Journal of Spacecraft and Rockets

VOLUME 7 MAY 1970 NUMBER 5

Orbital Refueling Techniques

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DURING the last 20 years, in-flight refueling of military aircraft has become routine. The extension of this operational technique to earth-orbital spacecraft will become mandatory as mission durations are extended up to one year and beyond. This paper reviews the more feasible and promising concepts for propellant transfer in orbit and discusses their compatibility with typical refueling requirements.

Several analyses are presented which relate to determining vapor-liquid-interface stability, pressurant requirements, transfer-line chilldown, receiver-tank thermodynamics, propellant-transfer dynamics and associated nonsteady flow problems, and dielectrophoresis. A simplified concept for estimating suction specific speeds of low-NPSH (net positive suction head) pumps is given. Finally, two figures of merit are suggested for use in system tradeoffs, one for the transfer system only and one for the over-all tanker vehicle.

Propellant Transfer System Concepts

Tank replacement is an obvious method for transferring small to moderately large quantities of propellants. Quick-disconnect couplings would be utilized to minimize required extra-vehicular activity (EVA) on the part of the astronauts. This concept appears to be attractive for resupply of life support fluids and for transferring small quantities of propellants.

The use of bladders (Fig. 1) or pistons for expulsion has definite advantages in a zero-g environment.[45] The problems of ullage control and liquid/vapor separation are eliminated, and the propellant outage is limited only by imperfections in the bladder design or its operation. No artificial gravity need be created to prevent vapor ingestion, as in the case of pump transfer systems. An ideal bladder structure

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Received May 28, 1969; presented as Paper 69-564 at the AIAA 5th Propulsion Joint Specialist Conference, U.S. Air Force Academy, Colo., June 9–13, 1969; revision received January 22, 1970.

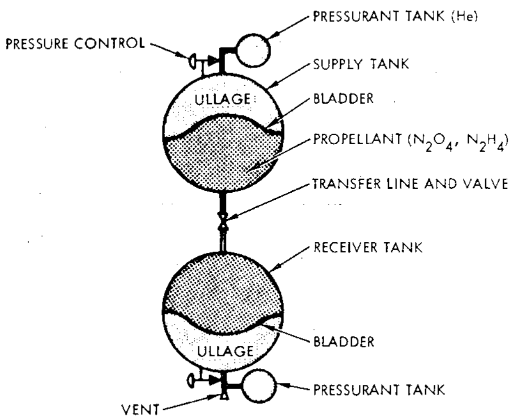


Fig. 1 Typical transfer system schematic A.

would be flexible over a wide range of temperatures, chemically inert to both pressurant and propellant, and impervious to pressurant gas and propellant vapors. Unfortunately, most bladder materials lose their flexibility at cryogenic temperatures. Possible candidate materials could be aluminized Mylar or Kapton. Leakage of the pressurant gas into the propellant can also be a problem. The use of laminated bladders utilizing a metal foil (with some loss in flexibility) as a diffusion barrier is one possible approach. Present bladder manufacturing technology does not permit sizes much above 50 in., and bladder cycle life is extremely limited. Because of these problems, the bladder technique has been restricted to use for small quantity, noncryogenic applications.

One straightforward concept for the transfer of liquid cryogens (LO_2, LH_2) is draining by linear or angular acceleration[45] (Fig. 2). After rendezvous, the tanker and the receiver spacecraft are coupled, either umbilically or by direct docking. A linear acceleration is then applied to cause gravity draining of the propellants within the mission allocated time. Baffles or screens are installed in the tanks to reduce disturbance torques or space vehicle oscillations caused by fluid momentum changes.

However, despite its simplicity, this propellant transfer system is limited as to flow rate. For example, when draining an LH_2 tanker vehicle similar in size to the Saturn SIVB stage, use of a 1-ft-diam line in an acceleration field of $10^{-4}g$ would permit transfer of only 8 lb/sec. Thus, it would take 1.5 hr to refill an SIVB stage with LH_2. If this time period is acceptable to the mission planner, this concept should receive major consideration. However, $10^{-4}g$ may be a rather high level to maintain for long periods of time. If the g level is reduced to $10^{-6}g$, the time required for the above transfer would be increased to 15 hr.

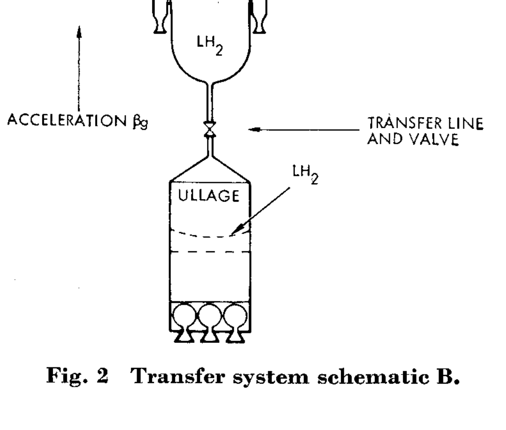


Fig. 2 Transfer system schematic B.

For the rapid transfer of a large quantity of a cryogenic propellant, a pump, pressurization system, or both may be required. Many different system concepts could be employed, such as: 1) an inert gas pressurant, e.g., helium (heating to increase gas specific volume and low acceleration level to control ullage in the tanker and to settle propellants in the receiver vehicle may be required); 2) a low-NPSH transfer pump (low g level to provide necessary NPSH and/or baffles, surface tension screens or dielectrophoretic fluid orientation may be required); 3) gas-generator pressurization (a pressure-fed combustion chamber and exhaust gas cooling are required); 4) main-tank-injection pressurization (see, e.g., Refs. 52–54; this concept is suitable only if hypergolic propellants are involved); 5) boil-off pressurant (back-flow pressure regulators and solar energy or other heat input may be required).

In order to select the preferred propellant transfer mode it is desirable to evaluate the various phenomena that would influence this choice (see Refs. 55–57 for some general discussions). The following section discusses the transfer process.

