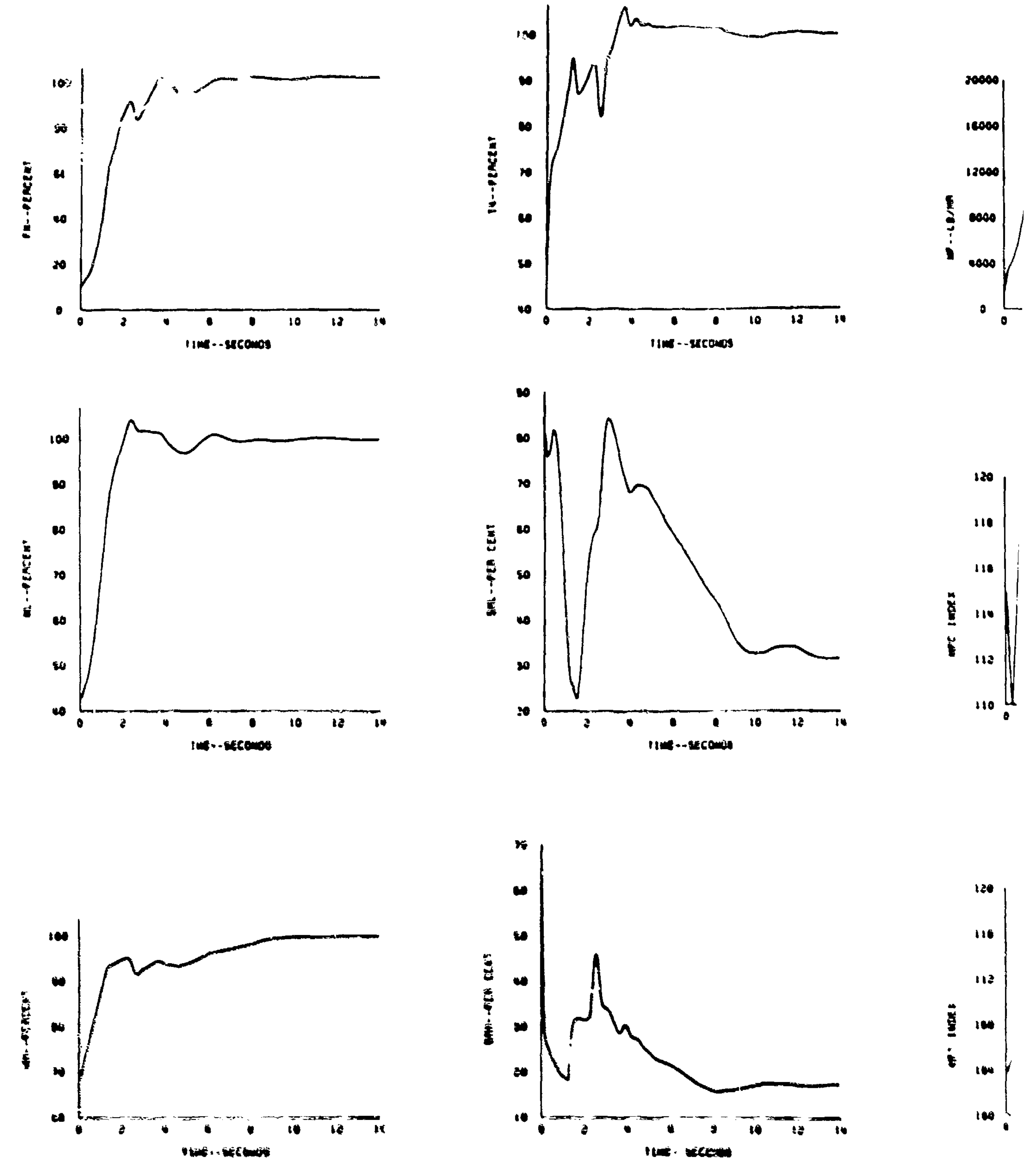
Decelerations

- o Intermediate to Idle

In each case, the throttle movement was a uniform rate for 1 second. The transients will be discussed in detail in the following sections with special emphasis on the static sea level case which presently is envisioned for JTD test stand operation.

Sea Level Static

The response of the engine and control to a full throttle acceleration (Idle to Intermediate power) is shown in Figure 7-7. The response is typical of today's high performance engines in which the fuel control experiences multiple limiters before reaching steady state performance. For the first second, the fuel control is on the acceleration schedule and then switches to the speed governor mode for the next second. At the switchover point, there is a short droop in the fuel flow caused by the dynamics involved in switching from the limiter to the governor. This phenomena is effected by the pump dynamics and the type of reset on the governor integrator, which will be shown later. At approximately two seconds, the NL limiter becomes dominant for approximately 1 second after which the primary nozzle has approached its steady state value and has control of NL. The A8 control gain cannot be raised to improve the geometry control


TIME--SECONDS
SML--PER CENT

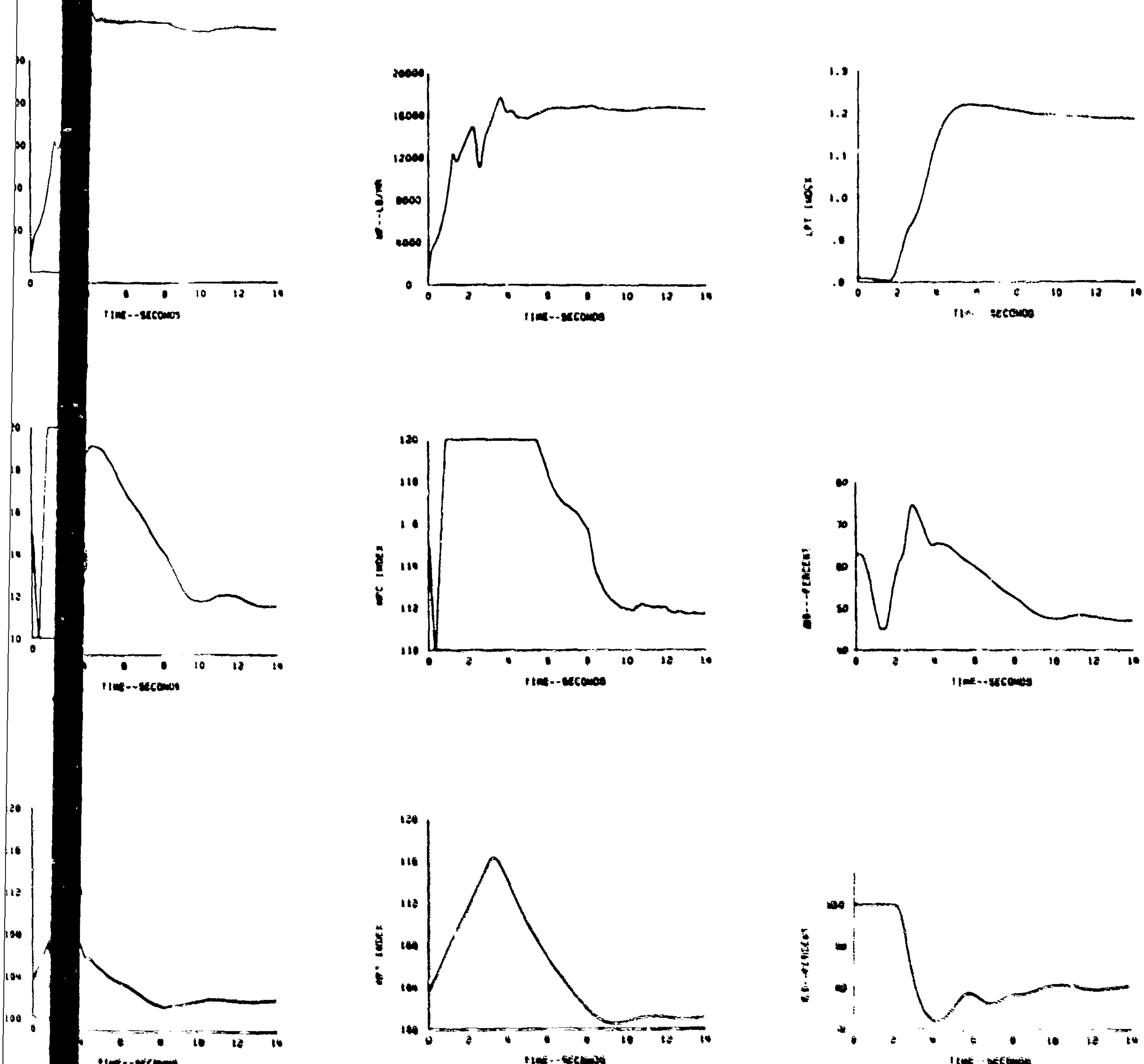


Figure 7-7 Idle to Intermediate Power Acceleration at Static Sea Level

of NL because of stability. The interaction of the fuel control NL limiter and the primary nozzle control makes analysis and dynamic compensation of the primary nozzle difficult. There is also coupling between the primary nozzle and the HPT control since the A8 control loop stability increases when the HPT is fixed. A more concentrated effort in this area is planned when a better definition of the nozzle actuation dynamics and the engine components for the JTD testing is available for the simulation.

At approximately 3 seconds, the T4.1 limiter becomes dominant for about 3 seconds. The point at which the limiter engages and the amount of T4 overshoot is mainly a function of the lag in the T4.1 thermocouple. The thermocouple has been compensated to reduce the effective lag to 0.2 seconds at static sea level intermediate power flow levels. In addition to degrading the fuel T4.1 limiter, the thermocouple lag greatly affects the HPT control loop. The HPT initially opens up because it thinks the engine is undertemperature because of the lag. This drives T4 overtemperature even more and providing an adverse effect on the ability of the turbine to accelerate the high speed rotor (NH). However, it will be shown later that the opening of the HPT improves the surge margin of the engine during the crucial initial phase of the transient. After 6 seconds, the steady state thrust condition has been achieved and the final portion of the transient allows some of the geometry to reach their final values with minimum effect on the thrust.

It should be noted that adequate surge margins are maintained throughout the transient and the compressor surge margin does not drop below the final steady state value for intermediate thrust.

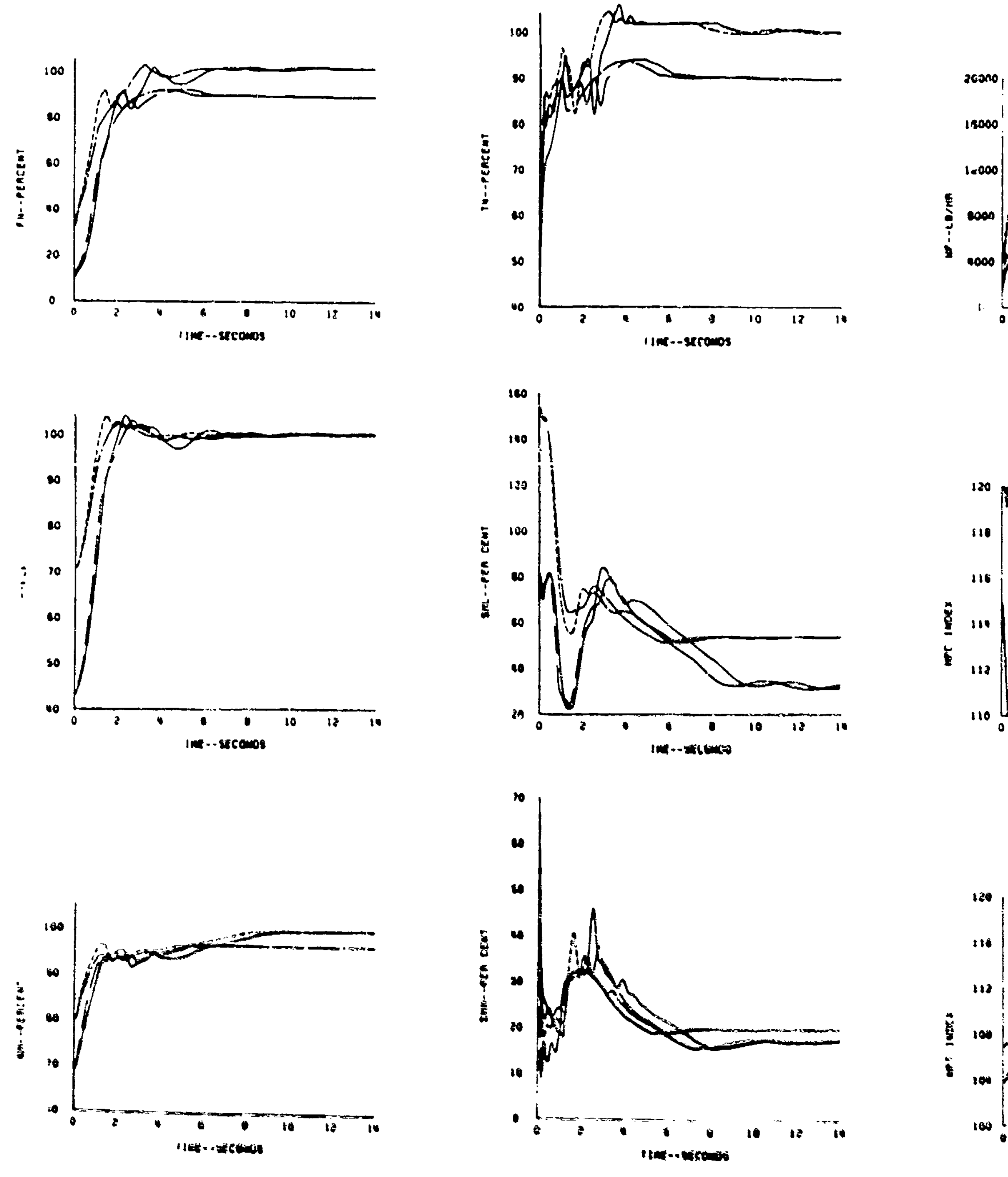
Accelerations for the four throttle motions given above are shown in Figure 7-8. The only significant observation after looking at these other conditions is the fact that the compressor surge margin dropped to 10% during the idle to part power (90%) acceleration. This was caused by two factors.

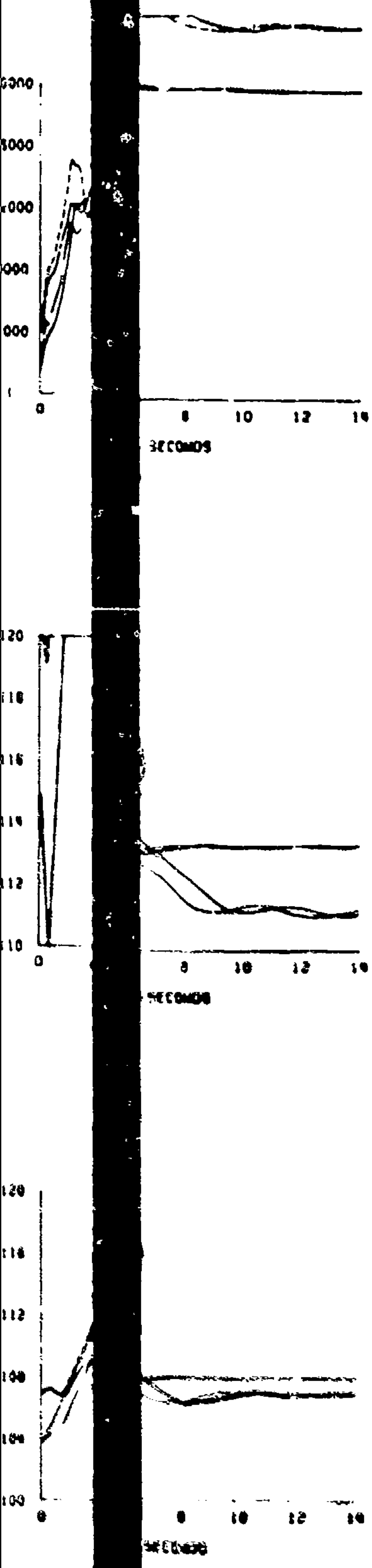
First, the acceleration schedule (W_f/P_3) was not reached and the $\Delta P/P$ limiter came into effect to avoid compressor surge. The acceleration schedule could be "tuned" to yield more surge margin. Continued evaluation of the $\Delta P/P$ limiter will dictate if additional adjustment is required. In addition, the HPT did not move much initially and thus there was little surge margin relief provided by the HPT.

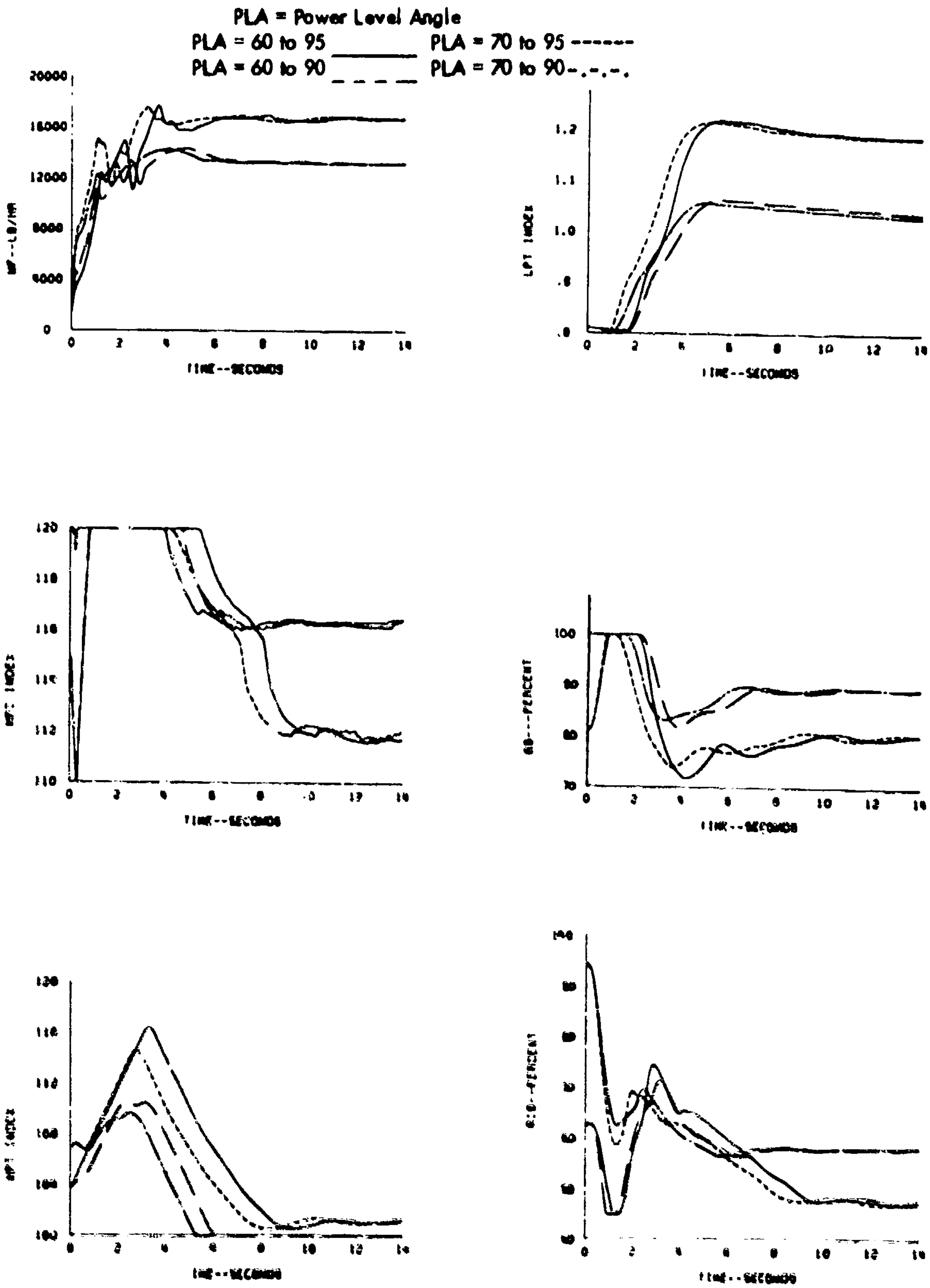
The deceleration for static sea level is shown in Figure 7-9. No problem with decelerations is expected with the proposed control mode.

Since conditioned air is not expected for the JTD testing, the inlet temperatures may vary from 0°F to 100°F during the tests. Figure 7-10 present the idle to intermediate accelerations over this temperature range. Nothing unusual was seen in these transients although some adjustment of the acceleration schedule at 0°F may be required to preserve surge margin.

Another feature expected during the JTD testing is an investigation of $\Delta P/P$ surge control as a substitution for the normal W_f/P_3 acceleration schedules. This will be accomplished by moving the W_f/P_3 schedule "out-of-the-way" and allowing the $\Delta P/P$ limiter to protect the compressor from surge. A comparison of the W_f/P_3 controlled acceleration and an acceleration where the W_f/P_3 acceleration schedule was moved to yield only $\Delta P/P$ surge control is shown in Figure 7-11. The thrust response benefits of $\Delta P/P$ surge control seem negligible and the penalties in surge margin are high. This occurs because no limiting control of fuel is created until surge is close. It can be seen that surge changes rapidly and a very fast control will be required to utilize only $\Delta P/P$ effectively. This will require a high interation rate if a digital control is


FN--PERCENT
TIME--SECONDS
TN--PERCENT
TIME--SECONDS
WF--LB/HR
TIME--SECONDS
SML--PER CENT





Fig

Figure 7-8 Selected Accelerations at Static Sea Level

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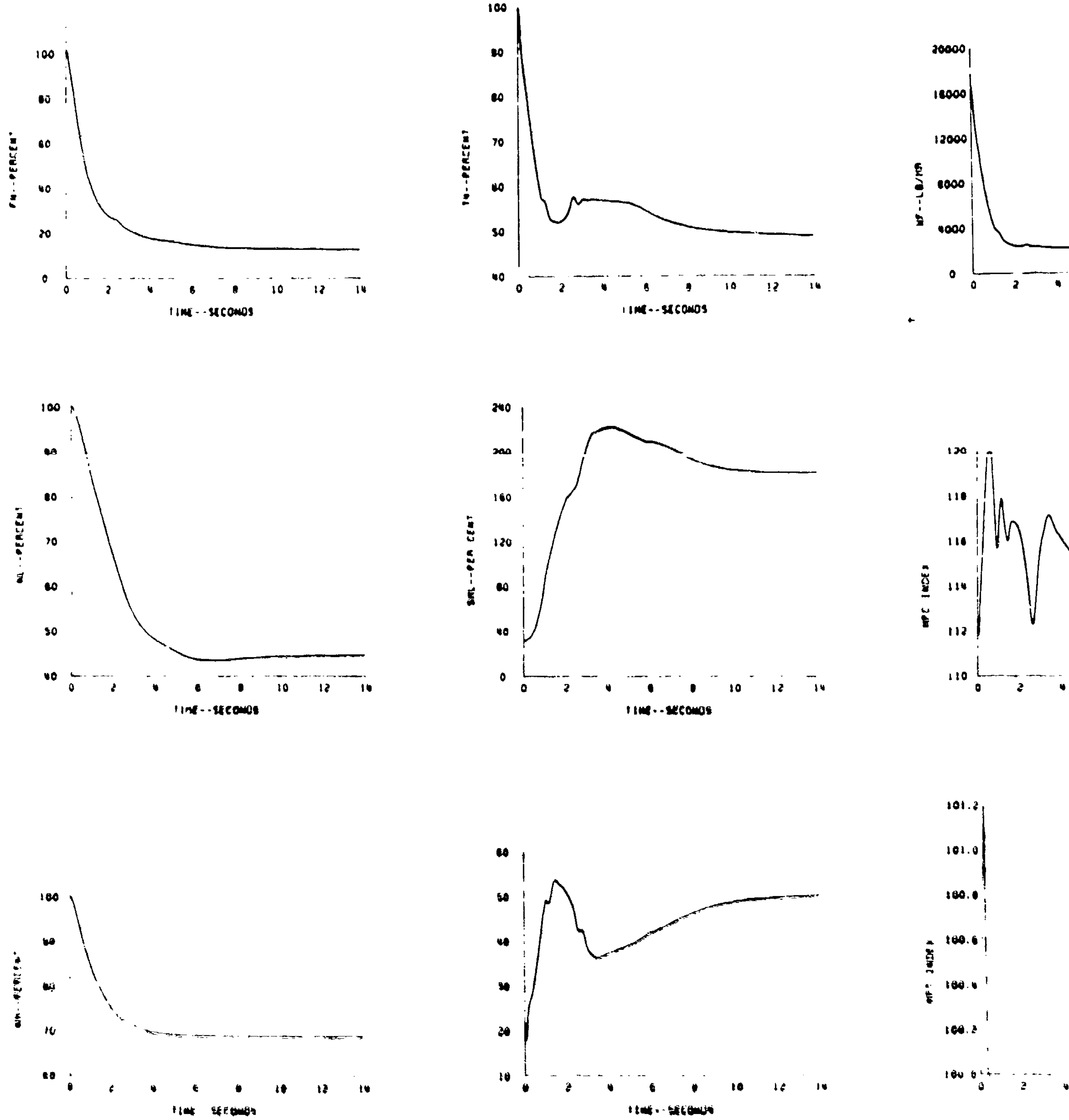

FN--PERCENT
TIME--SECONDS
TN--PERCENT
TIME--SECONDS
WF--LB/HR
TIME--SECONDS
SML--PER CENT
TIME--SECONDS

Figure 7

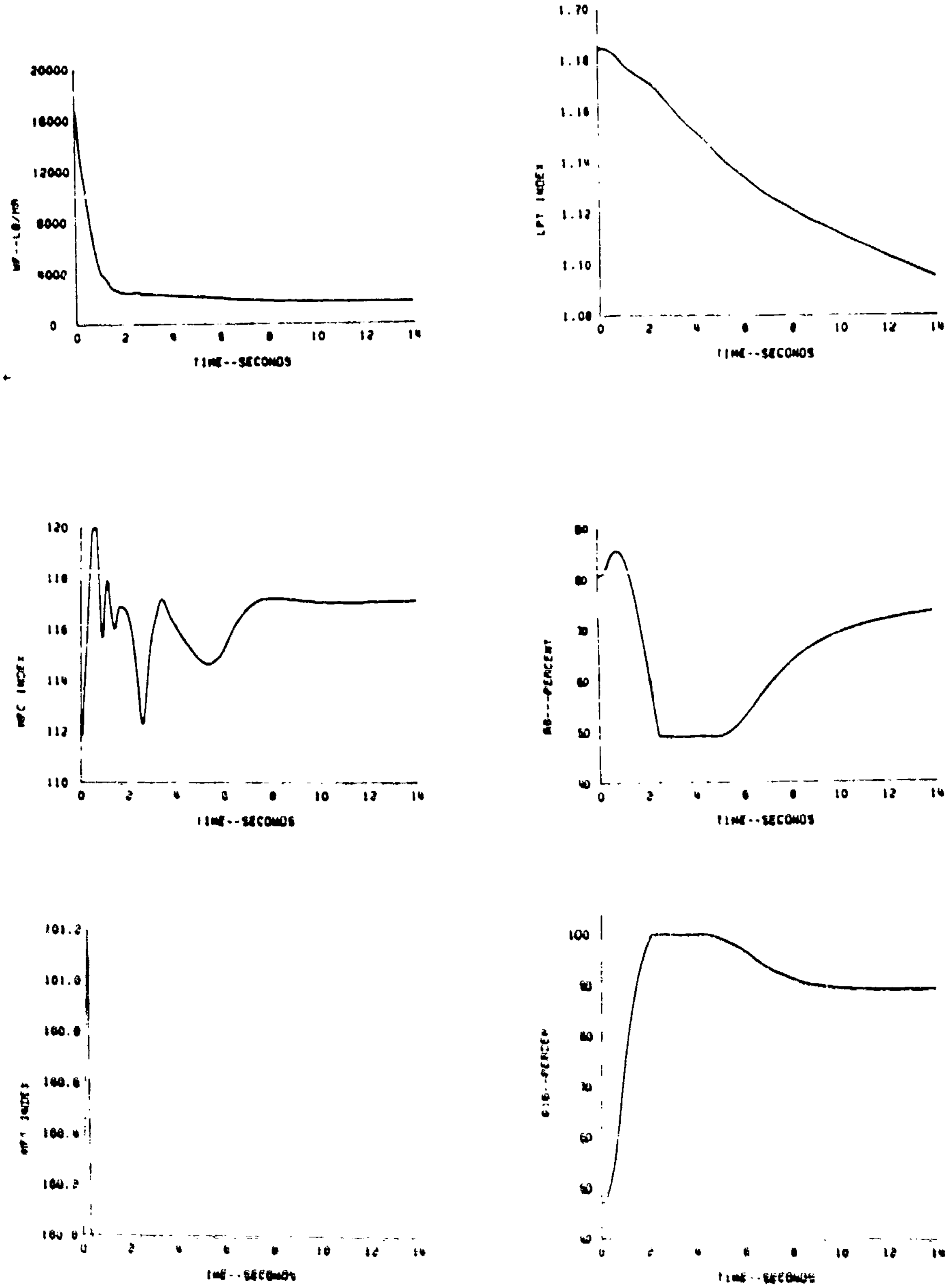
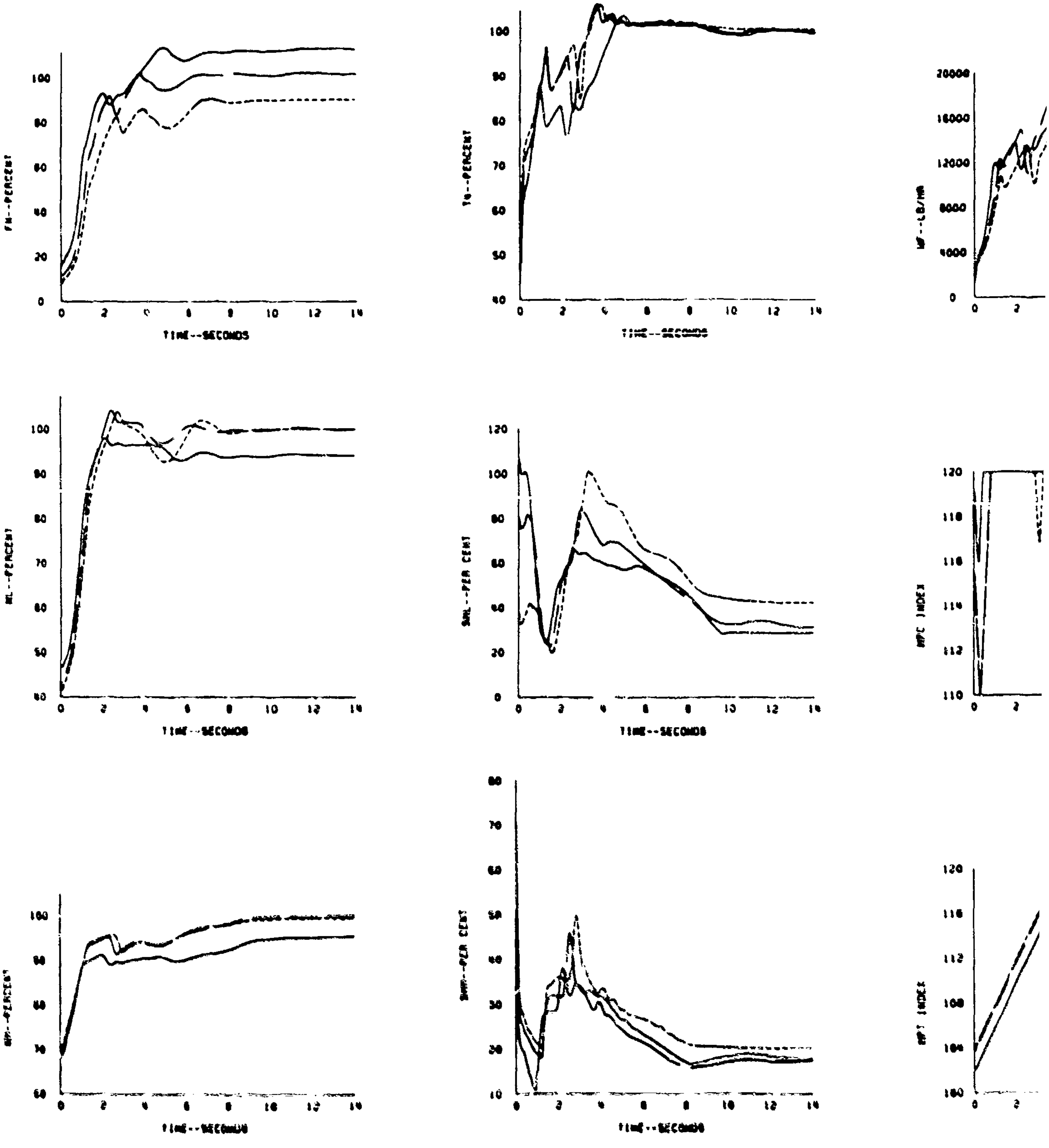


