Analysis of Two-Phase Flow in LH_2 Pumps for O_2/H_2 Rocket Engines

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The ability to pump two-phase hydrogen in O_2/H_2 rocket engines will reduce hydrogen tank weight, tank pressurization, engine start time, chilldown propellant loss, and complex start and preconditioning sequences. Therefore, an analysis was made to determine the two-phase pumping capability of liquid-hydrogen pumps and to establish hydrodynamic design criteria to improve two-phase pump performance. The analysis includes equilibrium and constant-quality flow processes and considers acoustic effects, compressible flow, and cavitation. Both cavitation and two-phase hydrogen experimental pump data were used in the analysis. Present hydrogen pumps can operate at ~30% vapor-volume fraction. Results from the analysis show that this capability can be doubled by improving pump configuration and hydrodynamic design. Vehicle tank pressure requirements for zero pump net positive suction head, chilldown time, and chilldown propellant requirements are presented as a function of pump inlet vapor-volume fraction.

Nomenclature

A = flow passage area normal to flow direction, in.^{2}
B = blockage fraction due to blade thickness and boundary layer
c = acoustic velocity, fps
C_m = axial velocity at inducer inlet, fps
D = tip diameter at inducer inlet, in.
g = gravitational constant, 32.2, ft/sec^2
h = inducer blade-to-blade spacing normal to flow direction, in.
i = angle between blade and flow direction at inducer leading edge, deg
M = Mach number
N = rotational speed, rpm
NPSH = tank net positive suction head, $144(P - P_V)/\rho_L$, ft
P = static pressure, psi
Q = volume flowrate, gpm
R = liquid-to-vapor density ratio
s = entropy, Btu/lb °R
T = temperature, °R
t = inducer blade thickness, in.
U = inducer blade tangential velocity, fps
$\dot{W}$ = weight flowrate, lb/sec
x = quality, vapor fraction by weight
α = vapor fraction by volume
β = inducer inlet blade angle relative to tangential direction, deg
γ = specific heat ratio for hydrogen vapor
ϕ = flow coefficient at inducer inlet (C_m/U)
ρ = flow density, lb/ft^3
σ = oblique shock wave angle, deg
τ = nondimensional NPSH ($2g$ NPSH/U^2)
ξ = two-phase factor, $\alpha\rho/\rho_V$

Subscripts

1 = inducer inlet; or upstream of shock wave
2 = point of maximum blockage within inducer; or downstream of shock wave
CX = constant-quality flow process
DES = design
EQ = equilibrium flow process
L,V = liquid and vapor
s = constant entropy flow process for vapor
T = inducer blade tip; or isothermal flow process for vapor

Superscript

* = flow conditions when choked

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Introduction

THE ability to pump two-phase hydrogen will permit hydrogen-fueled rocket vehicles to have lighter fuel tanks, simpler pressurization equipment, and faster starts and restarts with lower chilldown propellant losses and simpler start and preconditioning sequences for the following reasons. Many rocket vehicle missions require coasting periods, when the engine is shut off, followed by restarts. During the coasting period, heat soaks back from the turbine, which generally has a temperature between 1200° and 1600°F, to the pump and the inlet line, which are at the liquid-propellant temperature (−423°F for LH_2). If the coasting period is long enough, the pump, the turbine, and the environment will reach an equilibrium temperature of between −100 and +300°F.[4] Restarts are extremely difficult under these conditions, because the warm feed system flow passage surfaces vaporize the cryogenic propellants. This causes a drastic drop in propellant density which, in turn, causes a high system resistance and a low pump pressure rise (pump pressure rise is proportional to flow density), such that only a small fraction of the design flowrate may be delivered to

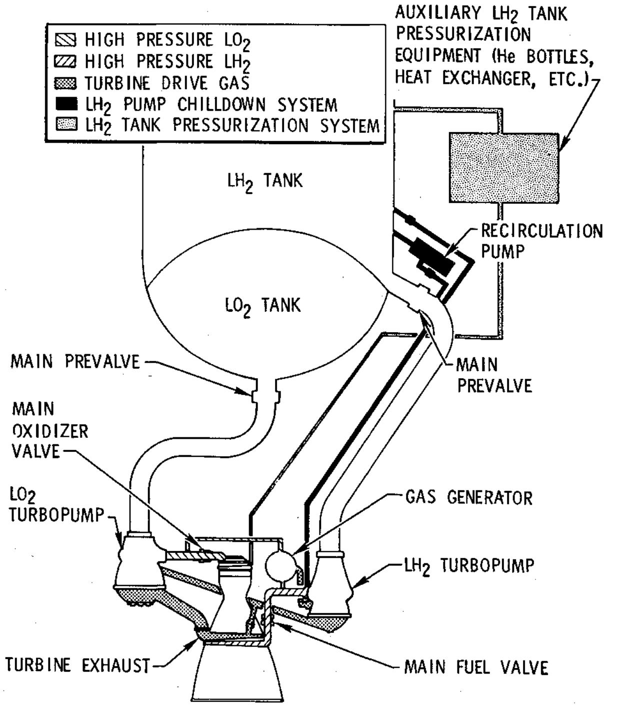

Fig. 1 Schematic of engine chilldown and tank pressurization systems in Saturn S-IVB stage.

the engine. Therefore, a separate preconditioning system (Fig. 1) is used in the S-IVB stage of the Saturn V vehicle to prechill the feed system so that restarts are possible.