Transfer Process Considerations

Initially, the supply tank is nearly filled with propellant and since it is in a zero-g environment the location of the liquid with respect to the ullage is uncertain. Before transfer can begin, the liquid must be placed in the desired position with respect to the ullage and the liquid outlet. After the supply vehicle has been connected to the receiver tank and propellant transfer is initiated, various two-phase problems may ensue. For example, during the course of supply-tank draining, the interface between the vapor and liquid must remain stable if blow-through is to be avoided when a gas pressurant expulsion system is utilized. The stability of this vapor-liquid interface is a sensitive function of g level and is predictable to some degree. In addition, when the supply tank is nearly empty, vapor ingestion can occur due to vortexing or dropout, or because the fluid being expelled is largely vapor. If this occurs prematurely, the outage during the transfer process can be unacceptably high. Vapor ingestion and the associated percent outage are also influenced by geometry (baffles, surface tension screens, etc.) and transfer rate. In the case of the transfer of cryogenic propellants, the time required for transfer-line chilldown, receiver-tank chilldown, and the associated problems of unsteady flow are significant. Large quantities of vapor are released initially in the receiver tank, giving rise to venting problems. Pressure waves arising in the transfer line because of the rapid evaporation of large quantities of liquid[47] propagate back to the supply tank causing disturbances in the supply tank hydrodynamics. Finally, the liquid, upon arriving in the receiver tank must be settled to prevent liquid venting loss and vehicle oscillations. Thus, the problem of the hydrodynamic stability of the liquid-vapor phase interface and fluid damping in the receiver tank arises. Short discussions of the present theoretical capabilities and available computational techniques for these problems follow.

Gravity Level Requirements for Vapor-Liquid Interface Stability

The problem is to be able to predict the position and behavior of the phase boundary as a function of acceleration level, tank geometry, fill and/or drain rate, and the physical properties of the fluid being transferred. Consider a cylindrical tank containing the liquid and vapor phase of some propellant as shown in Fig. 3.

The acceleration is assumed to be large enough so that the vapor-liquid interface is originally nearly perpendicular to the tank axis. Under these conditions the dispersion law

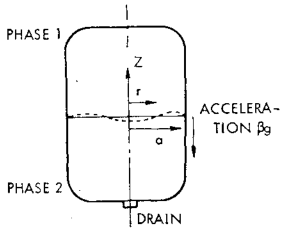


Fig. 3 Cylindrical tank.

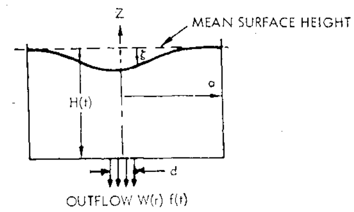


Fig. 4 Vapor ingestion problem.

for small oscillations of the interface is easily obtained. Use is made of the fact that small oscillatory flow is always potential flow and that the fluid velocity is derivable from a velocity potential $\hat{q} = -\nabla\phi$, where $\hat{q}$ is the fluid velocity, and ϕ is the velocity potential. If the fluid is incompressible (a reasonable assumption for low velocity), then ϕ satisfies Laplace's equation.[47] In cylindrical coordinates this is

$$\nabla^2\phi = \partial^2\phi/\partial r^2 + (1/r)\partial\phi/\partial r + \partial^2\phi/\partial z^2 = 0 \tag{1}$$

The velocity potential satisfies an equation of the form Eq. (1) in both phases. The solution is completed by standard methods to obtain the dispersion law, or the relationship between wave length and frequency of radial waves on the vapor-liquid interface. It is

$$(\rho_1 + \rho_2)\omega_n^2 = k_n\{\sigma k_n^2 + \beta_g(\rho_2 - \rho_1)\} \tag{2}$$

where ρ_1 is the density of the fluid on "top" and ρ_2 that of the fluid below, σ is the surface tension, and k_n is the wave number of the surface wave. The waves are steady (of constant amplitude) if $\omega_n^2 > 0$, but grow unbounded in time if $\omega_n^2 < 0$. For a cylinder of finite radius, the set k_n is not arbitrary but is given by the solutions of $J_1(k_n\alpha) = 0$, where α is the tank radius and J_1 is the Bessel function of order one.

From Eq. (2) it can be seen that if $\rho_2 > \rho_1$ (heavier phase on bottom) the interface is always stable ($\omega_n^2 > 0$) since every member of the set k_n is positive. Even if gravity is entirely absent ($\beta_g = 0$), the interface remains stable due to the effect of the surface tension. If the denser phase is on top, then $\rho_1 > \rho_2$ and Eq. (2) can be written as

$$(\rho_1 + \rho_2)\omega_n^2 = k_n\{\sigma k_n^2 - \beta_g|\rho_1 - \rho_2|\} \tag{3}$$

Note that the surface tension term is still stabilizing, but that those waves having wave length $\tau_n = 2\pi/k_n$, are unstable when

$$\beta_g|\rho_1 - \rho_2| > \sigma k_n^2 \tag{4}$$

which is the condition for settling of the liquid phase. Practically speaking, k_n must be chosen large enough so that any wave of appreciable wave length on the interface will grow. If k_n is chosen as the third harmonic k_3 for example, then almost any disturbance of the interface will result in the repositioning of the two phases, i.e., the settling of the liquid. Hence, settling is assured when the Bond number, i.e., the ratio of the gravity forces to the surface tension forces,

$$B_0 = \text{Bond No.} = \rho g a^2/\sigma \tag{5}$$

is large. However, the formulation of Eq. (4) has the advantage of indicating the minimum required value of β_g, since the liquid will definitely settle when $k_n = k_3$.

The problem of thermal stratification is discussed in Refs. 48 and 49.

Liquid-Vapor Interface during Draining and Vapor Ingestion

When the tank is draining or filling, a more complex phenomenon ensues. In a general sense one can assume that a gravity level sufficient for settling is also sufficient for interface stability during draining since the motion of the liquid is away from the interface, tending to increase effectively the stabilizing inertia term. During filling, a slightly higher gravity level would be required for settling, since the momentum of the liquid is toward the interface. A generalized case is shown in Fig. 4.

A draining rate $W(r)f(t)$, where W is taken as a parabolic function in r and $f(t)$ is an arbitrary function of time, is postulated. The shape of the interface, the mean liquid height $H(t)$, and the time for vapor ingestion inception are then calculated using the hydrodynamics of a nonviscous fluid. For the shape of the interface, characterized by its deviation from the mean $H(t)$, by $\zeta(r, t)$, at vapor ingestion, the percent outage is also calculated.

