SPACE SHUTTLE ATMOSPHERIC ASCENT FLIGHT DYNAMICS

by J. T. Patha, K. A. Noess, and M. V. Lines

The Boeing Company
Seattle, Washington

INTRODUCTION

An economical Space Shuttle is recognized to be the key to future space exploration. The Space Shuttle is envisioned to consist of a booster and orbiter with each having several flight phases. This paper is concerned with the atmospheric ascent flight phase of the mated composite booster and orbiter.

The composite recoverable Space Shuttle booster and orbiter exhibits unique flight control characteristics. This uniqueness results from large lifting surfaces and aerodynamic and structural assymetrics. An effective load relief technique reduces aerodynamic loads on both the booster and the orbiter. Reducing aerodynamic loads permits decreasing the structural weight of the lifting and stabilizing surfaces. An orbiter payload penalty is caused by trajectory deviations resulting from load relief. However, the net effect of an effective load relief technique is an increase in payload capability.

Atmospheric launch dynamics investigations have been carried out for different configuration types, which include expendable, straight wing, delta wing, and ballistic recoverable boosters.

FACTORS AFFECTING SPACE SHUTTLE ASCENT FLIGHT DYNAMICS

(Figure 1)

Factors that affect the ascent flight dynamics are vehicle mating geometry, vehicle aerodynamic and inertial characteristics, wind disturbances, maximum dynamic pressure, flexibility and slosh dynamics, rigid mode frequency and damping, aerodynamic control considerations, and the booster engine thrust vector actuation system. There also is an interaction between the ascent and entry flight dynamics. The vehicle ascent dynamics in terms of staging conditions have a strong influence on entry dynamics and control.

Typical mated vehicle design constraints are

- 95 percentile wind disturbances (Reference TMX-64589)[1]
- maximum dynamic pressure = 31,200 N/m^2 (652 lb/ft^2)(R-S-1C configuration)
- 3g maximum longitudinal load factor
- ± 5.15 deg TVC deflection (R-S-1C configuration)
- ± 5 deg/sec nozzle deflection rate limit under loaded condition (R-S-1C configuration)
- one engine out capability

FACTORS AFFECTING SPACE SHUTTLE ASCENT FLIGHT DYNAMICS

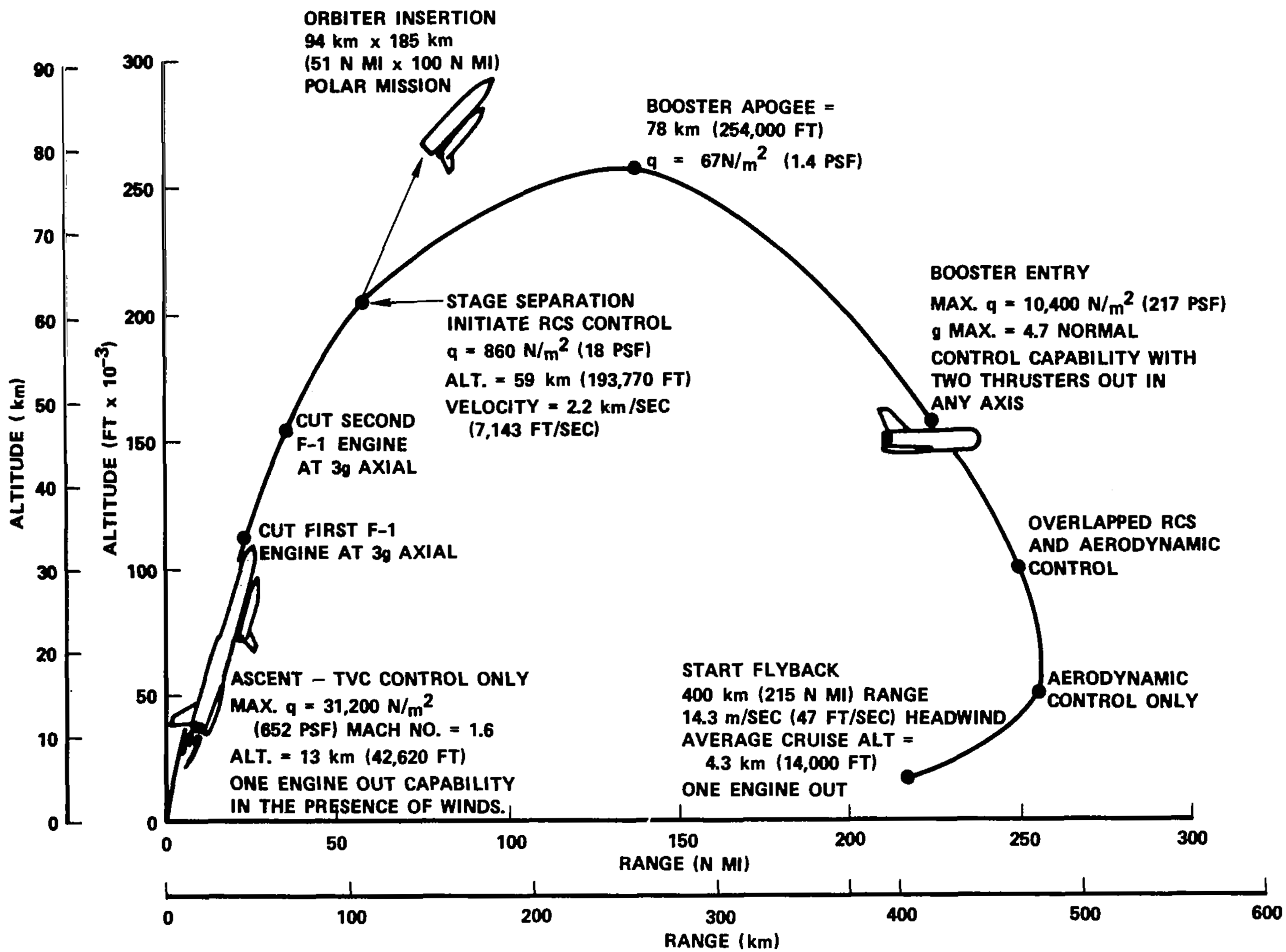


Figure 1

ASCENT FLIGHT DYNAMICS DESIGN OBJECTIVES

(Figure 2)

Stable flight dynamics and minimized system cost are design objectives for boost flight. The ability and need to satisfy these design objectives will vary for different space shuttle vehicle configurations. For example, if the design is not payload critical, then applying load relief to minimize booster structural weight would not be appropriate since this would unnecessarily increase the control system complexity. Also, if "off-the-shelf" hardware is available, then non-optimized but acceptable subsystem performance may be tolerated in order to reduce over-all system cost.