The turbopump can be started sooner and with less chilldown propellant losses if the pump has a two-phase pumping capability. Figure 2 shows the pump inlet vapor-volume fraction α_1 for a typical LO_2/LH_2 engine system vs chilldown time from flow start for an assumed flow transient.[4] As shown, approximately 80 sec of inlet line chilldown are required before a pure liquid is supplied to the pump inlet. Assuming the pump can tolerate an α_1 of 30% and has surfaces that are chilled enough to keep the flow pressure above the vapor pressure during the pump start transients, the chilldown times could be reduced by 20 sec (Fig. 2a), saving 200 lb of chilldown propellant (Fig. 2b). If α_1 were 50%, these savings would be approximately doubled.

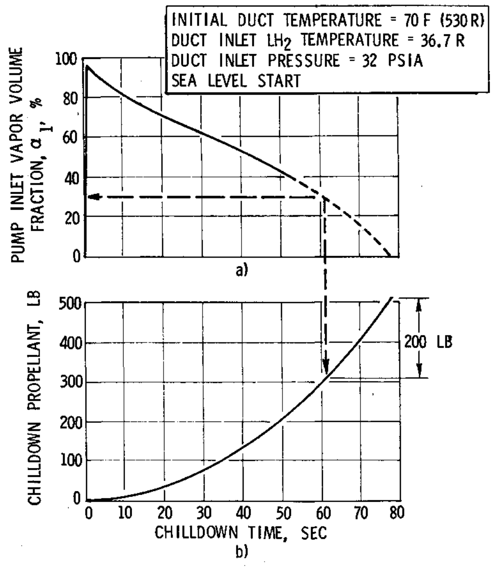

Fig. 2 Effect of vapor pumping capacity on chilldown time and chilldown propellant requirements for a typical LO_2/LH_2 engine system.

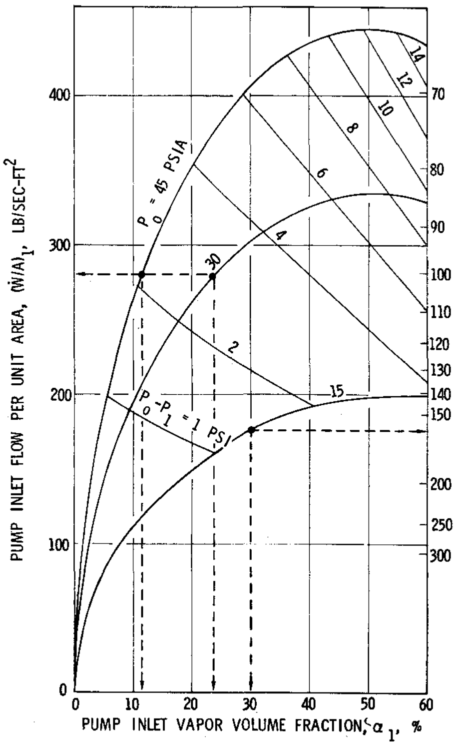

Fig. 3 Equilibrium isentropic flow expansion from saturated LH_2 in the tank to two-Phase H_2 at the pump inlet.

If the vehicle can be operated with saturated liquid propellants in the tanks (i.e., zero tank net positive suction head, NPSH), the tank pressurization equipment (Fig. 1) can be eliminated and the tank pressures can be reduced. This would reduce both the complexity and the weight of the vehicle. However, such an operation would require a two-phase pumping capability because the acceleration of the flow from zero velocity in the tank to approximately 60 fps in the inlet line would drop the flow static pressure below the vapor pressure. Referring to Fig. 3, the existing J-2 pump inlet flow per unit area of ~280 lb/sec-ft^2 requires allowable α_1 pumping capacities of 12% and 24% for zero tank NPSH at saturation pressures of 45 and 30 psia, respectively. To operate at 15 psia and 30% vapor, the inlet area must be increased to 160% of the original inlet area.

Studies of two-phase hydrogen pumping have been conducted by Ruggeri et al.,[1] Wilcox,[2] and Connelly and Meng.[3] The present study was recently conducted under a NASA program entitled "Thermodynamic Improvements in Liquid Hydrogen Turbopumps." The specific objectives of this paper are: 1) to show how two-phase flow limits pump

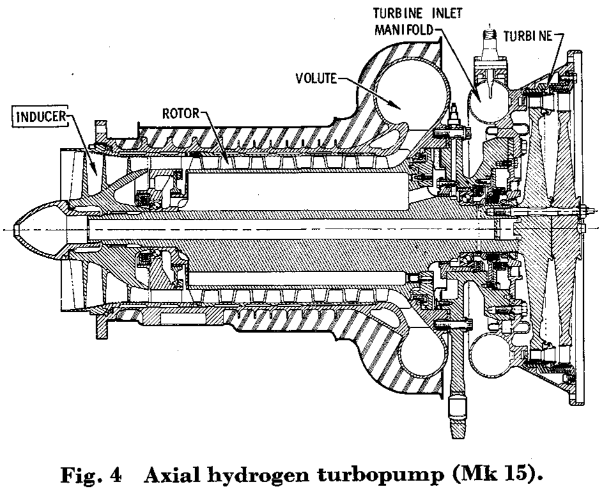

Fig. 4 Axial hydrogen turbopump (Mk 15).