Since the liquid is assumed to be nonviscous and the flow irrotational, the velocity q can be derived from a velocity potential ϕ satisfying Laplace's equation, Eq. (1). The solution in this case, however, must satisfy the conditions that the normal component of q vanish at the tank surface.[14–16] Thus,

$$u = \partial\phi/\partial r = 0 \quad \text{at} \quad r = a \tag{6}$$

and

$$w = \frac{\partial\phi}{\partial z} = \begin{cases} W(r)f(t) & 0 \le r \le d \\ 0 & d \le r \le a \end{cases} \tag{7}$$

where u is the radial component of q and w is the axial component. The kinematic free surface conditions relates ω to H and ζ. It states that the velocity of a fluid particle on the free surface moves with the velocity of the free surface

$$w = (\partial\phi/\partial z)|_{z=H+\zeta} = \hat{H} + \partial\zeta/\partial t + u(\partial\zeta/\partial r)|_{z=H+\zeta} \tag{8}$$

In addition, the dynamic condition that the stress be continuous across the interface between liquid and vapor phases at $z = H + \zeta$ requires that

$$p/\rho + \partial\phi/\partial t + \tfrac{1}{2}q^2 + gz|_{z=H+\zeta} = 0 \tag{9}$$

where the pressure on the free surfaces can be related to the surface tension forces by

$$p = -\sigma[\partial^2\zeta/\partial r^2 + (1/r)\partial\zeta/\partial r] \tag{10}$$

The system of Eqs. (1) and (6–10) is sufficient to solve the entire problem given an initial interface configuration. The solution is obtained by forming an eigen function expansion for ϕ with coefficients $A_n(t)$ and $B_n(t)$ which are functions of time, and a similar one for $\zeta(t)$, the shape of the interface with coefficient $C_n(t)$.[14–16] Nonlinear ordinary differential

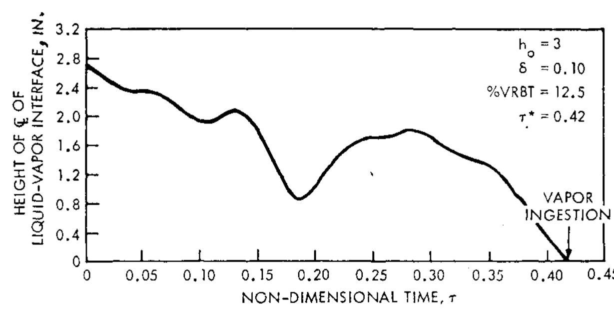


Fig. 5 Time-history of position of centerline of liquid-vapor interface during draining at $We = 10$.

equations are derived for the functions $A_n(t)$, $B_n(t)$, and $C_n(t)$, which are solved numerically on a digital computer. It is found that the behavior of the draining process is characterized primarily by the Weber number, or the ratio of inertia to surface tension forces. Figure 5 shows the results of a typical calculation.

Gas Pressurant Mass Determination

Much theoretical work has been done in attempts to predict the mass of the pressurant gas required to transfer a given quantity of cryogenic propellant,[1–3] and a detailed program exists[4] for which empirical inputs have been obtained by Nein and Thompson. However, for purposes of conducting engineering tradeoffs, a less complicated approach is available. For example, good estimates of the quantity Δm of pressurant gas as a function of its initial enthalpy h_i at saturation conditions and pressure p can be obtained from the "lumped parameter" results of Gluck and Kline,[5] who give for the Δm required to displace a volume of liquid ΔV

$$\Delta m = [(c_p/R)p\Delta V + c_pT_sm_t + Q]/[(h_i - h_s) + c_pT_s] \quad (11)$$

where c_p = specific heat at constant pressure; R = universal gas constant; T_s = saturation temperature of cryogen corresponding to pressure p; m_t = mass transfer at interface (total); h_i = enthalpy of entering pressurant gas at saturation conditions; and Q = total heat transferred to walls of gas phase enclosure during the time of transfer. The most uncertain factor in this expression is Q, but it can be estimated from the results of Ref. 4; m_t is usually negligible but can be estimated using the theory of thermal conduction at an evaporating boundary.[6] Most of the assumptions implicit in Eq. (13) have been verified by subsequent analysis,[7] so that reasonably accurate predictions should result. This equation also can be used for positive (i.e., bladder) propellant expulsion systems.

Transfer Line Chilldown

In the transfer of liquid cryogens, one of the most critical processes, in terms of influence on the over-all transfer process, may well be the initial transient flow which results from the cooling by the cryogen of the transfer lines. The formation of cryogen vapor in the chilldown of the transfer lines leads to large pressure and flow oscillations ("slug flow") and to the expulsion of large quantities of vapor into the receiver tank. The pressure oscillations can be quite large[8] and can lead to flow oscillations which would seriously disturb conditions in the supply tank. The duration of the transient period depends on the thermal mass of the transfer line, its initial temperature, and the rate of ambient heat addition to the transfer line. This period could be as long as 100 to 200 sec. This problem has been experimentally investigated, and simple analytical expression for estimating transfer line chilldown time has been developed.[8] The determination of the pressure oscillations induced by the chilldown process is probably the more critical for orbital refueling.

Ideally, this problem can be approached by solving the one-dimensional, two-phase, transient flow equations for a cylindrical pipe. A brief outline of the theory[9] follows. The major assumptions are 1) two-phase flow is one-dimensional and 2) kinetic and potential energy terms are small compared to thermal energy terms. The resulting conservation equations in terms of the liquid and vapor densities, ρ_l and ρ_g, the vapor velocity u_g, and the void fraction α are

mass conservation

$$(\partial/\partial t)[\rho_l(1-\alpha) + \rho_g\alpha] + (\partial/\partial z)[\rho_l(1-\alpha)u_l + \rho_gu_g\alpha] = 0 \quad (12)$$

energy conservation

$$(\partial/\partial t)[\rho_l(1-\alpha)E_l + \rho_g\alpha E_g] + (\partial/\partial t)[\rho_lv_lH_l(1-\alpha) + \rho_gu_gH_g\alpha] = q(P/A) \quad (13)$$

momentum conservation

$$(\partial/\partial t)[\rho_lu_l(1-\alpha) + \rho_gu_g\alpha] + (\partial/\partial z)[\rho_lu_l^2(1-\alpha) + \rho_gu_g^2\alpha] = -(\partial p/\partial z) - \tau_\omega(P/A) - [\rho_l(1-\alpha) + p_g\alpha]\beta_g \quad (14)$$

Here E and H refer to the internal energy and enthalpy, respectively, subscripted for the appropriate phase; q is the heat input at the wall; z, the coordinate along the pipe; t, the time; P, pipe perimeter; A, pipe cross-sectional area; p, the pressure; τ_ω, the shear stress at the wall; and β, the fractional gravity gradient directed along the pipe. These equations, when supplemented by the necessary subsidiary relations, have been successfully used to determine the hydrodynamic stability of closed-loop boiling systems.[8] This theory will also give a useful approximation for the transfer line chilldown transient.