ASCENT FLIGHT DYNAMICS DESIGN OBJECTIVES

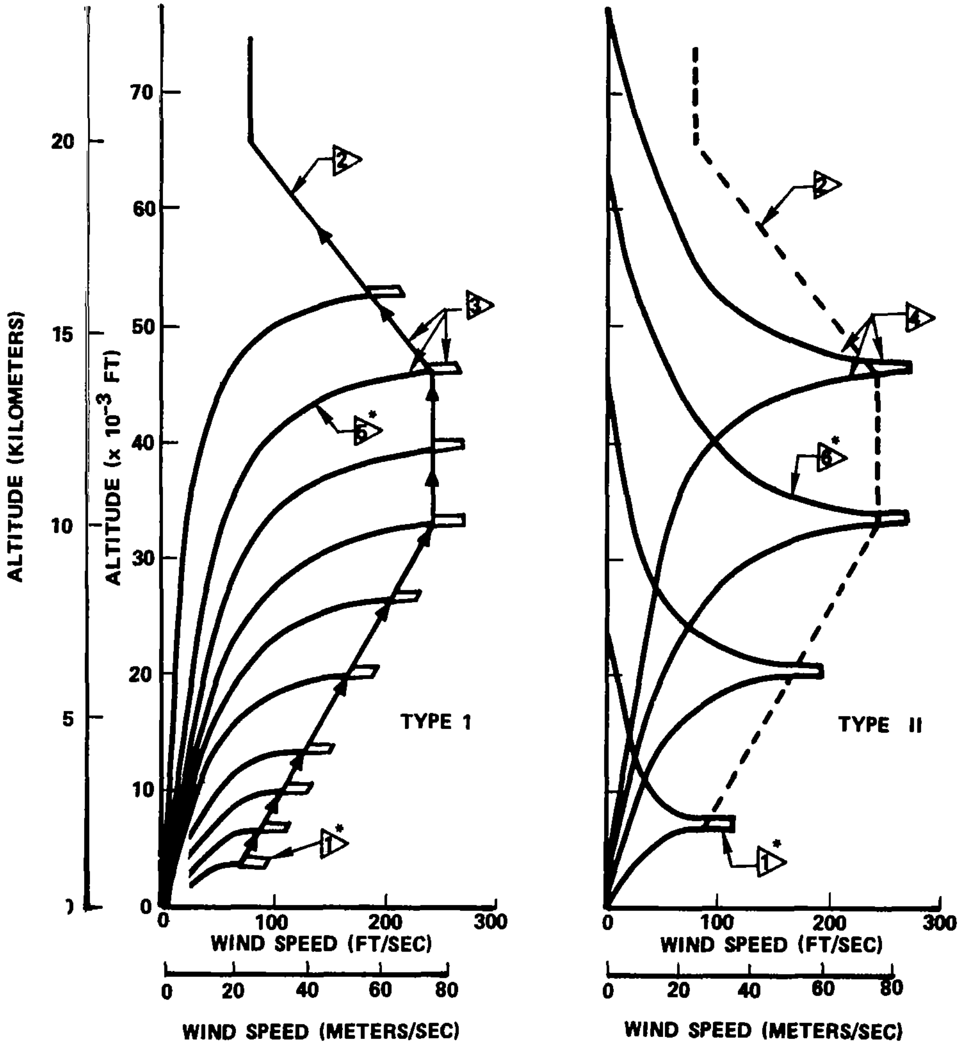
- CONFIGURATION MATING TO MINIMIZE LOADS AND NOZZLE DEFLECTIONS
- CONTROL LAW DEFINITION THAT MINIMIZES NOZZLE AND AERODYNAMIC CONTROL DEFLECTIONS AND RATES
- MAXIMIZE ORBITER PAYLOAD WEIGHT
- ADEQUATE RECOVERY FROM ACTUATOR AND ENGINE-OUT (MEET FAILURE CRITERIA)
- ADEQUATE ABORT CAPABILITY
- MEET FLEXIBILITY MODE STABILIZATION REQUIREMENTS
- SATISFY OTHER VEHICLE AND TRAJECTORY CONSTRAINTS

Figure 2

WIND DISTURBANCES
(Figure 3)

The wind disturbances during the booster ascent phase were derived from TMX-64589.[1] The magnitudes shown were used as head, tail, and cross winds. The Type I profiles are used in trajectory analyses because they result in the most severe flight path penalties. However, the Type II profiles (with back off shears) produce more severe control disturbances and are used to determine nozzle actuation system design requirements. A search with different gust altitudes is performed to establish where the trajectory, vehicle loads, and control system parameters are most sensitive. For a typical case the trajectory performance was found to be most sensitive with low altitude winds, i.e., approximately 1 km (3,280 ft.). Nozzle deflections were found to be most critical with cross winds at an intermediate altitude of 6 km (19,700 ft.), and vehicle loads were most critical for cross winds at an altitude of approximately 10 km (32,800 ft.).

