Evolving Solid Boosters For Space Missions

System studies have established big solids as real contenders for space vehicles capable of delivering 250-500 tons to orbit

BY WILLIAM COHEN
NASA OFFICE OF MANNED SPACE FLIGHT, WASHINGTON, D.C.

GREAT PROGRESS has been made during the last two years in proving the technology of large solid-propellant rocket motors and demonstrating their manufacturing and handling methods.

Not a great deal of proof has been produced, however, in relation to the question of the performance of a manned vehicle with a solid-propellant stage. I do not mean by this that there has been lack of calculation. Dozens of possible vehicle configurations, complete with thrust levels, payloads, and dimensions, have been produced. The little sketches on page 61 illustrate a few of the vehicles resulting from such computation.

The basic question unanswered by this sort of computation is related to the proposed launch vehicles and payloads: What are the vehicle, payload, and system problems arising from the use of a solid-propellant booster stage? The answer to this question does not stem from knowledge of propellant burning rate or nozzle design. It must come from a system organization which gives knowledge of all aspects of launch-vehicle and payload technology and which allows analysis of logistic and launch problems.

During the past year or so, NASA has established contracts and in-house programs to deal with this question. In doing this, the space agency has been carrying out a general policy on the potential use of solid-propulsion elements for large space vehicles. This policy was recently stated explicitly by Thomas Dixon, NASA deputy associate administrator, to a subcommittee of Congress: "We are giving solid rockets equal consideration with liquid rockets in making our decisions on future launch vehicle designs." This paper will summarize the results of these vehicle and system studies in relation to payload, vehicle, and solid-motor technology.

What are the special vehicle payload and system problems growing from the use of solid-propellant booster stages and what do the problems mean to the design of solid-propellant rocket motors? Let me attempt to provide some answers to these questions by using a typical example which may be called a baseline vehicle. Much of the data in the following discussion was generated under Contract NAS 8-2438 with Boeing Co.

Some fundamental specifications are placed on the baseline vehicle, and of course on the solid-propellant stage, by payload limitations and by guidance requirements. These may warrant detailed scrutiny if they result in severe mission penalties, but for the time being we will accept them. As far as the use of solid-rocket motors is concerned, the most influential specification is that the maximum aerodynamic loading on the vehicle be less than 1000 psf. This specification controls the thrust-to-weight ratio and in turn establishes the burning time of the solid motors. Other basic specifications are as follows: maximum g-load, 8; minimum thrust-to-weight ratio, 1.4; vehicle first-mode bending frequency, about 1 cps.

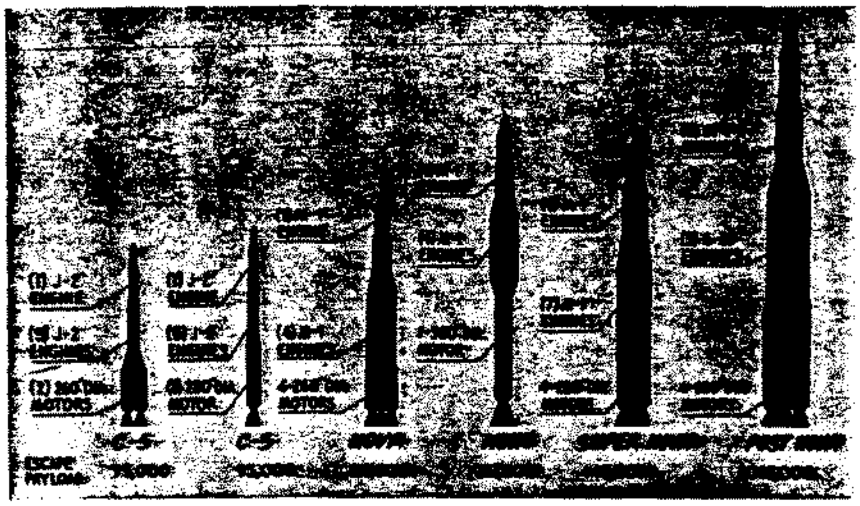
The baseline vehicle in question would deliver about 1/2-million lb into a parking orbit with two stages of propulsion. The first stage would consist of solid-propellant motors, the second stage would have liquid hydrogen–liquid oxygen engines delivering about 1.2-million lb of thrust each (called M-1 engines). We will not discuss in detail the propulsion elements of the third, or escape, stage other than to say that it probably would use liquid hydrogen–liquid oxygen engines smaller than the M-1 engine.

Now let us attempt to select the dimensions and performance characteristics of solid motors most desirable for this vehicle. What is the best combination of motor producibility, reliability, manufacturing costs, growth potential, handling problems, etc.?