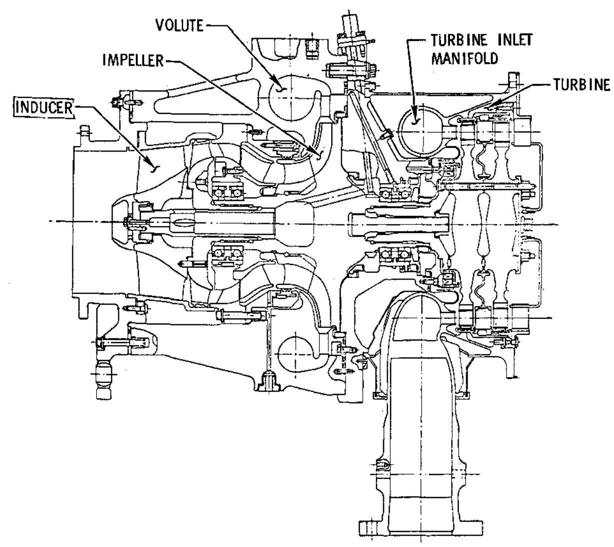

Fig. 5 Centrifugal hydrogen turbopump (Mk 29).

performance and affects both zero tank NPSH operation and warm engine starts, 2) to relate two-phase hydrogen pumping capability to flow geometry, operating point, and flow conditions, and 3) to recommend design approaches that will improve the two-phase pumping capability of liquid hydrogen pumps.

Two-Phase Flow Analysis

In a rocket engine pump, whether it is an axial (Fig. 4) or a centrifugal flow pump (Fig. 5), the initial pumping element is the inducer, which is designed to operate at low inlet total pressure, so that the tank pressure (hence, tank weight) may be low. The inlet total pressure required is minimized by reducing both the inlet velocity head and the required inlet static pressure. The inlet velocity head is kept low by making the inlet annulus area large. The inlet static pressure is kept low by making the blades at the inducer inlet thin, sharp, and uncambered, thereby minimizing the static pressure coefficient on the blade suction surfaces and, therefore, the amount of vapor that forms within the blade passages.

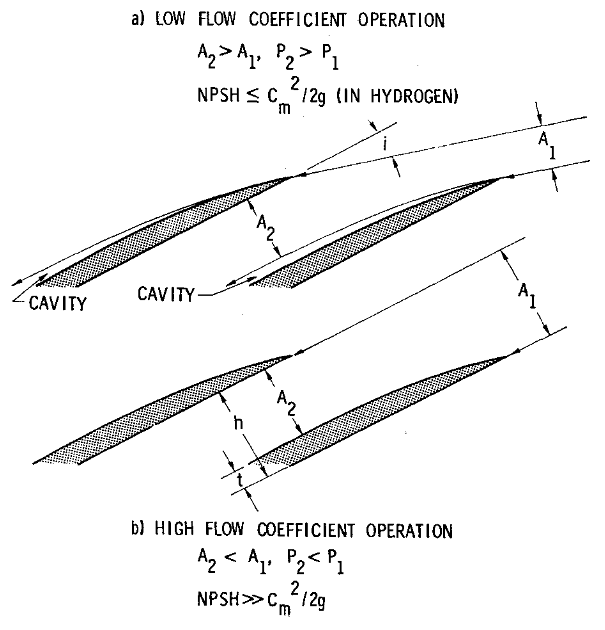

Fig. 6 Effect of flow coefficient on inducer inlet flow geometry.

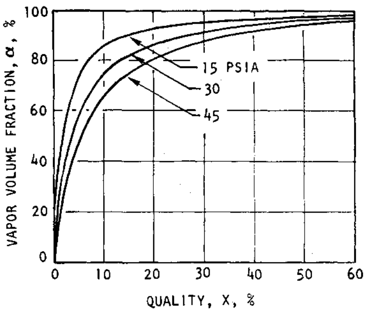

Fig. 7 Effect of quality and saturation pressure on vapor-volume fraction.

At typical turbopump operating speeds and flow temperatures, cryogenic hydrogen is the only propellant that can be pumped when the inducer inlet static pressure is equal to the flow vapor pressure and, therefore, is the only propellant that can be pumped in a two-phase condition. The reason for this unique capability is that hydrogen has high values of vapor-to-liquid density ratio and specific heat. As a result, the volume of vapor that is generated downstream of the flow discontinuity at the inducer leading edge (Fig. 6a) is much smaller in hydrogen than in other propellants. Therefore, an inducer is able to produce a head rise in hydrogen at a much lower value of NPSH than in other propellants. This is called the thermodynamic suppression head effect.[1,2,5]

Flow Processes

The two types of flow processes that theoretically form the boundaries of a real homogeneous two-phase flow process are equilibrium and constant-quality. In the equilibrium case, the vaporization and condensation rates are assumed to be high enough that the vapor temperature is always equal to the liquid temperature, both phases are always at saturation, and the quality (vapor fraction by weight) varies with pressure changes. These quality variations cause large changes in density because a small quality change corresponds to a large change in α (Fig. 7). An isentropic, equilibrium compression is illustrated by line 1-2 on Fig. 8. In contrast, for constant-quality flow, the condensation and vaporization rates are assumed to be so low that no significant phase change takes place. The temperatures of the phases differ as the pressure varies from the initial state, and an isentropic compression will subcool the liquid (line 3-4 in Fig. 8) and superheat the vapor (line 5-6).

Various experimental values of two-phase choking mass flowrate[6,7] indicate that the constant-quality and the equilibrium flow processes bracket most of the test results for qualities below 10% (74% vapor by volume in 30 psia H_2). A two-phase flow study for water[7] indicates that, at low quali-

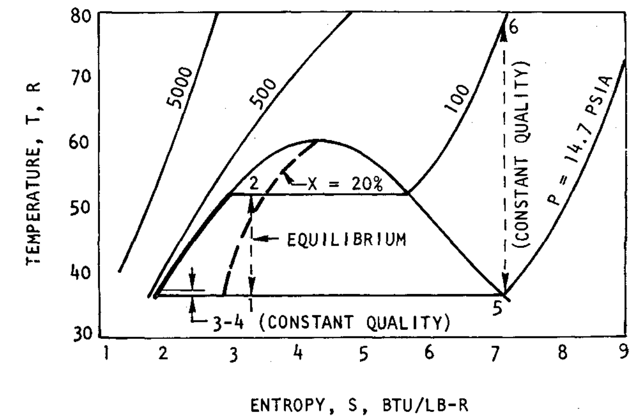

Fig. 8 Temperature-entropy chart for hydrogen.