WIND DISTURBANCES



NOTES:

1. SUPERIMPOSED GUST (TYPICAL)
2. 95% DESIGN WIND SPEED ENVELOPE (ETR)
3. TYPE I PROFILE: BUILDUP SHEAR; SUPERIMPOSED GUST AND THEN FOLLOWS THE 95% ENVELOPE (TYPICAL)
4. TYPE II PROFILE: TYPE I WITH BACKOFF SHEAR (TYPICAL)
5. ADJUSTED *99% SHEAR BUILDUP ENVELOPE (TYPICAL)
6. ADJUSTED 99% SHEAR BACKOFF ENVELOPE (TYPICAL)

* 0.85 TIMES 99% VALUES.

Figure 3

LOAD RELIEF CONTROL LOGIC

(Figure 4)

An improved load relief technique which has the potential to increase orbiter payload is described for large boosters with lifting surfaces. This new innovation provides the optimum level of load relief with the minimum trajectory disturbance.

A logic scheme is utilized to limit maximum $q\alpha$ during composite boost and the technique is as follows: Equations (1) thru (3) are the control laws. The control gain λ_3 is a multiplier in the pitch attitude loop as shown in equation (1) and is varied as shown in the diagram. For small values of $q\alpha$, i.e. $\alpha < \alpha_1$, then λ_3 is set equal to 1.0. As $q\alpha$ becomes larger, λ_3, as shown is reduced in magnitude and can result in pure weathercock control if $\alpha > \alpha_2$ and $\lambda_5 = 0$. The variables α_1 and α_2 which are shown in the diagram are computed from equations (4) and (5). It can be shown the short period frequency is maintained approximately constant as λ_3 changes by equation (3). $q\beta$ is limited through λ_4 in equation (2) by a scheme similar to the above technique.

For analytical convenience, α and β have been utilized as the feedback quantities. The equivalent acceleration feedbacks may be used during mechanization. A large number of simulated load relief trajectories have been accomplished with this technique and the rigid mode exhibits good stability characteristics. The vehicle maximum rates when switching from one control mode to another are not excessive, i.e. < 3 deg/sec. The question of flexible mode filtering and slosh requirements with this technique of load alleviation must be analyzed before the final control logic and gains can be selected.

The new contribution to the art is the application of the non-linear gain technique to optimize load relief. The non-linear technique minimizes the time the load control law is utilized for any given gust, i.e. load relief is used only if the load exceeds a preselected level. This has the net effect of minimizing the trajectory deviation resulting from load relief and results in maximizing the payload capability.

NOTE:

λ_2 and λ_5 pre-set constants

δ Nozzle deflection

q Dynamic pressure

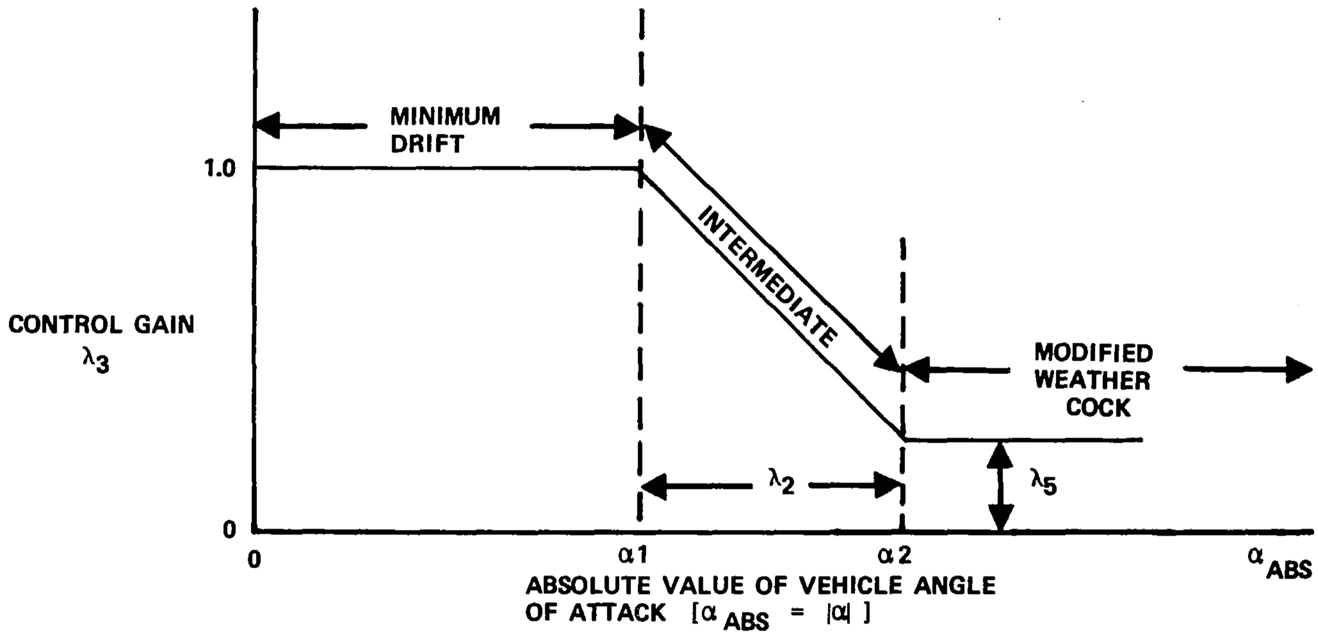
α Angle of attack

θ Attitude angle

K_α Computed for minimum drift law

1049

LOAD RELIEF CONTROL LOGIC



$$\delta Y_C = \lambda_3 \ K_\theta \ (\theta - \theta_C) + K_{\dot\theta} \ \dot\theta + \bar{K}_\alpha \alpha \quad \text{(PITCH)} \quad (1)$$

$$\delta Z_C = \lambda_4 \ K_\psi \ (\psi - \psi_C) + K_{\dot\psi} \ \dot\psi + \bar{K}_\beta \ \beta \quad \text{(YAW)} \quad (2)$$

$$\delta X_C = K_\phi \ (\phi - \phi_C) + K_{\dot\phi} \ \dot\phi \quad \text{(ROLL)} \quad (3)$$

$$\alpha_2 = \frac{(q\alpha)_{MAX}}{q} \quad (4)$$

$$\alpha_1 = \alpha_2 - \lambda_2 \quad (5)$$

$$\bar{K}_\alpha = K_\theta \ (1 - \lambda_3) + K_\alpha \quad (6)$$

Figure 4

PAYLOAD SENSITIVITY TO LOAD INDICATOR
(Figure 5)