Figure 7-9 Intermediate Power to Idle Deceleration at Static Sea Level


TIME--SECONDS
FN--PERCENT
TG--PERCENT
NL--PERCENT
SNL--PER CENT
NPC INDEX
NPT INDEX

Figure

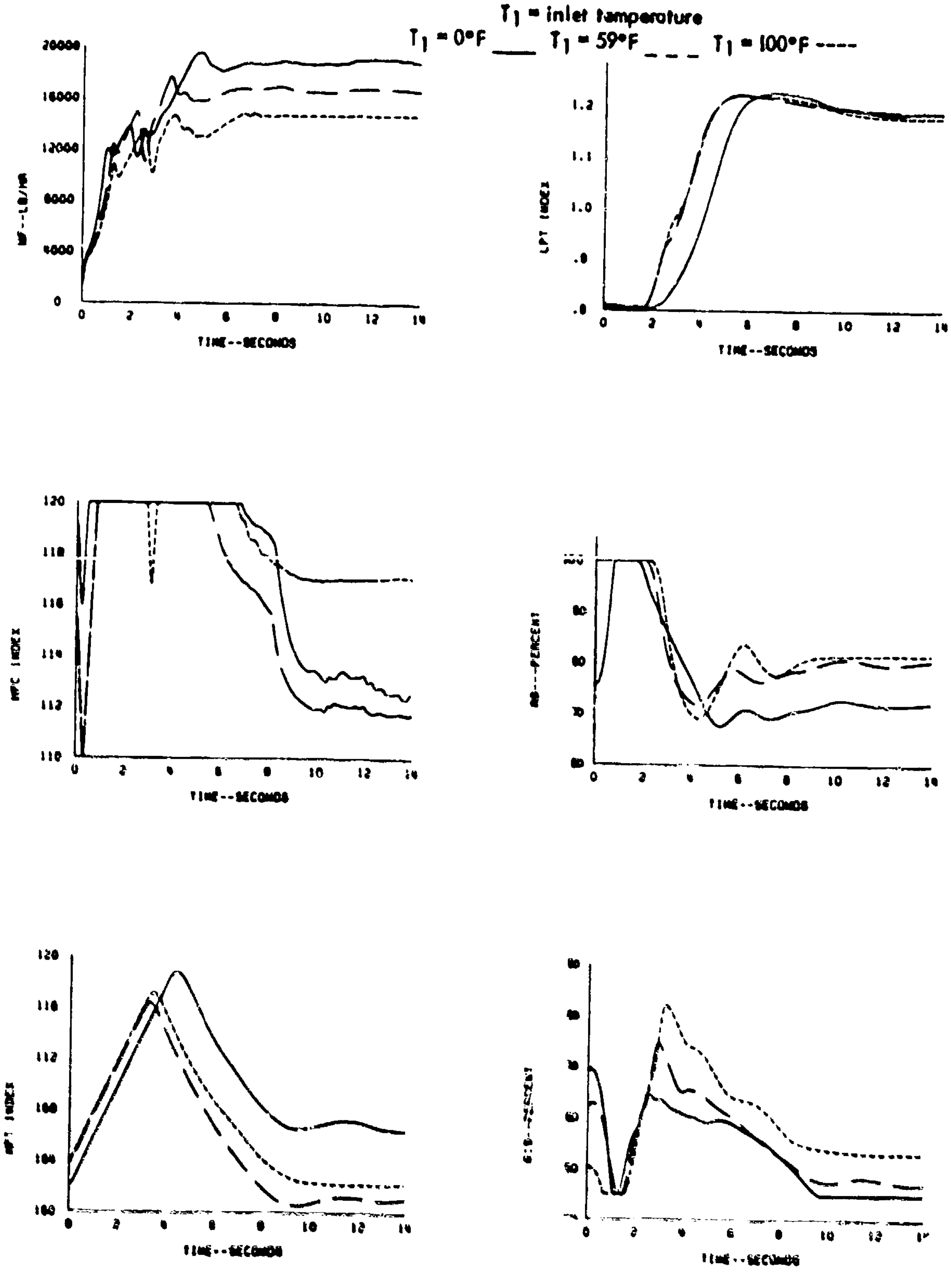


Figure 7-10 Hot, Cold, and Standard Day Accelerations at Static Sea Level

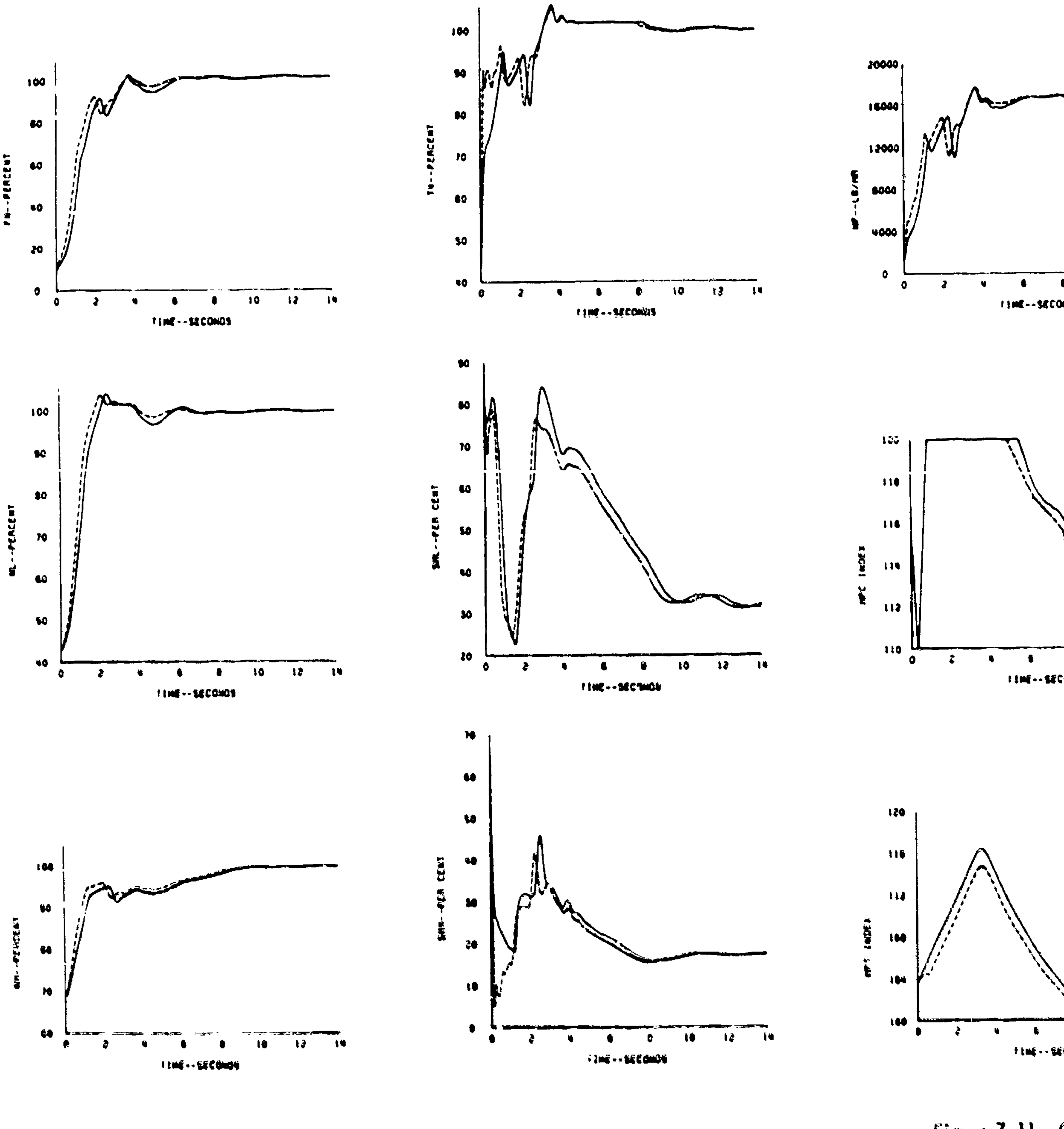


Figure 7-11

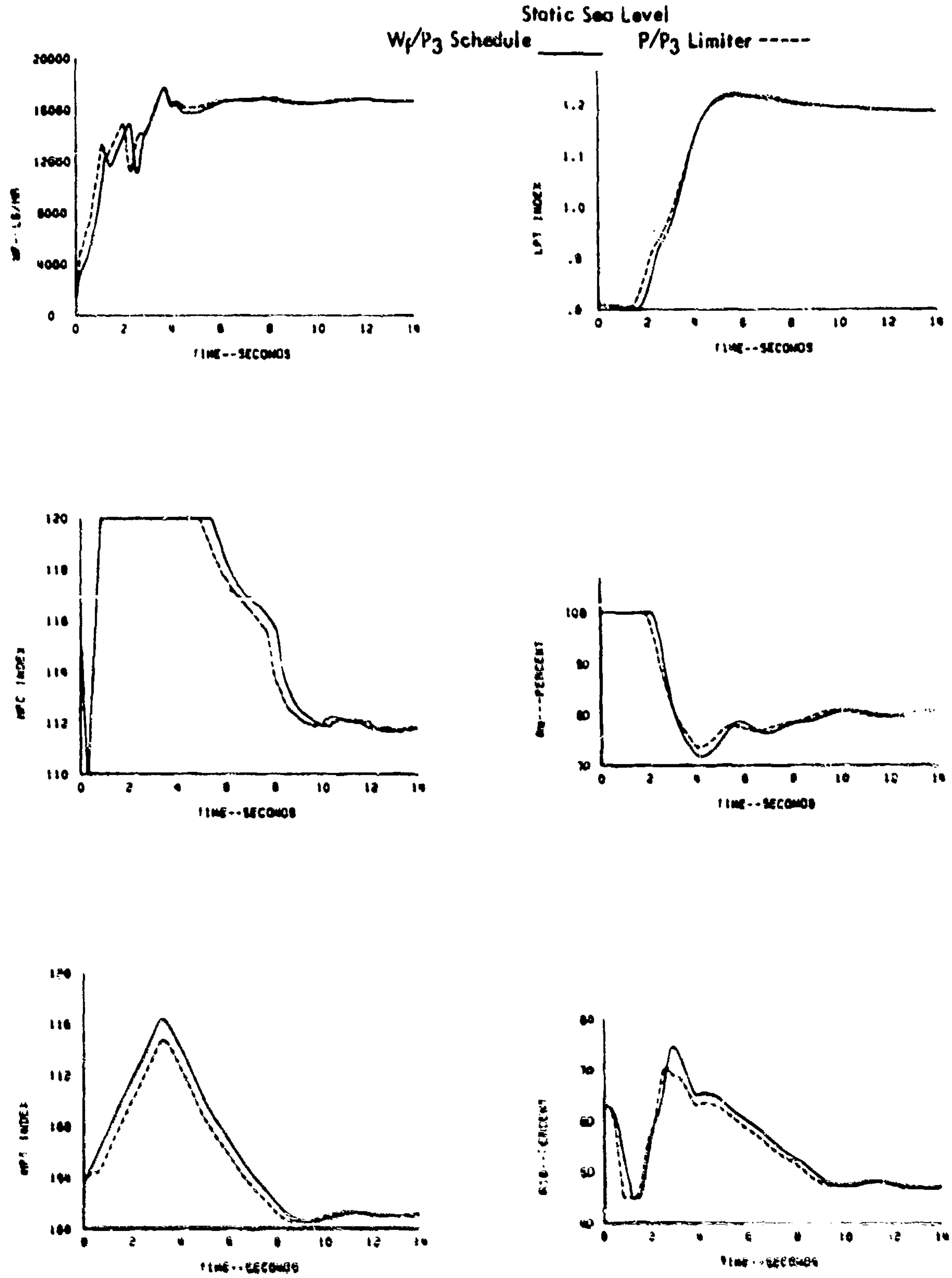


Figure 7-11 Comparison of Acceleration Modes

2

used and a fast pump and metering system to respond to the fuel command. Further investigation of $\Delta P/P$ surge control (with and without W_f/P_3 Acceleration Schedules) is expected since it does tie surge control more directly to the compressor and should be less sensitive to engine variations.

Early in the "open loop" geometry control mode studies, it was noted that the type of control used for the HPT had a large impact on the nature of the transient behavior of the entire control system. The HPT control is also important to the transient behavior of the "closed loop" control mode as seen in the comparison between acceleration with the selected mode and the same mode with a fixed HPT shown in Figure 7-12. With a fixed HPT, the response is much smoother since the thrust, temperature, and speed overshoots are reduced. However, 100% thrust is achieved approximately 2 seconds faster with the variable turbine even though some droop occurs after full thrust is achieved. The faster response is obtained with a significant increase in compressor surge margin. The response also suggests a significant coupling between the primary nozzle and the HPT since the A8 loop is very stable with a fixed HP turbine. Thus, the variable turbine "buys" faster response with improved surge margin at the expense of greater temperature excursions and more complex stability problems.

Many control gains and hardware dynamics were varied to determine the relative stability of the control. The results from some of the more interesting variations are presented in Figures 7-13 through 7-15 for changes in fuel control governor gain, integrator reset limits, and pump dynamics, respectively.

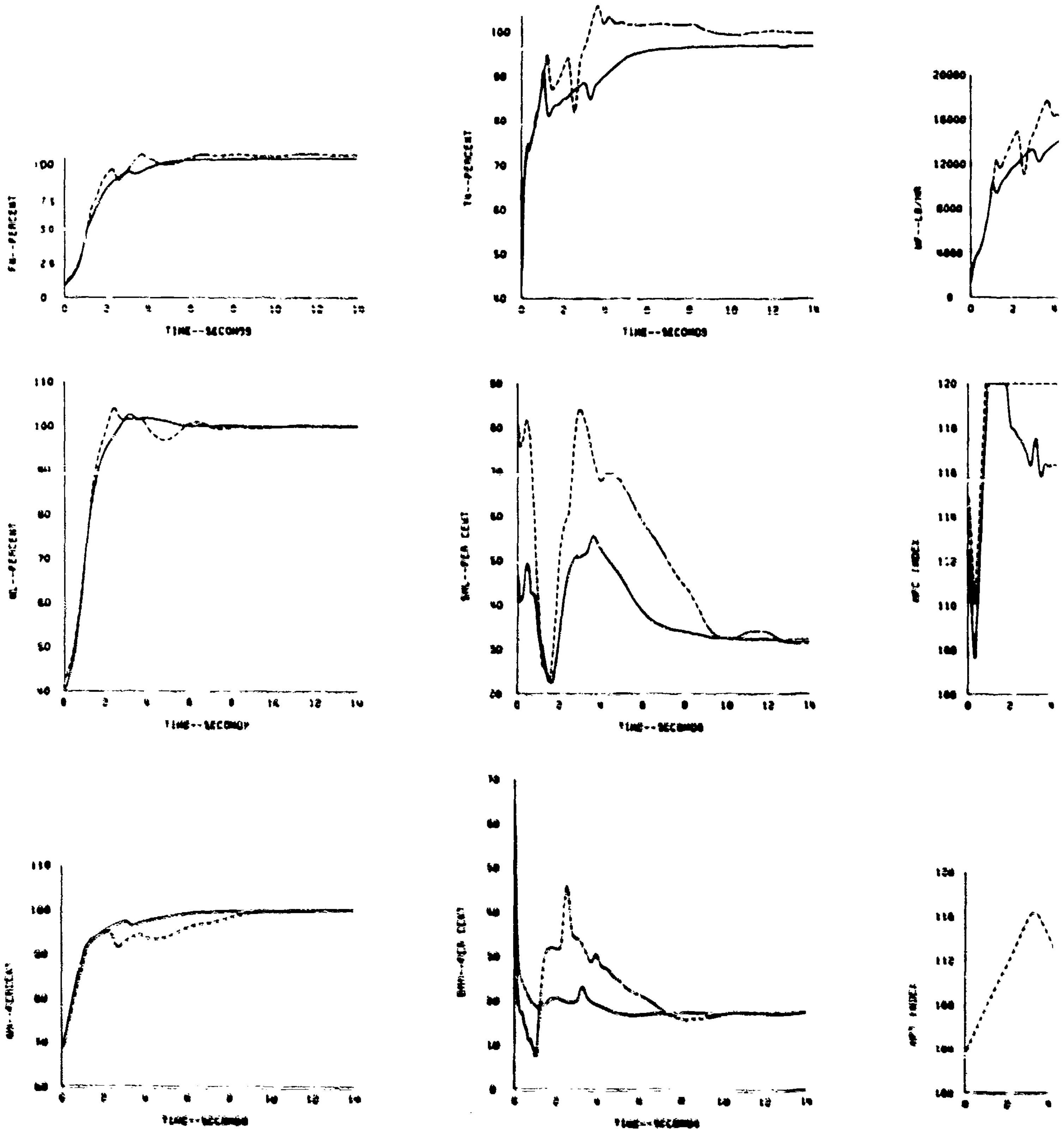
In addition to reducing the stability of the fuel control, increasing the integral gain of the governor reduces the thrust droop (undershoot) when the control is on the T4.1 limiter. A reduction in the ratio of governor proportional gain to integral gain smooths the transition from the acceleration schedule to the governor loop and from the NL limiter loop to the governor loop. However, the reduction increases the initial thrust overshoot and initial thrust and temperature undershoots as shown in Figure 7-13.

A small improvement in the thrust response time was obtained by limiting the minimum output of the governor integrator as seen in Figure 7-14. The cost of the improvement includes larger temperature overshoot, less surge margin and more fuel used. The use of limits may also be attractive in a digital mechanization to aid the scaling.

A fixed set of pump dynamics was used in the simulation for simplicity even though the pump dynamics may vary with flow rate. To investigate this variability with the proposed control mode, several different time constants for the pump were utilized to generate the responses shown in Figure 7-15. In addition to reducing the stability of the fuel control, the increased pump time constant effects the switchover to the acceleration schedule at the beginning of the transient which reduces the surge margin of the system. From these results, it is obvious that it is desirable to hold the pump lag to less than 0.1 seconds. This potential problem requires continued evaluation as the simulation is revised to reflect the true hardware and may even require some special compensation similar to that used with thermocouple lags if the pump lag is large.

<u>Mach 2.2 at 36,089 Ft. (11,000 m)</u>

The transient responses at this condition are very well behaved as seen in 7-16 through 7-18. The acceleration schedule controls the first 1-1/2 seconds of the transient and the T4.1 limiter is dominant from approximately 3-5 seconds into the transient. The governor controls the fuel at all other


TIME--SECONDS
TIME--SECONDS
WF--LB/HR
TIME--SECONDS
TIME--SECONDS
TIME--SECONDS
TIME--SECONDS

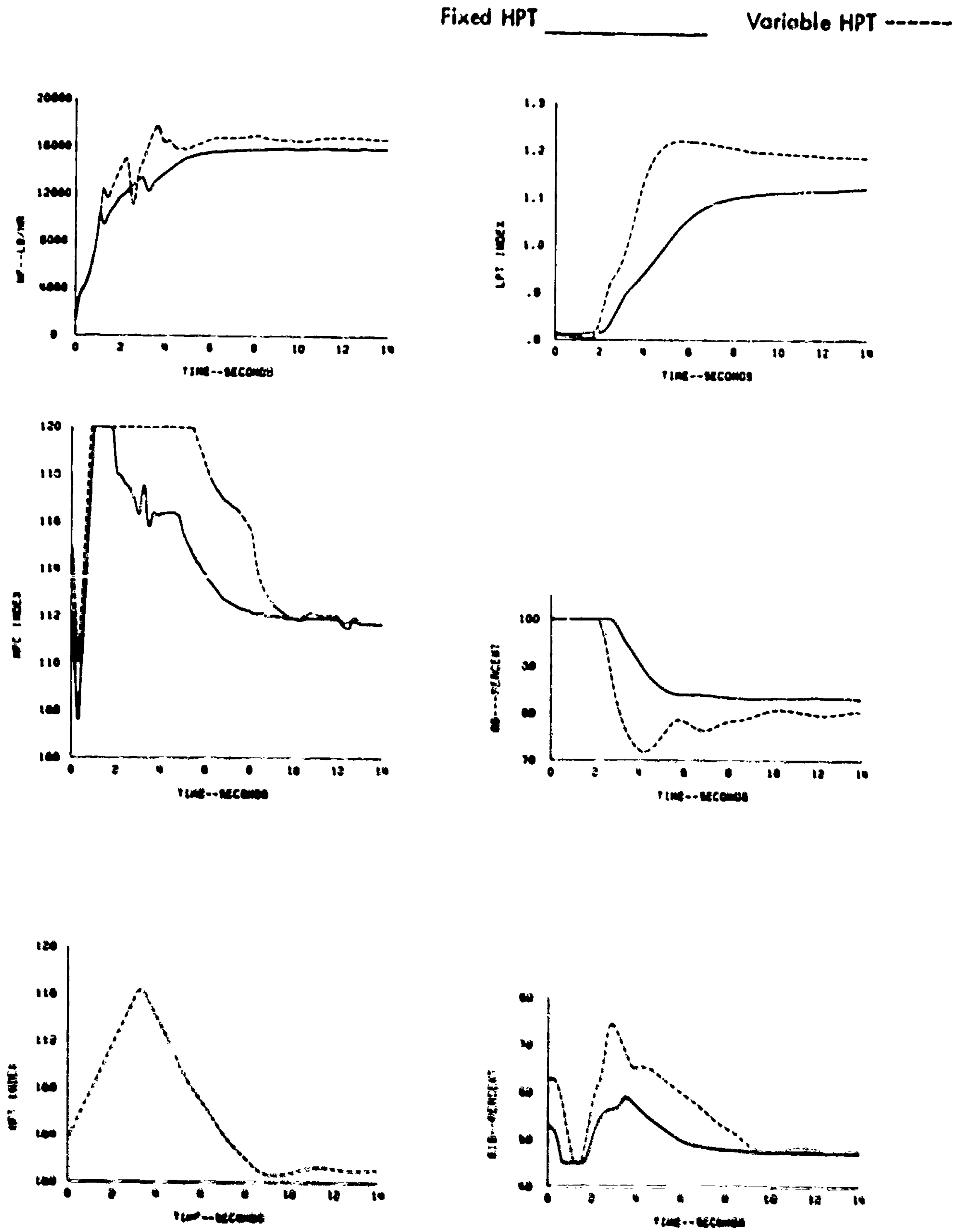
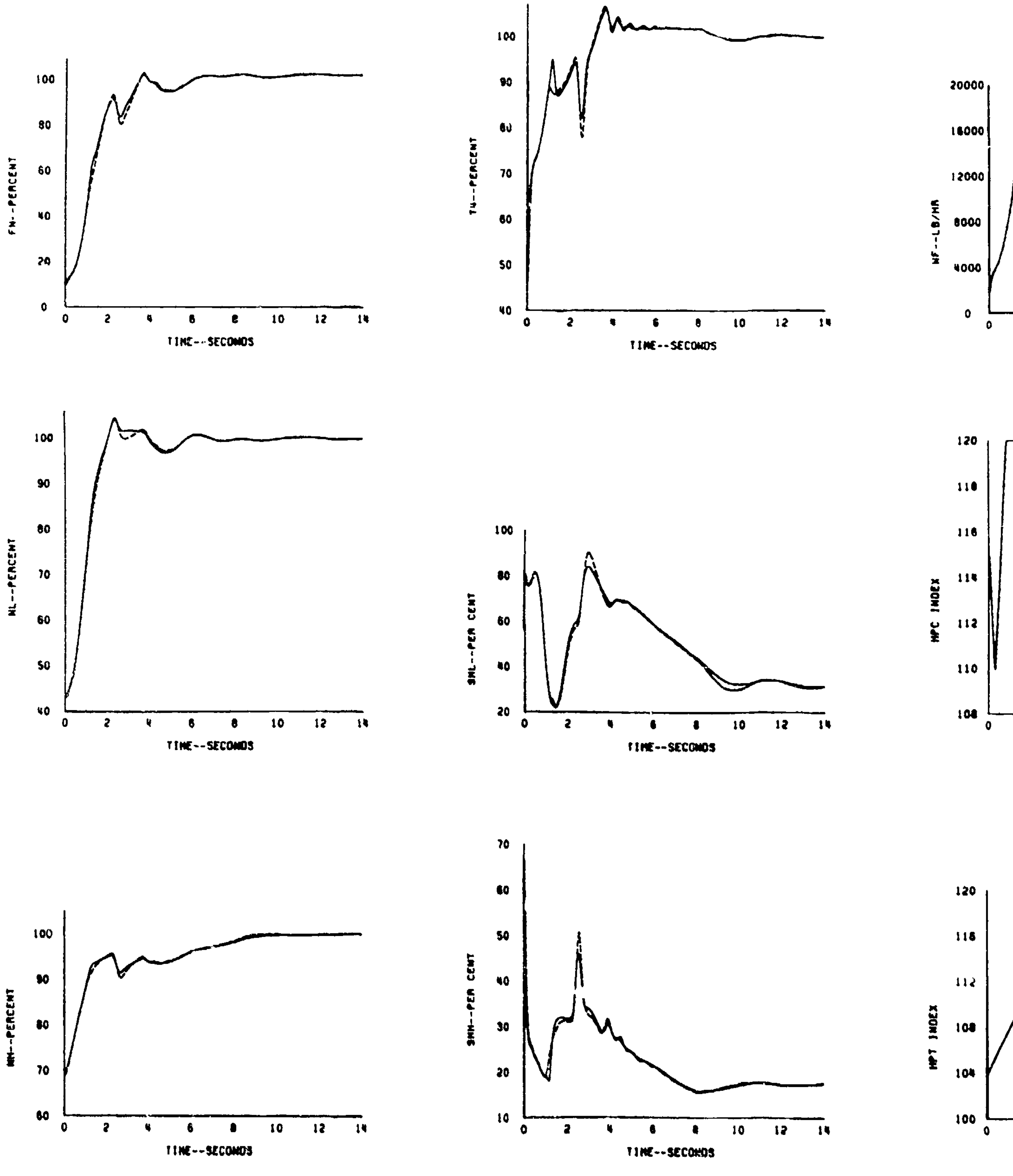


Figure 7-12 Comparison With Fixed Turbine at Static Sea Level


FN--PERCENT
TIME--SECONDS
T4--PERCENT
TIME--SECONDS
WF--LB/HR
NL--PERCENT
TIME--SECONDS
SML--PER CENT
TIME--SECONDS
HPC INDEX
NH--PERCENT
TIME--SECONDS
SMH--PER CENT
TIME--SECONDS
HPT INDEX

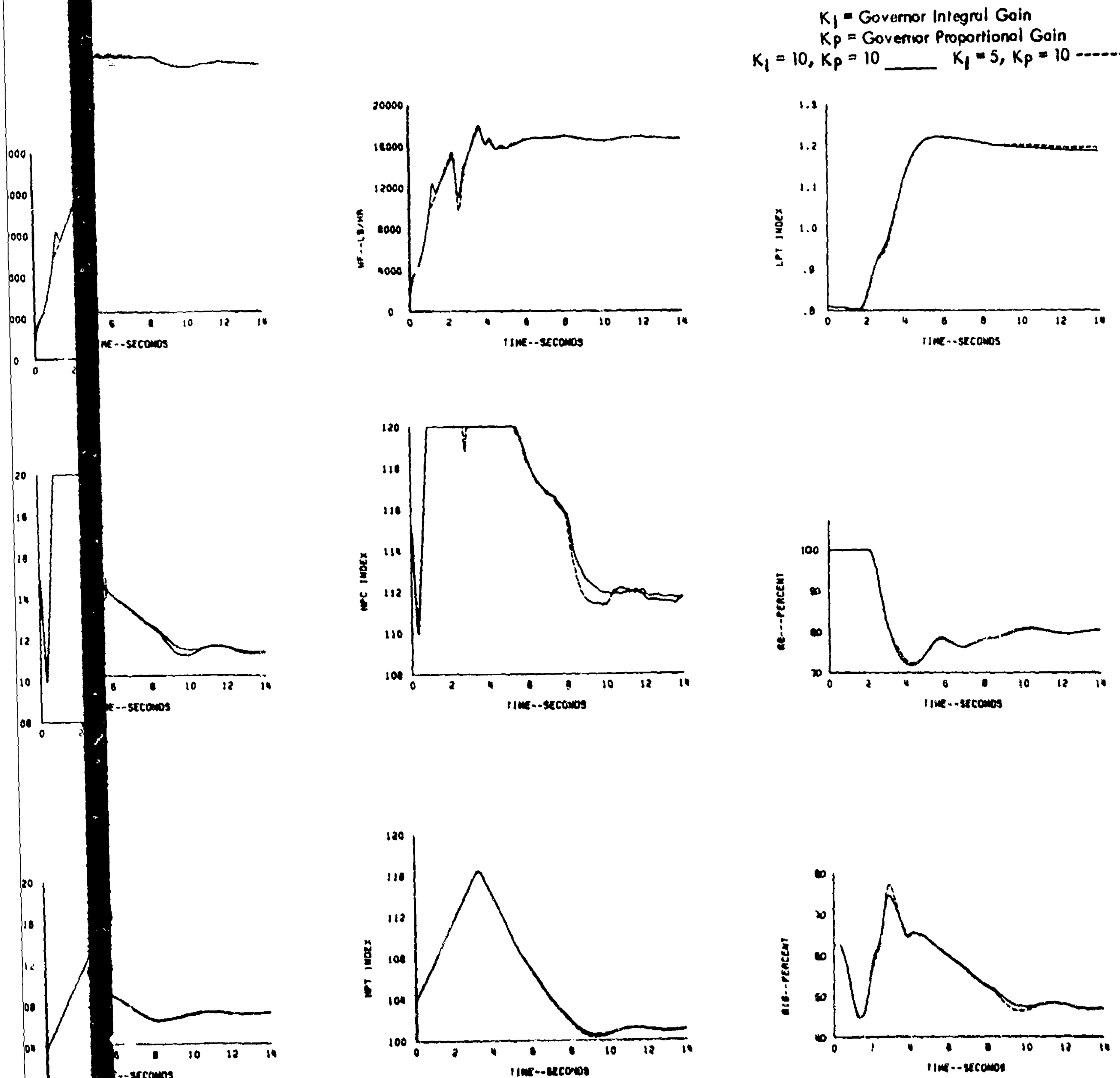
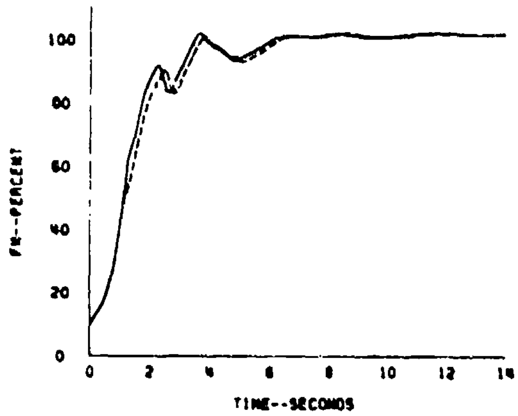


Figure 7-13 Comparison of Governor Gains at Static Sea Level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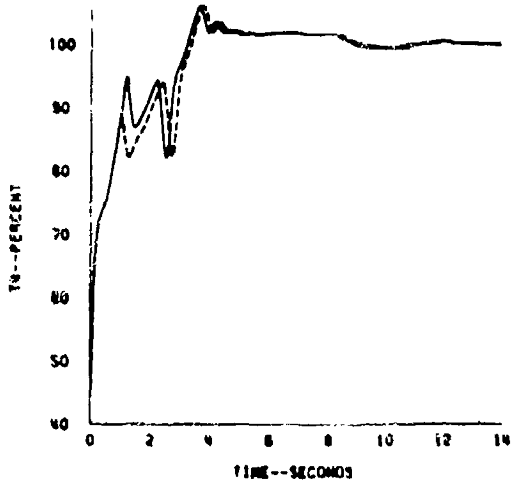
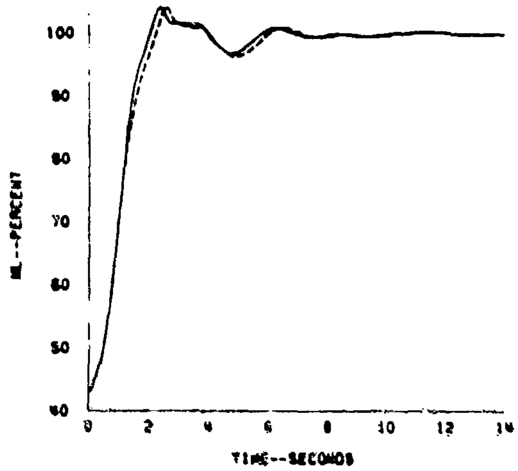
TIME--SECONDS


TIME--SECONDS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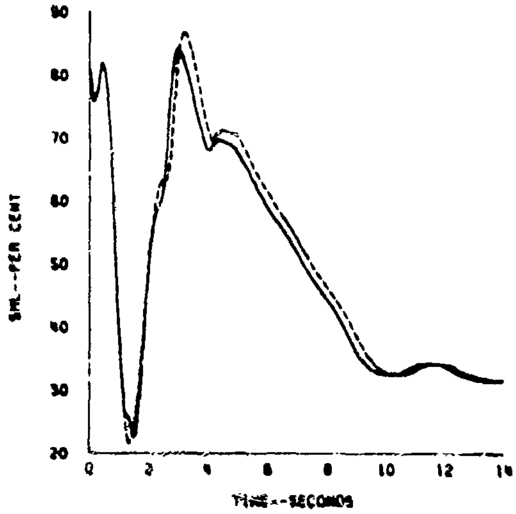
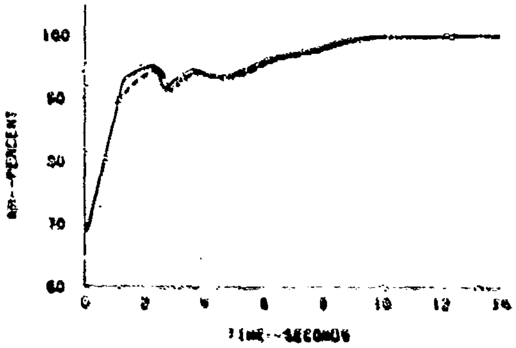
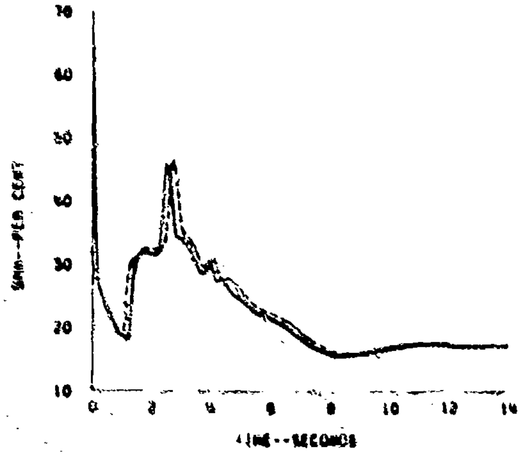
TIME--SECONDS