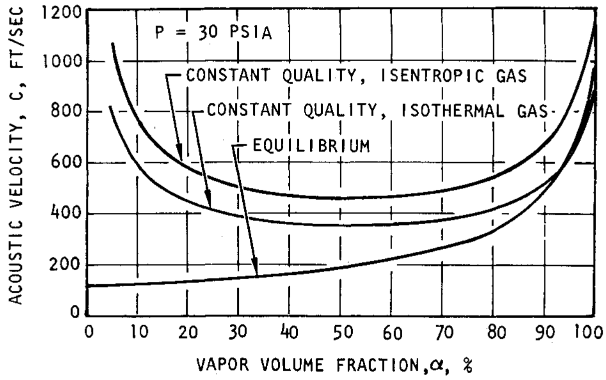

Fig. 9 Effect of flow process on the acoustic velocity of two-phase H_2.

ties, the vapor bubble growth rate is insufficient to maintain equilibrium and, at qualities below 1% (21% vapor by volume in 30 psia H_2), it is so low that a constant-quality flow process is probably approached. H. B. Karplus (Armour Research Foundation, Illinois Institute of Technology) measured the propagation velocity of a finite amplitude pressure wave through a two-phase boiling water mixture. His test results[8] covered the range $0 \leq \alpha \leq 50\%$ and (although not noted) agreed extremely well with the acoustic velocity prediction for a constant-quality flow process. However, for hydraulic flow passages with large length-to-diameter (L/D) ratios, a trend toward the equilibrium flow process is indicated. From Ref. 9, the results of choking mass flowrate experimental tests, in which two-phase hydrogen passed through a long tube ($L/D = 21$), correlated best with the metastable flow model in which the flow follows an equilibrium flow process until choking occurs and then accelerates at constant quality until constant-quality choking occurs. From Ref. 3, subsonic two-phase hydrogen testing through a tube with a hydraulic L/D of 12 produced results that agreed with equilibrium theory.

It was concluded from these studies that low-quality, two-phase H_2 will approach equilibrium when passing at subsonic velocities through a long, constant-diameter duct such

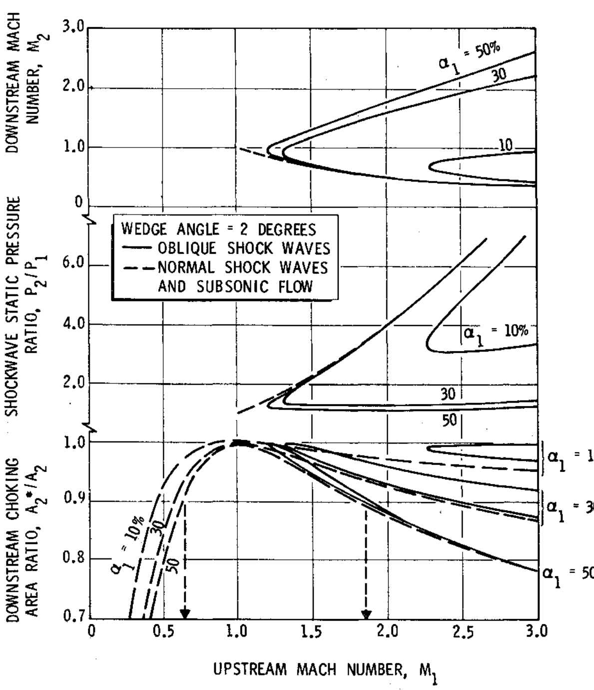

Fig. 10 Effect of vapor-volume fraction on flow parameters for constant-quality two-phase flow with isothermal gas.

as the duct that carries H_2 to the J-2 engine fuel pump (Mark 15 LH_2 pump). However, two-phase H_2 will probably approach a constant-quality flow process when passing through any sudden area contraction, because it will not have time to reach equilibrium. Therefore, when flow passes through an inducer, the high flow velocities and the rapid area changes will probably cause two-phase H_2 to follow a constant-quality flow process until the pressure becomes high enough to force a relatively gradual condensation.

Acoustic Velocity

The acoustic velocity for two-phase flow is much lower than for either phase alone. The equilibrium and the constant-quality flow processes have the two extremes in acoustic velocity for low-quality, two-phase flow. In equilibrium flow, a given pressure drop will cause vaporization as well as expansion of the existing vapor. This will cause a relatively large drop in density and, therefore, a low acoustic velocity because the vapor-to-liquid density ratio is very small (approximately 0.04 in 30 psia hydrogen). In constant-quality flow, however, no vaporization occurs and, therefore, the acoustic velocity is higher. The general expression for the acoustic velocity c in two-phase flow in which the liquid phase is incompressible is

$$c = [1 + x(R - 1)] \Big/ \left[\frac{x}{c_V^2} R^2 - \frac{\rho_L}{144g}(R - 1)\left(\frac{\partial x}{\partial P}\right)_S\right]^{1/2} \tag{1}$$

where $R = \rho_L/\rho_V = 770/P$.