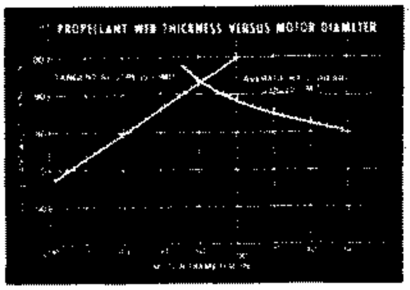
First, consider rough boundaries of accepted solid-rocket state of the art. The graph on page 61 shows one estimate of this territory. This rocket contractor believes that motors 360 in. in diam and with thrust levels of about 12-million lb can be made in what he designates the state of the art. This does not signify, of course, that there is no gradation of difficulty with size or thrust level. Beyond the 12-million-lb thrust level, problems of manufacture of inert parts and problems of propellant structure and composition become potentially severe enough to require individual development programs.

Having obtained a rough map of the territory, we seek the best motor diameter, or more specifically, the best length-to-diameter ratio. Here we must consider the total vehicle. A vehicle fineness ratio exceeding about 10 is not considered desirable because it has implications for the first-mode bending frequency which may lead to guidance problems. The solid motor length-to-diameter ratio, therefore, should be no greater, we believe, than 8 : 1. High length-to-diameter ratios may lead to high internal gas velocity or, stated in another way, to low port-to-throat ratio. We would expect from our knowledge of solid-rocket

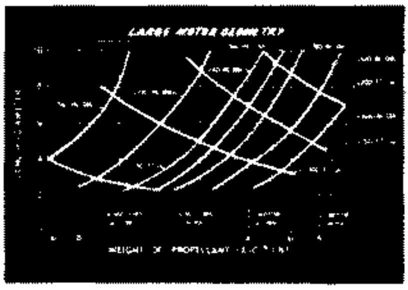
SOLID-BOOSTED LAUNCH VEHICLE STUDY CONFIGURATIONS



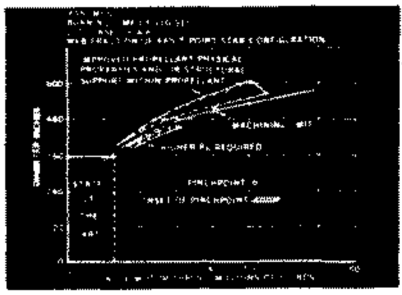
WEB THICKNESS VS. MOTOR DIAMETER



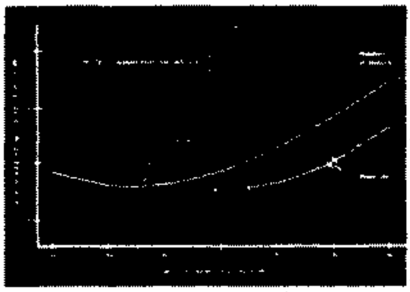
LARGE-MOTOR GEOMETRY



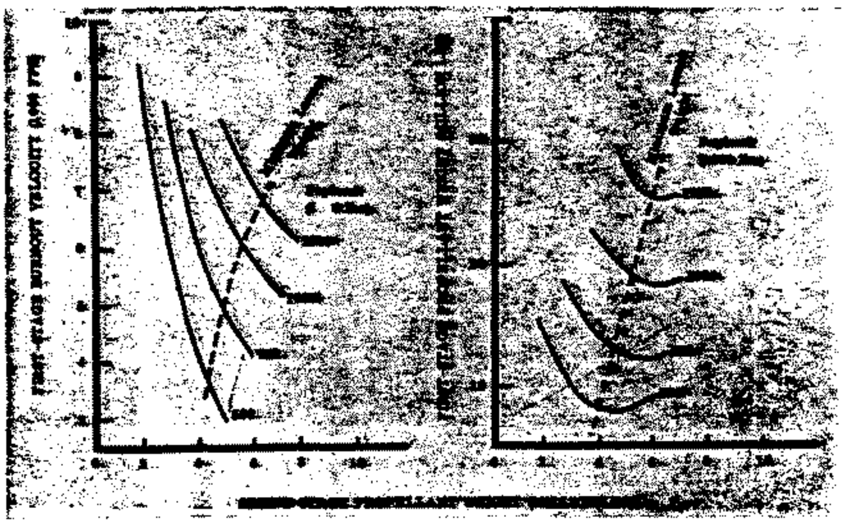
MOTOR DIAMETER CAPABILITIES



MOTOR DIAMETER EFFECTS



PROPELLANT WEIGHT AND VELOCITY FOR FIRST STAGE



technology that a minimum port-to-throat ratio of 1.5 will be a conservative design.