SML--PER CENT
TIME--SECONDS


TIME--SECONDS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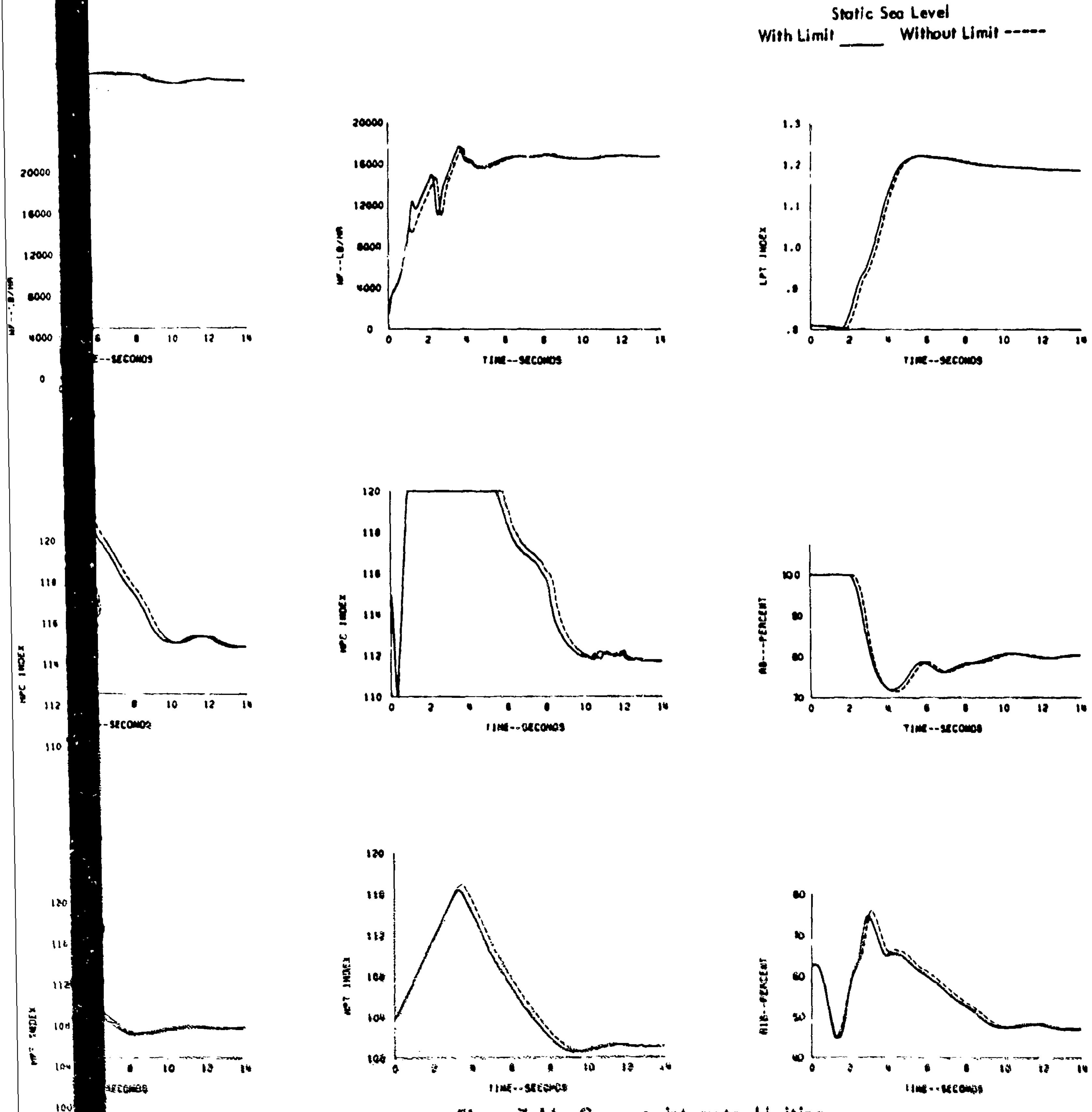
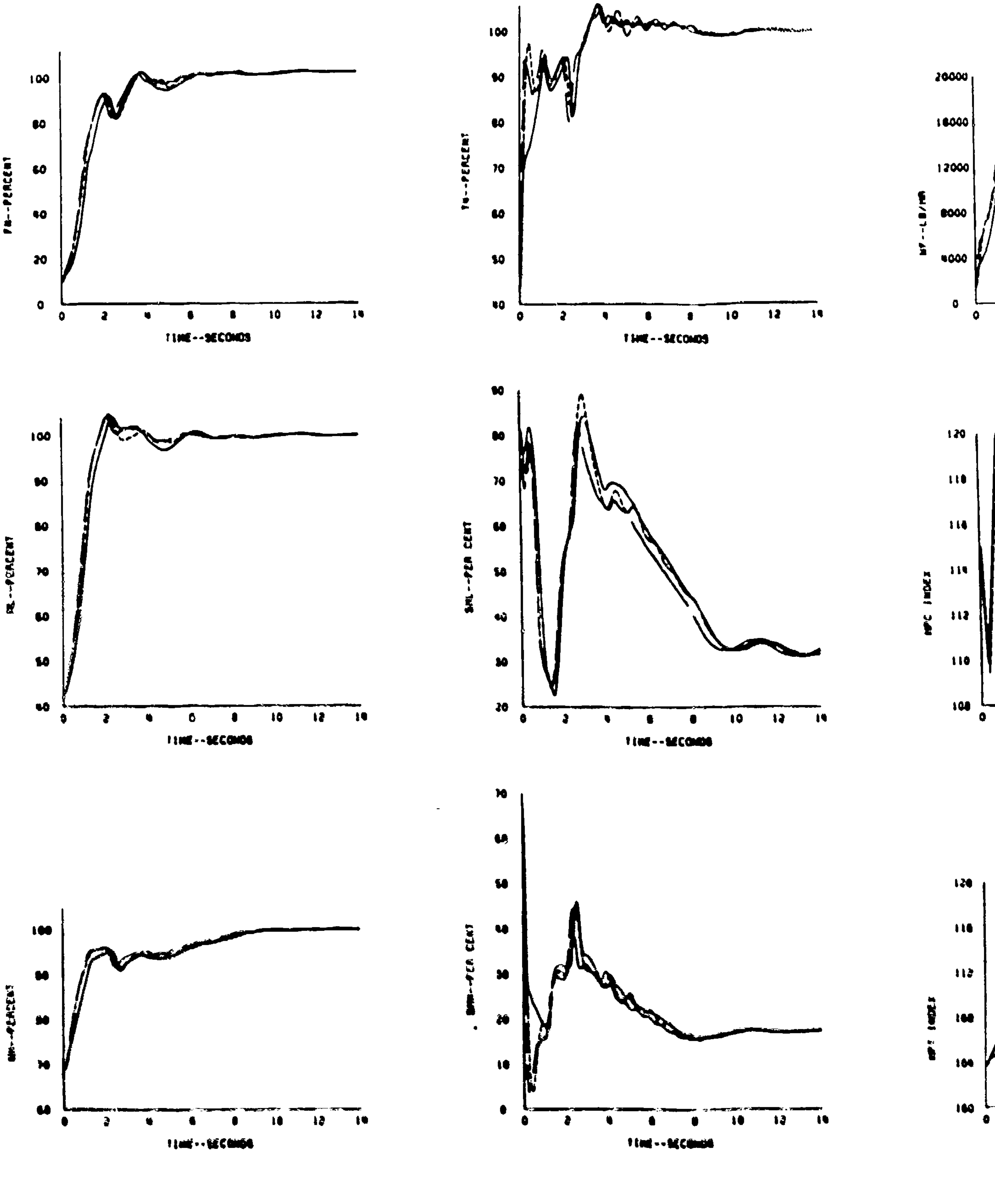


Figure 7-14 Governor Integrator Limiting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TIME--SECONDS
TIME--SECONDS
TIME--SECONDS
TIME--SECONDS
TIME--SECONDS

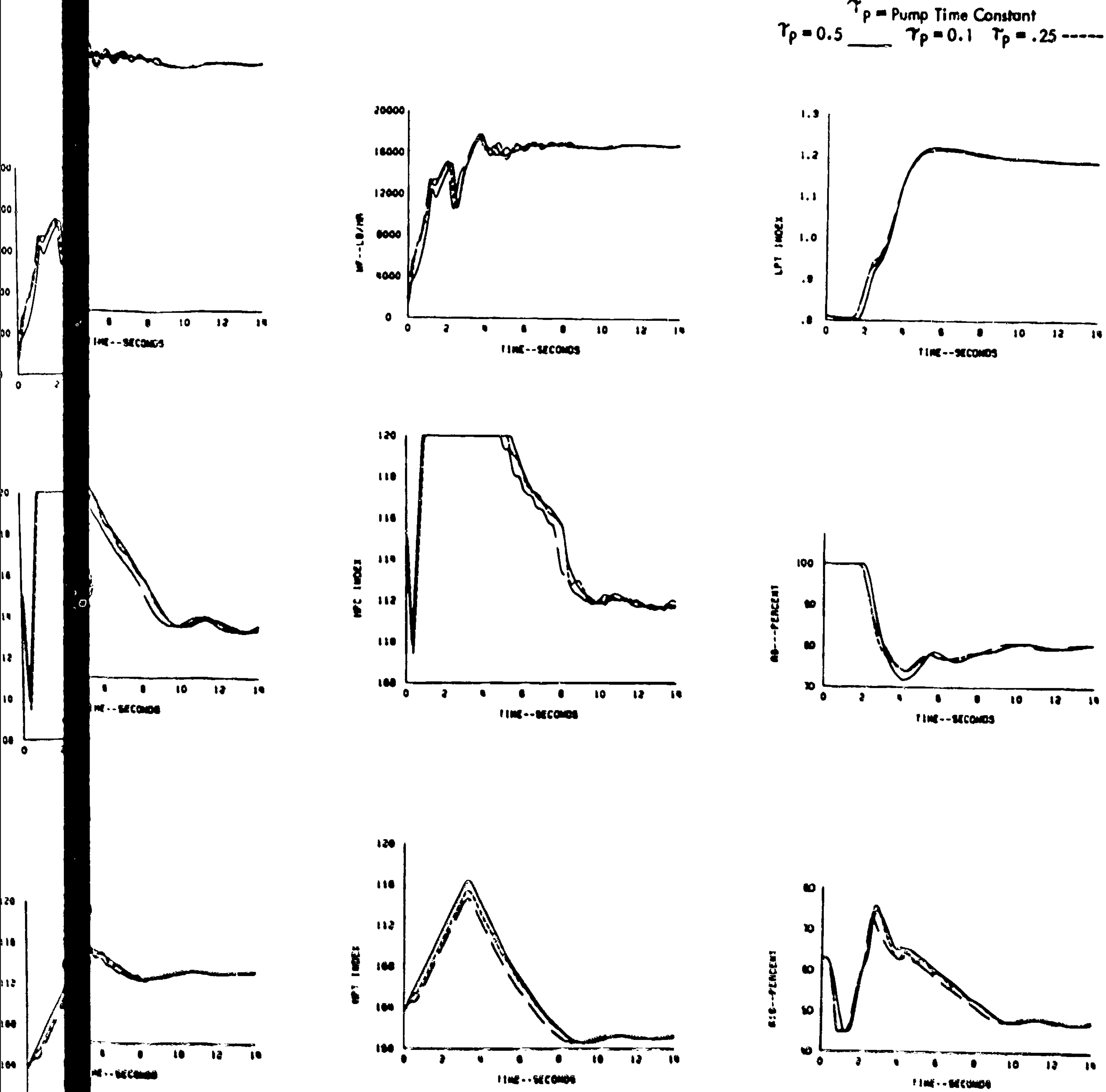
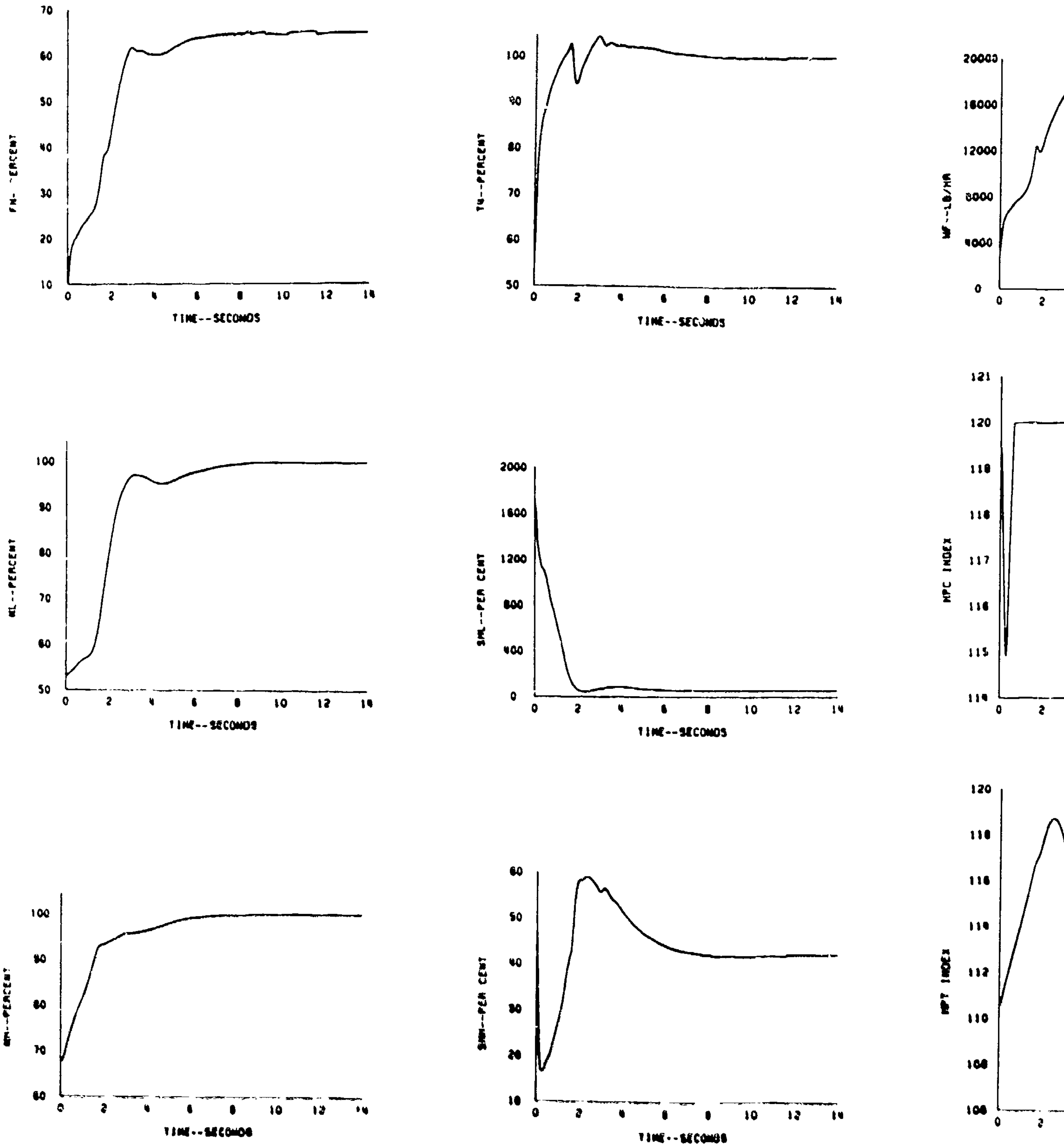


Figure 7-15 Comparison of Pump Dynamics at Static Sea Level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T4--PERCENT
WF--LB/HR
NL--PERCENT
SML--PER CENT
HPC INDEX
SMH--PER CENT
HPT INDEX

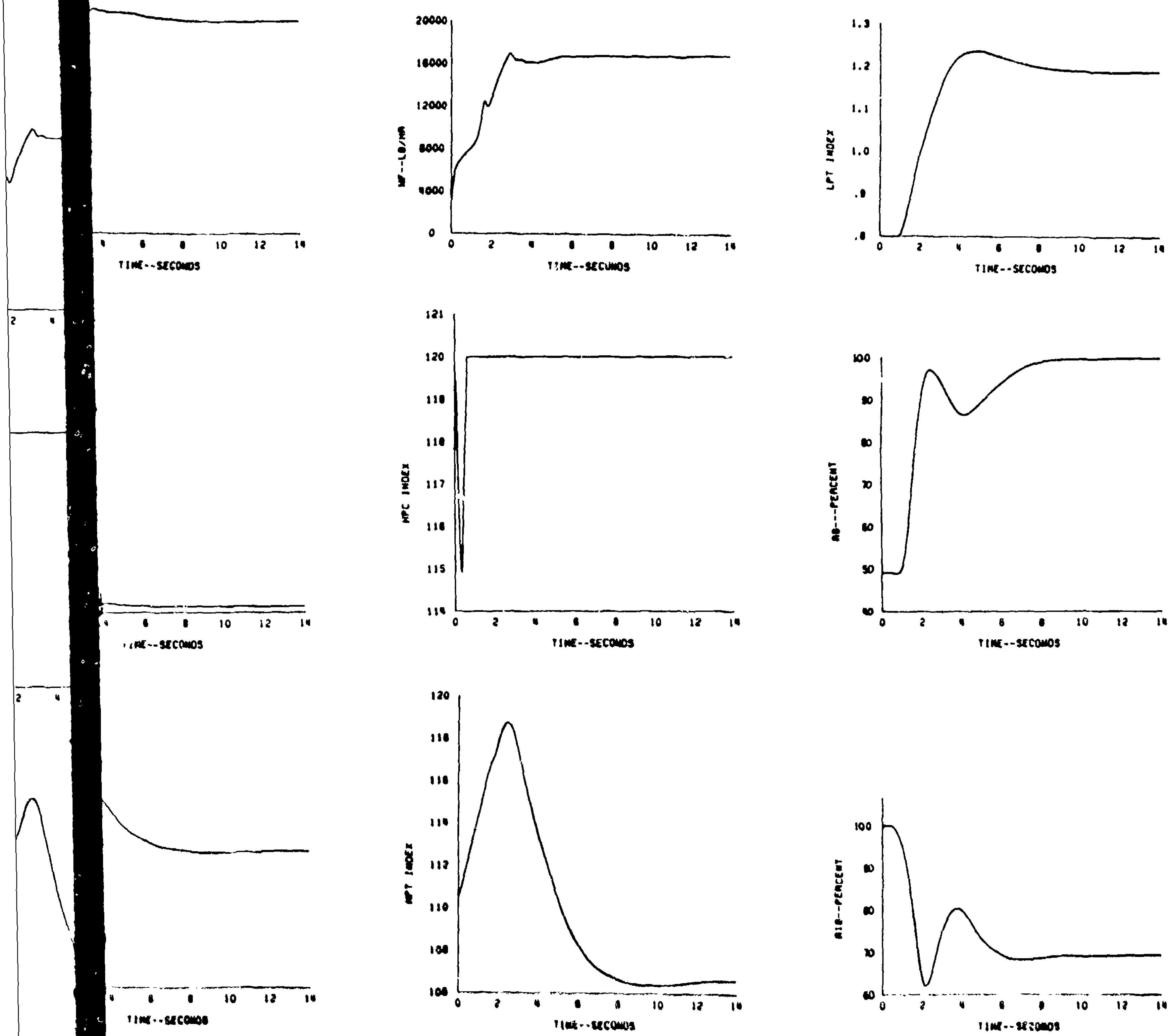
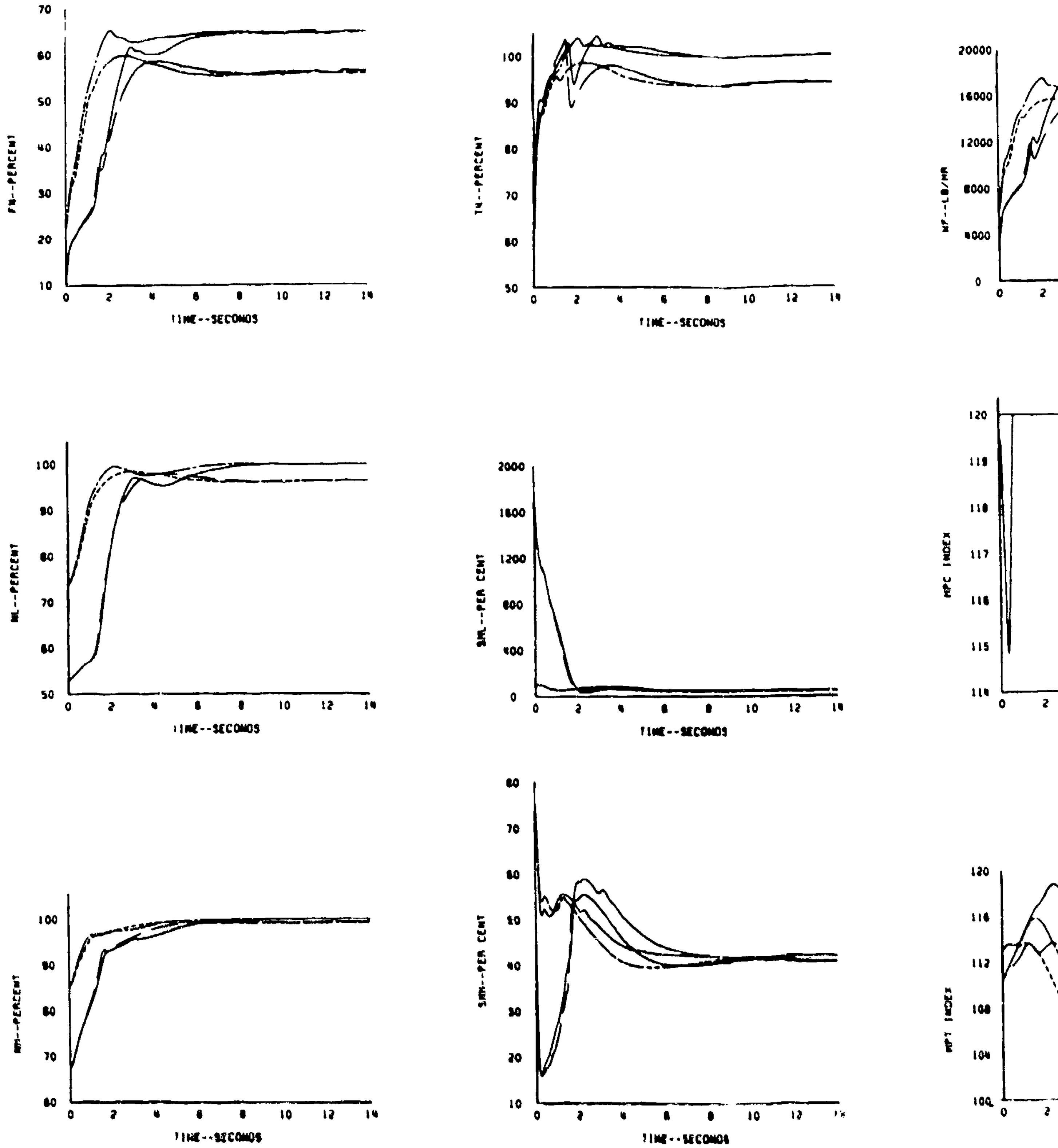
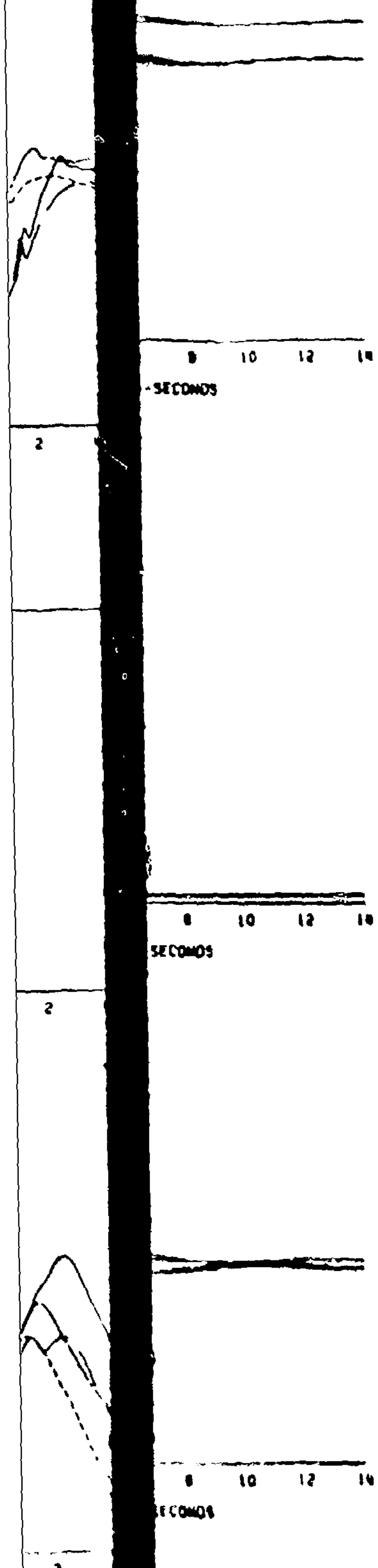


Figure 7-16 Idle to Intermediate Power Acceleration at Mach 2.2 and 36,089 Feet

Figu


FN--PERCENT
TIME--SECONDS
T4--PERCENT
TIME--SECONDS
WF--LB/HR
NL--PERCENT
TIME--SECONDS
SML--PER CENT
TIME--SECONDS
HPC INDEX
NH--PERCENT
TIME--SECONDS
SMH--PER CENT
TIME--SECONDS
HPT INDEX



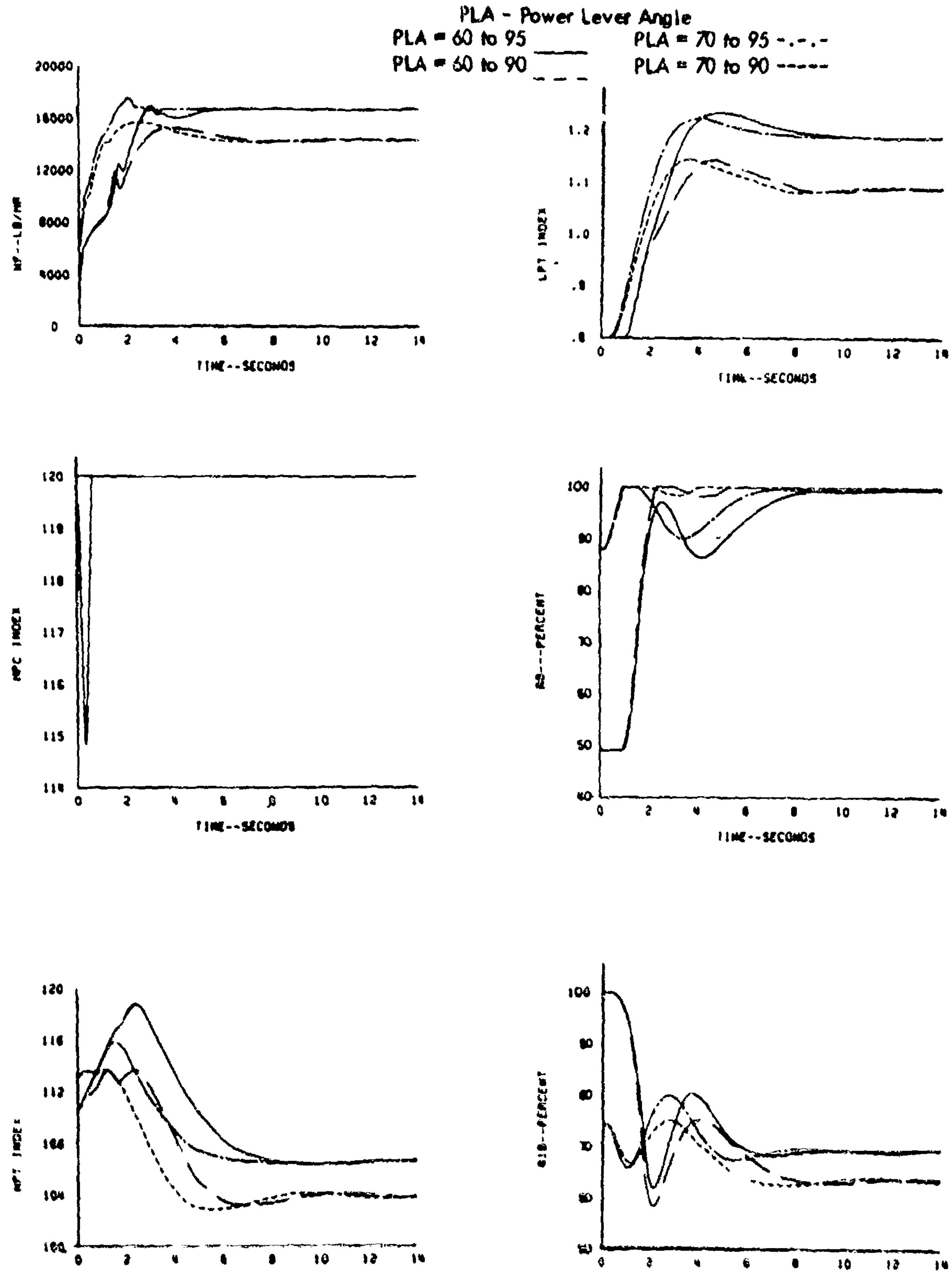
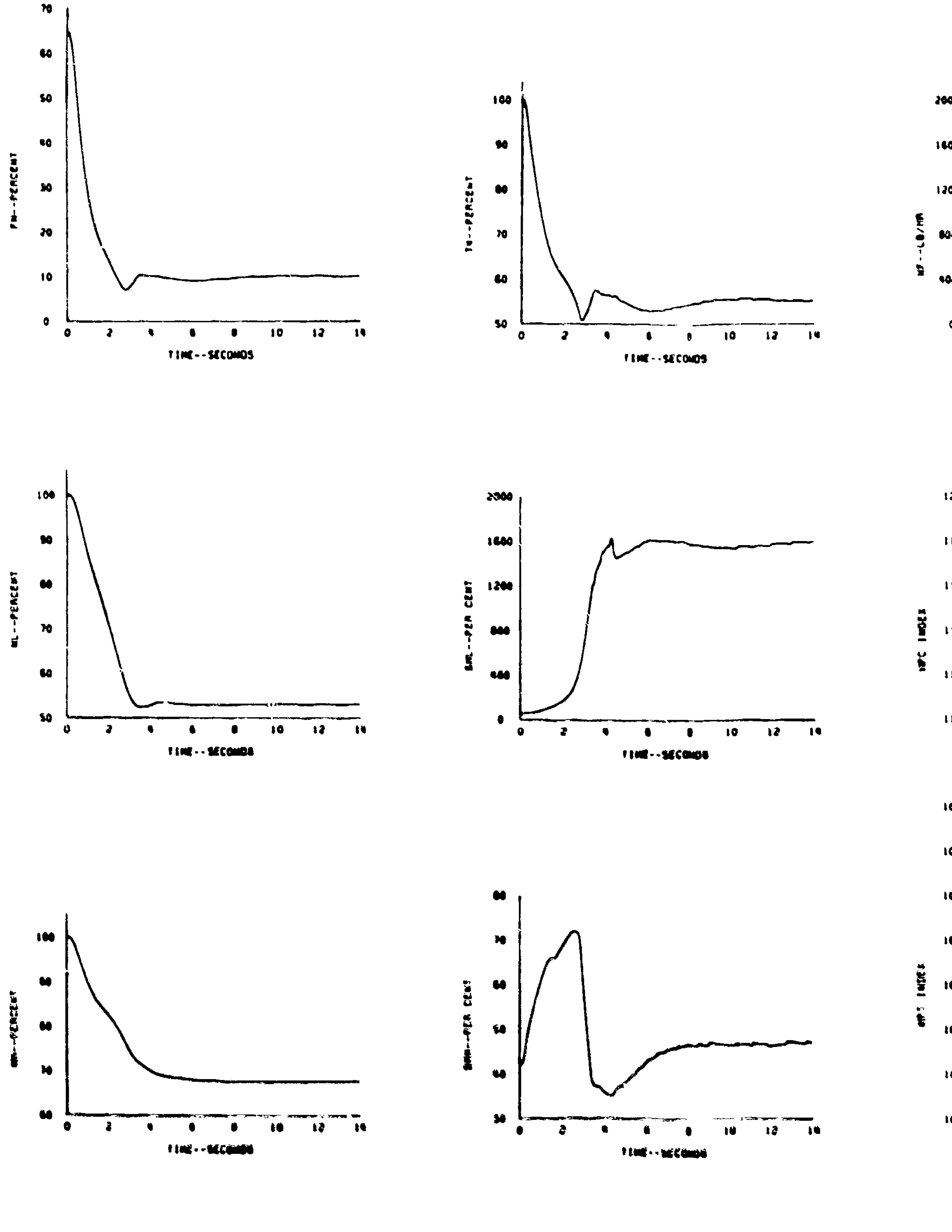


Figure 7-17 Selected Accelerations at Mach 2.2 and 36,089 Feet


TIME--SECONDS

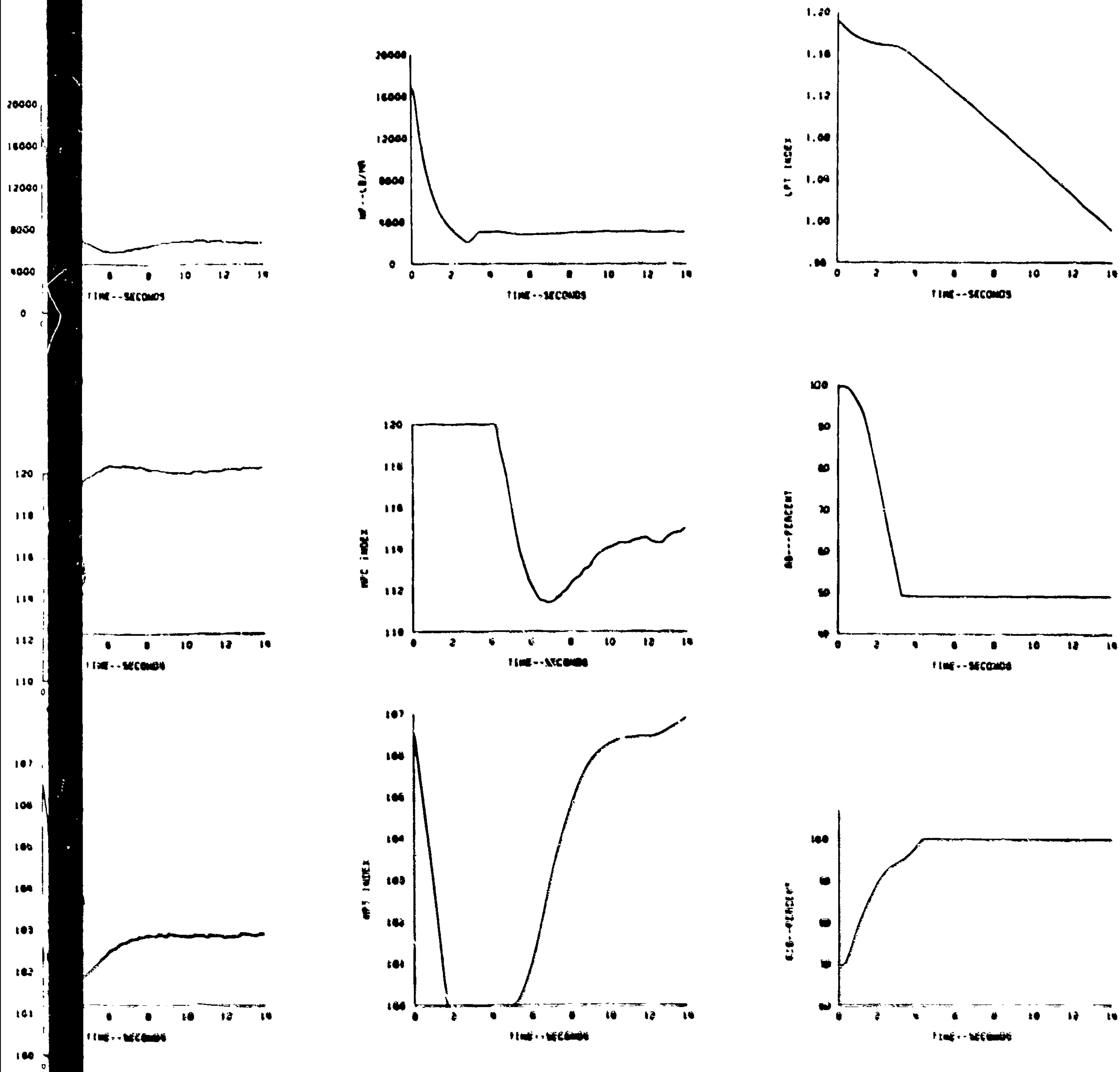


Figure 7-18 Intermediate Power to Idle Deceleration at Mach 2.2 and 36,089 Feet

times. The fact that the primary nozzle is already at the minimum stop and an appreciable error in NL for the A8 loop exists at idle forces the primary to remain at the minimum during the initial record of the transient. This retards the acceleration of the fan (NL) and keeps the fuel governor off the NL limiter and produces a better transient response. This information may be useful in future studies of the primary nozzle loop at static sea level.