By linearly correlating the hydrogen properties from Ref. 10, substituting the correlations into Eq. (1), and converting quality to vapor-volume fraction, the following three expressions for acoustic velocity in an equilibrium flow process c_{EQ}, an isothermal-gas, constant-quality (ρ_V/P = const) flow process $(c_{CX})_T$, and an isentropic-gas, constant-quality ($\rho_V/P^{1/\gamma}$ = const) flow process $(c_{CX})_s$ were obtained. (The last process was added because it was used in a shock-wave analysis that will be discussed later.)

$$c_{EQ} = 901 \Big/ \xi^{1/2} \left\{1 + \frac{(1 - 1/R)[2.21R(1/\alpha - 1) - 3.18]}{(11.25 - 0.1659T)^2}\right\}^{1/2} \tag{2}$$

$$(c_{CX})_T = 901/\xi^{1/2}, \quad (c_{CX})_s = 1170/\xi^{1/2} \tag{3}$$

where $\xi = \alpha\rho/\rho_V = \alpha[R - \alpha(R - 1)]$.

Figure 9 was generated from Eqs. (2) and (3) for a saturation pressure of 30 psia; $(c_{CX})_T$ and $(c_{CX})_s$ approach the single-phase c at $\alpha = 0$ and 100% and reach a minimum at $\alpha \simeq 50\%$, whereas c_{EQ} reaches a minimum at $\alpha = 0$, thereby creating a discontinuity between the pure liquid and the two-phase values at that vapor fraction. Both c_{EQ} and the c_{CX}'s increase with increasing static pressure. Also shown is that the isothermal-gas, constant-quality flow process is a reasonable approximation of the isentropic-gas, constant-quality flow process. This permitted a less complicated shock wave analysis.

The minimum c_{EQ} is $\sim\frac{1}{3}$ of the minimum $(c_{CX})_T$, and, at a lower saturation pressure of 15 psia, approaches the flow velocity through a hydrogen pump inlet duct. Since the flow process in the duct is probably close to equilibrium, this indicates that choking in the inlet duct is a definite possibility if the pump inlet pressure is low.

Two-Phase Compressible Flow

Since the inducer blade relative Mach numbers for a constant-quality, two-phase flow process generally range between 0.5 and 2.0, an analysis of two-phase compressible flow was conducted to determine the conditions under which choking would occur in the inducer flow passages. As discussed before, a constant-quality flow process with an isothermal-gas was assumed. In reality, some degree of vaporization and/or condensation will occur and, therefore, the more compressible isothermal-gas assumption could approximate the real situation more closely than the isentropic-gas assumption. The basic flow equations that describe a constant-quality flow process with an isothermal gaseous phase are

$$M_2^2 = \frac{M_1^2 - 2\alpha_1^2\left[\left(\frac{1-\alpha_1}{\alpha_1}\right)\left(\frac{P_2}{P_1} - 1\right) + \ln\left(\frac{P_2}{P_1}\right)\right]}{[\alpha_1 + (1 - \alpha_1)P_2/P_1]^2} \quad (4)$$

$$A_2/A_1 = P_1M_1/P_2M_2 \quad (5)$$

$$\alpha_2 = [1 + P_2(1 - \alpha_1)/P_1\alpha_1]^{-1} \quad (6)$$

For subsonic flow, M_2 in Eqs. (4) and (5) was set equal to 1, and the resulting equations were solved simultaneously to obtain the choking area ratio as a function of M_1 and α_1.

For two-phase mixtures of liquid water and gaseous nitrogen (which must follow a constant-quality flow process), Eddington[11] proved that normal and oblique shock waves occur in constant-quality, two-phase flow and obtained excellent agreement with theoretical predictions in which the gaseous phase was assumed to be isothermal. Therefore, since most of the two-phase hydrogen pump test data were taken at inducer inlet relative Mach numbers between 1 and 2 and because the inducer blade profile is a wedge with an angle of approximately 2°, normal and oblique-shock-wave relationships were developed to predict the choking area ratio downstream of the inducer inlet.

Figure 10 illustrates the two-phase compressible flow characteristics up to a Mach number of 3.0. Note that there are two oblique shock wave solutions for each upstream condition at which an oblique shock exists. One solution is a weak shock in which the shock angle (σ) is small and the downstream Mach number M_2 can be greater than 1.0. The other solution is a strong shock in which the shock angle is large and M_2 is always less than 1 and less than the value for a weak shock. As discussed in Ref. 12, the proper solution is a function of the downstream flow conditions. Because of the complex flow geometry downstream of the inducer and the wide range of possible operating conditions downstream of the pump, the selection of the proper oblique-shock-wave solution was not made. The important conclusion to be drawn from Fig. 10 is that, for $0.6 \leq M_1 \leq 1.8$ and for $\alpha_1 \leq 50\%$, the choking to inlet area ratios exceed 90% ($A^*/A \geq 0.9$) and, therefore, small flow-area contractions will cause choking.

Blade Blockage Effect

Since the allowable 10% effective area contraction could be caused by boundary layer effects, it may be concluded that, when the inlet flow is two-phase, choking is likely whenever the combined effects of high inducer inlet flow coefficient and inducer blade blockage cause the inlet flow area to equal the geometrical flow area within the inducer blades ($A_2/A_1 = 1$, Fig. 6). This is also true if the inlet flow is a saturated liquid. For example, assuming a pressure of 15 psia, a velocity of 600 fps, and liquid flow, an area contraction of only 4% ($A_2/A_1 = 0.96$) would reduce the static pressure to 0 psia if the flow remained liquid. This would cause a portion of the liquid to flash into vapor. Assuming a constant-quality flow process downstream of the flashing point, the flow situation would then be similar to that with two-phase at the inlet.