The sensitivity of orbiter payload weight to load parameters $q\alpha$ and $q\beta$ is shown for a typical large lifting vehicle. As $q\alpha$ and $q\beta$ are increased the payload penalty increases. The payload penalty results primarily from increased structural weight of booster and orbiter wings and tail surfaces. It is of interest to note the orbiter has the greater "payload lever." This is due to the fact that a weight reduction of about 5.45 kg (12 pounds) on the booster is required to gain 0.454 kg (one pound) of additional payload into orbit, while the ratio is 1 to 1 for the orbiter.

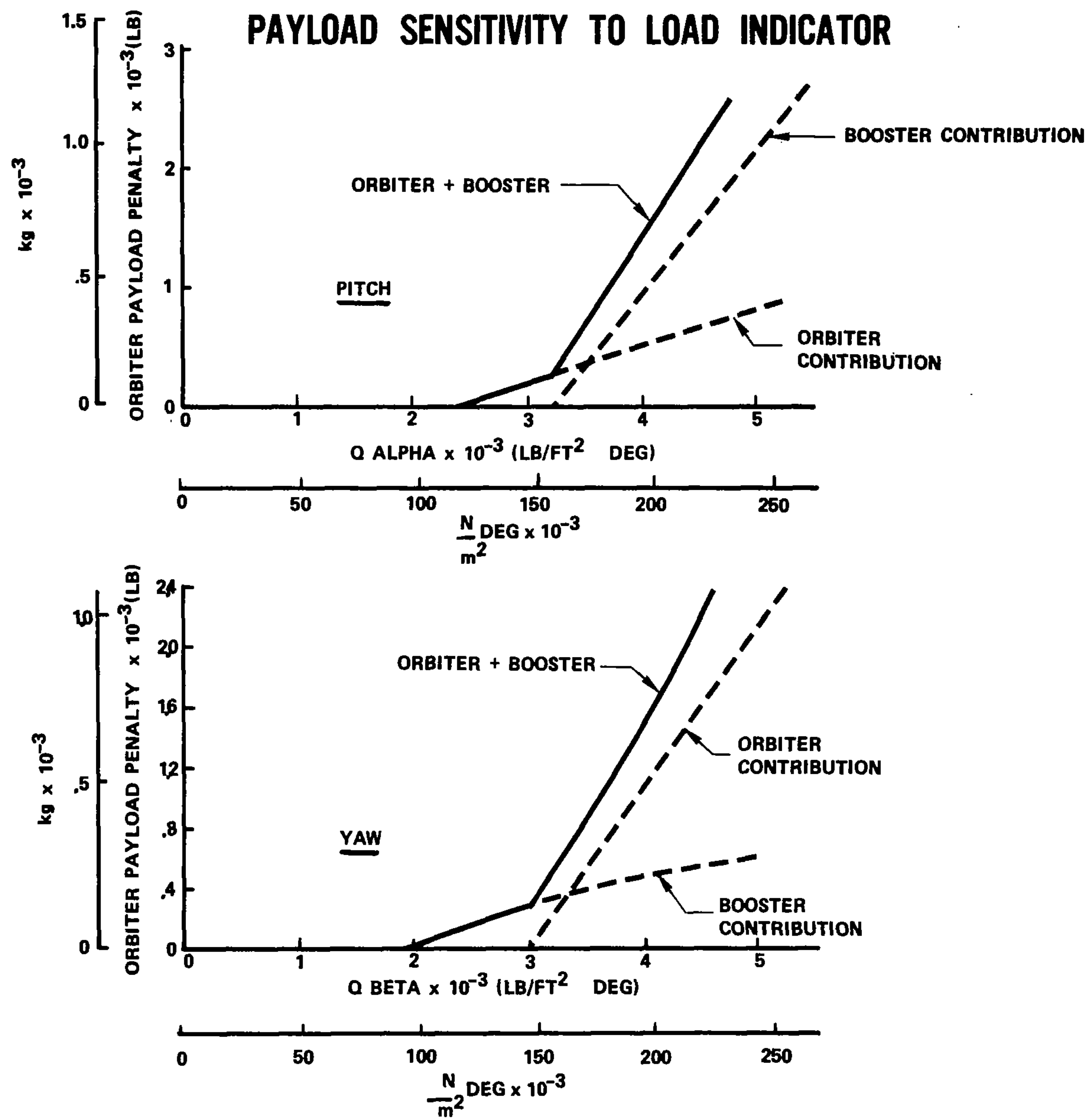

PAYLOAD SENSITIVITY TO LOAD INDICATOR
kg x 10^{-3}
ORBITER PAYLOAD PENALTY x 10^{-3} (LB)
1.5
1.0
.5
0
3
2
1
0
ORBITER + BOOSTER
BOOSTER CONTRIBUTION
PITCH
ORBITER CONTRIBUTION
0
1
2
3
4
5
Q ALPHA x 10^{-3} (LB/FT^2 DEG)
0
50
100
150
200
250
$\frac{N}{m^2}$ DEG x 10^{-3}
kg x 10^{-3}
ORBITER PAYLOAD PENALTY x 10^{-3} (LB)
1.0
.5
0
2.4
2.0
1.6
1.2
.8
.4
0
ORBITER + BOOSTER
ORBITER CONTRIBUTION
YAW
BOOSTER CONTRIBUTION
0
1
2
3
4
5
Q BETA x 10^{-3} (LB/FT^2 DEG)
0
50
100
150
200
250
$\frac{N}{m^2}$ DEG x 10^{-3}