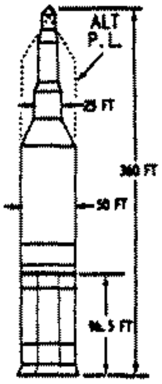
Another consideration will be possible requirements for growth in the solid motor. If we select a length-to-diameter ratio near the limits tolerable, we will hamper future growth. A graph on this page presents a matrix of length-to-diameter ratio, diameter, and propellant weight that blankets the region of interest. Selection of the motor dimensions is made roughly from such a matrix, but the final selection will result from repeated computations considering numerous factors not clearly defined at this point. For example, the selection of motor dimensions will influence the design of the stage structure and thereby the stage mass fraction. Again, selection of motor diameter will be influential in establishing the maximum expansion ratio of the nozzle and therefore will determine the usable specific impulse to be obtained from the stage.

These conflicting inputs or requirements will be analyzed in detail later in the program, but perhaps another map must be considered at this point. What is the ability of the propellant to withstand the stresses that will be placed on it as motor diameter increases? The graph on this page shows areas of limitation growing from shear stresses and tangential stress in a typical solid-propellant motor. Obviously, we should attempt to stay clear of the critical boundaries indicated by this point.

Now other vehicle parameters are introduced, and computations are made of the effect of variables such as combustion pressure. The graph at left, covering propellant weight and velocity effects, shows some important relationships and indicates the limitation on second stage thrust-to-weight ratio. More refined analysis continues, and by the time the information shown graphically here on motor-diameter effects can be plotted, a fairly refined concept of the stage and vehicle is being generated. The graph above right shows that motor diameter is not powerful in establishing launch weight in the size range from 180 to 280 in. in diam.

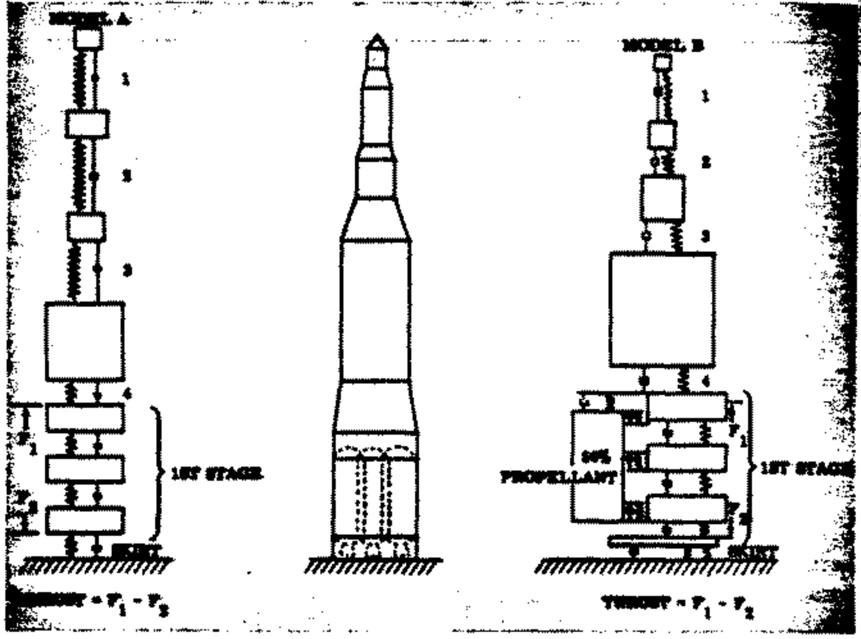
Further computation now relates the burning time of the first stage to the launch weight and second-stage properties, as shown in the graph on page 62. Here, we see that the burning time of the first-stage motors for the 1/2-million-lb payload is about 96 sec.

Finally, we see this vehicle design for meeting the specified payload and mission limitations:

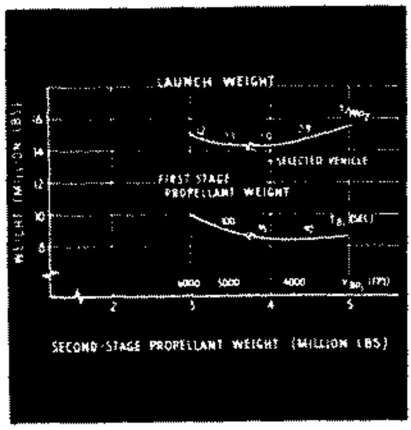


Orbital payload	508,000 lb
Escape payload	203,000 lb
Second stage	
W_{p_2} (Lox/LH_2)	3,750,000 lb
Engines	4 M-1s
F_2 (VAC)	4,800,000 lb
λ'_2 (at ignition)	0.920
W_{02}	4,090,430 lb
First Stage	
W_{p_1} (solid)	8,700,000 lb
Motors	260-in. diam
F_1 (SL)	21,200,000 lb
tb	106 sec
λ'_1 (at ignition)	0.877
W_{01}	9,927,800 lb
Total vehicle gross wt.	14,526,230 lb