The conversation-of-energy equation for the transfer line is also required. This equation can be easily derived under the following assumptions: 1) at any axial position the line wall has a single average temperature T_w, heat capacity c_w, and density ρ_w; 2) axial heat conduction is neglected; and 3) all heat loss from the line is to the coolant. The energy balance then states simply that the rate of energy loss from the transfer line is equal to the rate at which it is transferred to the cryogen. Allowance must also be made for external line heating due to solar radiation.

The subsidiary relations necessary to obtain a solution to Eqs. (12–14) are

1) An equation relating quality-void and slip ratio. This relation is known since quality in two-phase flow is defined by

$$1/x = 1 + [(1-\alpha)/\alpha](\rho_l/p_g)1/s \quad (15)$$

where x is the quality and s is the slip ratio.

2) An equation relating slip ratio to system and fluid parameters. Use of Bankoff's evaluation[10] is suggested.

3) An equation relating wall shear stress to quality. A Martinelli-Nelson type fraction factor multiplier correlation is suggested here. This correlation has been found to be applicable to a wide variety of boiling fluids including LH_2 (Ref. 11).

There exists in the literature[12,13] experimental determinations of q/A as a function of $(T_w - T_s)$, the difference between wall temperature and liquid saturation temperature, for LH_2 and other cryogens. Using these values, along with the subsidiary relations listed above, a complete solution to the transfer-line chilldown problem can be obtained by solving Eqs. (12–14) numerically.

Different transfer line problems arise during the transfer of storable propellants. In this case the problem is one of freezing, particularly during the transfer of hydrazine (N_2H_4) with a freezing point of 34°F. Collection of ice slush in the transfer line or receiver tank can lead to serve difficulties. This problem can probably be easily overcome, however, by use of heated or insulated transfer lines and increased rates of propellant transfer.

Receiver Tank Thermodynamics and Venting

When a cryogenic propellant is loaded into an initially warm tank, large quantities of vapor will be released during the process of chilling the tank. The disposition of this vapor is an important consideration in the over-all system design, since indiscriminant venting will exert an uncontrolled acceleration and could create a hazard for the personnel aboard and for the docked vehicles.

It is first necessary to determine the receiver-tank-wall temperature. Effects of solar radiation, earth albedo, and radiation to outer space must be assessed, and the average tank-wall temperature must be estimated. Then the total quantity of vapor which will be released can be predicted by a simple heat balance. However, the prediction of the duration of the release is more difficult, since the heat-transfer rates between the two-phase mixture in the receiver tank and the wall are unknown. If an estimate of the heat-transfer coefficient between the contents of the receiver tank (initially this will be both vapor and liquid from the transfer line) is available, then the chilldown time, or the time required for the receiver tank wall to reach the liquid saturation temperature, can be estimated by solving the lumped-parameter equations. The following equations express the conservation of energy of the tank wall and tank contents, respectively,

$$m_w c_w (dT_w/dt) = -hA(T_w - T_g) \tag{16}$$

$$(d/dt)(m_g c_g T_g) = hA(T_v - T_w) - \dot{w}_i(1 - X)U_{fg} - \dot{w}_i c_g(T_g - T_i) - \dot{w}_0 c_v T_v \tag{17}$$

where m_w, c_w, and T_w are the mass, specific heat, and temperature of the tank wall; m_g, c_g, and T_g are the same parameters for the ullage gas; T_i is the temperature of vapor entering tank; h is the convective heat-transfer coefficient; A, the tank internal area; U_{fg}, the latent heat of the liquid cryogen; X, the quality of the cryogen entering the receiver tank; and $\dot{w}_i$ and $\dot{w}_0$ are the mass flow rate into, and vent rate from, the receiver tank. The last two quantities are connected with m_g by the tank mass balance equation

$$dm_g/dt = \dot{w}_i - \dot{w}_0 \tag{18}$$

Further, the vent rate, $\dot{w}_0$ might be fixed by the venting system requirements. Equations (16–18) assume that any liquid cryogen entering the receiver tank is evaporated within the ullage gas and that only the gas exchanges heat directly with the tank wall. These are reasonable assumptions, at least for the initial phase of the tank chilldown. The set of Eqs. (16–18) can be solved numerically. In this way, an estimate of the vent requirements, chilldown time, and quantity of vapor formed as a function of time after the inception of transfer can be obtained. A reasonable estimate of the heat-transfer coefficient would be that calculated from boundary-layer theory at low Reynold's number. More sophisticated equations can be devised to take into account direct liquid-tank wall contact, but they will have the same form as those above. From Eqs. (16–18) and the gas law ($p_g V = m_g R T_g$), the pressure in the receiver tank can be calculated as a function of time.

Under some conditions, particularly in transferring LH_2, use of a heat exchanger to facilitate venting may be appropriate.[12,13] Figure 6 shows a system schematic and the corresponding thermodynamic processes. The two-phase fluid entering the vent line passes through the throttle valve to a state of lower pressure and temperature. The fluid is then passed through a heat exchanger, where the required heat of vaporization is obtained from an external source (solar radiation, space vehicle waste heat, etc.). The heat exchanger is designed to assure that only vapor leaves the vent. If liquid enters the venting system, it expands to a lower pressure and temperature through the throttle valve (1-2) and is vaporized in the heat exchanger tubes (2-3), absorbing heat that would otherwise enter the storage tank. The resulting vapor is further heated at constant pressure (3-3′) before expulsion to space. Process (2′-3′) is a possible process when pure vapor (quality = 1.0) enters the vent. It is expanded to ambient conditions with a lowering of its temperature but an increase in entropy. Considering the over-all venting system, only heat is added and only hydrogen gas leaves the system. Therefore, the propellant loss is independent of the quality of the fluid leaving the tank. Chilldown of the receiver tank during loading, if necessary, can be accomplished by using the same hardware as was used for the venting phase.