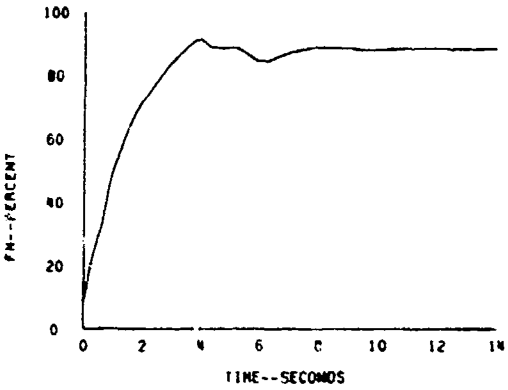
Mach 1.2 at 500 Ft. (152 meters)

The most significant feature of the accelerations was the limiting of the secondary nozzle travel to avoid fan surge. A minimum area of 135 sq. in. for the duct nozzle was established to prevent fan surge. Compressor surge margin is no problem and the acceleration schedule is not enacted during the transient. The only engine limits reached at intermediate power are T4.1 and P3. The HPC controls the compressor discharge pressure accurate enough to prevent any overshoot in P3 and thus the P3 limiter in the fuel control is not exercised. Therefore, only the governor and T4.1 limiter are exercised during the idle to intermediate power acceleration shown in Figure 7-19 and the other accelerations shown in Figure 7-21.

Mach .75 at 36,089 Ft. (11,000 meters)

The cold input was the most challenging of the flight condition selected to establish a workable acceleration schedule as noted in the development of the fuel control. It was also necessary to reduce the fuel control gains significantly at this flight condition to achieve stability. (The nonlinear gains might be eliminated by the use of W_f/P_3 instead of W_f in the governor and limiters.) The first problem with the acceleration schedule during an idle to intermediate power acceleration is shown in Figure 7-22 is preserving adequate surge margin. On the governor, the surge margins drop from near 30 percent to about 10 percent in less than 100 milliseconds before the acceleration schedule takes control. This rapid decrease in surge margin is too fast for the present $\Delta P/P$ surge control to compensate. Thus, either a very fast $\Delta P/P$ loop or an acceleration schedule is required at this flight condition. The lower gains and more stringent acceleration schedule required to maintain surge margin make the initial acceleration very slow compared to the other flight conditions. First, the corrected NL limiter and later the T4.1 limiter reduce the fuel flow which create two reductions in NH and a corresponding drop in the W_f/P_3 permitted by the acceleration schedule. If this drop is too great, the speed continues to decrease and the engine decelerates instead of accelerating. The deceleration is aided by the opening of the HPT because T4.1 decreases. Thus the acceleration schedule must be low enough to prevent surge, but not too low to prevent full acceleration of the engine. It may be advisable to sacrifice optimal steady state performance and establish a new operating line to allow more flexibility in the acceleration schedule.

A series of acceleration are shown in Figure 7-23 that display the same characteristics discussed above. The deceleration shown in Figure 7-24 indicates no problems in this transient mode.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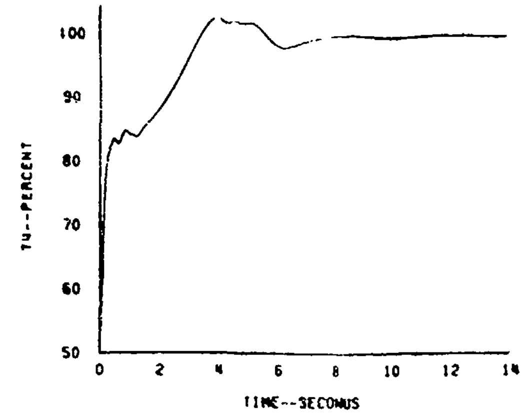
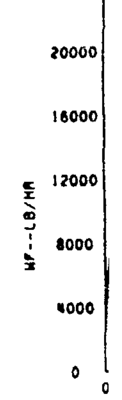
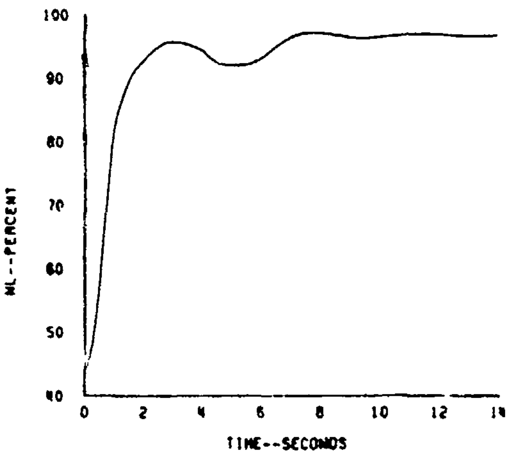
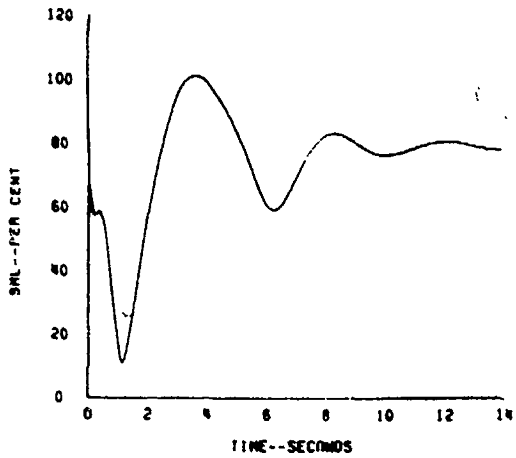
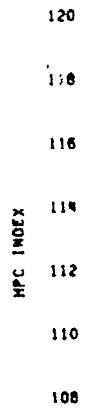
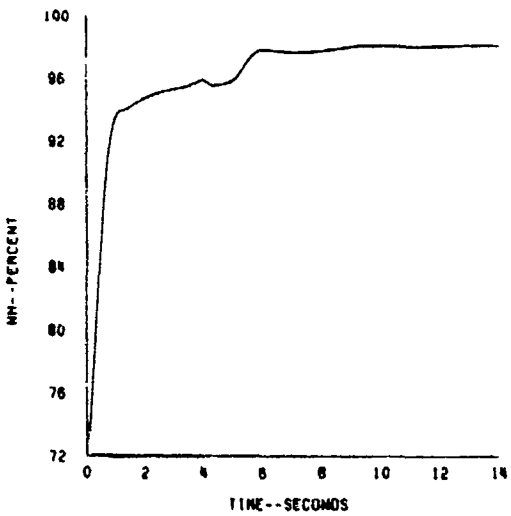

T4--PERCENT
TIME--SECONDS

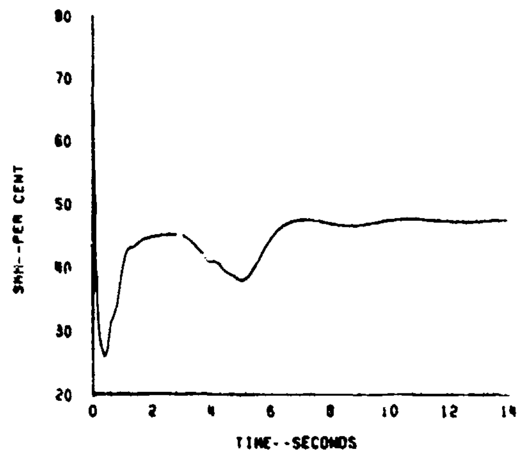

WF--LB/HR

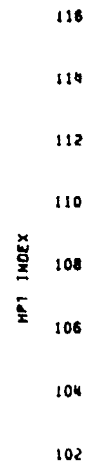

NL--PERCENT
TIME--SECONDS


SML--PER CENT
TIME--SECONDS


HPC INDEX


NH--PERCENT
TIME--SECONDS


SMH--PER CENT
TIME--SECONDS


HPT INDEX

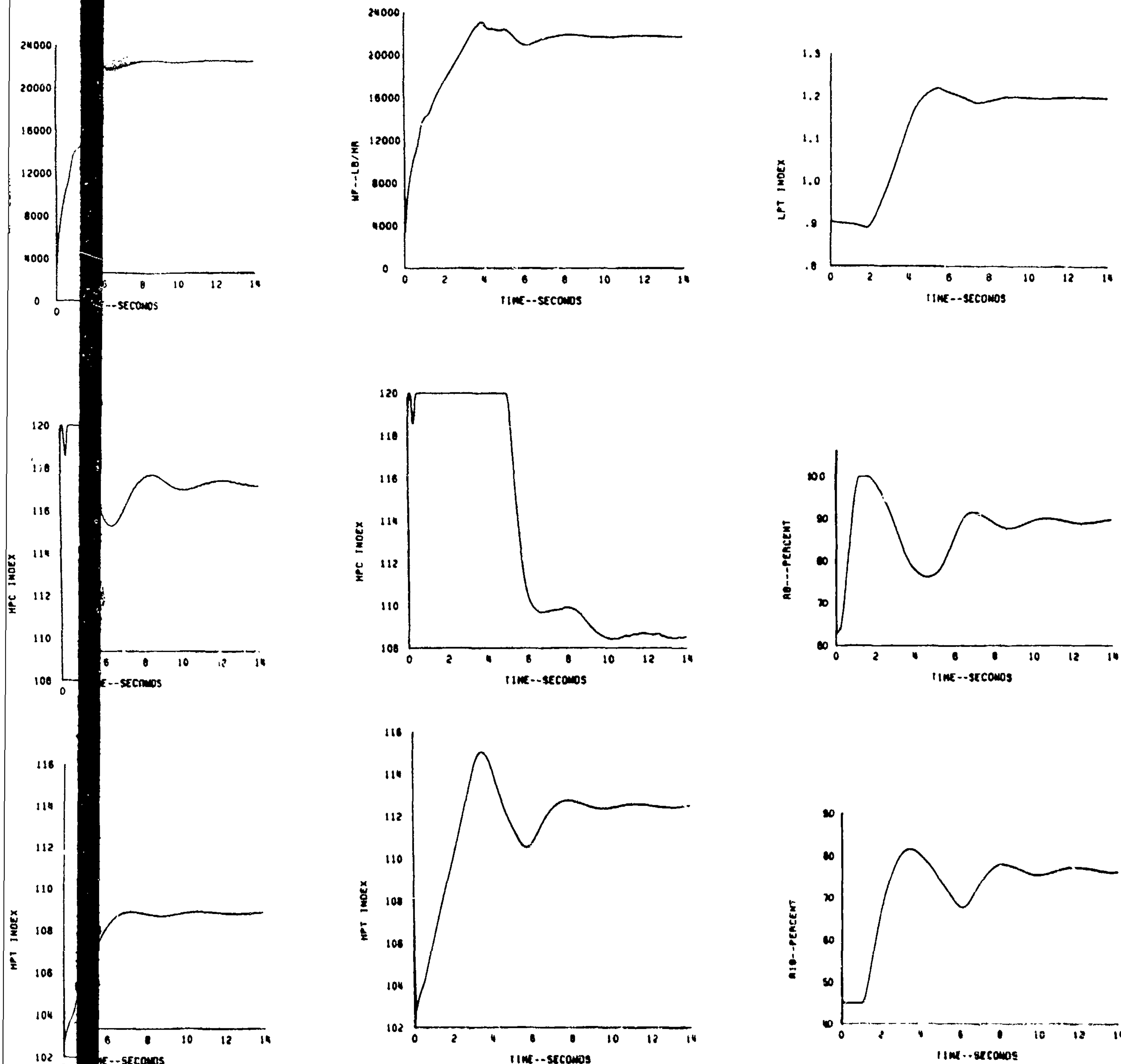
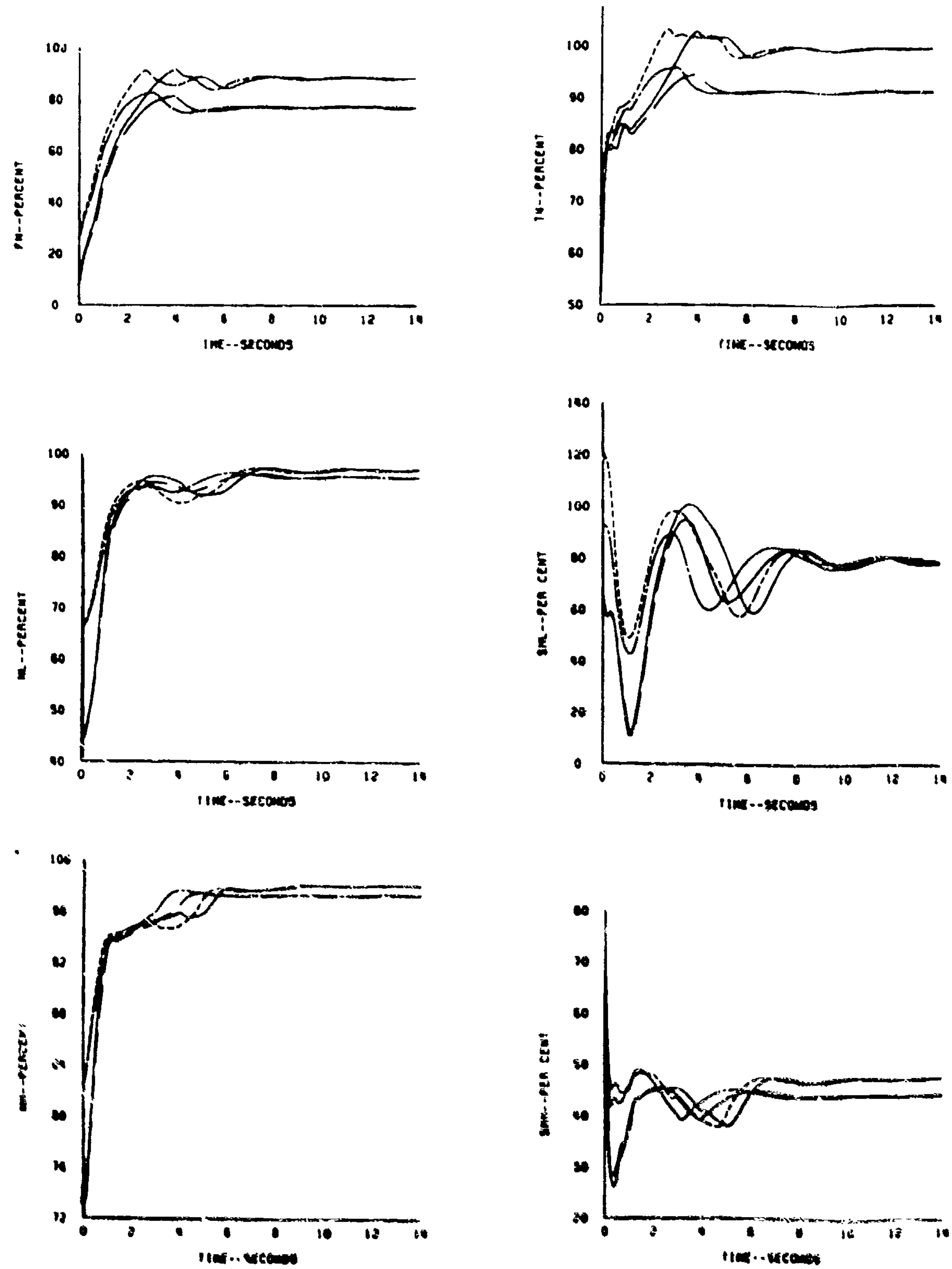


Figure 7-19 Idle to Intermediate Power Acceleration
at Mach 1.2 and 500 Feet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TIME--SECONDS
NL--PERCENT
TIME--SECONDS
TIME--SECONDS
SML--PER CENT
TIME--SECONDS
SMH--PER CENT
TIME--SECONDS

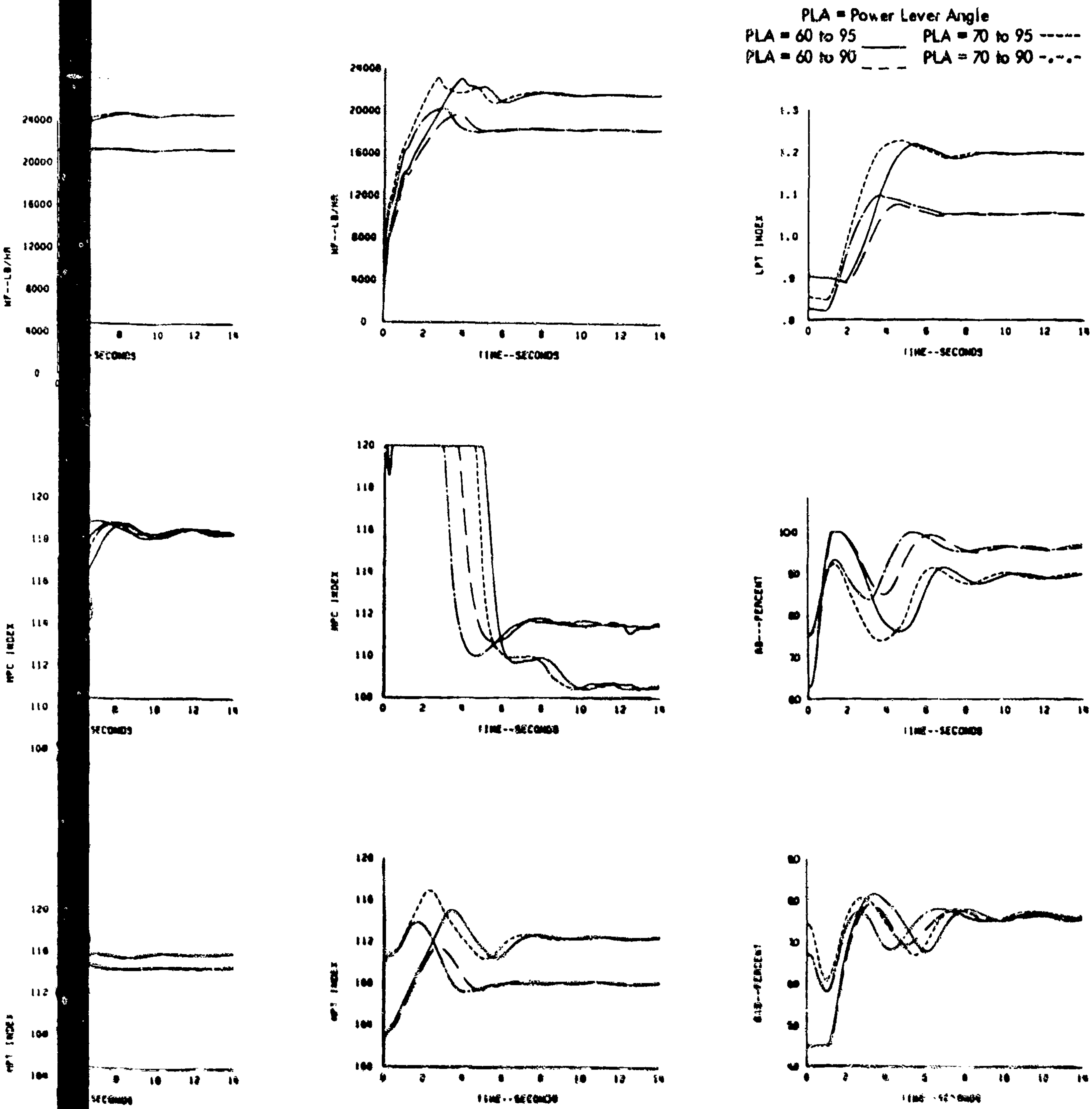
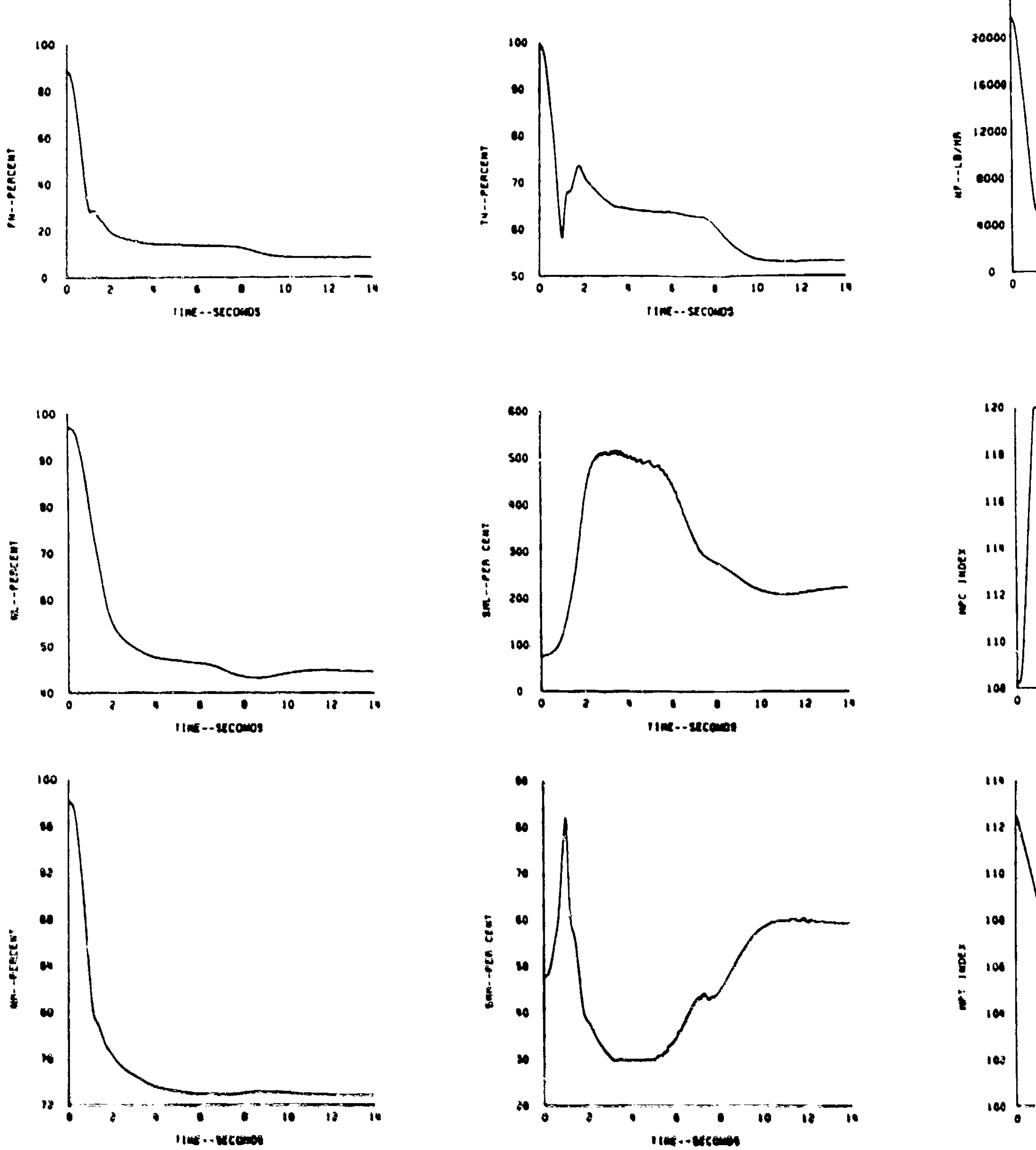


Figure 7-20 Selected Accelerations at Mach 1.2 and 500 Feet



FN--PERCENT
TIME--SECONDS
T4--PERCENT
TIME--SECONDS
WL--PERCENT
TIME--SECONDS
SML--PER CENT
TIME--SECONDS
NPC INDEX
TIME--SECONDS
SMH--PER CENT
TIME--SECONDS
NPT INDEX

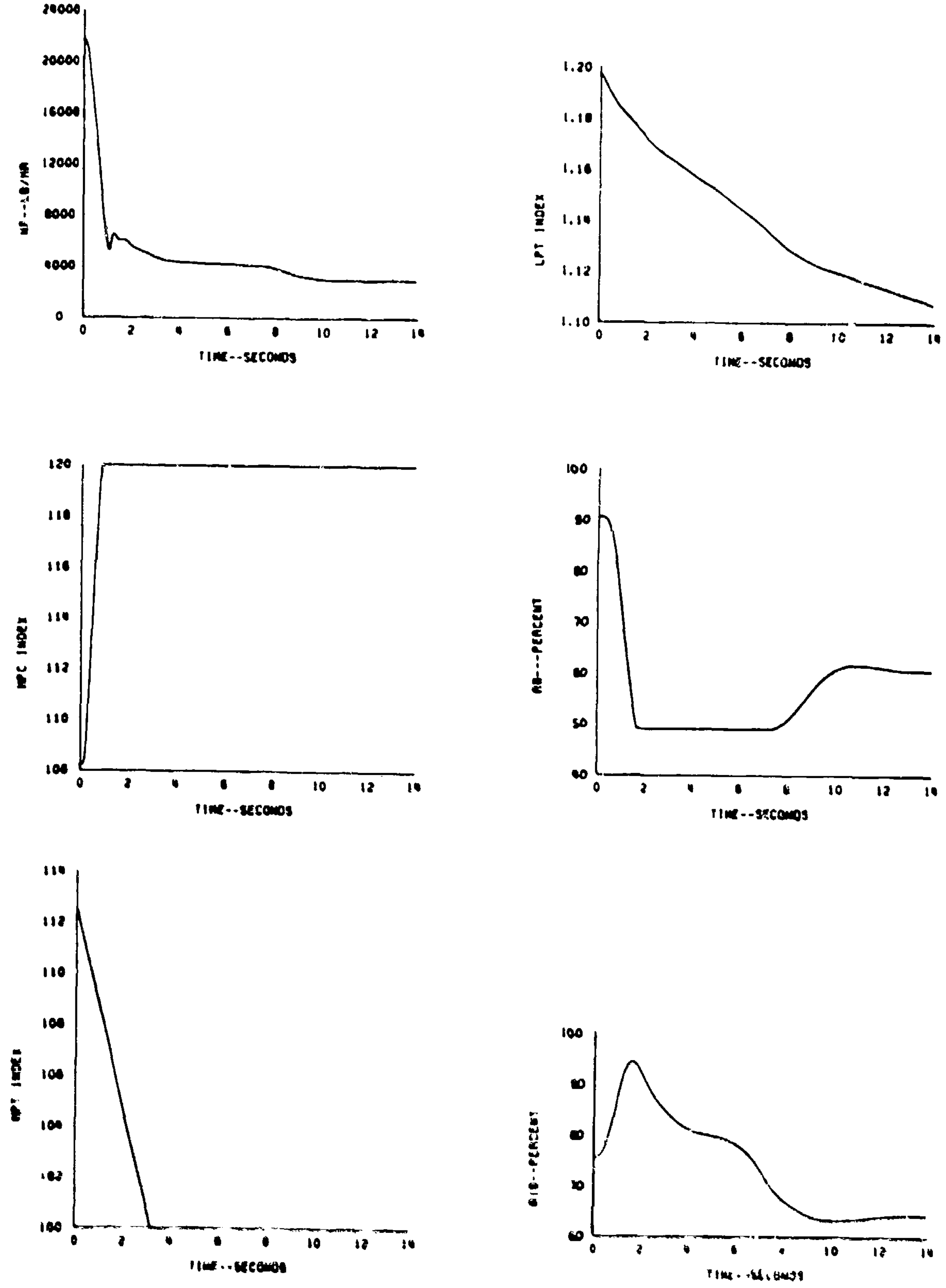
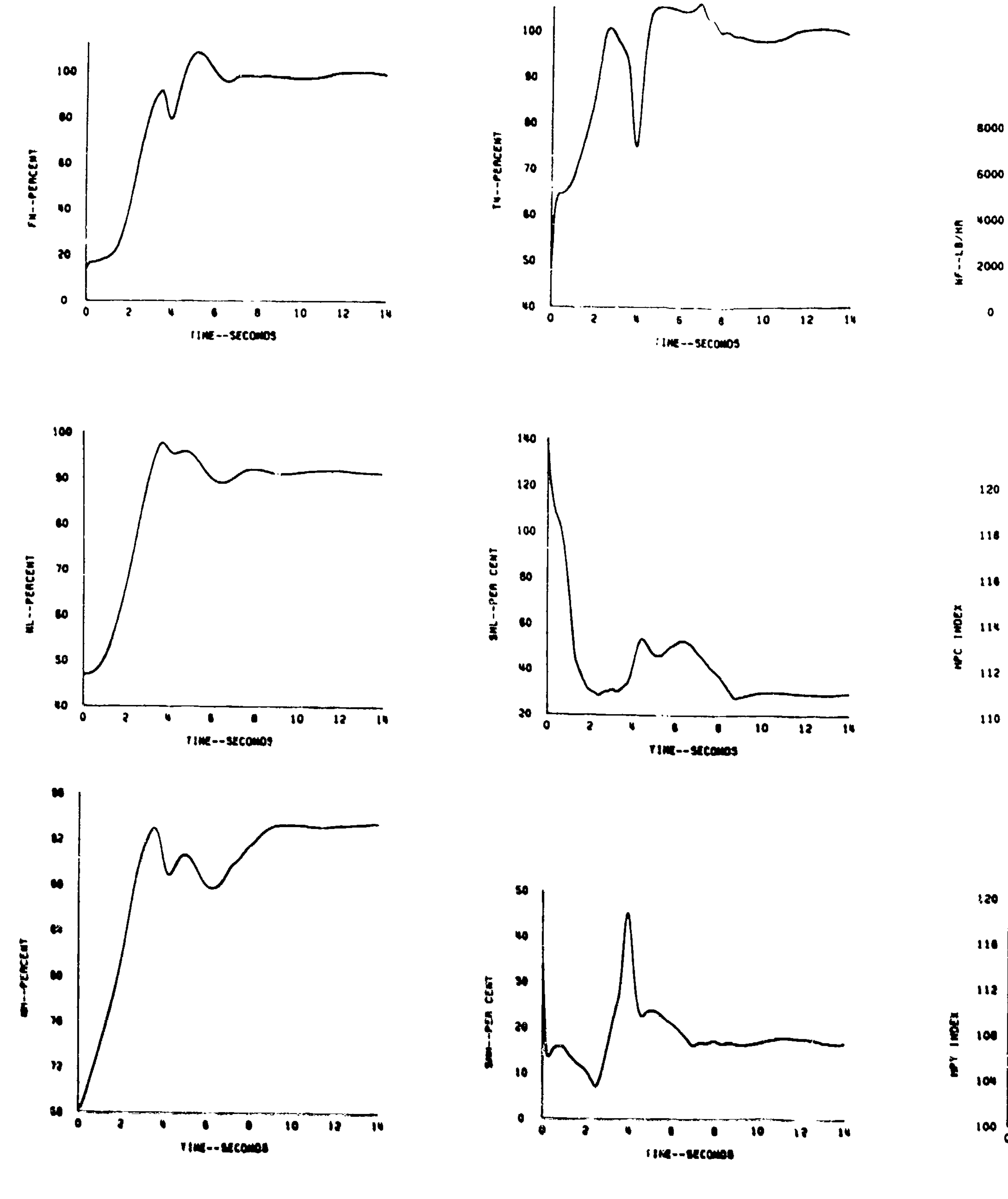