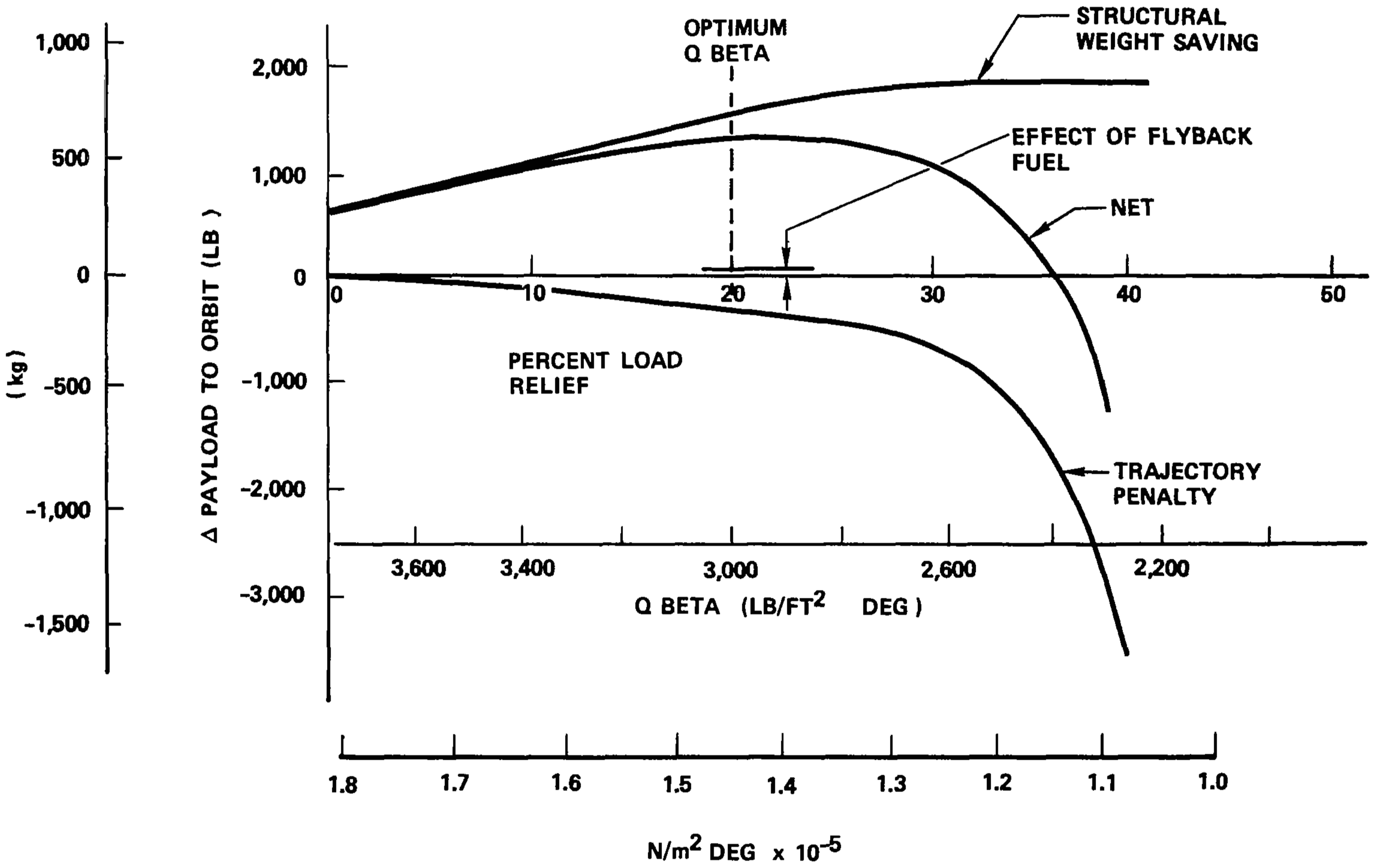
Figure 5

STRUCTURAL SAVING AND TRAJECTORY PENALTY
(Figure 6)

To find the optimum load relief level, Type I winds were investigated across the range of gust altitudes from 1 to 12 kilometers. A typical result from a 6 kilometer gust is shown. The structural weight saving results from the payload sensitivity to load indicator which was shown in Figure 5. The cross wind structural weight savings shown in Figure 6 is referenced to the maximum orbiter payload weight penalty without load relief which occurs for a Type I wind with a gust at 10 km altitude. This reference condition results in a maximum $q\beta$ of 201,000 N/m^2 (4200 psf deg) and a corresponding 863 kg (1900 lb) orbiter payload penalty shown in Figure 5. No load relief (0%) for the 6 km gust wind in Figure 6 results in approximately 180,000 N/m^2 (3750 psf deg) $q\beta$, which is an improvement of approximately 272 kg (600 lb) payload compared to the reference maximum $q\beta$. Further reduction of $q\beta$ in Figure 6, i.e. less than 180,000 N/m^2 (3750 psf deg $q\beta$), causes a further reduction in the orbiter payload penalty as shown in Figure 5 and results in the structural savings shown in Figure 6.

The trajectory penalty is a result of the energy required to compensate for the flight path deviation which results from rotating the vehicle into the wind to achieve load relief. The addition of the trajectory penalty and structural saving results in the net incremental increased or decreased payload to orbit. The cross wind disturbance causes the flyback range to be approximately 5.6 km (3 nautical miles) less. This has the effect of increasing the orbiter payload by the relatively small amount shown. The payload change is caused from the reduced flyback fuel. The effect of incremental changes in flyback range caused by head and tail winds during entry were also found to be equally small.