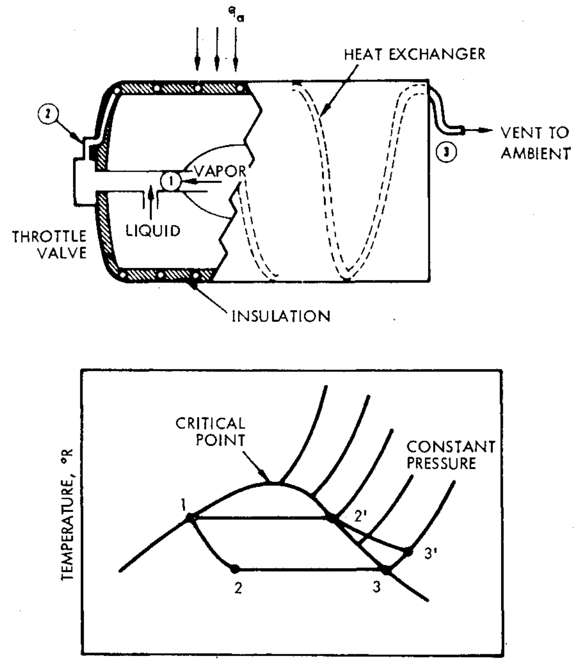


Fig. 6 Thermodynamic vent system.

Dynamic Considerations Associated with Orbital Propellant Transfer

When liquid is transferred from one vehicle to another, the kinetic energy imparted to the liquid will exert pressure on the walls of the container. In addition, the ensuing momentum changes will produce forces and moments on the tanker and receiver vehicles. Although these fluid pressures and momentum changes can be kept relatively small, the absence of damping or other restoring forces (other than reaction control forces employed) could produce tumbling or other dynamically unstable modes in the transfer system vehicles.

An illustration of a typical method of analysis to assess the magnitude of this problem follows. In Fig. 7, two tanks are depicted which are connected as shown by flexible tubing. The differential equations of motion for both vehicles are,

$$(d/dt)[m_1(t)\dot{x}_1] = T(t) + P_1(t) + \rho V_0^2 A_0 \tag{19}$$

$$(d/dt)[m_2(t)\dot{x}_2] = P_2(t) + \rho V_i^2 A_i \tag{20}$$

$$(d/dt)[I_1(t)\dot{\alpha}_1] = m_1(t) \tag{21}$$

$$(d/dt)[I_2(t)\dot{\alpha}_2] = m_2(t) \tag{22}$$

and where $m_i(t)$, the total mass of the propellant, is

$$m_i(t) = m_{iv} + \rho\pi r_i^2 h_i(t) = m_{iv} + m_{ip}(t) \tag{23}$$

where m_{iv} and m_{ip} are the empty vehicle and propellant masses, respectively, and $I_i(t)$, the moment of inertia of each vehicle about its center of mass is given by

$$I_i(t) = [I_{iv} + m_{iv}e_1^2] + [I_{ip} + m_{ip}f_i^2] \tag{24}$$

where I_{iv} and I_{ip} are the moments of inertia of the vehicle and propellant about their own mass centers.

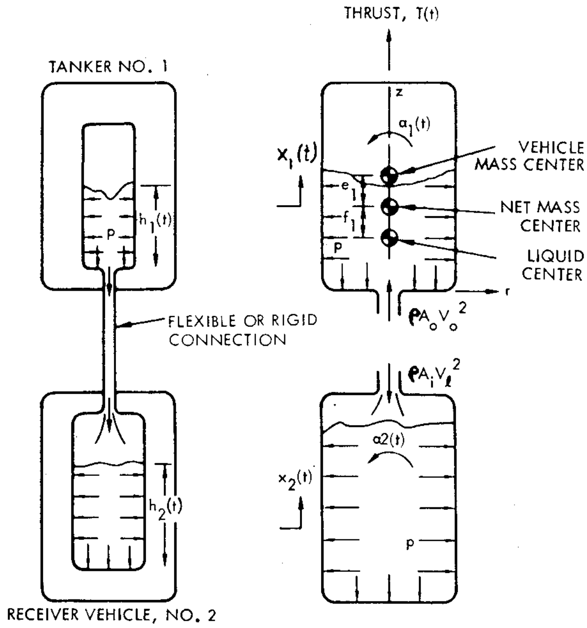


Fig. 7 Geometry.

In Eqs. (19–22), $T(t)$ is a thrust which could be applied to the tanker vehicle to create an acceleration field, and the quantities ρAV^2 are forces applied to each tank at the outlet (or inlet) to account for the flux of momentum out of (into) the tanks during the propellant transfer. The outlet and inlet velocity would depend upon the mode of propellant transfer; e.g., pressure-fed, "gravity-fed," etc.

The forces $P_i(t)$ and moments $m_i(t)$ generated by the propellant motion within the tanks are obtained by integration of the fluid pressures on the walls and bottom of the tank from Bernoulli's equation. For a linear approximation

$$p = \rho(\partial\phi/\partial t) \tag{25}$$

where ρ is the liquid density and ϕ is a potential function from a theoretical draining study computer program.[14,15] In terms of ϕ, the force and moment equations have the form

$$P_i(t) = \rho \iint_{\text{wall}} [\partial\phi_i/\partial t]_{r=a_i} r \cos\theta dr dz; \quad i = 1,2 \tag{26}$$

$$m_i(t) = \rho \iint_{\text{bottom}} [\partial\phi_i/\partial t]_{\text{bottom}} (r\cos\theta) r dr d\theta + \rho \iint_{\text{wall}} [\partial\phi_i/\partial t]_{r=a}\, z \cos\theta d\theta dz \tag{27}$$

For tubing having appreciable flexibility, additional forces would have to be included in Eqs. (19–22). These equations would have to be determined for each mode of propellant transfer under consideration. In addition, for a transfer system concept using flexible tubing, its dynamic response would also have to be investigated to assess its stability. It would be essential to avoid the critical gyrating oscillations caused by the tether cable during the Gemini II flight. The appropriate equations for the transfer tube have the form

$$EI\frac{\partial_y{}^4}{\partial x^4} + S\frac{\partial_y{}^2}{\partial x^2} + \rho v^2\frac{\partial_y{}^2}{\partial x^2} + 2\rho v\frac{\partial_y{}^2}{\partial x\partial t} + \frac{m}{l}\frac{\partial_y{}^2}{\partial t^2} = 0 \tag{28}$$

where y is the transverse deformation of the tube; EI, the flexural rigidity; S, the axial compressive force exerted on the tube; and m/l is the mass per unit length of pipe including the propellant. Equation (28) must be examined for each configuration from the view point of critical flow conditions, etc.