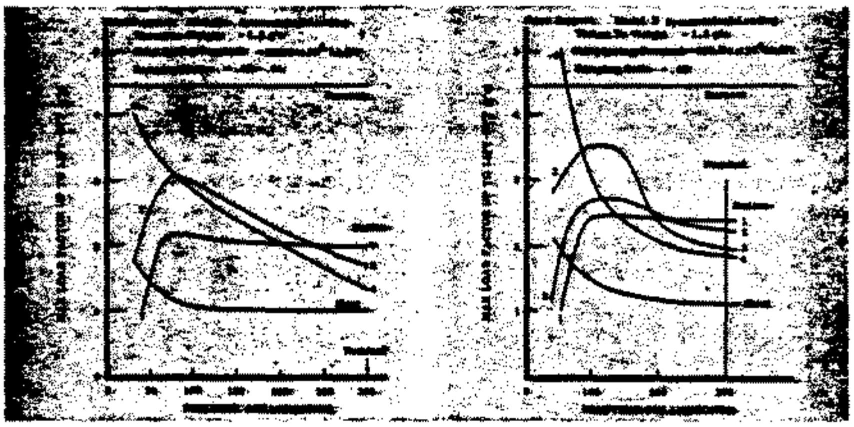
DYNAMIC MODEL OF 250-TON SKIRT-SUPPORTED VEHICLE



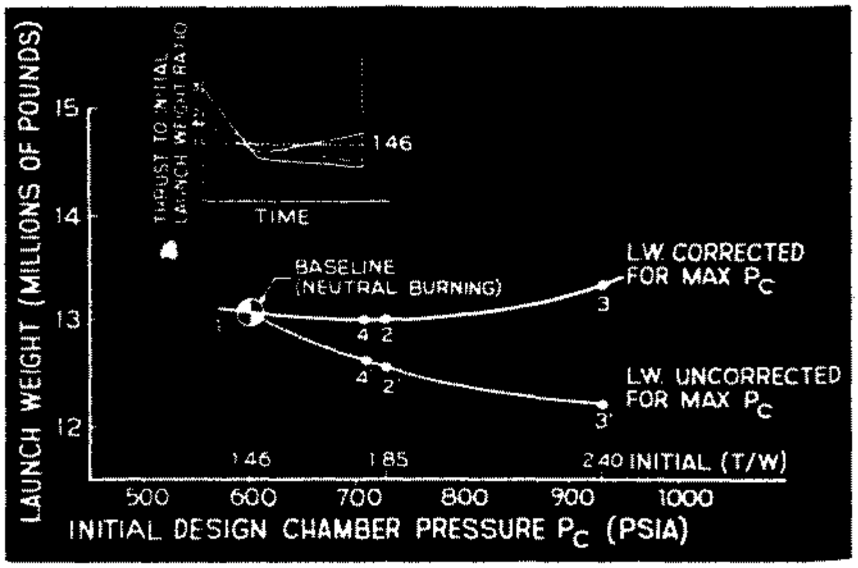
STAGE SIZING



PRESSURE-RISE-TIME EFFECTS



TAILORING FIRST-STAGE THRUST—TIME HISTORY



The four solid-propellant motors 260 in. in diam contain a total of 8.7-million lb of propellant. The thrust at launch from this solid-propellant stage would be 21.2-million lb. This stage, together with a second stage made of four liquid-propellant M-1 engines, could place a payload of 508,000 lb into an 177-km orbit. The vehicle, including payload, runs about 360 ft high and has a fineness ratio of about 8 to 1. Its first-stage diameter would be 50 ft.

This solid-propellant stage is a reasonable compromise to accommodate the limitations of motor manufacture, stage manufacture, and growth potential. The motors can grow in length to give a stage containing 19.8-million lb of propellant and providing

42-million lb thrust at takeoff. This stage, with an appropriate second stage, can place 915,000 lb into orbit. The limitation on growth of this vehicle comes not from the limitations of either the first- or second-stage motors, but from the fact that the vehicle's first-mode bending frequency begins to drop to a hazardous region when further lengthening of the vehicle occurs.

Having established that solid motors of the proper dimensions, thrust level, burning time, and propellant composition can be made, we face the important question of vehicle dynamics, control, and stage operation. Many structural details must also be involved in analysis of these parameters. The first influence considered here will be the dynamic effect on the vehicle due to the starting transients of the solid motors. In doing this, dynamic models of the vehicle are established, as illustrated on page 62, one based on the assumption that the solid propellant provides no damping and the second on the assumption that 50% of the solid propellant effectively provides damping.