STRUCTURAL SAVING AND TRAJECTORY PENALTY (CROSSWIND)



NOTE: 6 KILOMETER GUST

Figure 6

1053

POTENTIAL PAYLOAD IMPROVEMENT FROM LOAD RELIEF

(Figure 7)

A summary of the load relief payload to orbit trade study is shown. For the case investigated, load relief gave a total payload improvement of 880 kg (1940 pounds). The net cross wind payoff of 500 kg (1100 pounds) resulting from structural improvement occurs at a $q\beta$ of 140,000 N/m^2 degree (2930 psf degree). A lesser payoff for head winds of 382 kg (840 pounds) occurs at approximately 110,000 N/m^2 degree (2300 psf degree) $q\alpha$. Load relief for a tail wind is not critical from a payload standpoint because considerable energy is added from the wind velocity component directed approximately parallel and with the same heading as the vehicle velocity vector. However, it is important to load relief to the same value of $q\alpha$ for tail winds as for head winds in order to take advantage of the structural weight savings for the head winds.

POTENTIAL PAYLOAD IMPROVEMENT FROM LOAD RELIEF

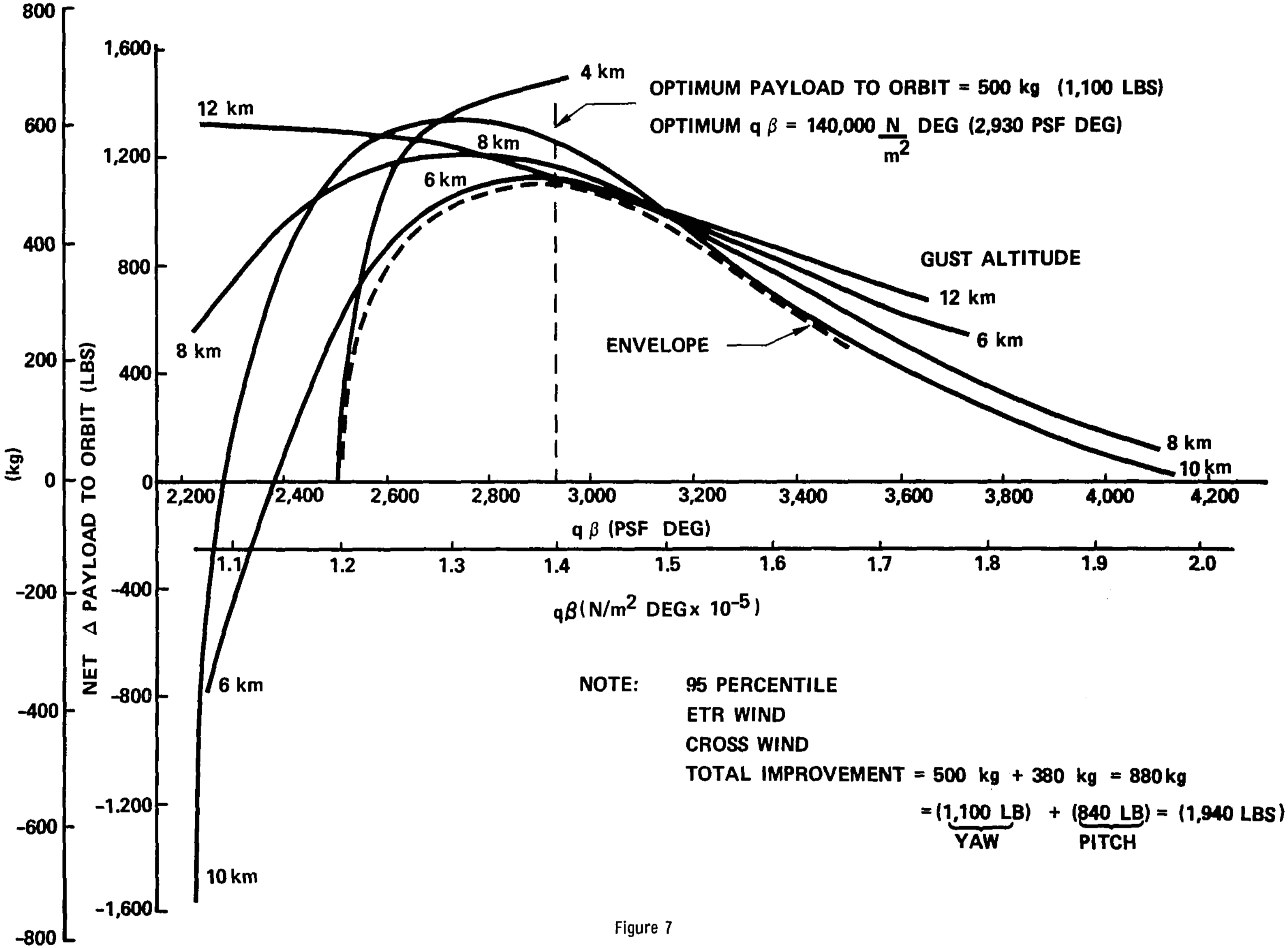


Figure 7

LOAD RELIEF EFFECT ON BOOST TRAJECTORY
(Figure 8)

The effect of load relief on the trajectory is shown in Figure 8 for a cross wind with a gust occurring at 10 kilometers. When no load relief is employed the vehicle drifted to a large positive cross range. Then if excess load relief is employed, the vehicle went to a large negative cross range. When a near optimum load relief was employed the cross range was only 0.763 kilometers (2500 feet) at staging.