Propellant Settling and Orientation

Under most conditions of transfer, a linear acceleration will be present for purposes of ullage control in the supply tank and the reduction of vapor ingestion at the supply tank outlet. As was previously discussed, this level may or may not be sufficient for the settling of the propellants in the receiver tank without baffles or other ullage control devices. Since transfer to a wide variety of receiver vehicles is conceivable, consideration should be given to predicting minimum "g" levels required for purely gravity settling in the receiver tank. The analyses previously presented for propellant draining Eqs. (6–11) may also be used to study the behavior of the liquid-ullage interface during tank filling. Similarly, the amplitude of the free surface oscillations can be calculated as a function of transfer rate, inlet size, and gravity level. Figure 8 shows the results of a typical calculation. The distortion of the interface, $\zeta(t)$, at the tank centerline is plotted as a function of time after initiation of filling. Here, a indicates the tank radius, δ the inlet radius, σ the surface tension of the liquid, ρ its density, g the gravity level, α the filling rate, and h_0 the initial liquid level in the receiver tank. If the amplitude of fluid oscillations is considered to be excessive, the use of baffles, screens, or other control methods must be considered.

Antislosh Baffles

Baffles and retention devices, e.g., surface tension screens[17,18,44] may be needed to control the motion of the propellants in the receiver and supply tanks during propellant transfer. Baffles are required in the supply tank to diffuse the incoming pressurizing gas and to prevent the ingestion of vapor during the drain process due to sloshing and dropout. In the receiver tank, baffles are also required to control sloshing and to dissipate the kinetic energy of the incoming propellant in a controlled fashion, and they aid in minimizing propellant stratification by providing better mixing.[16] Induced propellant motion would continue for long periods following a transfer if it were not effectively damped,[16] and the resulting pressures and moments on the structure would feed random disturbances to the attitude control system. The design and prediction of baffle performance under various g conditions are outlined in Refs. 17, 18, 44 and 50.

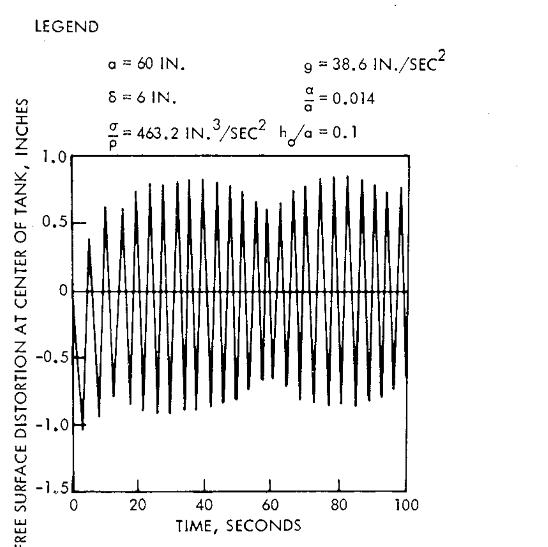
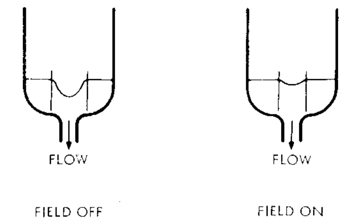


Fig. 8 Time history of free-surface distortion at center of tank during filling.

Fig. 9 Pull-through with and without electric field.



Dielectrophoresis

A relatively new method of orienting fluids with good dielectric characteristics against their vapors is by the use of electric polarization forces. Numerous analytical and experimental studies,[19–27] have demonstrated the feasibility of such a method to control liquid propellants in a low-g environment. The basis for this orientation is the dielectrophoretic force density **F**

$$\mathbf{F} = -(\epsilon_0/2)\mathbf{E}\cdot\mathbf{E}\nabla K \quad (29)$$

where ϵ_0 is the permittivity of free space, **E** is the electric field intensity, and K is the dielectric constant.

In general, the total electrostatic force density can be written as,

$$\mathbf{F} = \omega_e\mathbf{E} - (\epsilon_0/2)\mathbf{E}\cdot\mathbf{E}\nabla K + (\epsilon_0/2)\nabla(\mathbf{E}\cdot\mathbf{E}\rho\partial K/\partial\rho) \quad (30)$$

where the first term is due to the force on the free charge ω_e in the liquid and the last term is commonly referred to as the electrostriction force. The first is a destabilizing force and requires that the applied voltage be of an a.c. mode and of low frequency. The last term has sometimes been credited with being the force which orients the liquid.[24] This term, however, being the gradient of a scalar quantity, can be combined with the hydrostatic pressure in any analysis of an incompressible fluid. If this is done and the required boundary conditions applied, it can be shown that this term will have no effect on the motion of the liquid.[19]

Dielectrophoresis should be considered as a possible method of preventing vapor pull-through in the transfer tank.[24] This phenomena occurs because the liquid flowing out of the tank moves much faster directly above the outlet line than near the tank wall. Thus, the liquid surface dips (sometimes called drop-out) as shown in Fig. 9. This dipping adversely affects tank outage and could result in vapor ingestion and slug flow in the transfer line.

By placing a set of parallel screen mesh electrodes on either side of the drain and perpendicular to the tank bottom as shown in Fig. 9, the problem of vapor pull-through, to a large extent, can be alleviated.[24] The condition for no vapor pull-through is

$$\mathbf{F} = \epsilon_0(K_1 - K_2)E_0^2/2\Delta\rho g h_c \quad (31)$$

where h_c is the height of the screen mesh from the bottom of the tank. As an example of the magnitudes of the applied fields needed to prevent vapor pull-through, consider the LH_2 tank shown in Fig. 10.

For hydrogen, $K_1 = 1.25$, $K_2 = 1.0$, $\Delta\rho = 70$ kg/m^3. From Fig. 10, $h_c = 3$m. From these values, $E_0 = 44$ kv/m for a value of $g = 10^{-5}$m/sec^2. This can be achieved by a small voltage of 5.6 kv for the 0.127-m electrode spacing.