The graphs on page 62 give the results of the analysis when all motors ignite simultaneously. It can be seen that for reasonable ignition times the loads at the various locations along the vehicle are small and well below the load factor produced by the peak load at the burnout of the first stage. The effect of propellant damping is found to be negligible on the load factors up to the time of liftoff. After liftoff, the load factors for Stations 1 through 4 are considerably greater. The largest factor occurs when the oscillations of the upper stations are in phase. This takes place about 500 millisec after ignition and subsequently damps out. Peak load factors after liftoff for the Model B configuration (see diagram on page 62) occur at Station 1 with a ratio of 3.78. This is still below the maximum factor occurring at burnout of the vehicle. The peak value is greatly reduced if the ignition period is 500 rather than 300 millisec. The value at Station 1 then becomes 1.83.

Very short ignition time—for example, 80 millisec—will cause a large increase in load factors after liftoff—possibly 500% greater than the factors for the 500-millisec ignition. This probably results from the fact that the rise time is approaching the system's natural period.

The load factor for the normal ignition period of 300 millisec was computed on the assumption of thrust-to-weight ratio of 1.5. A higher thrust-to-weight ratio (for example, 2.5) results in greater load factors. For 2.5 the peak load factor after liftoff becomes 4.43, close to the peak produced at burnout.

At first glance it would seem that the effect of nonsimultaneous ignition of the four motors, a situation which is quite likely to occur, will be important. In analyzing this effect, the following ignition sequence was predicted. The right motor was loaded first; the two central motors were loaded 50 millisec later; and finally the fourth motor was ignited 100 millisec after the two central ones. This unsymmetrical ignition and loading resulted in slightly lower load factor in the upper portion of the vehicle. Very little rotation of the vehicle occurred due to unsymmetrical loading up to the time the vehicle began to lift off; and as will be shown later, the effect of the unsymmetrical thrust after liftoff was trivial, easily compensated for by the control system.

Now let us look at the influence of the motor's thrust-time curve on payload or, more specifically, on the stage mass fraction and control requirements. It would seem desirable to tailor the thrust-time curve of the motor to optimize performance in relation to aerodynamic effects. Some work along this line has been done by practically every solid-rocket company, and many are convinced that special curves are indeed desirable. The system studies described here, however, which included the variations of thrust-time curves and the effect on stage mass-fraction resulting from the change of thrust-time curve, came to a rather surprising conclusion. *There seems to be little or no weight advantage or payload advantage to be gained from the specification of special thrust-time curves.* The graph on page 62 illustrates this situation, showing little difference in launch weight when corrections are made for change in stage mass fraction.

A very real and important problem in relation to the use of solid-propellant motors now must be considered in detail. This problem arises from the variations that must be expected in performance among a cluster of four motors in a stage. The extremes of payload capability must be realistically ascertained in terms of known variance in burning time and propellant load. The motor variations and tolerances for this analysis were obtained by examination of recent quality-control experience in large-scale propellant processing and in the manufacture of inert components for motors 100 and 120 in. in diam. The tolerances found in these real programs were combined in sign to establish maximum possible variation.

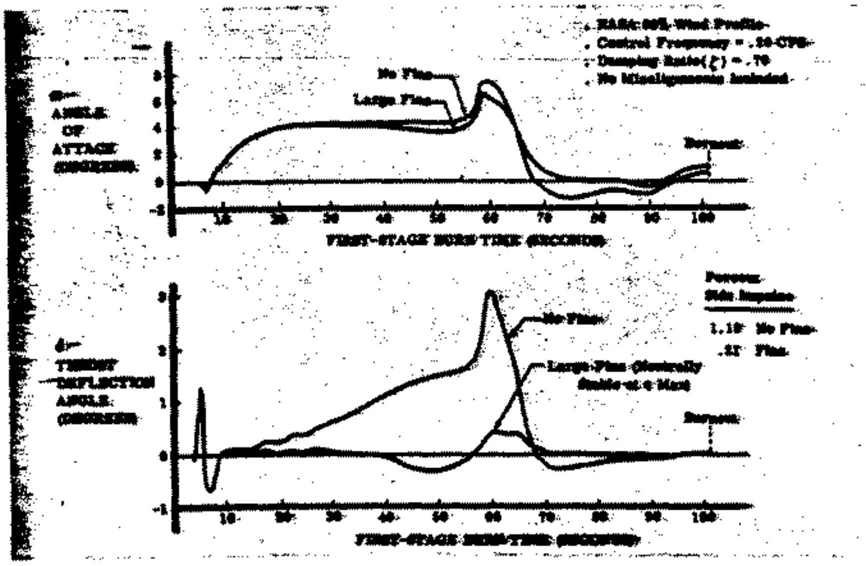
Motor performance parameter variations were calculated, using minimum and maximum combinations of the variables. This is a conservative approach in that the probability is remote that greater variance will ever