Figure 7-21 Intermediate Power to Idle Deceleration at Mach 1.2 and 500 Feet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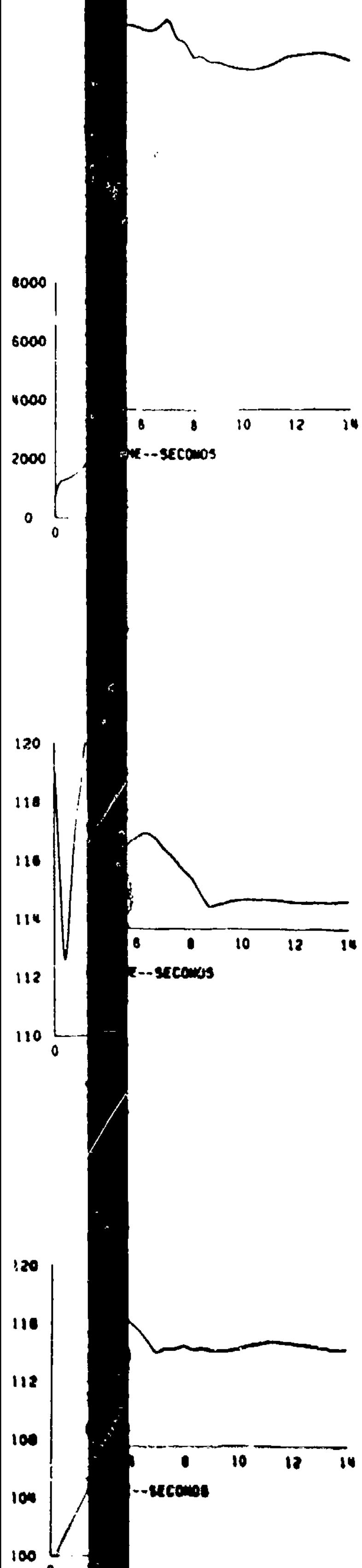
WF--LB/HR
NL--PERCENT
SML--PER CENT
NPC INDEX
SMH--PER CENT
NPY INDEX
TIME--SECONDS



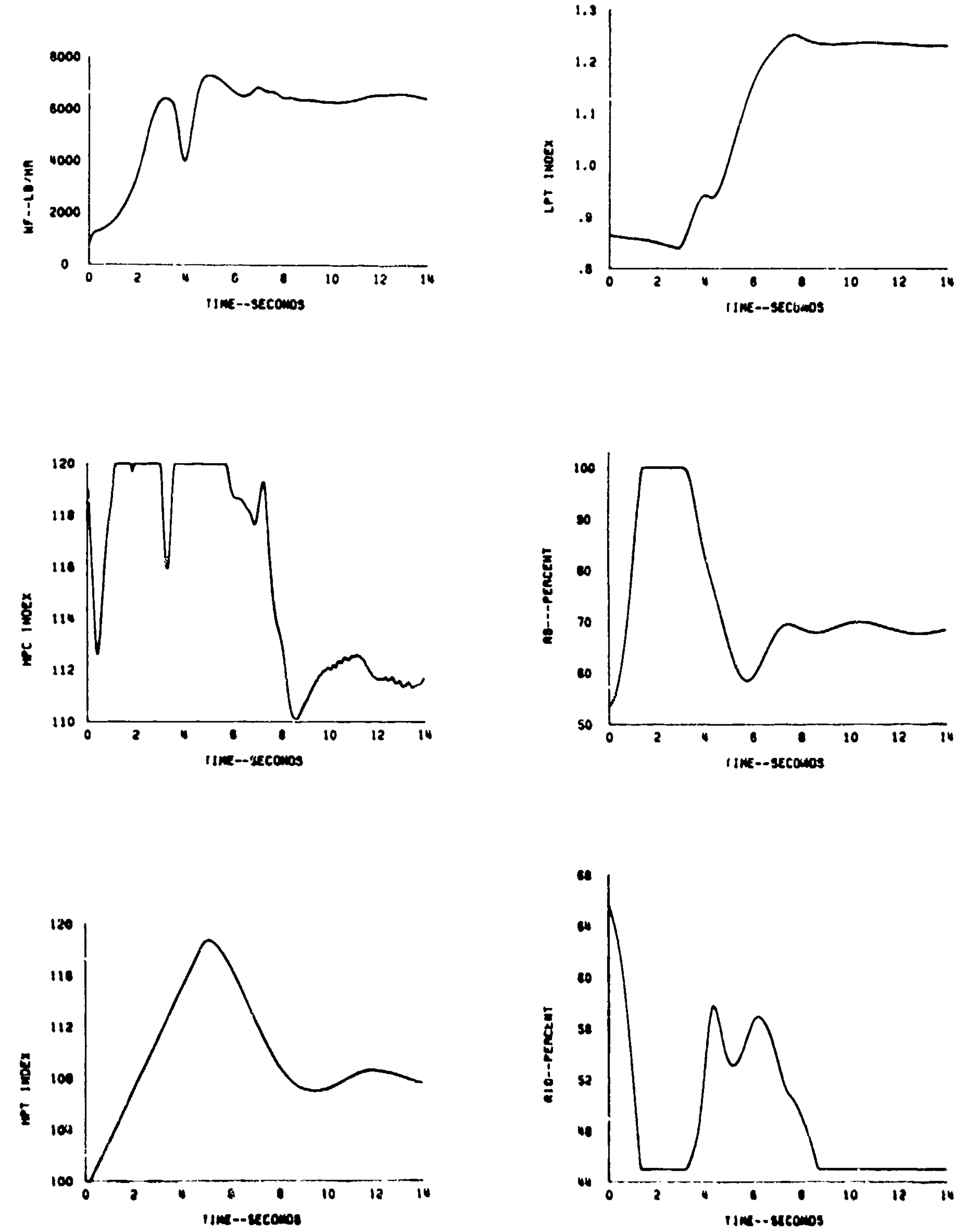
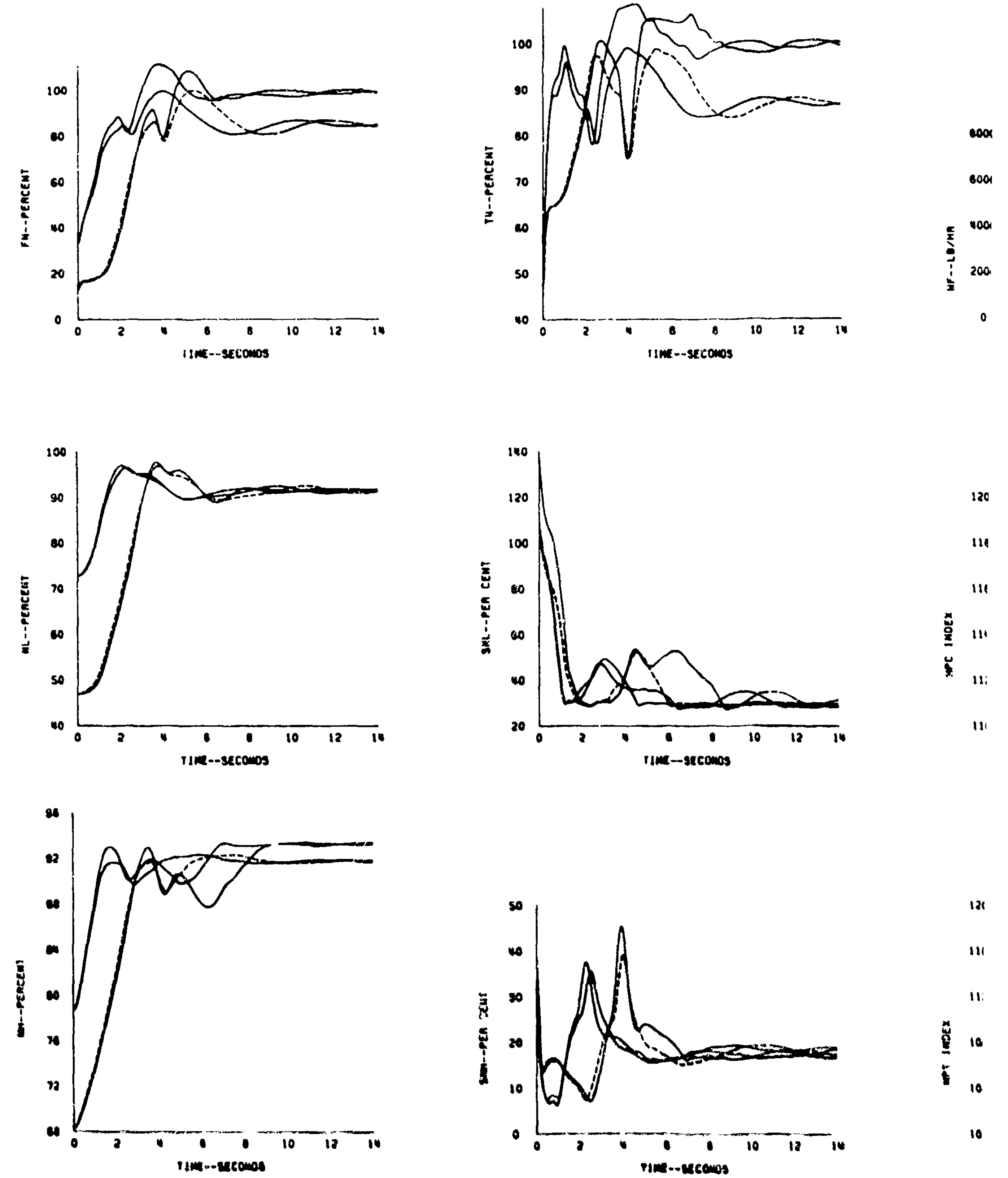


Figure 7-22 Idle to Intermediate Power at Mach 0.75 and 36,089 Feet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TIME--SECONDS
TN--PERCENT
TIME--SECONDS
WF--LB/HR
NL--PERCENT
TIME--SECONDS
SML--PER CENT
TIME--SECONDS
NH--PERCENT
TIME--SECONDS
SMH--PER CENT
TIME--SECONDS
HPT INDEX

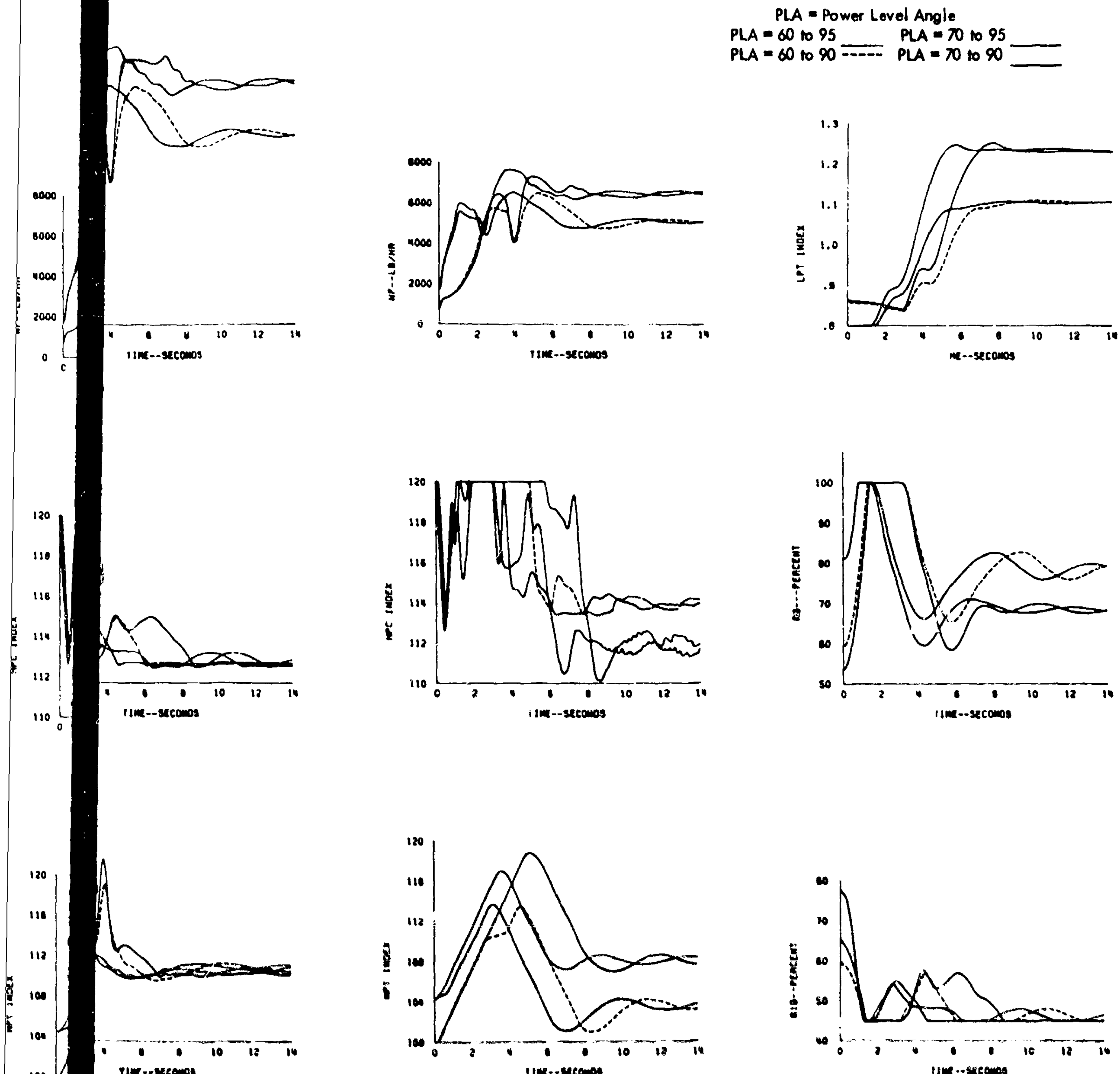
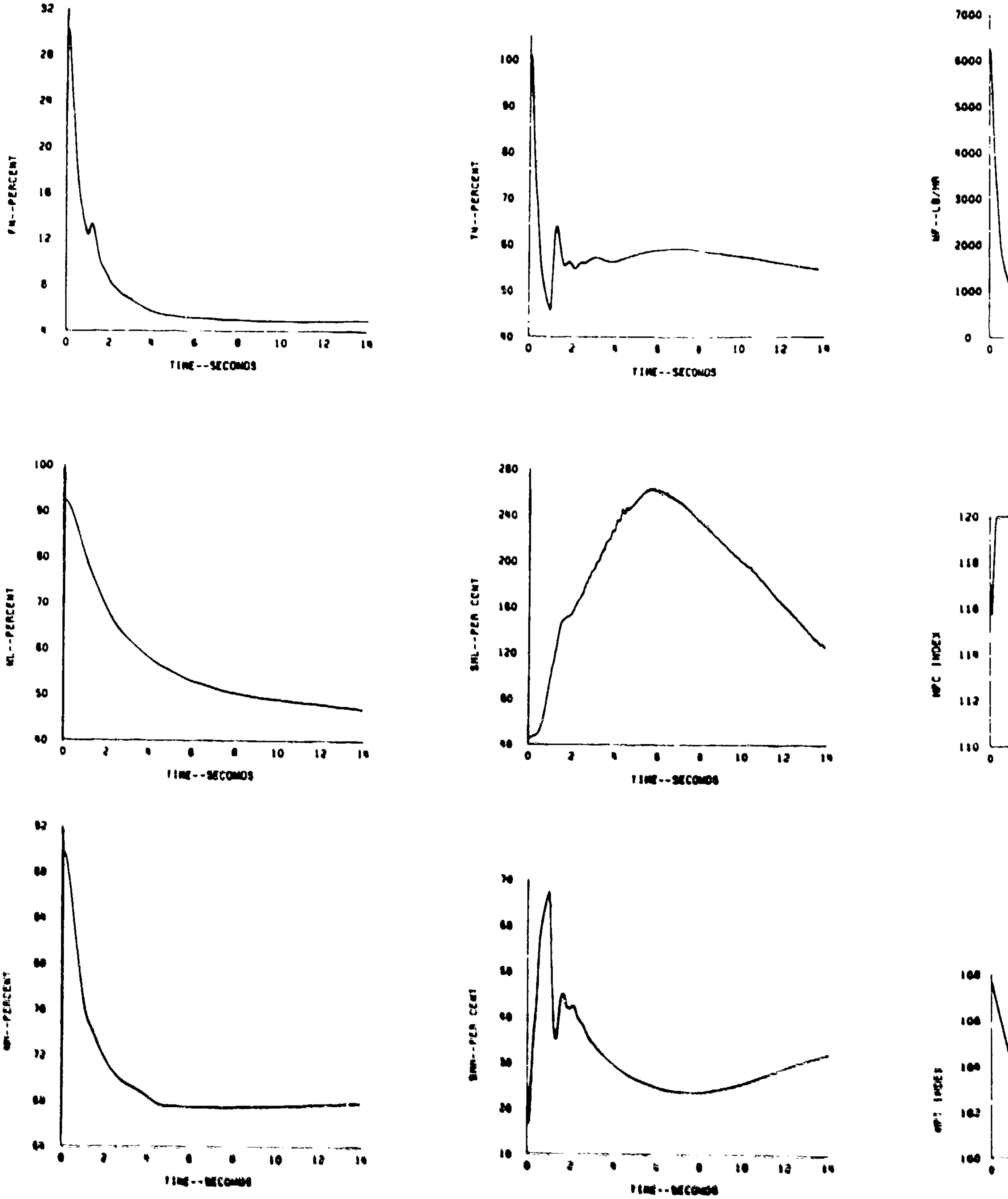


Figure 7-23 Selected Accelerations at Mach 0.75 and 36,089 Feet


FN--PERCENT
TIME--SECONDS
T4--PERCENT
TIME--SECONDS
WF--LB/HR
NL--PERCENT
TIME--SECONDS
SML--PER CENT
TIME--SECONDS
HPC INDEX
NH--PERCENT
TIME--SECONDS
SMH--PER CENT
TIME--SECONDS

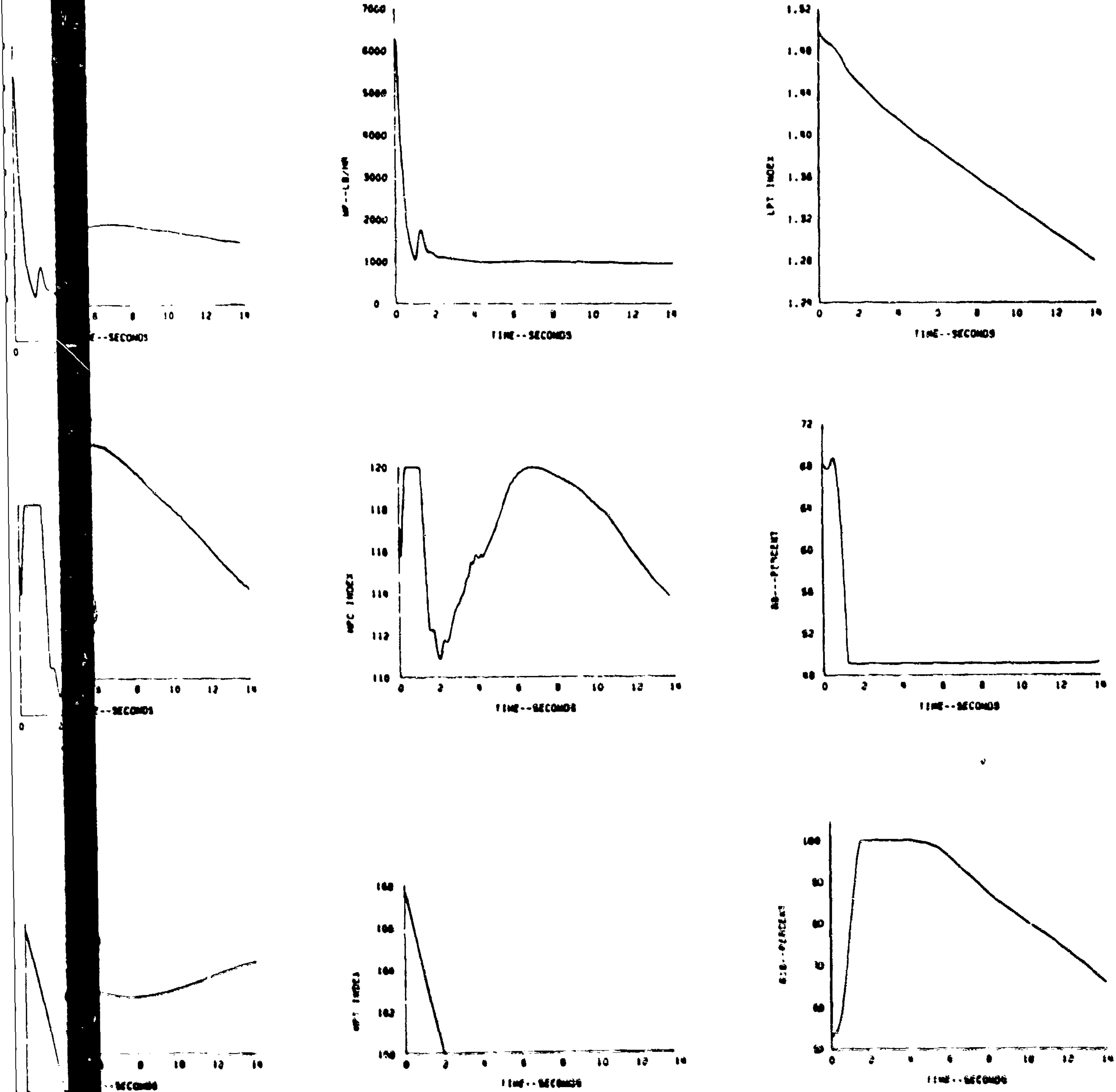


Figure 7-24 Intermediate Power to Idle Deceleration at Mach .75 and 36,089 Feet

8.0 BACKUP CONTROL MODE

In the event of a failure in the primary controller, it is desirable that the engine control automatically revert to a secondary or backup control. For economy and reliability, it is desirable to keep the backup control as simple as possible. Two different criteria were considered. The simplest would only provide safe operation for a fly-home capability with emphasis on low SFC at cruise condition. The second would provide for at least 90% thrust capability in accordance with MIL-E-5007D.

The backup system is simplified by reducing the number of controlled geometry variables to a minimum and fixing the remaining geometry in a fail-safe position. The electrical/mechanical design of such geometry would automatically provide fail-safe operation by automatically positioning the geometry in the desired position when the primary controller fails. Potential failures other than the primary controller (i.e., actuators, pump, etc.) would also have to be considered in accordance with the probability of such a failure.

The high pressure compressor (HPC) is the only geometry that must be variable by design since the basic surge margin of the HPC is obtained by varying the vanes. All other geometry can remain fixed with different effects on the transient response and maximum thrust of the engine. The following two cases were studied.

- o Nominal Performance Fixed Geometry

Fan	= 100 index
HPC surge control	= Varied vs corrected speed per design
HPC flow control	= 120 index
HPT	= 100 index
LPT	= Maximum cooling air requirement

Primary Nozzle = 180 square inches

Secondary Nozzle = 220 square inches

- Maximum Thrust Fixed Geometry

Fan = 100 index

HPC surge control = varied vs corrected speed per design

HPC flow control = 112 index

HPT position = 102 index

LPT position = Maximum cooling air requirement

Primary nozzle = 230 square inches

Secondary nozzle = 135 square inches

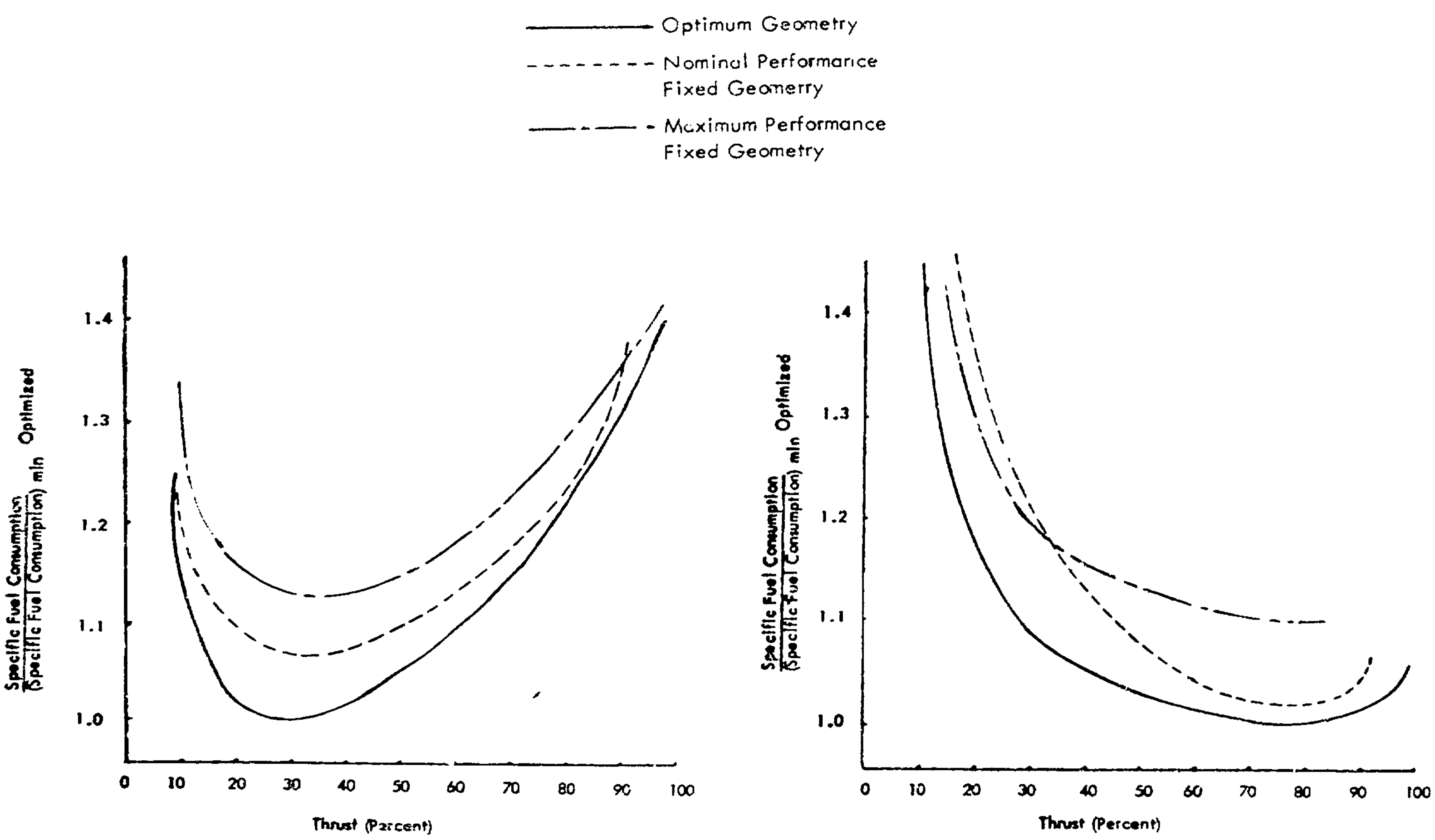
The nominal performance fixed geometry represents the average or mean value of the various geometry positions observed over the thrust range for static sea level conditions. The one exception is the geometry setting for the low pressure turbine (LPT) which was set to the maximum turbine temperature cooling air requirement to protect the turbine stators. The maximum thrust configuration represents the geometry requirements for 100% thrust at static sea level (standard day) conditions.

The steady-state response for the following three flight conditions were considered

- Static sea level
- Mach 1.2 at 500 foot altitude
- Mach 2.2 at 36,089 foot altitude

The sfc curves for the first two cases are shown in Figure 8-1.

The following conclusions were drawn for the nominal performance fixed geometry:



a) S.S.L.

b) Mach 1.2/500'

Figure 8-1 SFC Comparison for Fixed and Variable Geometry

- Approximately 90% of maximum thrust was obtained at static sea level.
- Utilization of nominal fixed geometry resulted in a 5% sacrifice in sfc in the mid-thrust range at static sea level conditions.
- A higher thrust level was achieved at Mach 1.2 and 500 foot altitude with this configuration than with the maximum thrust configuration defined below.
- The effect on sfc was acceptable in the upper thrust range with this configuration at Mach 1.2 at 500 feet.

The following conclusions were drawn for the maximum thrust configuration:

- Maximum thrust was achieved at static sea level.
- The SFC at static sea level was increased by 12% in the mid-thrust range with this configuration.
- Only 85% of maximum thrust was obtained at Mach 2.2 and 36,087 foot altitude.
- Fan surge margin was reduced to less than 10% at Mach 1.2 and 500 foot altitude.
- The sfc was degraded by 10% with this configuration at Mach 1.2 at 500 feet.

Further study of the data revealed that the major factor in loss of thrust and fan surge margin was the fixed secondary nozzle area. Thus, the secondary nozzle was varied according to the optimal geometry position control laws schedule for the fully variable engine. The nominal performance configuration obtained nearly maximum thrust for static sea level and Mach 1.2 at 500 foot altitude as did the maximum thrust configuration. The fan surge margins for the maximum thrust configuration returned to near the levels obtained with a fully variable geometry engine.

This study indicates that "fly-home" performance can be obtained by fixing all the geometry other than the HPC. However, such a system will probably fall short of the specification requirement of 90% thrust with the backup control over the entire flight envelope. To achieve maximum or near maximum dry thrust over the entire flight envelope and acceptable fan surge margin, the HPC and secondary (duct) nozzle must be variable.

Backup System

To continue the development of a backup control for the JTD, it has been assumed that the requirements of MIL-E-5007D with respect to maximum thrust must be met. Thus, the backup system must provide control of the following:

- HPC surge control geometry
- Duct nozzle area
- Fuel flow

The HPC surge control geometry requirements are dictated by the compressor design. The geometry must be positioned as a function of corrected NH and the "ideal" schedule has been assumed in the generation of the component maps. As far as the JTD simulation is concerned, the control designer has no flexibility in changing the "ideal" schedule.

Several options are available for the control of the secondary or duct nozzle. The primary control mode could be used or the engine could be reoptimized with the other geometry fixed and a new control mode developed. However, a simpler control mode that would achieve nearly optimum thrust was sought to reduce the complexity (and cost) of the backup control. The area could not be scheduled on PLA because different A18 areas are required at intermediate power to achieve at least 90% power. Near maximum thrust can be assured if the duct nozzle area is a function of the flight condition or more practically --inlet temperature. This criteria also eliminates the fan surge problem noted with a fixed duct nozzle. Thus, the duct nozzle is scheduled as a function of inlet temperature to achieve near maximum thrust.

The fuel control will be a major factor in the dynamic response of the engine. A successful manual or backup fuel control utilized on the TF41 engine schedules fuel flow as a function of PLA and P2.1 as shown in Figure 8.2. Operating lines for several flight conditions are plotted on a graph of W_f vs. P2.1. The specification requirement of 90% thrust is achieved by constructing the "schedule" for intermediate thrust (PLA = 95 degrees) that intersects all the operating lines at points representing a thrust above 90% as shown in Figure 8-2. Additional PLA lines are drawn to yield reasonably linear thrust response on the backup control.