Fig. 10 Schematic of LH_2 tank.

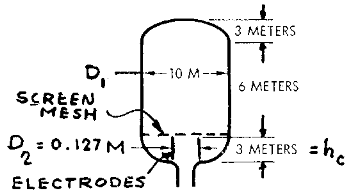
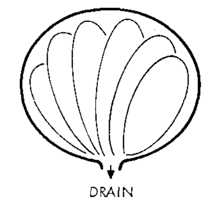


Fig. 11 Electrode configuration.



Since LH_2 electrical breakdown strengths are high enough to handle such voltages easily and equipment necessary to generate such voltages is very light, the application of dielectrophoresis to prevent vapor pull-through appears attractive.

Dielectrophoresis also can be used to position the propellant in either the transfer tank for draining or in the receiver tank for propellant utilization. A typical electrode configuration for positioning the propellant is shown in Fig. 11.

Surface Tension Devices and Screens

Two typical designs for orienting the vapor ullage toward the vent outlet and containing the liquid in the screened compartment for the transfer through the discharge line are shown in Fig. 12. In the standpipe arrangement, the shape and size must be calculated to provide for the most stable interface with the gas ullage positioned away from the liquid discharge. In the double-screen configuration, the outer screen mesh is sized so that any gas bubbles that break through the outer screen will be purged by the buoyancy force during operation in the positive g field. Wire mesh and perforated sheets are commonly used for containing the liquid.[45] The screen mesh or pore size is selected so there will be a sufficient Δp, so that no breakthrough of the gas bubble will occur; this phenomenon depends on the liquid surface tension and liquid contact surface area.

Propellant Mass Gaging

To determine the quantity of propellant transferred and the amount of propellant left in the supply tank, and to meet propel'ant loading accuracy objectives, it is necessary to use a gaging system. Propellant mass determination poses a special problem under zero-g or low conditions, because the location of liquid and vapor phases is not distinctly defined under such environments.[51] The gaging systems considered have included the following types: pressure-volume-temperature,[38] acoustical,[42] radioactive tracer gas,[43,45] nuclear radiation attenuation,[40,41] capacitance,[38,43] positive displacement,[44] density-volume,[44] optical,[44] radio-frequency,[43] flow-rate measurement,[44] and point sensor.[38,39] These systems are in various states of development and are capable of providing loading accuracies from $\pm1\%$ to $\pm5\%$ for nonvented systems. Venting requirements increase the complexity of these systems. A detailed system engineering

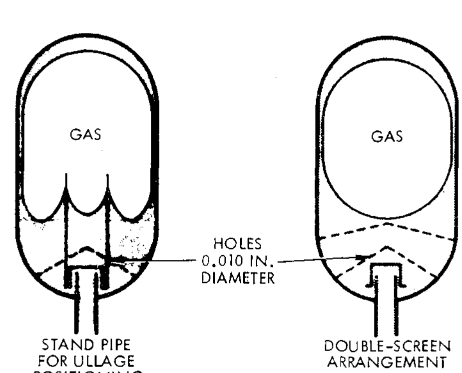


Fig. 12 Propellant orientation.

analysis is required to determine the particular approach to take and the cost effectiveness of the loading accuracy requirements.

Low-Net-Positive-Suction-Head Pumps

The use of low-NPSH pumps for propellant transfer (Fig. 13) is a promising approach, particularly where cryogenic fluids and large flow rates are involved. The conventional approach to the determination of the required NPSH must be modified if a more accurate assessment is to be made. A suggested analytical technique for accomplishing this[28,29] is summarized here.

Similarity in centrifugal pump performance is given by the well-known expression for specific speed N_s

$$N_s = nQ^{1/2}/H^{3/4} = \text{const} \tag{32}$$

where n = speed, rpm; Q = flow rate, gpm; and H = head, ft. At constant specific speed, prediction of cavitation inception from previous test data can be made by applying the Thoma-Moody parameter σ

$$\sigma = H_{sv}/H = \text{const} \tag{33}$$

where H_{sv} = net positive suction head, ft. An additional relationship, applicable within the restrictions of the similarity laws (maintenance of geometric, kinematic, and dynamic similarity from model to prototype pump) is that of suction specific speed S

$$S = nQ^{1/2}/H_{sv}{}^{3/4} = \text{const} \tag{34}$$

Since the two parameters, σ and S have parallel use in the same field of application, it is desirable to state an analytic expression for the relation between them. This expression, which has been derived previously from the explicit expressions for S and N_s is

$$\sigma = (N_s/S)^{4/3} = \text{const} \tag{35}$$

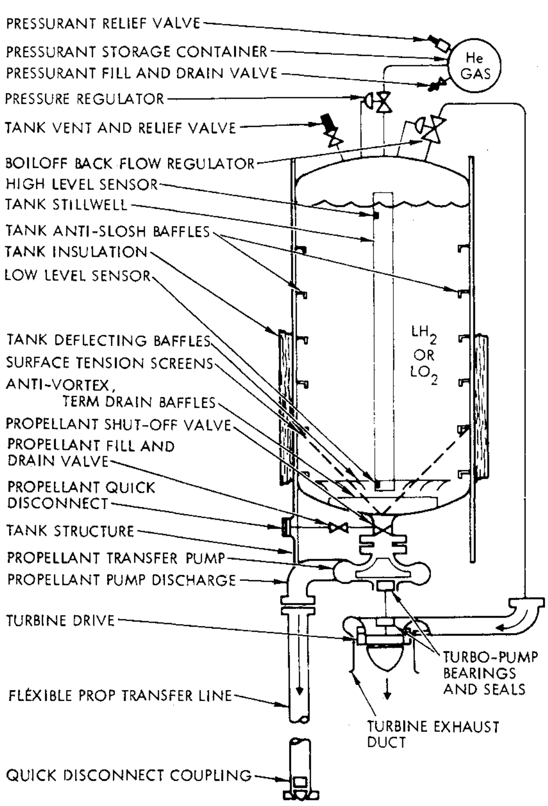


Fig. 13 Typical propellant transfer system.