MOTOR-TO-MOTOR-VARIANCE PARAMETERS FOR 260-IN. DESIGN

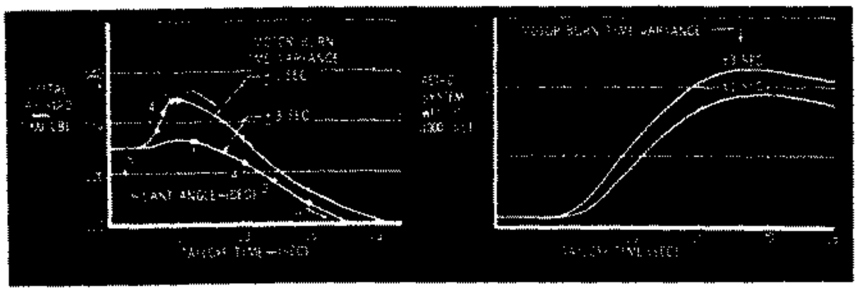
Variable	Nominal mean	Std. dev.[1]
Propellant burning rate, ips		
First	0.57[3,2]	±0.08
Second	0.57[3,4]	±0.035
Propellant density, lb/in^3	0.064	±0.208
Characteristic exhaust velocity, fps	5278	±0.316
Initial nozzle-throat diam, in.	87.0	±0.023
Initial exit diam, in.	214.0	±0.0603
Nozzle-throat-insert erosion rate,[5] ips	0.006	±5.55

1. Percent of mean for 260-in. grain.
2. 600 batches are assumed to be required for the 2,178,000 lb loaded in the 260-in. motor. Batch-to-batch variation of ±2% (max.) is assumed. The motor-to-motor performance variation is greatly reduced from this value because of the large number of batches involved. These assumptions must be re-examined for continuous propellant-production processes.
3. At 600 psia for non-erosive burning.
4. For 0.5-F temperature difference between motors, variations in $_{\sigma}K$ were neglected.
5. Pressure-molded graphite cloth and phenolic resin.

CONTROL TIME HISTORY



EFFECTS OF TAILOFF TIME

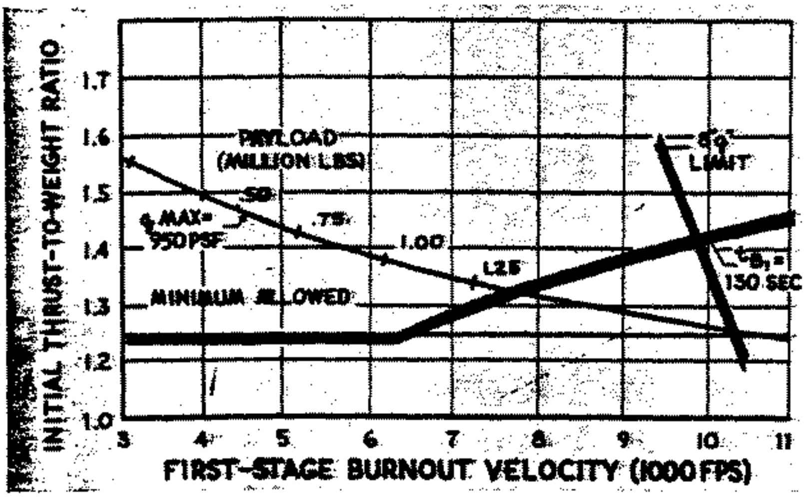


(a) On *Payload*

(b) On Retro System Single-Plane Separation

CLUSTER CONCEPTS

PERFORMANCE BOUNDARIES (FOUR M-1s in STAGE 2)



occur. The table on page 63 give the nominal values of the pertinen motor variables and their standard deviations over a temperature rang centered about 80 F. By combining the various extremes shown in the table for a 260-in. motor, operating a temperatures of 60, 80, and 100 F, results were obtained which show the effects of the simultaneous occurrence of the three-sigma variation of the motor parameter, with various parameters combined in sign to produce the worst possible condition.

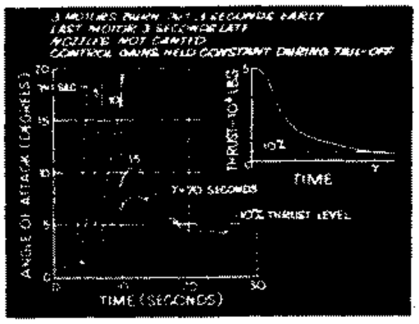
The major effect was produced by the lower variance limit on thrust at the lower temperature. This combination reduced thrust-to-weight ratio to a value of 1.39 rather than the nominal value of 1.46. The effect of this extreme is either a payload loss or the expenditure of some of the reserve velocity increment designed into the upper stages of the vehicle. It results in 10,000-lb reduction in the orbital payload. Alternatively, it would take 30% of the velocity reserve designed into the second stage.