Combining the three control loops described above yields the backup control system shown in Figure 8-3. All the other geometry will move to the pre-determined "fail-safe" position at a fixed rate established to achieve a smooth transition to backup. The fail-safe positions and fail rates established in this study are

Geometry	Fail-Safe Position	Fail-Rate
HPC flow	120	10 units/sec
HPT	102	4 units/sec
LPT	1.25	.2 units/sec
A8	260 sq. in.	50 in.^{2}/sec

Figure 8-4 and Figure 8-5 show the transfer from primary control to backup during steady state operation at intermediate power and part-power for sea level static conditions. At intermediate power, the thrust drops approximately

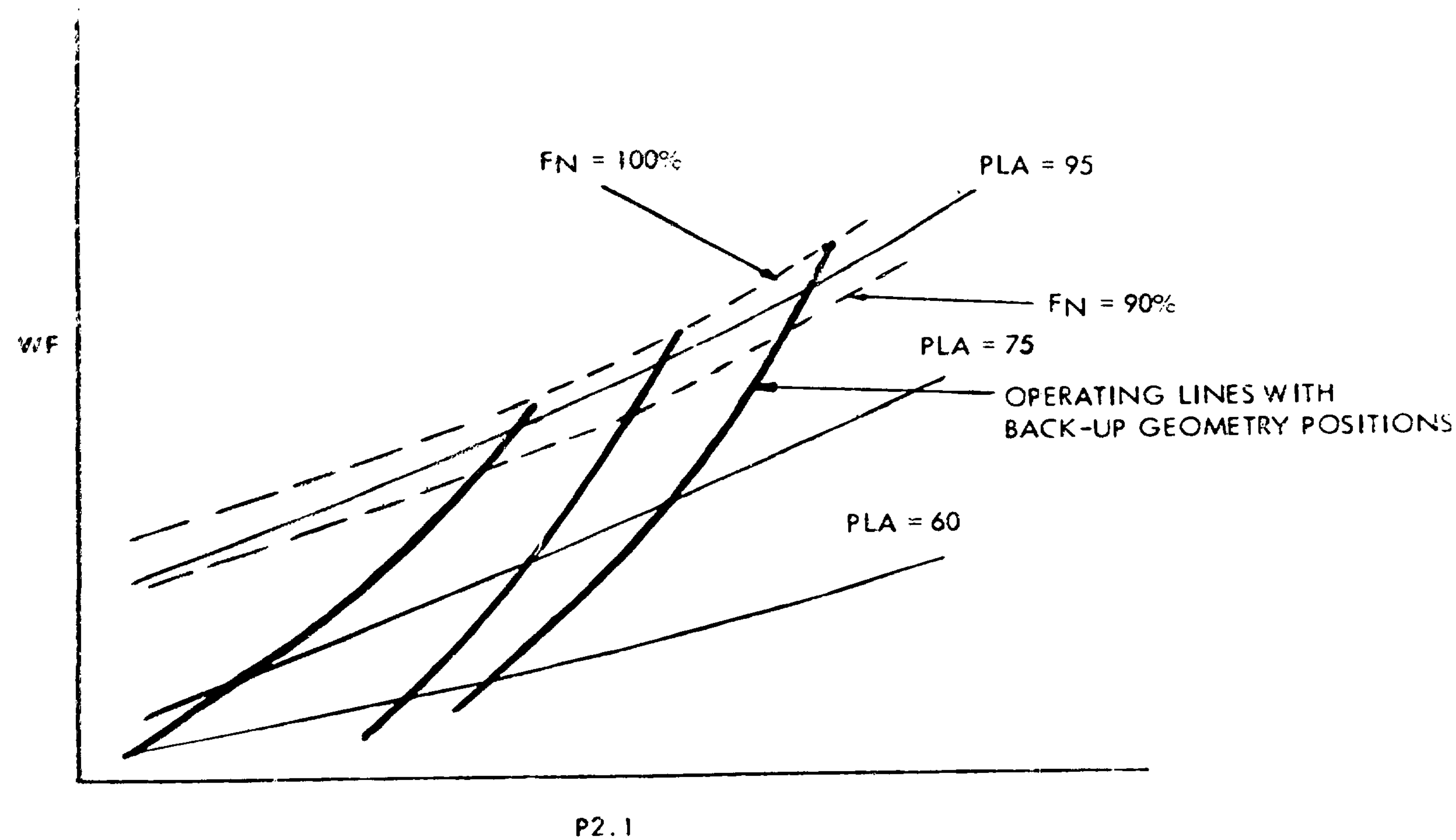


Figure 8-2 Development of Backup Fuel Schedule

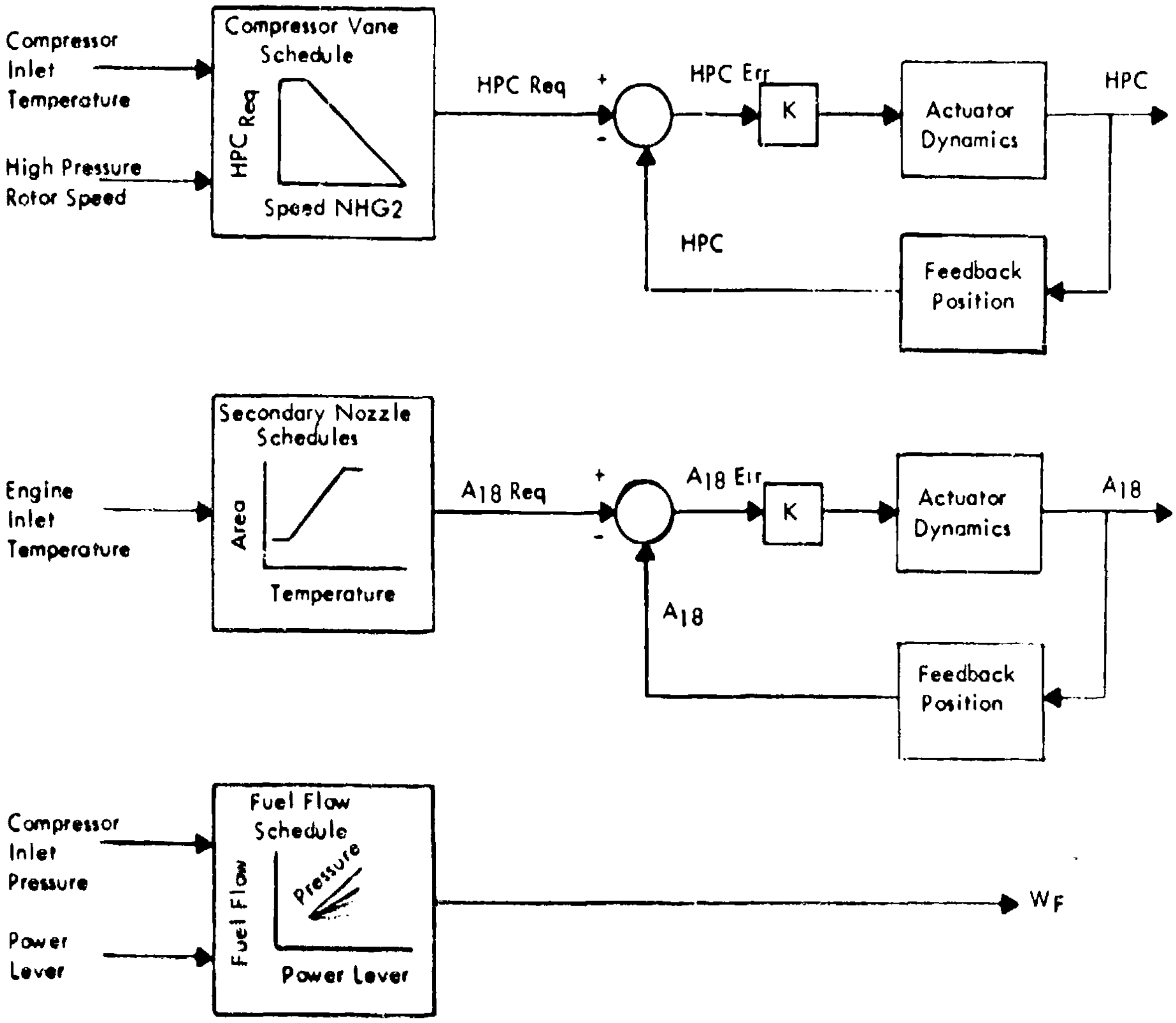
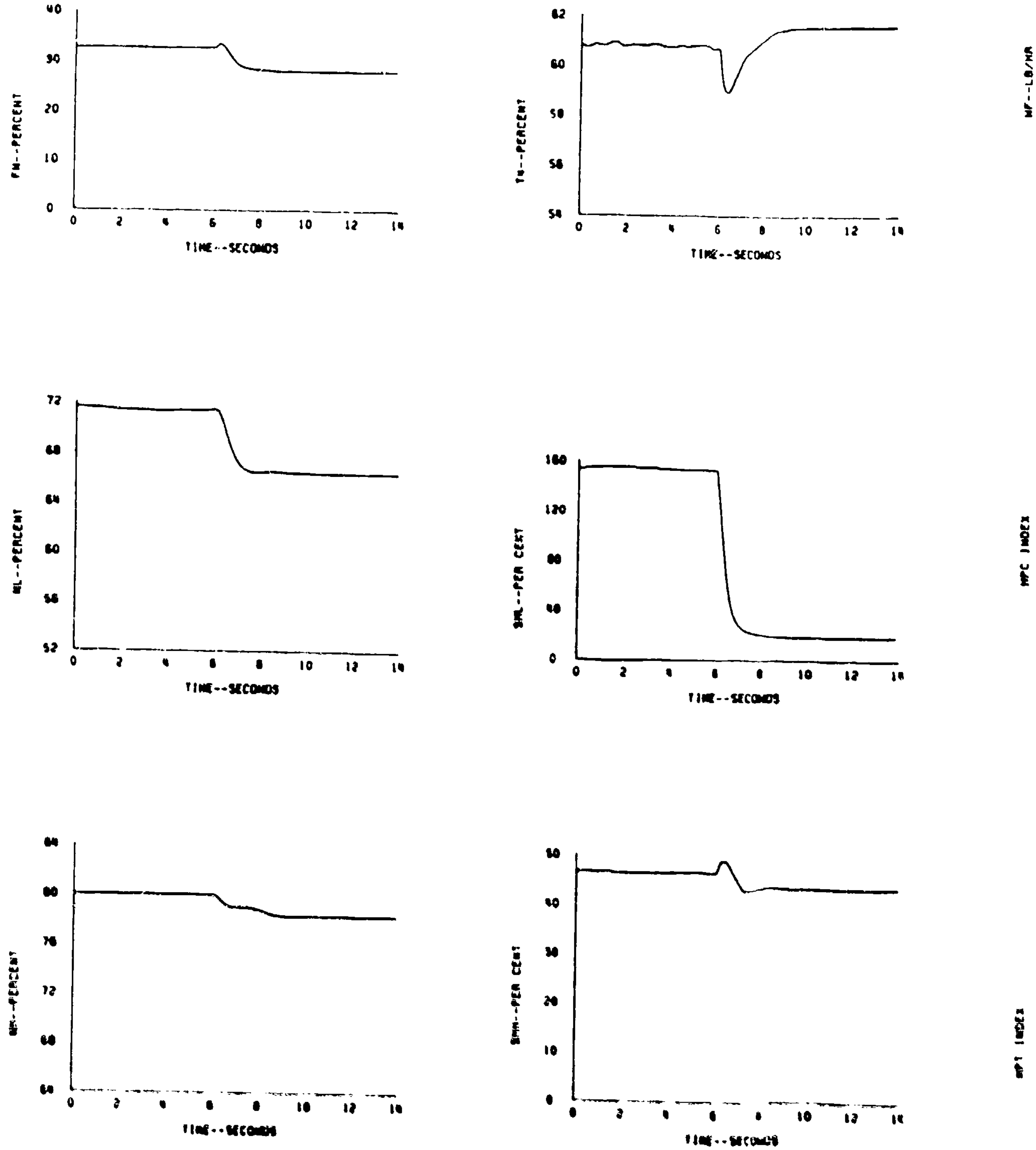


Figure 8-3 Backup Control Mode Diagram


FN--PERCENT
TIME--SECONDS
TN--PERCENT
TIME--SECONDS
NL--PERCENT
TIME--SECONDS
SML--PER CENT
TIME--SECONDS
NH--PERCENT
TIME--SECONDS
SMH--PER CENT
TIME--SECONDS
WF--LB/HR
HPC INDEX
HPT INDEX

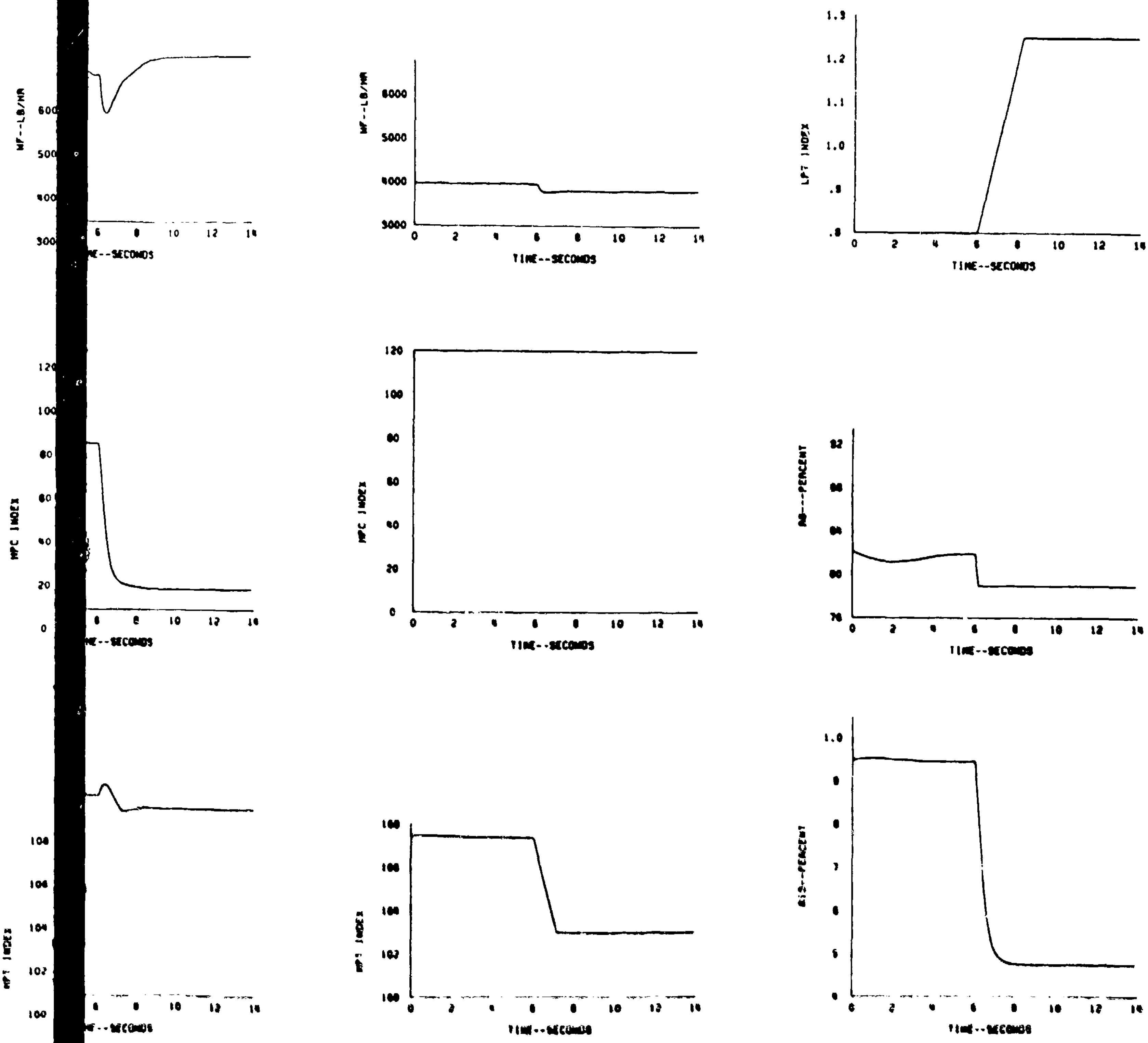
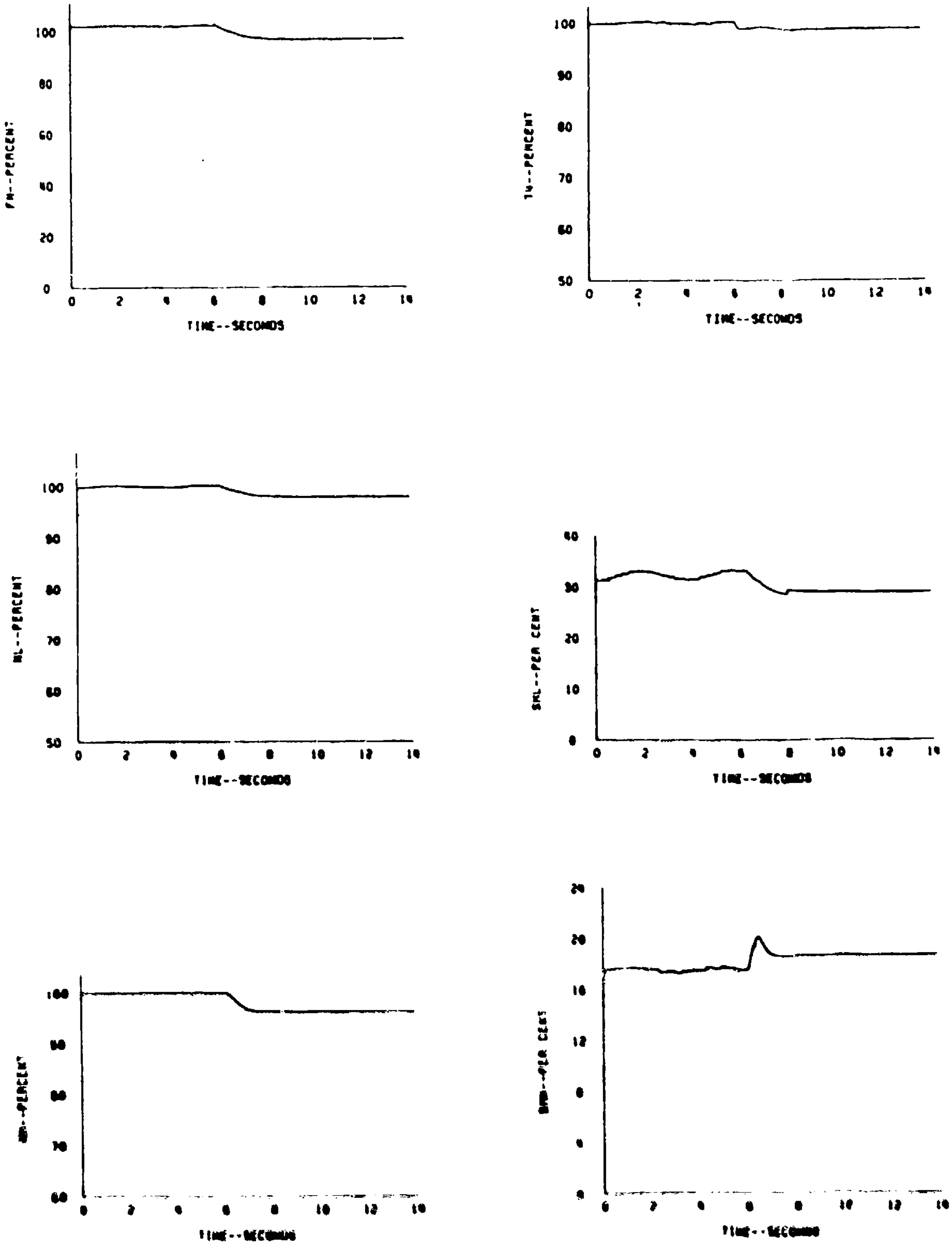


Figure 8-4 Switchover to Backup at Part Power


FN--PERCENT
TIME--SECONDS
T4--PERCENT
TIME--SECONDS
NL--PERCENT
TIME--SECONDS
SML--PER CENT
TIME--SECONDS

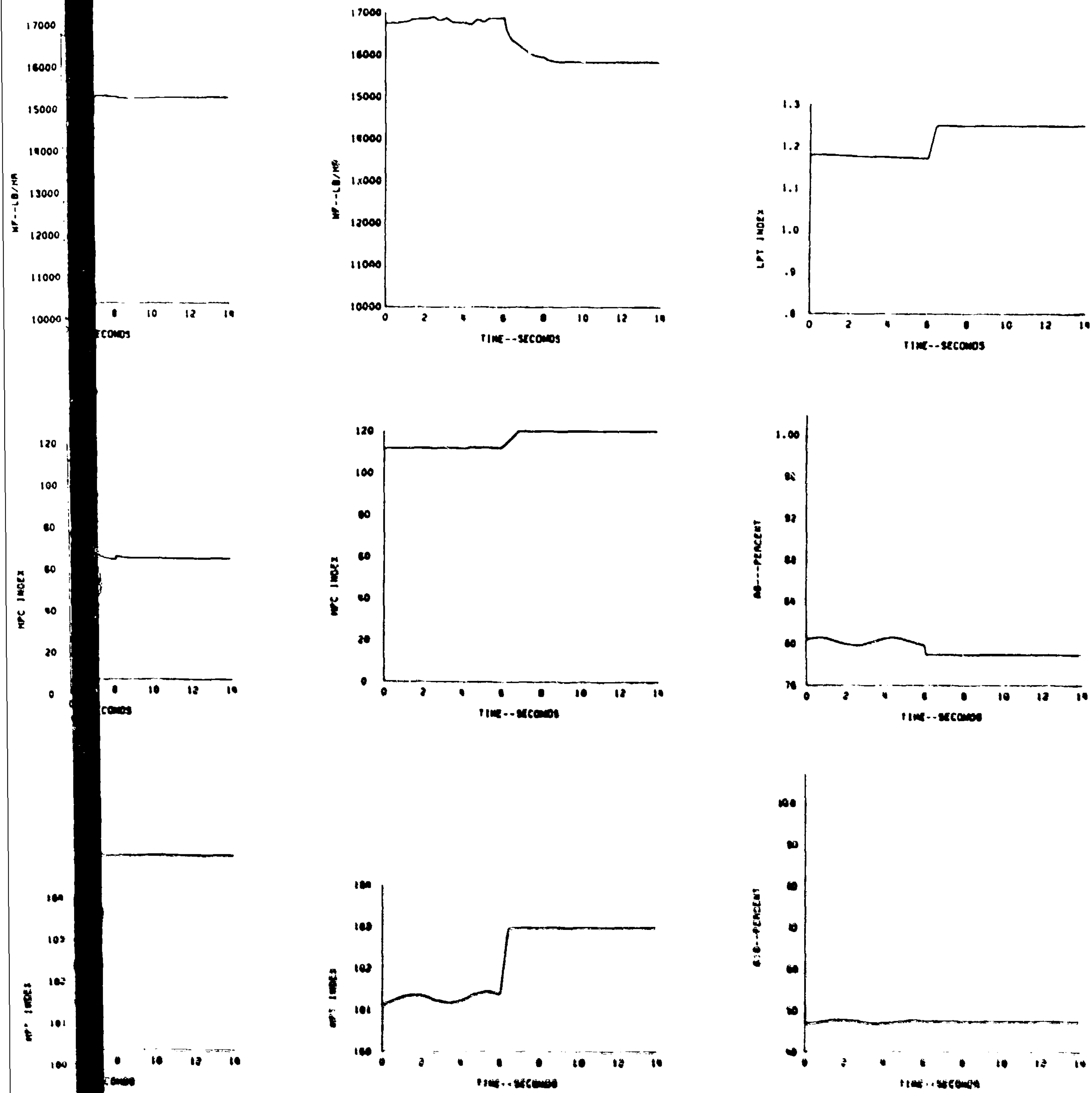


Figure 8-5 Switchover to Backup at Intermediate Power

4% when the backup control assumes control of the engine. The thrust drop is slightly more at the part power position selected. Switchover to backup during a transient is more demanding on the backup mode since no limiters exist in this mode to protect the engine. Figures 8-6 and 8-7 show that the backup proposed here also works well when switchover occurs during an acceleration. Also shown is the drastic difference in response created by simulating a failure at different times during the transient. If the switchover occurs early in the transient (within the first second), the response approximates the acceleration of the backup system which can be characterized by a single lag with a 4 to 5 second time constant. However, if the switchover occurs near or during the period when the normal response is on one of the limiters, larger overshoots and a faster backup response can be expected. In the cases investigated to date, none of the engine constraints were seriously violated during the switchover. This point requires continued attention as better engine definition is obtained since the backup control has no "limiters" and relies on its slower response to protect the engine during transients.

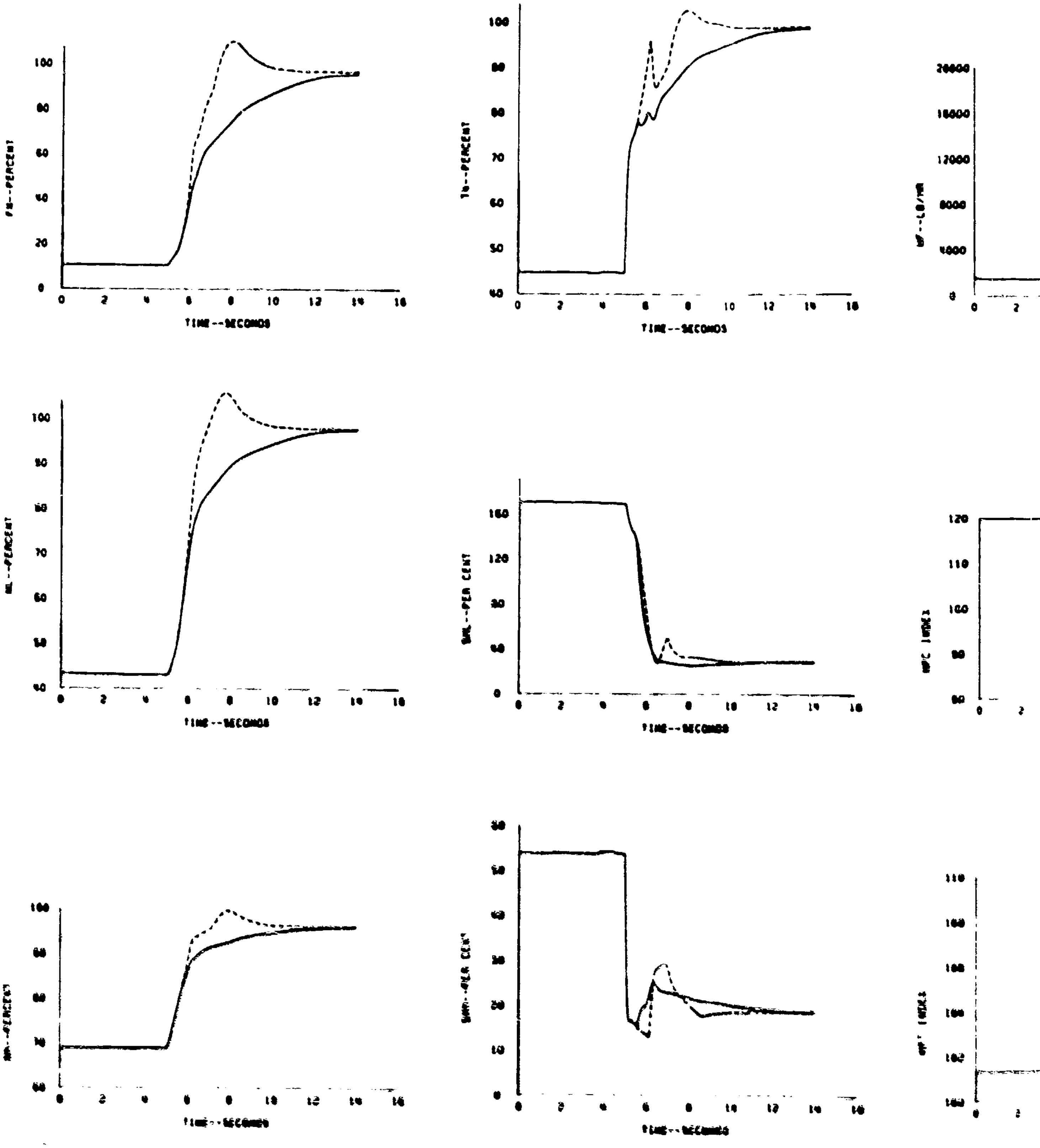


Figure 8

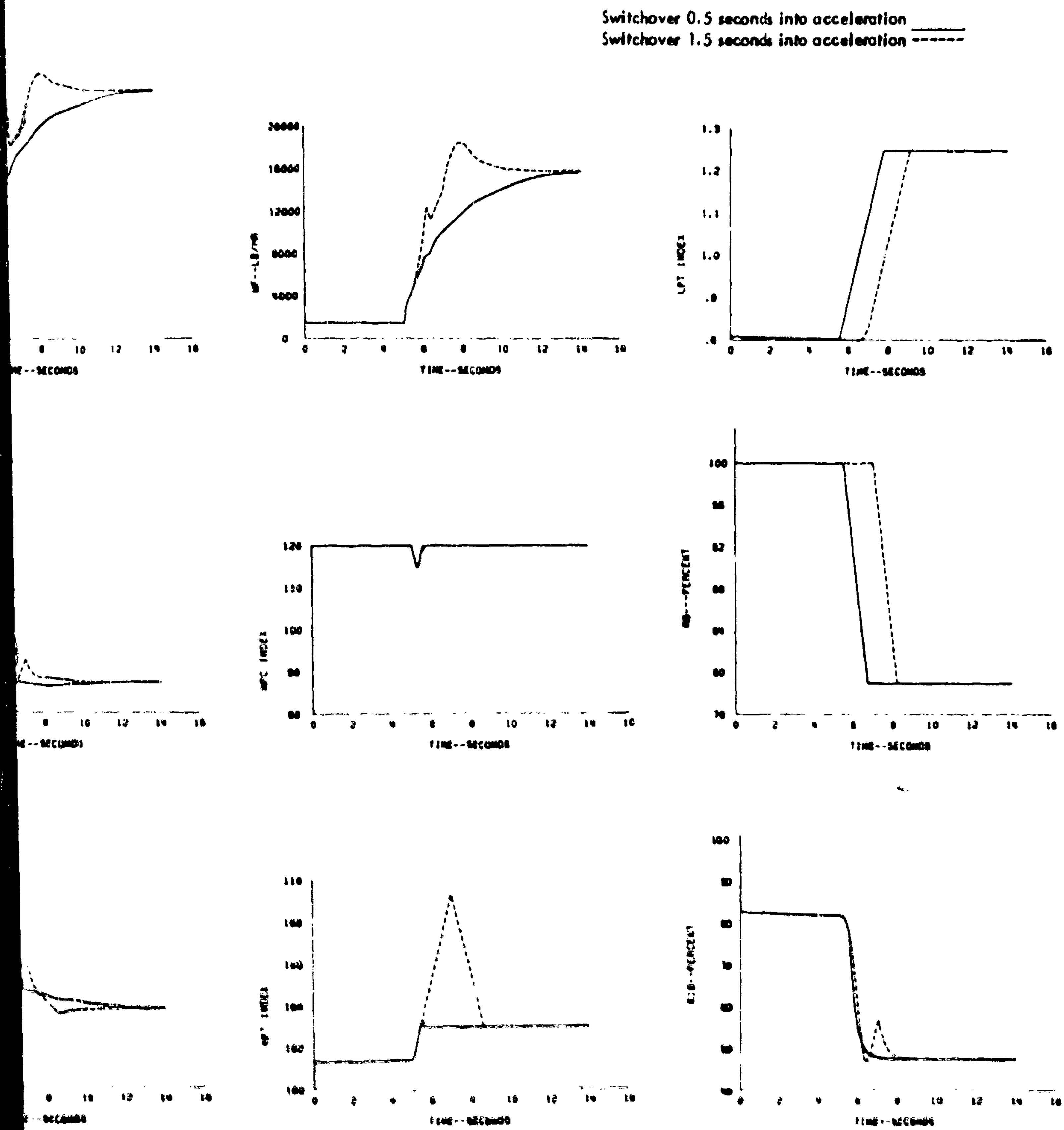


Figure 8-6 Switchover to Backup During Partial Acceleration

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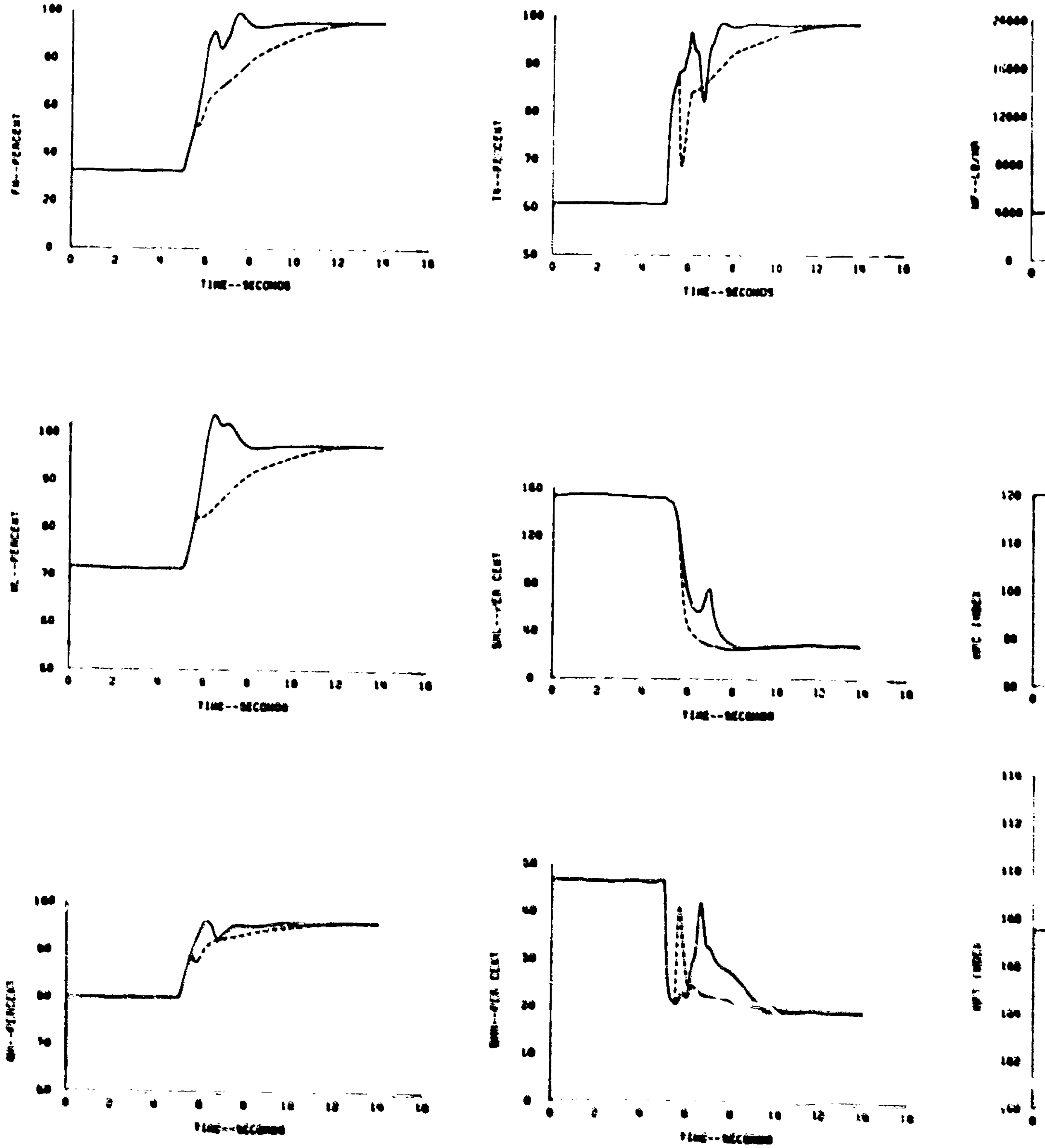
TIME--SECONDS

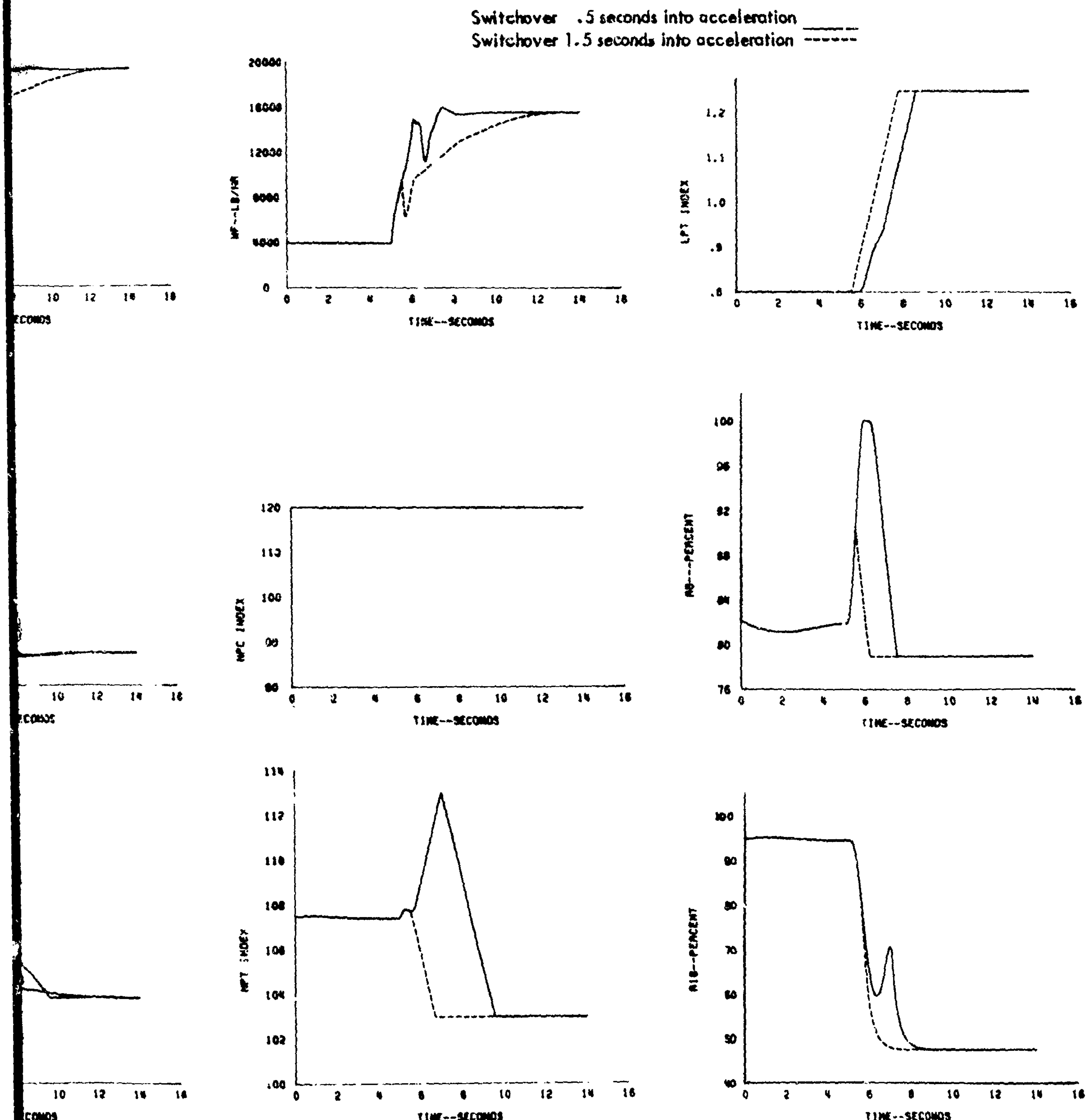

Figure 8-7 Switchover to Backup During Full Throttle Acceleration

9.0 CONTROL SYSTEM IMPLEMENTATION CONCEPT

An analysis was made of the total computational requirements necessary to achieve control of the various engine functions on an augmented fully variable geometry engine and the interrelated logic of assuring proper sequencing of each function. Additional considerations of integrated propulsion control, airframe control interface requirements, and condition monitoring requirements were also evaluated. These complex system requirements can most readily be met, on a cost effective basis, through the utilization of a full authority digital electronic controller. Current developments in digital controllers and interface circuits suitable for engine control show great promise of continuing the evolution into more reliable and lower cost assemblies. Therefore, the control system implementation effort has been directed toward a reprogrammable full authority digital control system suitable for engine mounting. This approach provides for maximum flexibility for control mode changes during engine development and modifications during production. Study effort has been directed toward a detailed conceptual definition of the digital controller. This assembly will include not only the central data processor, but also input/output circuitry, some engine sensors and transducers, power conditioning circuitry, and all engine control logic. These studies include computer capacity, redundancy considerations, self-test, and failure mode characteristics.