The theoretical maximum vapor-volume flowrate that can be pumped is equal to the difference between the volume flowrate at the limit and the liquid-volume flowrate. We can write this in flow coefficient form by treating the vapor as a void fraction,

$$\alpha_1^* = (\phi_1^* - \phi_L)/\phi_1^* = 1 - \phi_L/\phi_1^* \quad (7)$$

The allowable vapor fraction expressed by Eq. (7) decreases linearly from 100% at $\phi_L = 0$ to 0% when $\phi_L = \phi_1^*$ (flow coefficient at $A_2/A_1 = 1$). Equation (7) can be reduced to more basic terms by making a few simple approximations. Because A_2/A_1 equals 1 at the blockage limit and because the flow angles are small,

$$(i/\beta)^* \approx B \quad (8)$$

$$\phi_1^*/\phi_{DES} = (1 - B)/[1 - (i/\beta)_{DES}] \quad (9)$$

$$\alpha_1^* \approx 1 - (\phi_L/\phi_{DES})[1 - (i/\beta)_{DES}]/(1 - B) \quad (10)$$

Table 1 Summary of inducer design data and two-phase test results at 10% loss in pressure rise ($\Delta P/\Delta P_{LIQ} = 90\%$)

Inducer	Mark 15 LH_2	Model Mark 29	Mark 25
$\varphi_{T\,DES}$ ($i_{DES}/\beta = 0.425$)	0.0735	0.075	0.102
β_T, deg	7.35	7.5	10.2
D_T, in.	7.80	6.56	7.85
Blade blockage, %	21	22	18
Blade blockage, with 10% boundary layer, %	29	30	26
Test fluid	LH_2	Water	LH_2
T_{line}, °R	37.7–46.2	Ambient	40.1–41.6
Q_L, gpm	8470–9310	800–1700	7290–10,530
N, rpm	27,270	6322	18,100–24,200
ϕ_L/ϕ_{DES}	0.966–1.064	0.66–1.39	0.812–0.993
α_{line}, %	0–24.4	0	14.2–38.3
α_{line}, with 0.5 lb/sec recirculation, %	6.1–30.4	DNA	DNA

Analysis of Experimental Data

Pertinent design data and two-phase test data for three pumps and inducers are listed in Table 1 in the order of increasing design flow coefficient. The Mark 15 LH_2 pump (Fig. 4) was tested in two-phase H_2 by Rocketdyne under the J-2X engine program. Two-phase hydrogen was generated by a Frantz screen that was installed upstream of the pump inlet. The vapor fractions were determined from measurements of pressure and temperature in the pure liquid

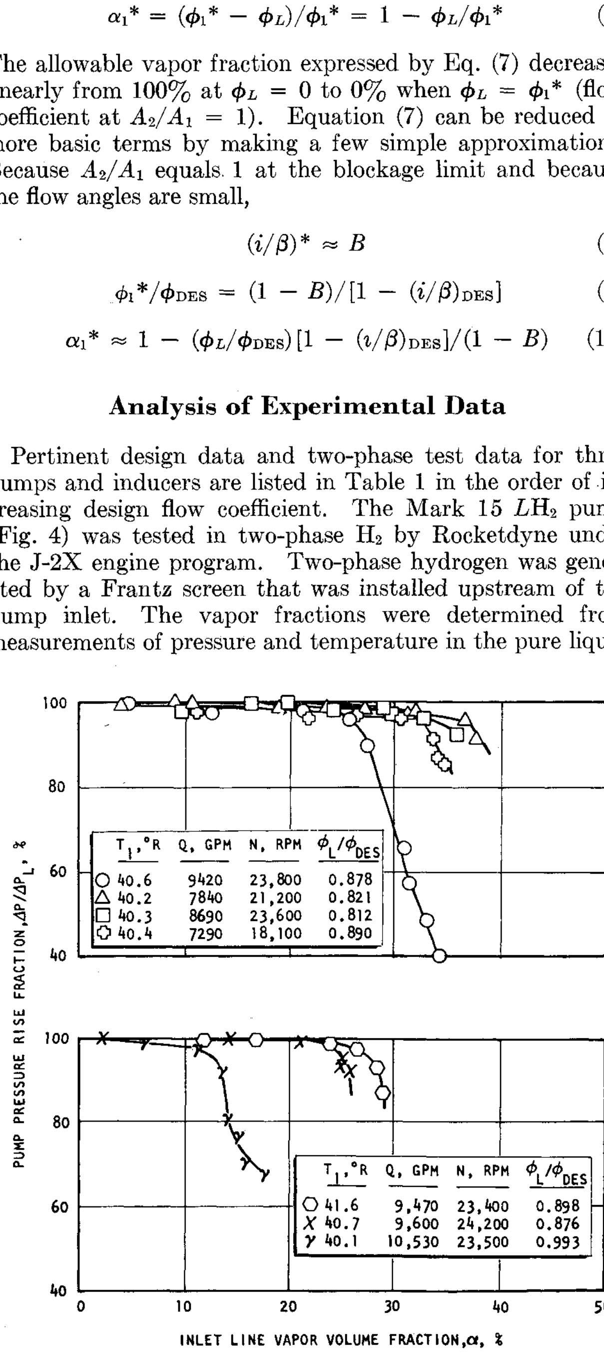

Fig. 11 Mark 25 LH_2 pump test results in two-phase hydrogen.

upstream of the screen and in the two-phase fluid downstream of the screen.