The ignition pressure-rise rate is influenced by the same motor variables that influence steady-state chamber pressure and thrust. In the occurrence of a limit variance, therefore, the fast ignition transient will occur in the same motor with high steady-state thrust. Our studies indicate, again, that motor variances at ignition and liftoff do not cause shock loading or control problems because of the very rapid thrust buildup in relation to the control frequency. It must be understood, of course, that this satisfactory situation does not cover failure of one or more motors to ignite.

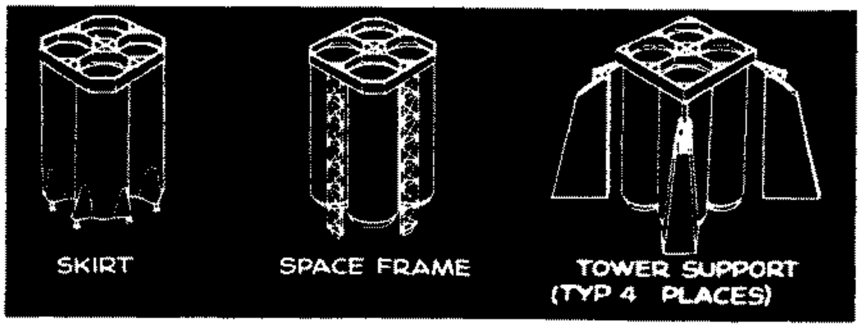
As to the immediate stability and thrust-vector-control requirements, analysis of the vehicle shows that the important first-mode body bending frequency is of the order of 6 to 11 times the pitch-control frequency. This is consistent with conditions prevailing in all-liquid-booster vehicles, and means that stable flight can be obtained in a solid-boosted vehicle by a conventional approach to sensor location and simple electronic filters.

Vehicle control combines dynamic and static requirements. Static requirements grow from various misalignments and variances, while dynamic requirements grow from wind disturbances and vehicle maneuvers.

CONTROL DURING TAILOFF



VEHICLE-SUPPORT CONCEPTS



The jet deflection to maintain control and the amount of side impulse required for the static and dynamic disturbances would be as follows for a vehicle without fins and a vehicle with fins:

Performance	Fins	No Fins
Max. nozzle defl, ± deg.	1.2	4
Max. nozzle vel, ± deg/sec.	3.34	20
Max. nozzle accel, ± rad/sec.	0.55	3.28
Servo break freq, cps	1.5	1.5
Nom. vehicle cont. freq, cps	0.18	0.18
Control side-impulse, %	1.91	2.26
Max. hinge moment (W/Skirts), in-lb × 10^{-4}	4.912	6.665

The curves on page 64 show the control requirements during the first-stage burning period in terms of angle of attack and degrees of thrust deflection. It is important to note that, even for the extreme situation represented by the NASA 99% wind profile, the dynamic thrust-deflection angle requirement does not exceed about 3 deg for an unfinned vehicle, and is appreciably less for a finned vehicle. The NASA 99% wind profile means that there is only 1% probability that design winds will be exceeded during the worst weather months. This severe requirement has importance when the details of possible vector-control systems are examined. Systems which depend upon stored impulse, such as the secondary-fluid-injection system, are greatly influenced by the assumption of either a 99% or a 95% standard wind profile.

Vehicles of this class, we see, have a relatively moderate requirement for either total side impulse, jet-deflection angle, or vector-control angular velocity. The extreme combinations of probable variances produced by the motor structure and performance tolerances are easily accommodated in a system with a 4-deg jet deflection capability and a side-impulse level of about 3% of the main impulse. If fins are used with the vehicle, the requirements are greatly reduced. It is important to note, however, that the tradeoff between vector-control impulse, fin weight, and cost must be carefully made, since it is possible that fins in the end cause penalties rather than gain.

It becomes evident that the problem with respect to control during the flight of the first stage is one of engineering, cost, and reliability optimization. These factors are subject to differences arising from opinions, and definite and unequivocable selection will be difficult.

LET us turn attention now to the burnout period. The primary influence on control of the vehicle just before staging, and on rotation rate of the first and second stages just after separation, is the variation in thrust level among motors in the stage. This variation in turn is governed almost completely by differences in web burning time or, more specifically, in the tailoff characteristics of the motors. Selection of the optimum tailoff conditions, and as a concomitant selection of optimum nozzle cant-angle, must be made in terms of many variables involved in the stage-separation maneuver.