Preliminary studies have been conducted to determine the best approach in achieving adequate redundancy or backup for critical functions. The studies are based upon backup control functional requirements and mode selection described in Section 8.0. Hydromechanical systems, dual channel controllers, time sharing of computers between engines, redundancy of prime control loops within the controller, or a simplified electronic backup controller are some of the approaches considered. The nature of transfer of control from the prime controller to the backup to assure safe, surge-tree operation is a prime consideration in selecting backup implementation.

Overall System Concept

The overall control system functions are shown on the functional block diagram of Figure 9-1. The system consists of the following functional subsystems:

- Digital Electronic Controller
- Gas Generator Fuel System - This consists of the fuel pump, flow control element and the necessary auxiliary components for the main fuel system. The objective is to minimize the number of components necessary for fuel control and take full advantage of current pump development efforts.

  The centrifugal vapor core pump with an auxiliary retractable vane pump for starting flows is defined for the preliminary concept. The metering valve pressure drop control is performed by the pump control elements to obtain a minimum number of control elements.

- Hydraulic Actuators for Compressor Geometry Control - The actuators for compressor stator vanes for acceleration surge control and airflow matching are a part of the main fuel system, with hydraulic pressure supplied by the main fuel pump.
- Pneumatic Actuators - Four air motor actuators are required for the hot section of the engine. Air motor drives are utilized for both H.P. turbine stator vane and L.P. turbine jet flap controls, and both the primary and secondary nozzle areas.
- Electrical Power Generation and Regulation - A permanent magnet

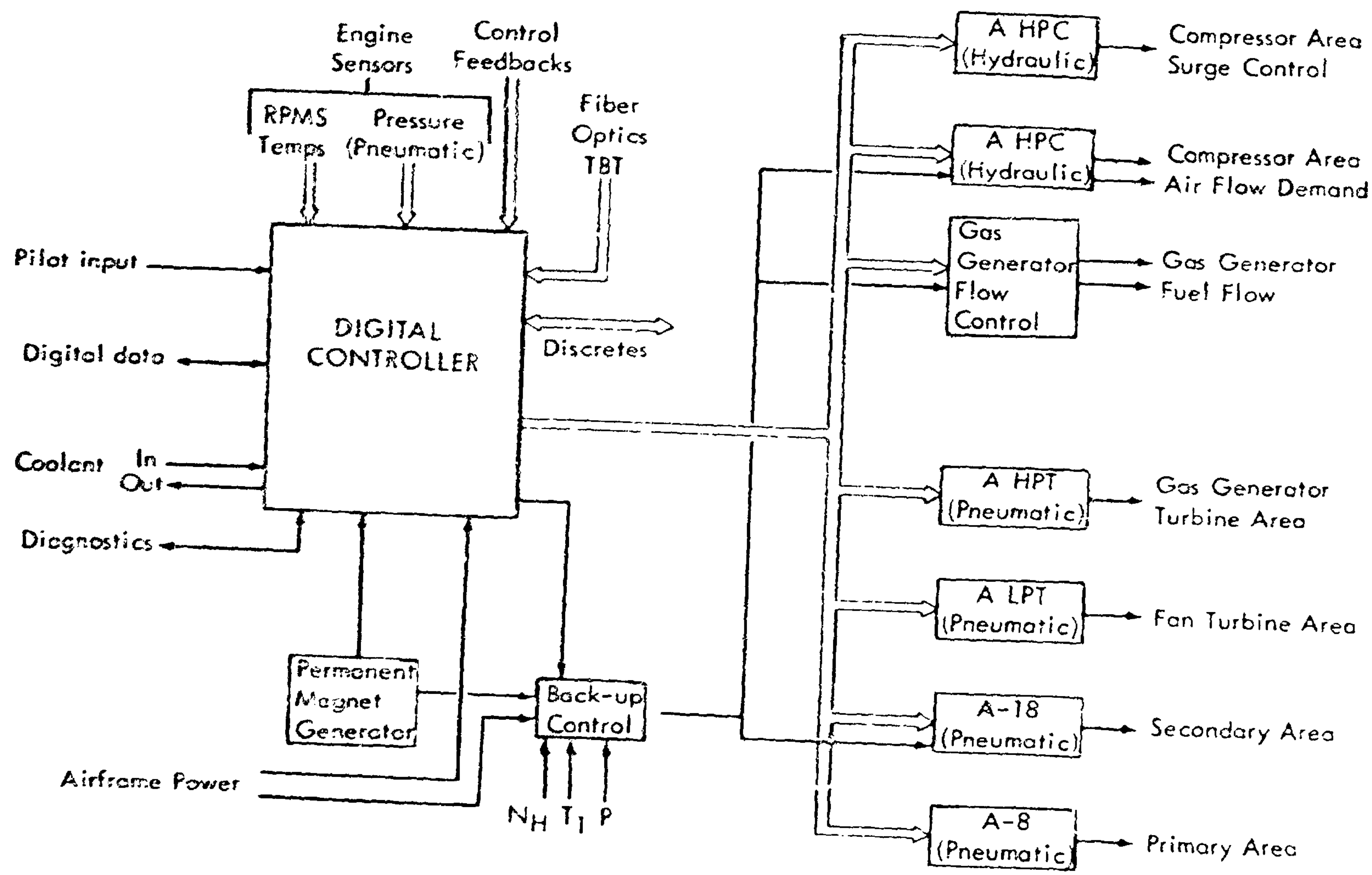

Figure 9-1 Functional Block Diagram of Control System Concept for JTD Engine

generator and voltage regulator is a part of the system with additional power conditioning required as part of the electronic controller package.

- Sensors and Interfaces - Pressure sensors are included as part of the electronic controller package and cooled as part of the electronic cooling load. Position sensors and devices to effect electro-hydraulic or electro-pneumatic interfaces are generally considered with the geometry effector as a specific component. Sensors for the engine rotor speeds and temperatures will be mounted as convenient on the engine or control components.

- Backup Control - In the preliminary definition, the backup control is a separate analog electronic package. Separate cooling and power supply are provided with the control electronics.

Redundancy Considerations

Redundancy definition will be the result of future trade studies and reliability analysis. The basic philosophy to be implemented is one of achieving the required reliability with a minimum number of parts.

The backup control is a major redundant element but is simplified as much as possible to maintain safe operation of the aircraft under pilot supervision. Generally, it is considered that electrohydraulic and electropneumatic interfaces should consist of redundant elements. Here the philosophy is that if one element fails, it will be detected by the diagnostic system in the digital computer and transfer made to the second element and normal full operation continued.

Redundant position feedback elements will be eliminated where possible based on mode (closed loop) considerations and the probability of somewhat deteriorated but safe backup performance. Critical sensors will have redundant elements.

These can be held to a minimum by utilizing the power of the digital computer to synthesize engine pressures and temperatures from measured operating conditions. Aircraft sensors or sensors associated with the engine monitoring systems can also provide some redundancy through the interface communication links of the digital electronic controller.

Failure Detection and Transfer

The failure detection is a part of the self-test and diagnostic features of the controller software. It should be noted from a systems concept that failure detection applies to both the primary digital computer and the backup control.

Of significant importance to system considerations is the transfer from the primary control to the backup control. Since the primary mode is considerably different from the backup mode, the output requests could cause severe transients during transfer if provision are not made in the design. This study has shown that limiting the actuator rates during the transition is sufficient to avoid severe geometry transients. In addition, the pump and metering dynamics provide sufficient filtering to smooth the fuel flow transient even if switchover and back-up occurs during an acceleration transient (see Figures 8.4 through 8.7).

Full Authority Digital Electronic Controller

The electronic controller is conceptually designed as a flightweight engine mounted configuration having capability for total engine control. Figure 9-2 illustrates in block diagram form the JTD Digital Controller concept. The principal components included in the controller are the printed circuit boards which contain the input/output signal conditioning, digital computer-memory section, power condition circuitry, and the engine pressure transducer module. A pictorial exploded-view representation of the proposed design showing the various component features is shown in Figure 9-3.

The control assembly incorporates fuel cooling of the electronic components to enable operation in the hostile thermal environment encountered in engine mounting.

Cooling fuel is distributed to parallel flow channels in the controller housing walls. The cooling fuel supply line enters an inlet manifold in the control housing that distributes the flow equally. An outlet manifold is included to collect the fuel at the discharge. Conductive metal heat paths are built into each printed circuit card module and into the power supply and pressure transducer assemblies. The heat paths are formed by including an aluminum or a copper grid in each printed circuit card module and by using aluminum mounting frames for the power supply and the pressure transducer assemblies. High power electrical components are mounted in intimate contact with the heat paths. The self-generated heat is conducted away from the components to the fuel cooled walls of the housing assembly.

The housing assembly is designed to mount to the engine case on vibration dampers. The spring rate of the mounts and the percentage of critical damping

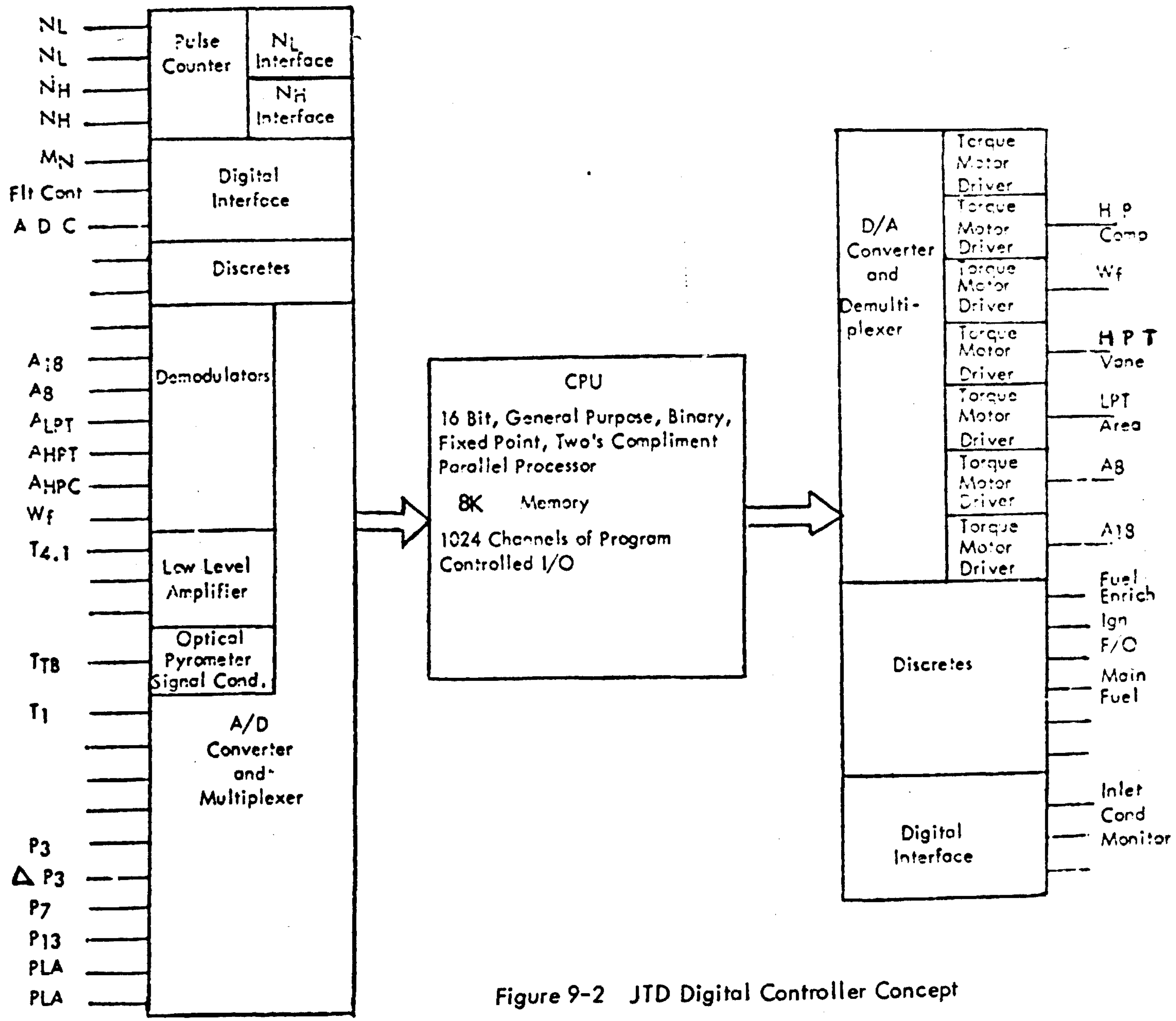

Figure 9-2 JTD Digital Controller Concept

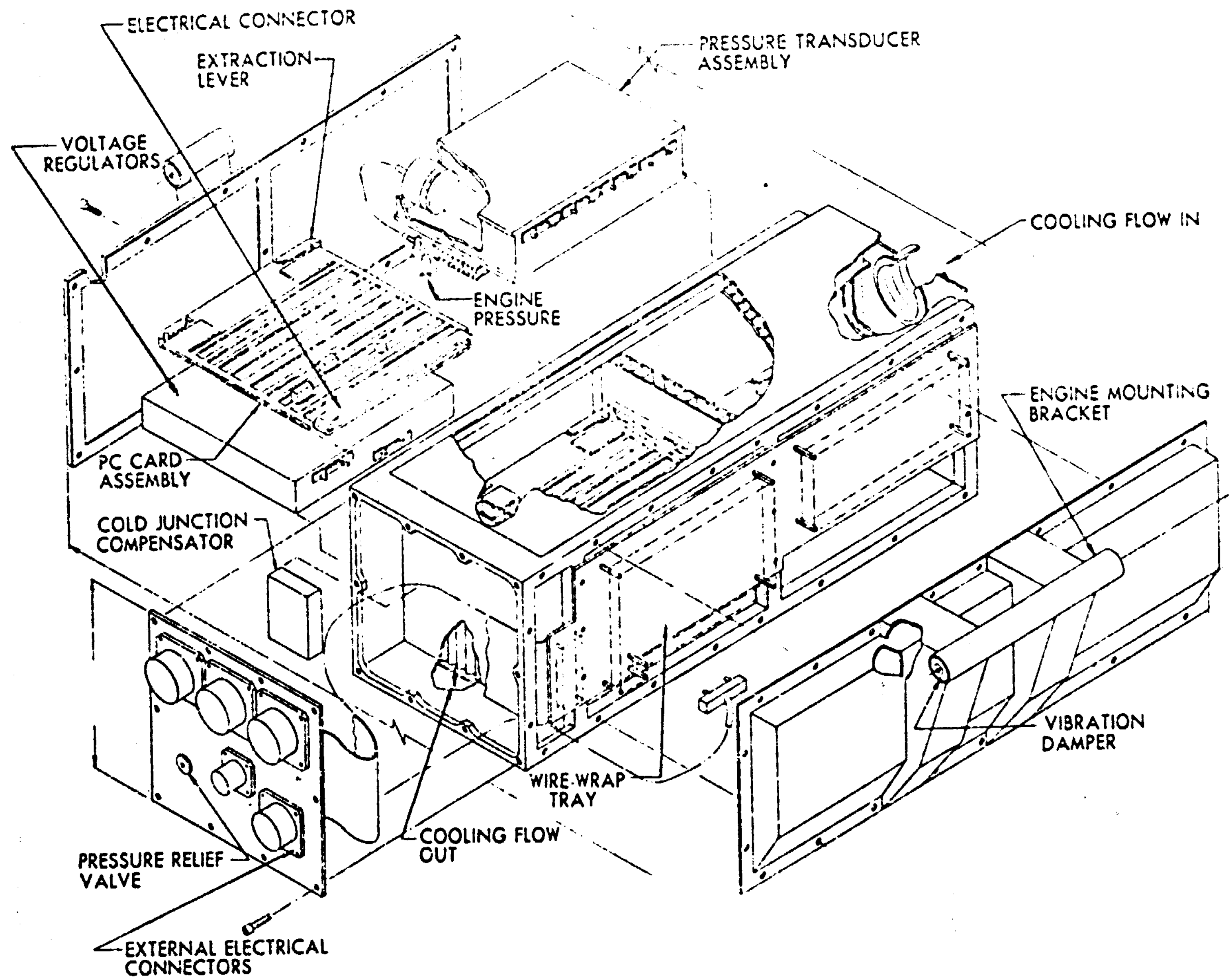

Figure 9-3 Full Authority Digital Controller Concept

will be adjusted to produce the best possible protection for the components of the controller.

The controller design includes features which minimize cost and time required to perform testing, troubleshooting, and replacement and repair of component subassemblies. The electronic unit utilizes modular construction and plug-in subassemblies.

Electrical Power Generation

The generator which supplies the electrical power for engine controls is an advanced, thermally efficient unit. Its design is based upon the concept of combining the best features of the conventional permanent magnet alternator (PMA) with those of the homopolar induction alternator (HIA). Regulation of the output is accomplished by controlling field current via a switching type regulator. As a result, the alternator delivers only the power required by the electrical load. When compared to conventional PMA's, heat dissipation in the rectifier/regulator system is reduced by 50% to 90%, depending upon the percent of rated electrical load connected. The alternator is driven by the accessory gear box at approximately 28,000 rpm at 100% engine speed. It has no brushes or slip rings, and magnets of high coercive force material such as samarium cobalt are used. In the event of electrical failure of the alternator, an automatic transfer system shifts the engine electrical loads to the aircraft bus. A separate PMG winding and regulator for the backup control will be provided.

In addition to the DC output for engine controls, the alternator supplies two independent, wild frequency AC outputs to energize the engine ignition system(s).

Gas Generator Fuel System

A concept of the total fuel system is shown in Figure 9-4 in block diagram schematic form. This diagram shows the fuel flow paths and necessary inter-

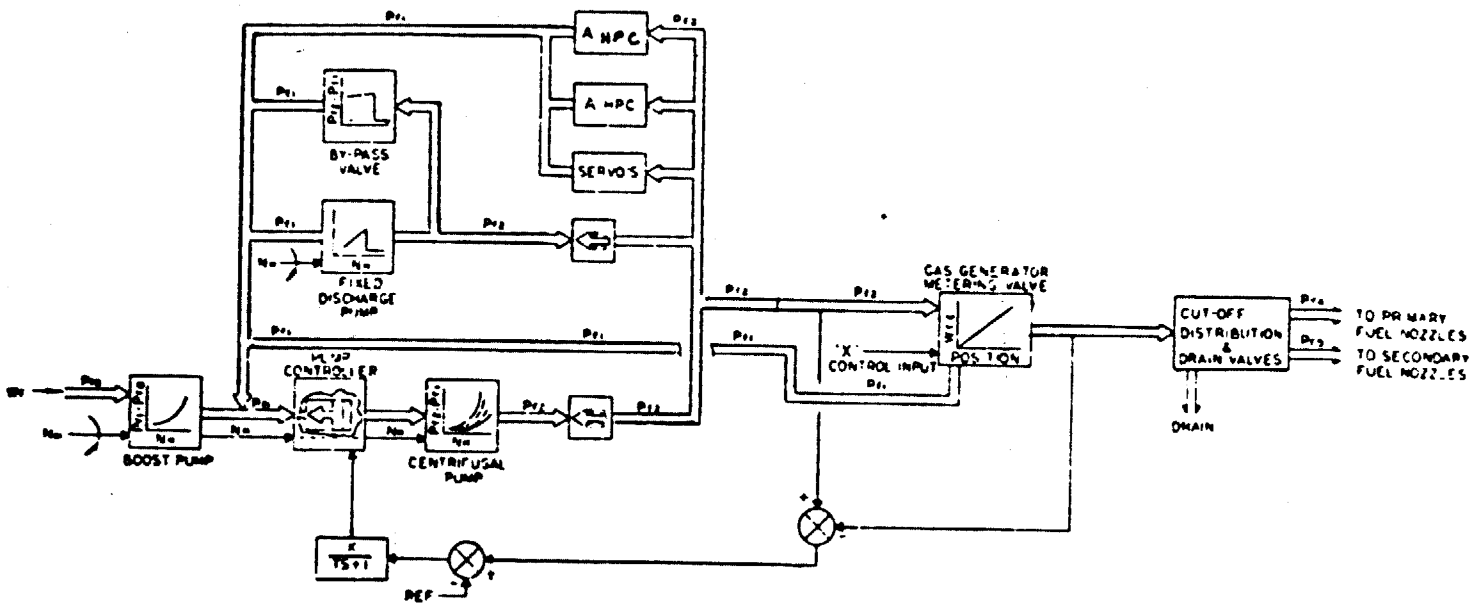

Figure 9-4 JTD Fuel System Concept

connections between units as presently structured. The major fuel system components are shown as separate schematics for clarity of presentation. A part of future study relating to maintainability, survivability, and reliability will consist of considerations of unifying packages or using separate packages to define the best system concept.

Fuel Pumping System

The preferred fuel pump configuration results from an Air Force funded effort (Contract No. 33657-73-C-0618) in conjunction with Chandler Evans Company. Such a pump, referred to as the retracting vane/vapor core pump is shown in Figure 9-5.

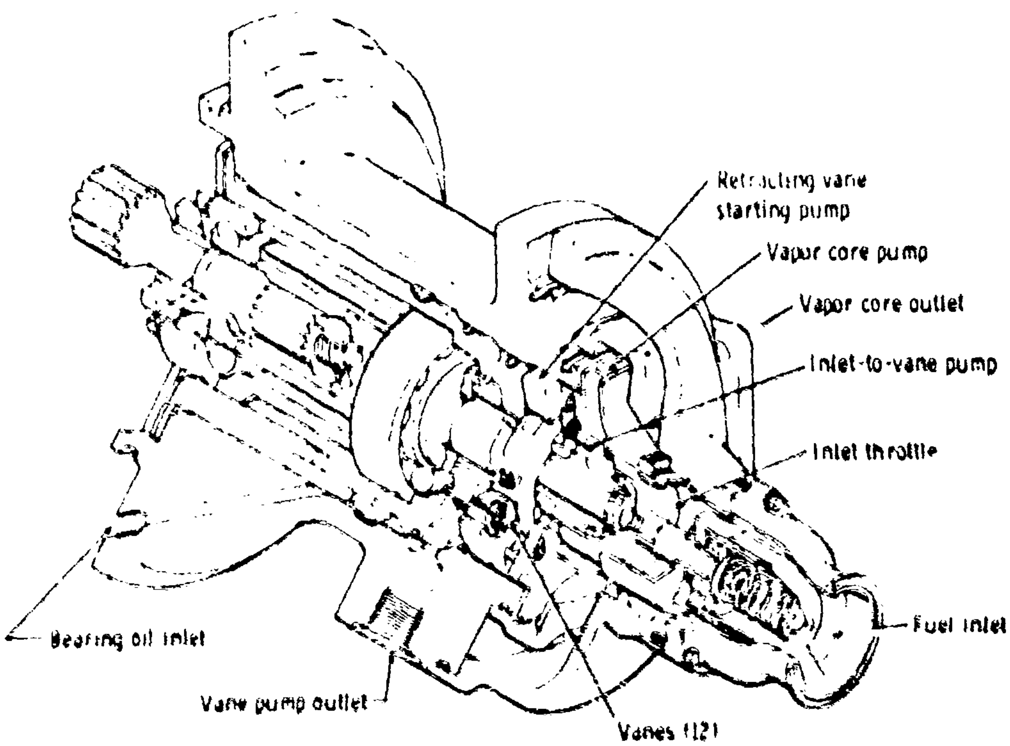

Figure 9-5 Retracting Vane/ Vapor Core Fuel Pump Assembly

The principal advantage accruing from the use of this pump is low heat rise, leaving a greater fuel heat sink capacity available for ancillary cooling. Low heat rise is effected by means of inlet throttling the vapor core pump to create, in effect, a variable displacement pump, servo regulated to hold a constant pressure differential across the metering element.

The retracting vane element constitutes a fixed positive displacement pump suitable for providing engine requirements at starting conditions. In order to avoid excess capacity at operating speeds, the pumping vanes retract before reaching engine idle speed, leaving the engine running on the vapor core pump.

Gas Generator Fuel Control

The gas generator fuel control shown schematically as Figure 9-6 consists of a flat surface sliding metering valve, with head control by the variable pressure pump, a cutoff/pressurizing valve, a servo pressure regulator, a wash filter, and an automatic control to divide flow between the primary and secondary fuel nozzles.

When the power lever is moved from cutoff, the cutoff solenoid is energized and opens whenever pump pressure is sufficient to overcome the pressurizing valve spring. Initial low flow is through the primary burner nozzles. As flow increases, nozzle pressure increases and the spring loaded valve starts to open allowing increasing flow to the secondary nozzles.

The metering valve is positioned by a torquemotor controlled servo. Valve position is fed back to the computer by a signal from an LVDT. Actuation pressure to move the metering valve is provided by a servo pressure regulator. Position of the metering valve yields a flow area. The head across the area is provided by the fuel pump control mechanism. The flow is proportional to the area multiplied by the square root of the head. Accuracy of this flow meter thus

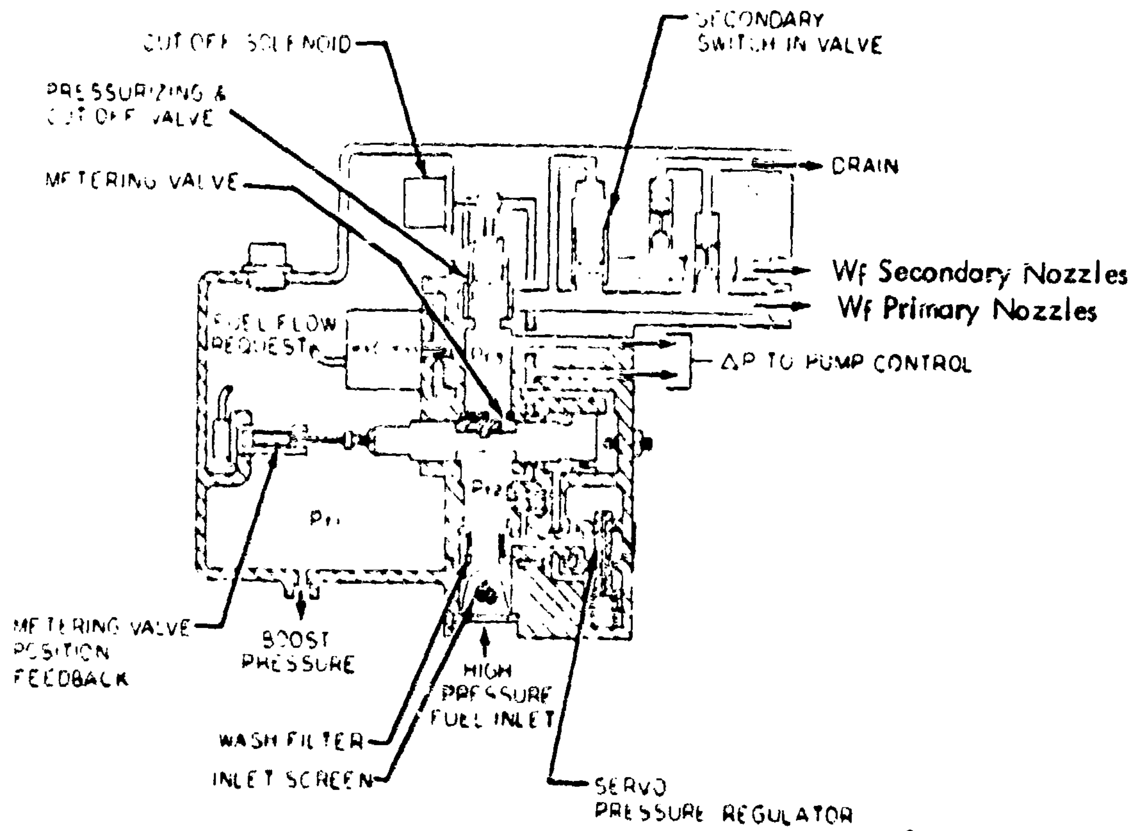

Figure 9-6 Gas Generator Fuel Control

depends on the accuracy of the head control and the accuracy of area to position conversion. The metering valve position is fed back for dynamic compensation if needed and for self-test/diagnostic purposes.

Flow measurement is necessary for light off, a minimum flow limit especially at altitude, and at least for a maximum limit on flow in event of blowout (totally or partially). Most control, however, is closed loop on an engine variable or variables.

The metering valve consists of a flat plate with the metering orifice cut into the plate. A second flat plate slides over the orifice. The metering head loads the movable plate against the orifice plate.

Compressor Geometry Actuators

Fuel pressure hydraulics is probably the most suitable medium for operating the high pressure compressor variable geometry. The actuator is comprised of a hydraulic cylinder for force exertion, a torque motor servo valve for flow control, and an LVDT for position feedback. Since compressor geometry is one of the parameters in the back-up control mode, the torquemotor servo must be fitted with double magnetic coils for the sake of redundancy.

Figure 9-7 shows a conceptual arrangement of the compressor geometry actuation mechanism. As shown, two points of actuation are involved; one for scheduling compressor stators to maintain suitable surge margin designated surge control actuator, and one for shifting gain and position of all stators to optimize the compressor for engine flow demand.

These two compressor actuators are operated by the digital controller with LVDT feedback signals used to complete each individual control loop.

Pneumatic Actuation Systems

Actuation of gas turbine engine variable geometry systems can be effectively controlled by the use of high speed pneumatic gear motors using engine compressor discharge air. The use of high speed pneumatic motors with slew speeds of 10,000 to 25,000 rpm in conjunction with high ratio transmissions of 200:1 to 1500:1 have been used successfully. This combination provides adequate pneumatic system stiffness. The pneumatic systems permit operation in higher

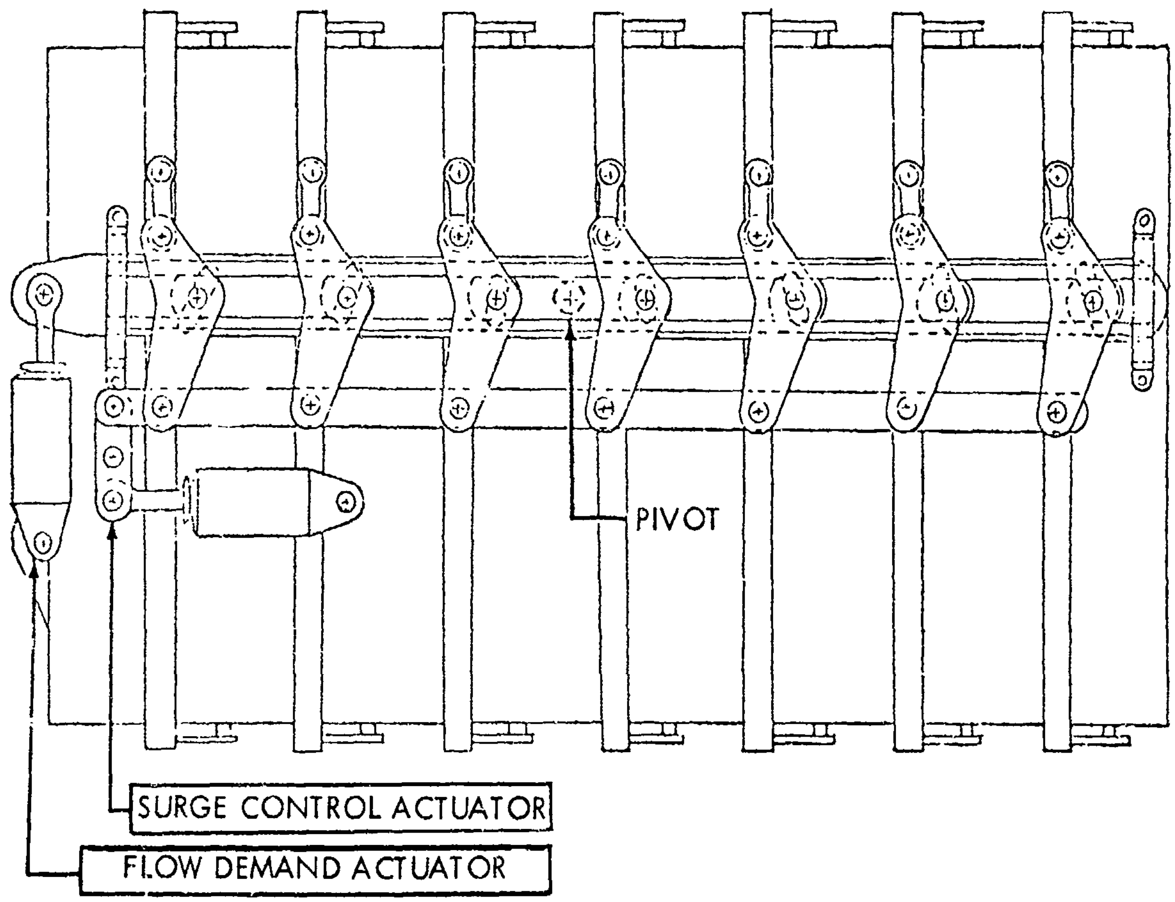

Figure 9-7 Compressor Geometry Actuation Concept

temperature environment than can be obtained with hydraulic (fuel or oil) systems with less expense and weight than would be required to provide hydraulic pressure and cooling.