LOAD RELIEF EFFECT ON BOOST TRAJECTORY

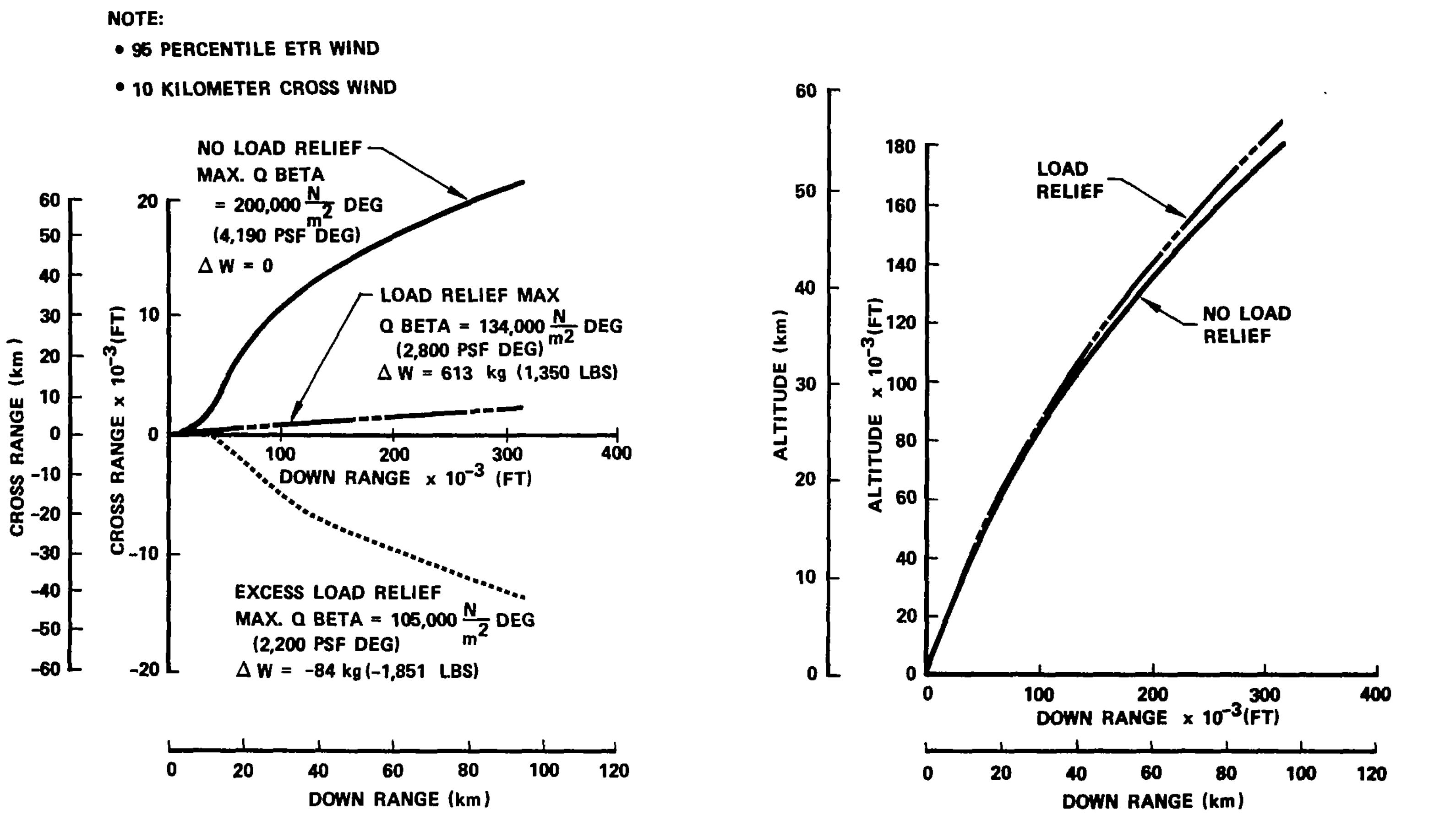


Figure 8

1057

NOZZLE DEFLECTION DYNAMICS

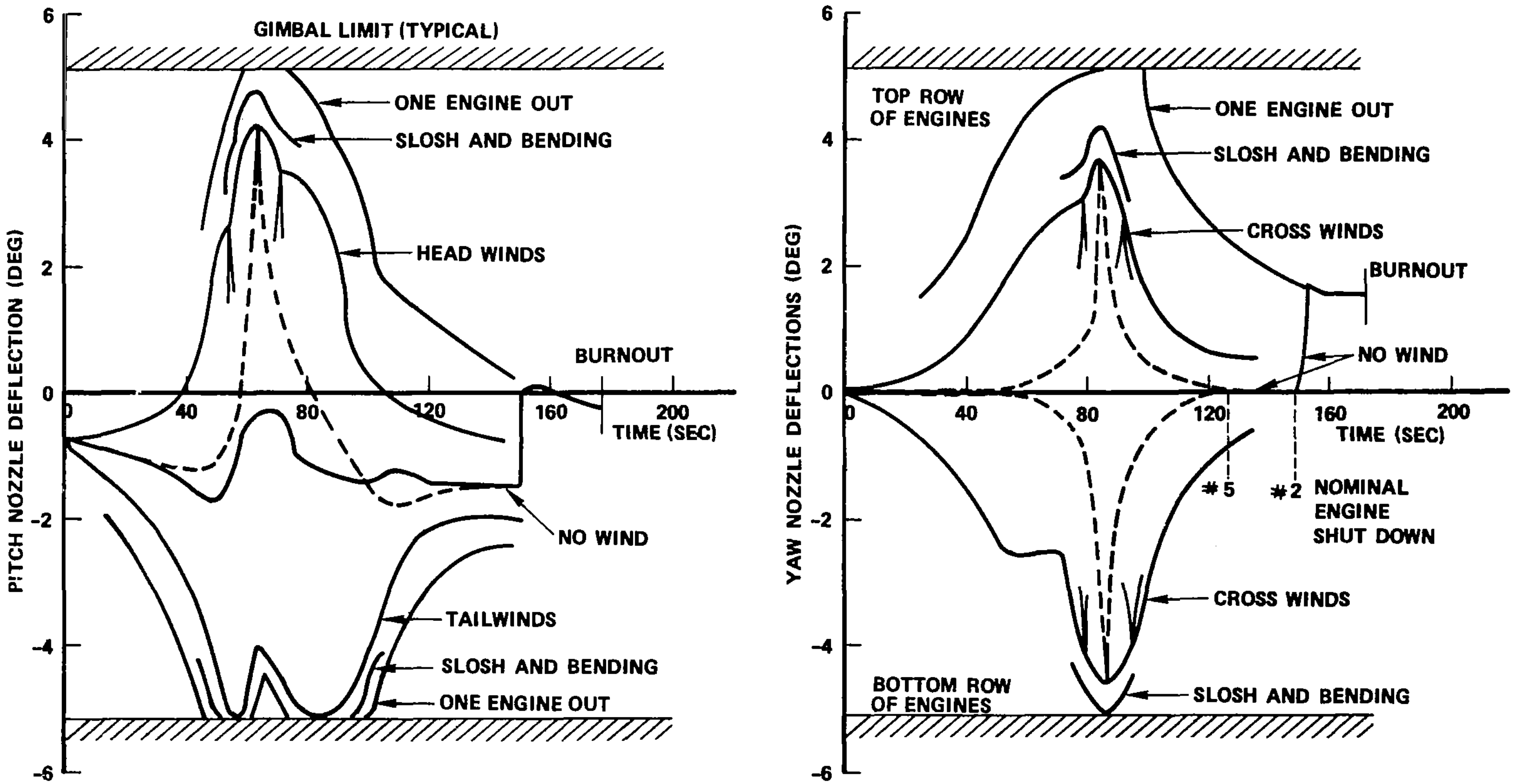
(Figure 9)