The model inducer for the Mark 29 LH_2 pump (Fig. 5) was tested in water by Rocketdyne under the J-2S engine program. No α_1 data were obtained, because these were cavitation tests in water. However, the wide range of flow coefficients over which this inducer was tested provides an excellent illustration of the effects of blade blockage.

Rocketdyne's Mark 25 hydrogen pump was tested in two-phase H_2 by the Nuclear Rocket Development Station in Nev. The method used to generate and measure vapor at the pump inlet was similar to that used in the previously discussed Mark 15 pump testing. Pump pressure rise vs α_1 is shown in Fig. 11 for various combinations of inlet saturation pressure, inlet liquid volume flowrate, pump rotational speed, and flow coefficient ratio.

Inducer Blade Blockage and Stator Cavitation

Assuming 10% boundary layer and using the design data in Table 1, a high NPSH was required to force the flow through the Mark 29 LH_2 pump inducer blade rows whenever the inlet flow coefficient exceeded the theoretical limit [Eq. (9)]. Below the theoretical limit, the NPSH requirements were low. Test data for a NASA inducer, which was able to pump two-phase hydrogen during operation below the theoretical limit (assuming 10% boundary layer), also showed excellent agreement with the theoretical inducer blade blockage limit.

For the Mark 25 and Mark 15 pumps, the 10%-loss-in-pump-pressure-rise data points are shown on Fig. 12. With the exception of the high ϕ_L/ϕ_{DES} points, most of the data fall between the prediction with no boundary layer and the prediction with 10% boundary layer. These good correlations between test and theory indicate that, with the exception of the high ϕ_L/ϕ_{DES} data points, the upper limit to two-phase pumping capability is probably choking due to blade blockage. In Fig. 11, the pump pressure rises drop suddenly when this choking occurs. The data points in Fig. 12 that do not agree with theory are due to inlet choking and stator cavitation, respectively, as discussed below.

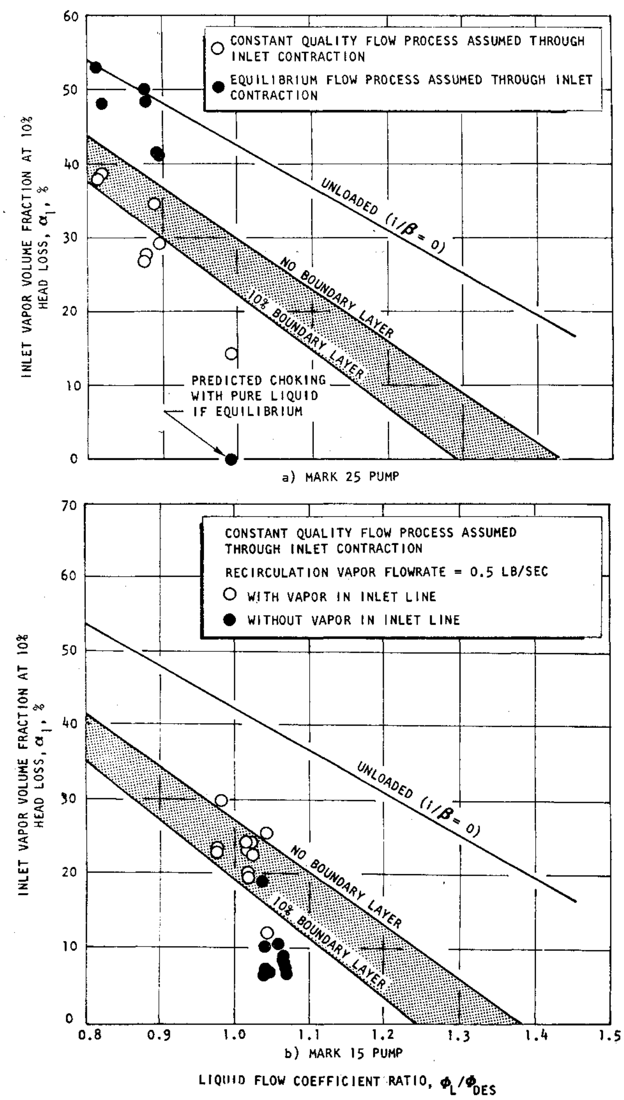

Fig. 12 Comparisons of hydrogen pump two-phase test results with theoretical predictions.

Stator Cavitation

For $\phi_L/\phi_{DES} > 1.03$, the α_1's for the Mark 15 LH_2 pump were much less than predicted (Fig. 12b). High-flow-coefficient cavitation data[4] for the Mark 15 pump indicate that the inducer pressure rise is unaffected by NPSH, whereas the pressure rise across both the inducer and its stator (Fig. 4) is strongly affected. Therefore, the deviations from theory in Fig. 12b were probably caused by stator cavitation. However, for the Mark 25 pump, stator cavitation was not apparent in either the two-phase testing or earlier cavitation tests in which testing was conducted up to 130% of design flow coefficient. It may be concluded that stator cavitation is an important consideration here, and that the problem can be alleviated through proper design. Therefore, similar to inducer-blade design, these stator blades should be thin, with a positive incidence at ϕ_{DES} and little or no camber on their forward portions.