The importance of this operation may perhaps be best understood by reference to the graph at top here. Here we see a situation in which three motors of a four-motor stage burn out 3 sec early, while one motor burns out 3 sec late—the most severe burnout condition in relation to the guidance and control problem. When all but one motor in the cluster has completed thrust decay, the remaining motor can not maintain attitude control unless it can at least be vectored through the center of gravity. This condition arises when each motor has a fast tailoff but a relatively long burn-time variance.

The critical parameter with respect to the successful staging maneuver is the angle of attack, which should not exceed 10 deg. A 15-sec tailoff time reaches the 10-deg level. A 20-sec tailoff time falls well under this level for motors with nozzles not canted. Nozzle canting will minimize the angular effects at staging for a 1-sec burnout time.

There is a wide range of choice in establishing the optimum tailoff characteristics of the motors and in selecting nozzle cant-angle. Nozzle canting carries with it, of course, a penalty in payload from the loss of impulse, shown in the graph on page 64. On the other hand, long tailoff requiring lower cant angle carries with it implications of penalty, since it builds up a requirement for retro-thrust motors to insure clean separation The adjacent graph on page 64 indicates weight penalties associated with retro-thrust motors.

In general, it may be stated that nozzle canting probably will not be required if tailoff time is greater than five times the motor variance; but burntime variance should be low—possibly ±1 sec—to minimize retro requirements.

Upon separation, the second stage will undergo a dispersion from the desired attitude which will increase during the coast period prior to startup of the second-stage engines. The most important factors in the dispersion during this period are the initial angle of attack, the pitch rate, and the dynamic pressure. We have discussed the level of angle of attack to be expected with various combinations of nozzle cant angle and tailoff periods for the most severe set of burnout circumstances. If we assume that the 10-deg maximum angle of attack will not be exceeded, and use aerodynamic pressure at staging

ing the following:

AERODYNAMICIST

To work on problems relating to the external ballistics, stability and aerodynamic loading of high altitude sounding rockets. Duties include analytical and experimental evaluation of rocket configurations used in a wide variety of upper atmosphere research missions. BS or MS measurement systems; plan and coordinate environmental test program. BS or MS in ME or physics.

RADIOWAVE PROPAGATION ENGR.

Will operate transmitters and field intensity measuring and reporting equipment and supervise technicians and non-skilled crew in site installations. Make decisions involving engineering judgement (measurement techniques and procedures) and perform limited field data analysis. BS in EE with experience in communications or field testing. Positions available in Washington, D. C., Arizona, and overseas.

ROCKET TEST SUPERVISOR

To work with Rocket Test Manager coordinating activities of instrumentation staff with other segments in the test group and throughout ARC. Responsibilities include design, selection, modification, and operation of instrument components and systems used in static testing of propulsion units. BS or MS in EE or physics and several years' experience.

Send resume to: Dept. 184
Director, Professional Personnel

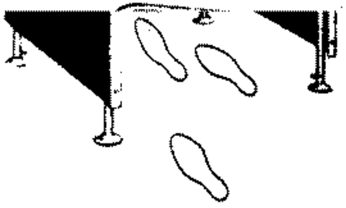
Atlantic Research
CORPORATION
Alexandria, Virginia
(A residential suburb
of Washington, D.C.)

An equal opportunity employer

structures for binding motors into a stage, for making an interstage between the solid and liquid stages, and for supporting the vehicle. Many designs have been laid out, and the effects of these designs on the critical vehicle properties have been excepts considered in the vehicle analysis.

Our discussion has centered around a baseline vehicle with orbital payload of approximately 1/2-million lb. It is interesting to ask what larger payloads might be carried by a solid-booster vehicle using motors of about the same as those in the baseline vehicle. The graph at bottom of page 64 indicates that payloads well over a million pounds may be carried, within the limitations stated earlier.

We have carried the solid-propellant stage through its useful career. All of the regions of potential difficulty have received some examination. We observe that no fundamental problem exists to prevent the use of solid motors and stages for these large missions, but that the area requiring more detailed investigation is the establishment of optimum burnout conditions. Although the best tradeoff among a number of variables must be made, we observe that under the most severe bounding conditions the separation maneuver is practical. ◆◆



10 SAFETY

HAWS

DECONTAMINATION SHOWER model 8562

Ready for action! 16-FULLJET nozzles drench the body when large Push-Type instant-action ball valve is opened. Here is the *instant* first aid so vital to countering body contamination. A Haws stainless steel (Model 7900-B) eye/face-wash is mounted on paneled side to provide a complete "one-stop" safety station. Functional parts are rugged red brass. Write for detailed specs: *Haws Drinking Faucet Company, 1443 Fourth St., Berkeley 10, California.*

HAWS Since 1909
EMERGENCY EQUIPMENT