Typical pitch and yaw plane gimbal requirements are shown. The nozzle deflection time histories with and without disturbances are shown. Engine shutdown results in unsymmetrical moments and the vehicle is retrimmed at about 150 seconds. The tail winds require the nozzle to deflect to the limit of -5.15 degrees, while the head winds require a maxim deflection of 4.0 degrees. The additional required nozzle deflection for one engine out and slosh and bending were added to the wind requirements to give the combined envelope. The elevons were set at -10 degrees. The elevons would be set more negative in a final analysis to center the wind deflection requirements in pitch.

The yaw nozzle deflection requirements are also shown. These data were developed in a manner similar to the pitch requirements.

As shown the requirements in pitch and yaw exceed 5.15 degrees with one engine out in the presence of 95 percentile winds. However, the vehicle is aerodynamically stable and the vehicle and trajectory transients are acceptable for the short time (less than 2 seconds) the nozzle deflection is saturated. Peak body axis rates during load relief transients are less than 3 deg/sec. Peak TVC nozzle rates are 5 deg/sec for a time period of less than 0.5 second.

NOZZLE DEFLECTION DYNAMICS



NOTE:
- 95 PERCENTILE ETR WIND
- NO AILERON CONTROL
- -10 DEG ELEVATOR

—— ENVELOPE OF TIME HISTORY
---- TYPICAL TIME HISTORY

Figure 9

1059

CONCLUSIONS

(Figure 10)

To be complete, the atmospheric launch dynamics investigations of the composite vehicle must include integrated studies of: Wind Disturbance Definition; Load Indicator Comparison; Space Shuttle Ascent Simulation Requirements; Vehicle Mating; Analysis of Control Laws; Engine Gimbal Studies (including engine out and hydraulic failure); Control Sensitivity Study; Aerodynamic Sensitivity Study; Ascent Guidance Techniques; and Configuration Comparisons.

The developed load alleviation control law was found to have the potential to significantly improve payload to orbit capability. The dynamic interaction of the non-linear relief technique with slosh and vehicle flexibility must be investigated in detail in order to complete the analysis. It should also be noted that the importance of payload savings through load alleviation is highly configuration dependent. Some shuttle configurations are not payload critical and therefore the added complexity of load alleviation may not be warranted.

Of interest is that the maximum vehicle loads, nozzle deflections, and trajectory deviations occur at different gust wind disturbance altitudes.

It was also determined that a severe trajectory deviation with attendant payload loss will result if the vehicle has too much inherent aerodynamic stability, i.e., the vehicle "weathercocks" too much into the wind with a practical control authority.

CONCLUSIONS

- CRITICAL LOADS, NOZZLE DEFLECTION AND TRAJECTORY DEVIATIONS OCCUR AT DIFFERENT GUST WIND DISTURBANCE ALTITUDES.
- IF THE VEHICLE IS TOO AERODYNAMICALLY STABLE OR IF LOAD RELIEF IS OVER APPLIED – THEN A PAYLOAD LOSS WILL OCCUR FROM THE TRAJECTORY DEVIATION.
- THE NONLINEAR LOAD RELIEF TECHNIQUE IS DEMONSTRATED TO HAVE A POTENTIAL SIGNIFICANT SPACE SHUTTLE PAYLOAD TO ORBIT IMPROVEMENT 908 kg (2,000 LB) TYPICAL FOR CONFIGURATIONS WITH LARGE LIFTING SURFACES.
- PROPER ORBITER AND BOOSTER VEHICLE MATING MUST BE ACHIEVED BEFORE LOAD RELIEF CAN BE APPLIED ADVANTAGEOUSLY.
- SPACE SHUTTLE LOAD RELIEF REDUCES NOZZLE DEFLECTION REQUIREMENTS APPROXIMATELY 40 PERCENT.

Figure 10

REFERENCE

1. Daniels, Glenn E., ed.: Terrestrial Environment (Climatic) Criteria Guidelines for Use in Space Vehicle Development, 1971 Revision. NASA TM X-64589, 1971.