Air motor drives have been selected for power source for mechanical actuators for the turbines and exhaust nozzles. Figure 9-8 illustrates the turbine control configuration and Figure 9-9 the exit nozzle configuration. Both systems use a pneumatic motor control unit which accepts command signals from, and provides a position feedback electrical signal to, the engine digital control. The input electrical interface is a flat armature (dry type) torquemotor to operate the gear motor air valve controlling direction of compressor discharge air to the high speed gear motor. The feedback electrical interface is a resolver driven by gear motor rotation through a high reduction feedback transmission. The schematics include a proportional mechanical feedback mechanism that will on electrical failure provide a predetermined failure position for the system output.

The pneumatic motor control unit is connected to the system actuators by dual flexible drive cables to a primary actuator, which is in turn connected to multiple secondary actuators by flexible loop cables. This type of arrangement permits location of the pneumatic motor control and individual actuators in remote positions and through rigging allows synchronous operation. The actuators provide the bulk of gear reduction from motor rotation to output rotation which permits the use of minimum size drive cables.

Turbine Geometry

Schematic Figure 9-8 indicates the system be used for turbine geometry actuation concept for both the high pressure and low pressure turbines. The motor control valve in this case can be operated directly from the torquemotor. The primary actuator uses a face gear driven by the dual input cables to drive a planocentric high ratio transmission which in turn rotates an output eccentric less than 120° to provide a relatively linear output stroke. The secondary actuators are identical to the planocentric stage of the primary actuator. The output links are attached to the synchronizing ring or jet flap valve to provide

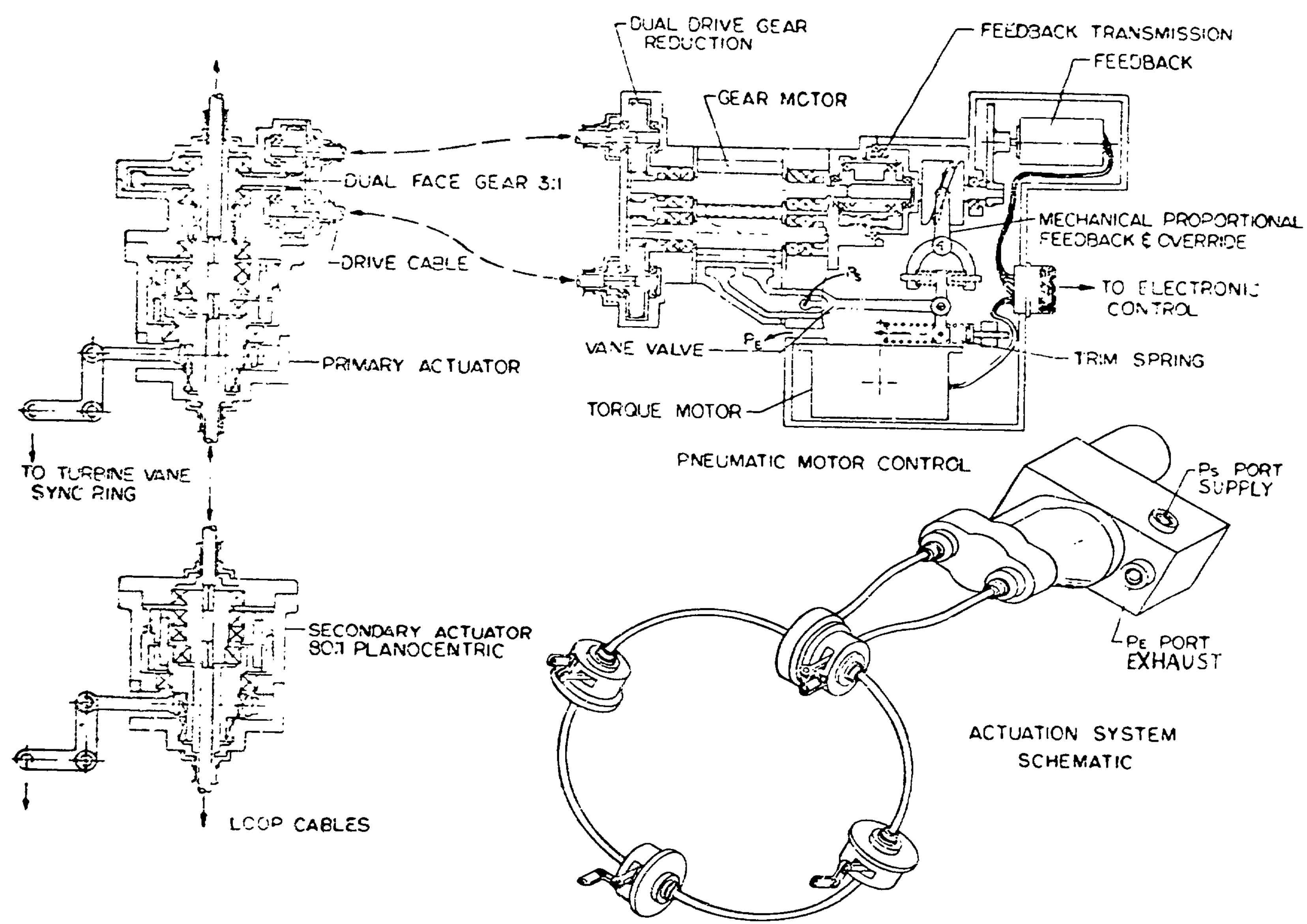

Figure 9-8 Turbine Geometry Actuato.

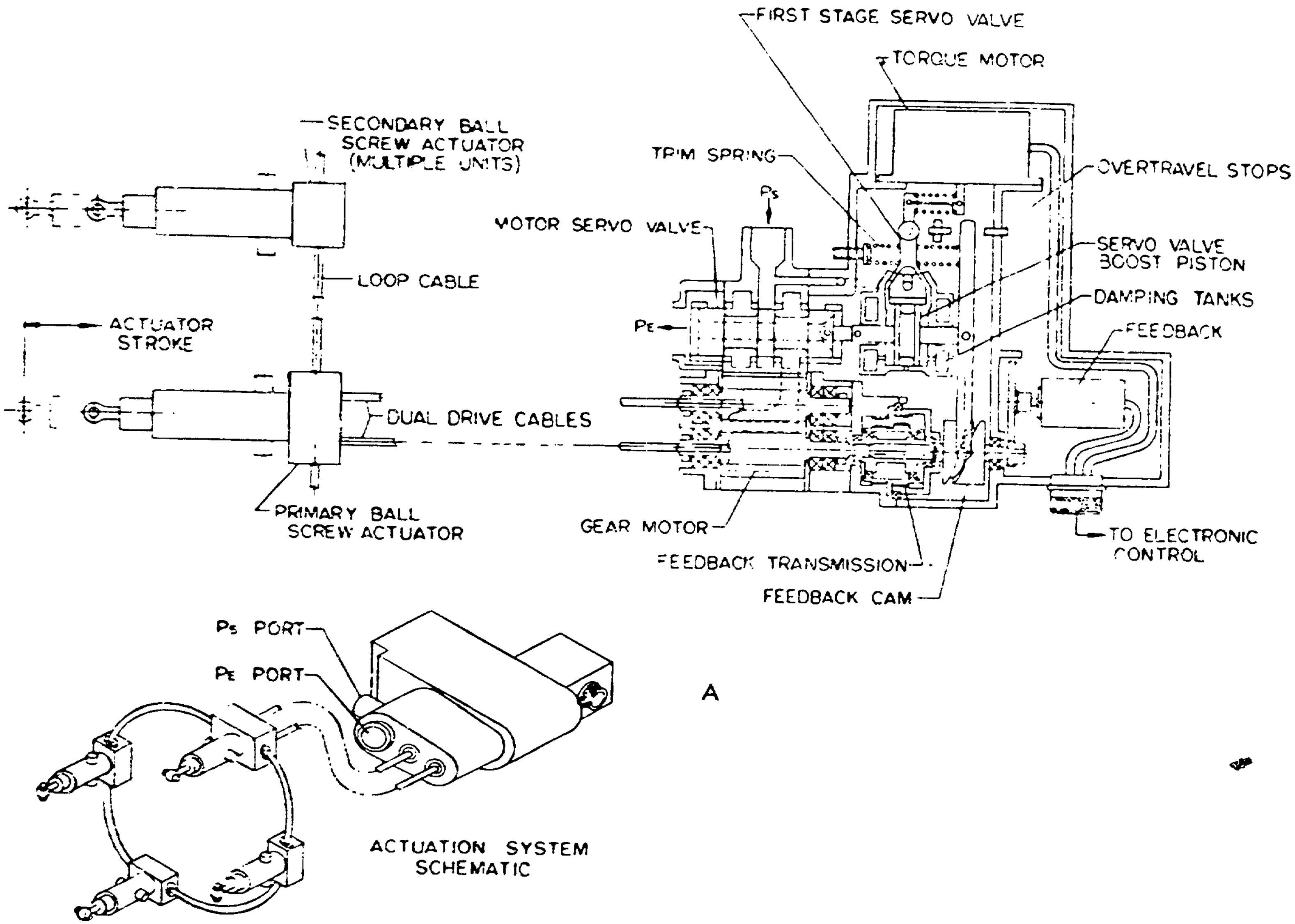

Figure 9-9 Exit Nozzle Actuator Schematic

the coordinated stroke and force required.

Systems of this type are currently being developed under Navy contract N000140-76-C-0205.

Primary and Secondary Exit and Duct Nozzle Area

Schematic Figure 9-9 illustrates the actuation system required for gas generator exhaust nozzle or fan exhaust nozzle systems. The power and stroke requirement will be considerably larger then for the turbine actuator requirements resulting in 10 to 20 times greater motor displacement. In this case, it will be necessary to use a two stage control valve for the motor. The first stage of the valve will be similar to that used on the small actuctor and will operate the second stage with a force feedback to null the first stage. The primary actuator will again be driven by dual drive cables. The secondary actuators will be driven by loop cables from the primary. A right angle gear reduction on the order of 10:1 will be used in each actuator to drive a ball screw to provide the relatively long stroke requirements. The translating plug primary exhaust nozzle actuator system is of a similar configuration with the ball screw actuator within the plug housing.

Sensor and Transducer Implementation

The sensors required for implementating the preliminary control mode fall into the categories of temperature, pressure, position and speed sensing. Figure 9-10 shows the concept of system implementation of sensors and transducers for a full authority digital controller. Preliminary studies of the full authority digital electronic control system have resulted in the following sensor requirements and preliminary selections.

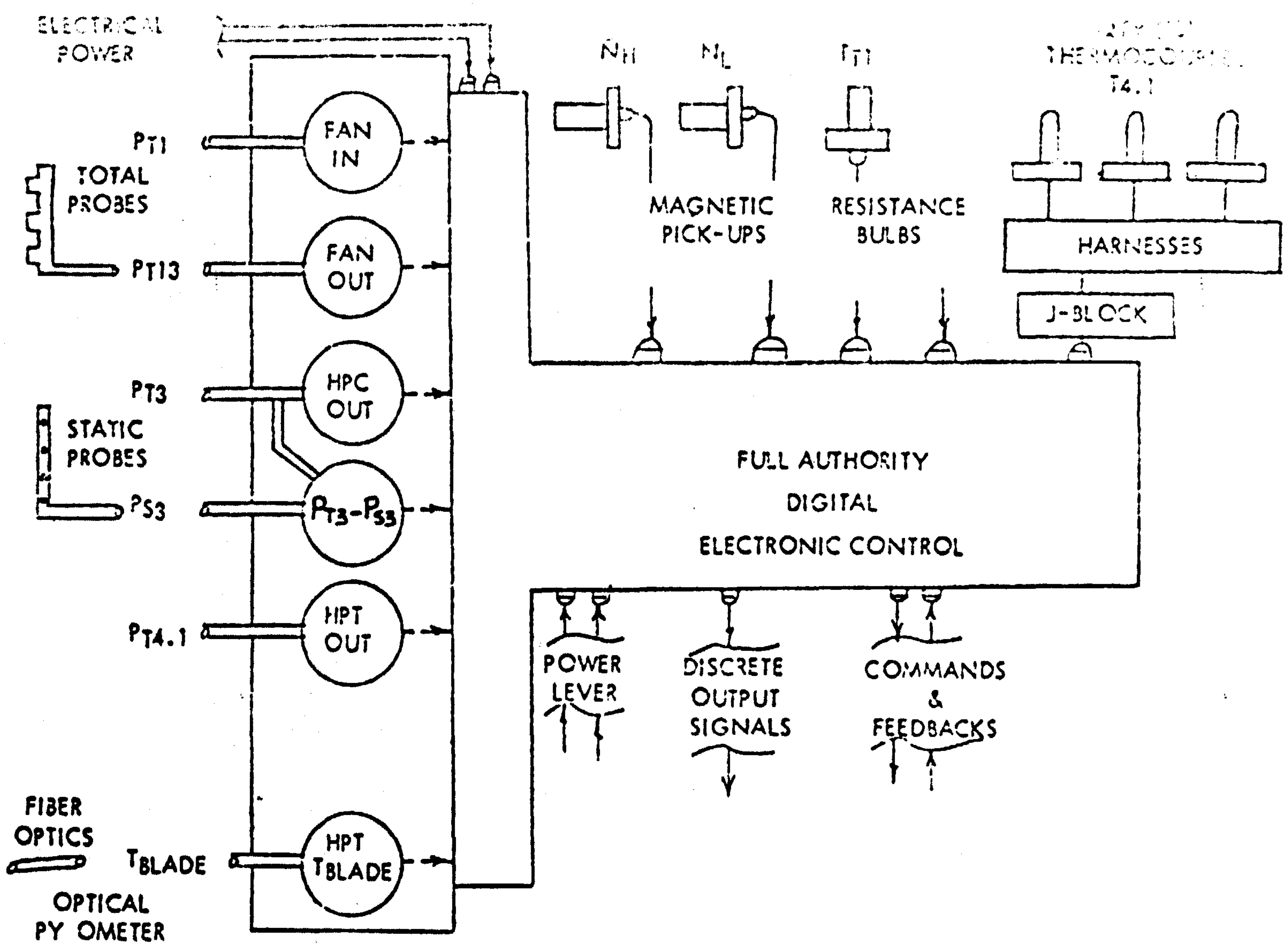

Figure 9-10 Sensor and Transducer with Full Authority Digital Controller

Temperature Sensors

Parameter	Range	Type
TI Engine Inlet	-65 to 400°F	Resistance Probe
T4.1 L.P. Turbine Inlet Gas Stream	0 to 2700°F (Avg)	Thoriated Platinum vs. Pt 40% Rh Thermocouple
TBT H.P. Turbine Blade	1200 to 1800°F	Optical Pyrometer

The resistance probes are platinum sensors. Two of these probes are utilized to provide redundancy.

The T4.1 is a single input signal which is the average from a thermocouple harness interconnecting approximately ten thoriated platinum vs. platinum 40% rhodium thermocouples for an accurate average gas stream temperature. This thermocouple configuration was developed under Air Force contract No. F33615-74-C-2069 by Engelhard Industries for compatibility with the DDA JTD engine.

Turbine blade temperature sensing will be accomplished by an optical pyrometer system comprised of the optical head, flexible fiber optic light cable and the silicon cell detector. The detector portion of the system which contains the electronics, is to be accommodated in the electronic controller to achieve a satisfactory thermal environment. The optical head will be of the aperture or lens type, incorporate purge air, and view a selected target area of the H.P. turbine blade to enable effective temperature limiting for turbine blade protection. Individual and average peak blade temperature signal processing is planned.

Figure 9-11 shows the installation of the T4.1 thermocouples and optical pyrometer head in the engine.

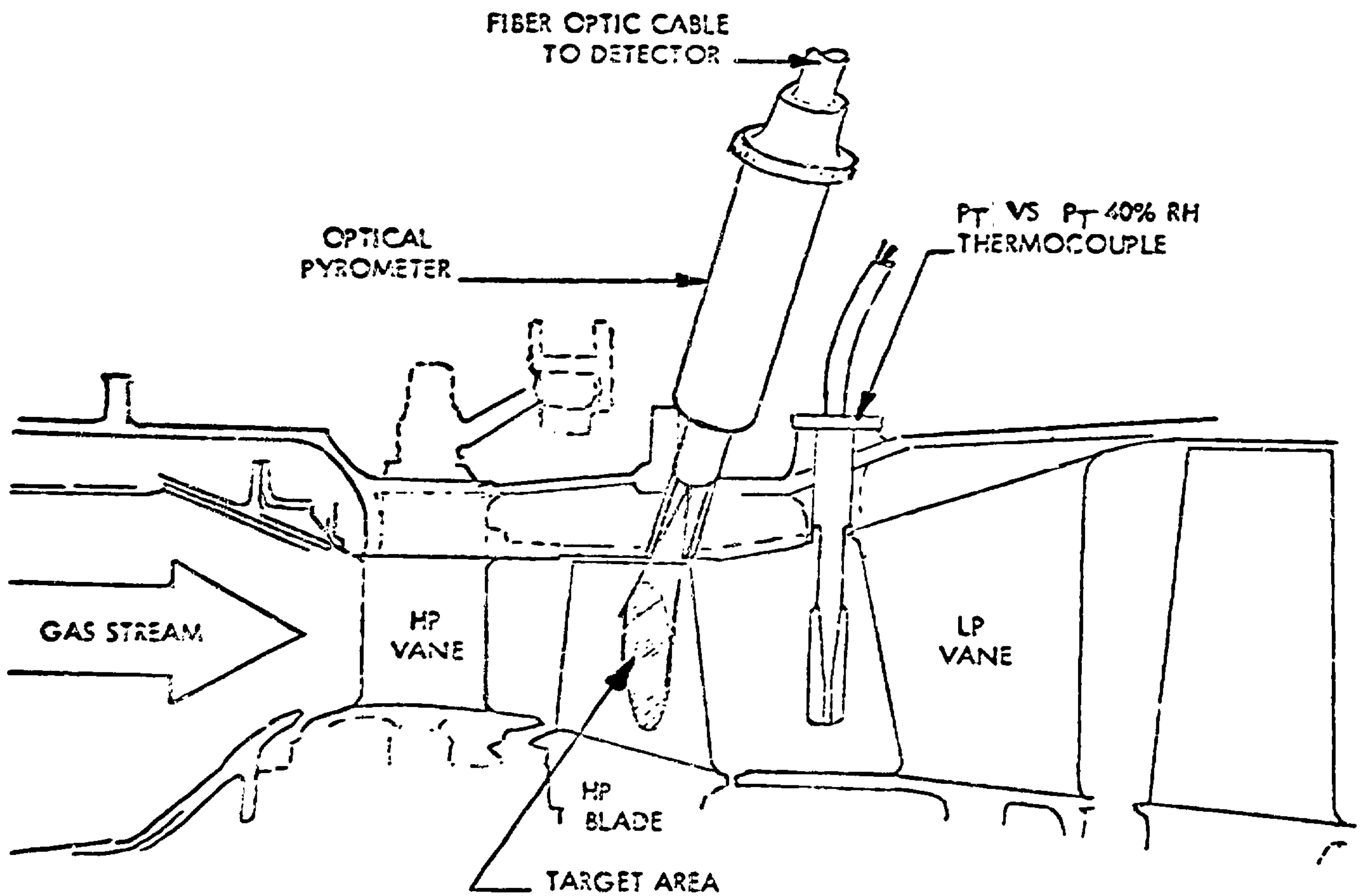

Figure 9-11 Turbine Temperature Sensing Concepts

Pressure Sensors

Pressure	Sensed Location	Sensor Range	Accuracy Goal
PT1	Engine Inlet	0 to 50 psia	± .05% FS to 20% FS linearly increasing to ± .1 % FS at 80% FS ± .1 % FS above 80% FS
PT13	Fan Duct	0 to 100 psia	(Same as for PT1)
PT3	HP Compressor Outlet	0 to 400 psia	± .05% FS
PT3-PS3	HP Compressor Outlet	0 to 40 psid	± .05% FS
PT4.1	HP Turbine Outlet	0 to 100 psia	± .1% FS
PT7	Tailpipe	0 to 125 psia	(Same as for PT1)

The preliminary response requirement for these pressure sensors is the equivalent of 0.02 second time constant. The above accuracy goals are sensor accuracies and do not include conversion errors. Conversion accuracies of ± 0.1% FS are expected. The requirement for redundancy of pressure sensing requires further analysis based upon failure mode and effects considerations.

Preliminary conceptual design of the control system indicates that the pressure transducers should be integrated within the engine mounted electronic control package. This arrangement minimizes problems of EMC signal reliability, and system simplicity. The electronic package thermal and mechanical environment also provides for maximum accuracy and reliability of pressure sensing. Additionally, the prime responsibility of sensing and electrical conversion is embodied within one component.

Position Sensors

In the preliminary implementation concept, L.V.D.T.'s will be used for generation of position signals on the following functions: Core Nozzle Area (A8), Fan

Nozzle Area (A18), Compressor Stator Area (A_{HPC}), L.P. Turbine Area (A_{LPT}), Engine Fuel Flow (W_f).

The LVDT's are 4-wire units with excitation supplied by the full authority digital electronic controller. Other high temperature position sensors, such as a quartz capacitive position sensor, are also being considered in areas such as the exhaust nozzle application.

Power lever inputs are planned to be from potentiometers. Preliminary concept for redundancy of this critical input is to utilize two potentiometers interfacing into separate A/D converters. Consideration of digital PLA inputs are also under study for advanced airframe interface compatibility.

Speed

Both high and low rotor speeds will be sensed by magnetic pulse pickups for inputs to the digital controller. Two pickups will be provided for each speed sense. The L.P. rotor speed pickup will be located in close proximity to the L.P. turbine for the maximum degree of overspeed protection.

10.0 JTD CONTROL SYSTEM PRELIMINARY TEST PLAN

A preliminary test plan has been formulated for the evaluation of the total contr system prior to use on the JTD engine. This program plan includes the develop and validation of software for implementing the final control mode for the demonstrator test stand running. The test program also includes the evaluation of eac of the control system components and subsystems prior to use on the JTD engine testing. This test plan, shown in Figure 10-1, provides for the functional and environmental evaluation of each major component as an integral part of indiv component development programs. During these component development tests, the dynamic characteristics of the sensors, transducers and actuation devices shall be established. This information, along with engine component characteristics, will be used to update the control system simulation to assure a realistic control mode evaluation of the JTD system prior to engine demonstration.

The performance characteristics and endurance capability of each component must be evaluated by suitable bench tests and on the GMA 200 ATEGG test programs. Further, a total control system bench test is needed prior to the JTD engine test to assure total system compatibility and to verity operational charac teristics. Accomplishment of this test program is essential to a safe and suc JTD test program.

The following component level tests and evaluation plans have been defined a part of recommended control component development programs.

<u>Full Authority Digital Controller</u>

This program is structured to accomplish the checkout of a flightweight engine mounted digital controller and the required software for operation of the JTD prior to use on the engine. The digital controller development evaluation

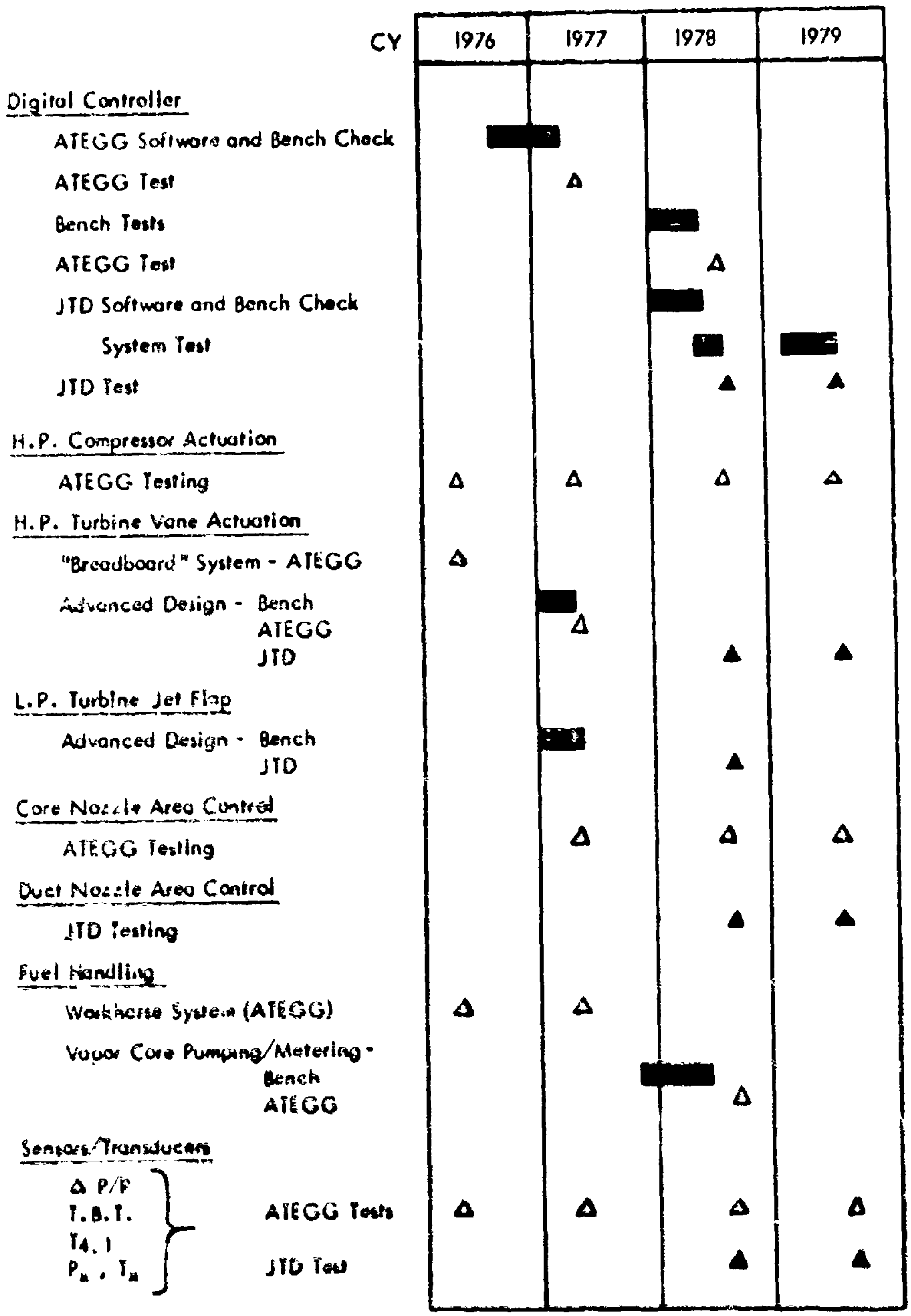

Figure 10-1 JTD Control System Proposed Test Plan

program will include software checkout and environmental and function testing of the controller assembly prior to use on the ATEGG and JTD demonstration tests.

Software Verification & Validation

Three areas of verification are:

- o Verification of program equations and organization
- o Verification of scaling and dimensions
- o Test problem reviews

A detailed software validation program will be prepared. The validation effort will demonstrate that the fixed point mechanization of the control introduces negligible error, and that the actual coded program, when executed in the Digital Control Section of the controller will meet all of the performance requirements.

After the software has been validated, a complete software documentation package will be prepared, including:

- o Program Narratives
- o Detailed Flowcharts
- o Definitions of Scalings for all Parameters and Variables
- o Definition of all Parameter Values
- o Computer Interface Data
- o Programming Manual

In addition, software change procedures will be recommended and documented to provide for orderly software changes as required during the development.

Design Substantiation Testing

The demonstration controller will undergo tests to substantiate the overall design concepts as well as to verify the capability of hardware to satisfy the conditions of this particular engine mounted application. Tests will be performed on individual components and sensors to verify correct function and adequate performance. Additional testing will be performed to verify adequate thermal protection and mechanical integrity.

Digital Controller Support Equipment

The controller support equipment will consist of a minicomputer based test console to allow interactive testing of the controller using an input-output simulator. The support equipment is composed of a readily available commercial minicomputer and peripherals. This minicomputer-directed system is capable of maintaining the computer as well as providing a means of loading and debugging software programs.

Advanced Fuel Pump and Metering System

An advanced pump and metering system test program shall be conducted to evaluate the characteristics and integrity for suitability for use on the JTD. Bench testing shall include performance, frequency response, cyclic endurance and contaminated fuel testing.

Turbine Nozzle Area Control Systems

Air motor driven mechanical actuation systems for the JTD variable HP turbine vane and for the LP turbine "Jet Flap" modulation control shall be fully evaluated prior to use on the engine. Bench testing shall be conducted to fully evaluate the electrical interface compatibility with the digital controller, response characteristics, environmental tolerance and functional characteristics. In addition to bench testing, the HP turbine actuation system will be used on the GMA 200 ATEGG demonstration for functional and environmental evaluation prior to use on the JTD.

Sensors/Transducers

The sensors required for implementing the selected control mode fall into the categories of temperature, pressure, position and speed sensing. As these sensors and transducers are defined, they will be evaluated by bench tests and/or on the GMA 200 ATEGG program to assess accuracy, response, stability and for environmental tolerance.

11.0 CONCLUSIONS

This study has shown a method of designing a control to yield optimum steady state performance (minimum sfc and maximum thrust) over a wide range of flight conditions that will also operate adequately transiently. To achieve this goal, the system should position the geometry indirectly by using the variable geometry components to control engine parameters (corrected to the engine inlet) to the optimal values. The following observations indicate the most effective combinations of geometry and engine parameter for the JTD utilized in this study.

- The core nozzle has the best control over low pressure spool speed.
- The duct nozzle can control duct conditions (pressure or flow).
- The high pressure compressor and high pressure turbine are effective controls for compressor discharge pressure and flow.
- The high pressure turbine is an effective control for hot section temperatures.
- The low pressure turbine has limited control on hot section temperatures.
- The low pressure turbine is an effective control for hot section pressures.
- Control by the compressor and turbines is limited since their nominal position is at or near one end of their travel.

The transient performance of the engine and control greatly influence the selection of the best combination of geometry and engine parameters.

The complexity of the control mode for JTD requires a digital controller. The use of the digital controller allows great flexibility in the use of signal synthesis, variable gains and compensation, alternate control modes and selection logic, and the application of optimal and adaptive control. The full potential of the digital controller has not been exploited in this program.

The attainment of optimal steady state performance and adequate transient performance does not tax the dynamics of the actuators although the high temperatures of the engine do present a difficult design requirement. However, the accuracy requirements and operating ranges for some of the pressure and temperature transducers do exceed the performance of today's proven technology.

12.0 RECOMMENDATIONS

Two critical areas need additional and continuing effort to assure successful control of the JTD. The first involves the continued updating of the present engine and control component simulation as component test data (from gas generator, rig and bench testing) becomes available. The control mode must be continually evaluated dynamically on the updated simulation under the projected test conditions. This includes the continued evaluation of more complex actuator and pump models to assure that the simple models within the simulation are adequate and update the simulation where required.

The second area requires the development of components to meet the special requirement of the JTD which include

- o The dual actuation system for the HPC.
- o Actuation systems for the hot section (HPT and LPT)
- o High accuracy (.05 percent full scale) pressure transducers that operate up to the maximum CDP pressure.
- o Temperature transducers that will survive in the hot section environment.
- o Reliable digital computer that can survive the engine environment.

The use of a digital controller opens many fruitful areas of expansion of the control mode given here including:

- o Signal synthesis with special emphasis on the hot section temperatures that are impossible or difficult to measure.
- o Variable compensation of control loop including compensation of thermocouples based upon flow rate estimated from the $\Delta P/P$ sensor.

- Sophisticated fail detection and isolation
- Alternate control modes in case of detected sensor failure.

However, it is recommended that any advanced control mode concentrate on a JTD derivative engine which may or may not be demonstratable on the JTD.

The application of the variable geometry turbofan engine to supersonic aircraft also will require integrating the engine control with inlet control and addition of an augmentor. The supersonic inlet will impose flow and distortion requirements on the control which will impose new steady state and dynamic goals on the variable geometry controls. While the augmentor usually has a separate control mode, the variable geometry will be used to smooth the transition to and from augmentation.

This study concentrated on optimal steady state performance and logical extension is optimal transient performance. A "pseudo optimal transient" mode can also be developed by constructing a steady state operating mode that maximizes the rotor speeds at the expense of sfc. Such a control mode would minimize the effect of the major response lags; the rotor dynamics. Geometry position would be the major modulator of thrust. Also, the application of modern control theory to this problem is a natural since "optimal state variable control" provides optimal transient performance about the steady state operating line. Truly optimal performance may be obtainable by combining optimal state variable control and the optimal steady state control principles developed here.

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