Inlet Choking

The α_1 for the highest ϕ_L/ϕ_{DES} data point for the Mark 25 pump (obtained at low temperature and high flowrate) is 14.5% rather than the predicted value of 23% based on the inducer-blade blockage limit (Fig. 12a), but, as just pointed out, this difference was not due to stator cavitation. Since the flowrate is between the choking flowrates for equilibrium and constant-quality flow processes through the inlet contraction, choking probably occurred at the annulus just upstream of the inducer leading edge. For equilibrium flow through the inlet area contraction, the inlet would be choked even at $\alpha_1 = 0$, and, for constant-quality flow, the inlet will not choke at all, and inducer-blade blockage will be the limiting factor. Thus, the flow process through the

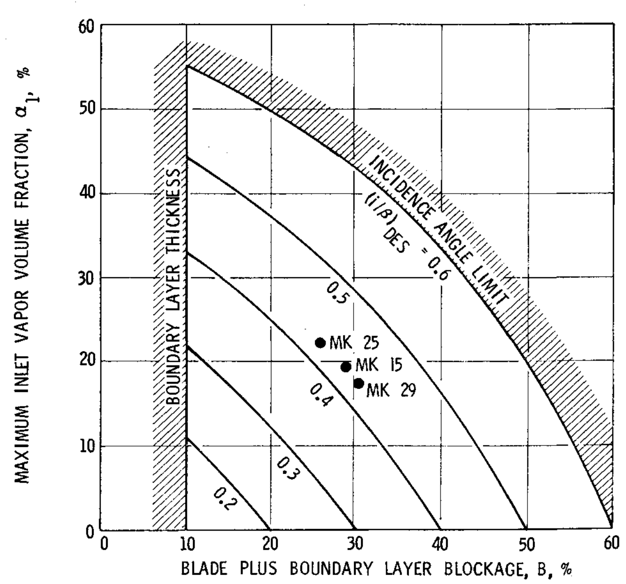

Fig. 13 Effect of design incidence angle and blockage on vapor pumping capacity during operation at design liquid flow coefficient.

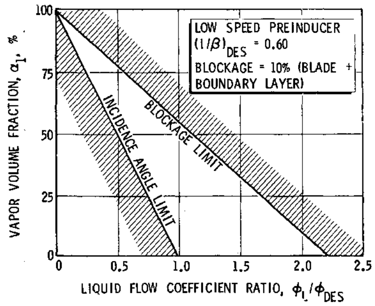

Fig. 14 Maximum vapor pumping capabilities of hydrogen inducers.

inlet contraction must lie between these two extremes. It may be concluded that a) some vaporization occurred as the flow passed through the inlet contraction and that the deviation from theory of the high ϕ_L/ϕ_{DES} data point in Fig. 12a was due to choking at the inducer inlet annulus, and b) although the flow process through the contraction is probably closer to constant-quality than to equilibrium, an equilibrium flow process should be assumed during the design of a pump so as to ensure sufficient flow area.

Vapor Recirculation

Approximately 0.5 lb/sec of bearing coolant was recirculated through the Mark 15 LH_2 pump inducer (Fig. 4) during the two-phase tests. Since this recirculated flow was close to 100% vapor, and since the vapor at the pump inlet is the sum of this vapor and the inlet line vapor, the allowable α_1 in the inlet line was severely limited (Table 1), particularly at low inlet pressures where the recirculated vapor occupies a large volume. To avoid this problem, hydrogen pumps should be designed to return all recirculated flows downstream of the inducer. The Mark 25 pump test data were unaffected by this problem because the flows that would have been recirculated were ducted into a separate line.

Results of Analysis

Assuming operation at ϕ_{DES} (based on liquid volume flowrate), the influences of blockage fraction and inducer leading edge incidence to blade angle ratio on allowable α_1 are illustrated in Fig. 13 [based on Eq. (10)]. As shown, the allowable α_1's for existing high-tip-speed inducers could be increased from 20% to 45% by redesigning at the maximum leading edge incidence angle that will permit two-phase pumping. By using a separate low-speed inducer (preinducer) upstream of the main turbopump, the allowable α_1 can be further increased to 55%, because the preinducer blade thickness can be reduced for the lower running speed.

The operating region of α_1 for a preinducer is shown in Fig. 14. Choking due to blade blockage provides the upper limit to the α_1 that can be pumped. The preinducer leading edge incidence angle provides the lower limit. Choking in the inlet annulus will not occur for this case because a low-speed preinducer has a large inlet area and, therefore, a low inlet flow per unit area (Fig. 3).

To avoid factors that can limit two-phase pumping such as a) inducer stator cavitation, b) choking in the annulus at the inducer blade leading edge, and c) vapor recirculation, the inducer should be designed with; 1) thin stator blades with positive incidence at design and without camber on the forward portion, 2) an inlet annulus large enough to pass the flow during an equilibrium flow expansion from the tank to the inducer, and 3) no recirculation of vapor through the inducer. An alternative method for eliminating the stator cavitation problem is to eliminate the stator by using a straight centrifugal pump in which the flow passes from the inducer directly into the impeller.

Conclusions

The ability to pump two-phase hydrogen will permit zero tank NPSH (saturated LH_2 in the tank) operation, a reduction in both the time and the propellant required to chill the inlet line during warm restarts, and a reduction in the required tank saturation pressure. The good correlations between the predictions and the experimental test data indicate that the basic limit to pumping two-phase hydrogen occurs when the combined effects of high-flow-coefficient operation and blade blockage cause the flow area within the inducer blade passage to be less than the inlet flow area. When this occurs, both two-phase and pure saturated liquid flows will choke. At design liquid flow coefficient, allowable pump inlet vapor-volume fractions of 20% have been obtained experimentally. By designing at the maximum leading edge incidence angle that will permit two-phase pumping, this value can be doubled and, by also using a low-speed preinducer, this value can be increased nearly three times. Other flow phenomena, such as inducer stator cavitation, choking in the inlet annulus, and vapor recirculation through the inducer, can also limit the vapor fraction that can be pumped. However, these limitations can be avoided by proper design.

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