The preceding analyses for cavitation inception are incomplete in that the effects of thermodynamic, heat transfer, and fluid properties (other than density and viscosity) are not taken into consideration. The use of the "cavitation tendency ratio" (CTR) as outlined in Ref. 25 considerably improves the estimate for NPSH, particularly for cryogenic fluids. The cavitation tendency ratio is defined as,

$$\text{CTR} = B_1/B_2 = H_{sv_2}/H_{sv_1} \tag{36}$$

and where

$$B = (\lambda v_l/v_g)/[(1 - c_p/v_l)\partial T/\partial p] \tag{37}$$

where B is the cavitation tendency number, subscripts 1 and 2 refer to fluid 1 and fluid 2, λ is the latent heat of vaporization, v_l and v_g are the specific volumes of liquid and gas, c_p is the specific heat of gas, and $\partial T/\partial p$ is the influence coefficient for fluid temperature-vapor pressure variation. By Eqs. (36) and (34) we can relate CTR to S:

$$\text{CTR} = (S_2/S_1)^{4/3} \tag{38}$$

By determining the theoretical CTR between two fluids and using Eq. (38), it is possible to make more accurate predictions of the allowable S of one fluid (e.g., LH_2) when test data are available with respect to S for another fluid (e.g. H_2O). As an example, the CTR between water at 60°F and LH_2 is 3320. Thus, if a suction specific speed of 10,000 is conservatively assumed as achievable with water, the extrapolated value for LH_2 is $10{,}000 \times (3320)^{3/4} = 4{,}300{,}000$. This high value indicates that operation of LH_2 pumps at or near zero NPSH conditions may be feasible. If this is the case, an ullage-gas-driven turbopump with surface-tension-screen fluid orientation may prove to be an attractive orbital refueling concept.

Figure-of-Merit for Concept Selection

If an identical set of mission requirements and the related guidelines and constraints are imposed on each transfer system concept, and if preliminary reliability analyses are made for each concept, it is possible to identify two figures-of-merit for each. For the transfer system itself, the figure of merit is

$$\Phi_{TS} = \lambda R_{TS}/\beta \tag{39}$$

where R_{TS} is the over-all reliability of the transfer system and where

$$\lambda = \text{mass fraction } W_{TP}/(W_{TP} + W_I) \tag{40}$$

where W_{TP} = weight of weight of transferred propellants (lb), and W_I = total inert weight of transfer concept subsystems (lb), and

$$\begin{aligned}\beta &= \text{transfer system cost effectiveness}\\ &= (C'_{\text{RDT\&E}} + C'_{\text{FA}} + C'_{\text{OPER}})/W_{TP}\end{aligned} \tag{41}$$

where $C'_{\text{RDT\&E}}$ = total transfer system development cost (dollars), C'_{FA} = total cost of a production transfer system (only subsystems directly used in transferring propellants) (dollars), and C'_{OPER} = total operational cost pertaining to transfer system only (dollars).

For the over-all tanker system, the figure of merit is

$$\Phi_{TV} = \delta R_{TV}/\alpha \tag{42}$$

where R_{TV} is the over-all reliability of the tanker vehicle, and

$$\begin{aligned}\delta &= \text{payload fraction}\\ &= W_{TP}/(W_{TP} + W_I + W_{TV} + W_{TVP})\end{aligned} \tag{43}$$

where W_{TV} = total inert weight of *other* tanker vehicle subsystems (lb), and W_{TVP} = total tanker vehicle propulsion system propellant weight, and where

$$\alpha = \text{tanker vehicle cost effectiveness} = (C_{RDT\&E} + C_{FA} + C_{OPER})/W_{TP} \quad (44)$$

where $C_{RDT\&E}$ = total over-all tanker vehicle development cost (dollars), C_{FA} = total cost of a production tanker vehicle system (including transfer system) (dollars), and C_{OPER} = total operational cost to place tanker vehicle system in earth orbit (dollars).

Finally, practical considerations such as safety, maintainability, availability, and development risk must be taken into account. These factors combined with the figures-of-merit Φ_{TS} and Φ_{TV} can be used as a basis for orbital refueling transfer system selection.

Concluding Remarks

The various orbital refueling techniques discussed and the critical phenomenon associated with them are amenable to theoretical evaluation. However, before an adequate level of technology readiness can be achieved, methods must be devised for experimental verification of these systems both on earth and in earth orbit. Experiment packages should be devised to assess the validity of the assumptions made in the analyses and to bridge the gap between theory and reality. Trade-off analyses should be used to the extent possible, of course, to select the system(s) most worthy of in-orbit testing.

Three major control parameters have strong influences on the selection of the transfer mode; the quantity of propellant to be transferred, the time allotted for the transfer, and the properties of the propellant. Selection of specific techniques also depends, of course, on reliability, weight, cost, safety, development risk, and compatibility with existing Kennedy Space Center (KSC) checkout, launch, and communication and tracking operations. Also mission environmental factors, such as altitude (gravity gradient strength), hard vacuum, solar radiation, micrometeroids, solar flares, drag make-up and station keeping, and orbital debris must be considered. The impacts of these parameters must be evaluated, and various tradeoff analyses must be conducted, before the most effective system solution can be determined.

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Vacuum Startup of Reactors for Catalytic Decomposition of Hydrazine

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Experimental results characterizing the first-pulse startup of monopropellant hydrazine thrusters, using a spontaneous catalyst (Shell 405), are presented and analyzed. Typical reactor dynamics are presented as approximations in simulating spacecraft control system dynamics. Possible reasons for first-pulse pressure spiking are explored as an approach toward controlling or alleviating catalyst attrition. Among the variables investigated are injector/catalyst configuration and initial temperature, vacuum exposure time and catalyst degassing, thrust level, and water immersion. Potential problems of propellant valve leakage and injector clogging are discussed. Several apparent trends are postulated from the test results, but additional study is needed for a clear understanding of the mechanisms involved.

Nomenclature

C_p = heat capacity of liquid
D = diameter
H_f = heat of fusion
H_v = heat of vaporization
m = molecular weight
P = pressure
R = universal gas constant
T = temperature
V = volume
$\dot{W}$ = weight flowrate
w = weight
X = the quantity of propellant entering the vapor phase = $(H_f + C_p\Delta T)/(H_v + H_f)$
θ = time

Subscripts

a = ambient
c = reactor chamber (T_c or P_c) or command (θ_c)
f = fluid
i = injector or ignition delay

Received January 28, 1970. This work was performed under Air Force Contract F04701-69-C-066. The author is grateful to D. J. Griep and F. W. Cox of the Electronics Division for their assistance in conducting the experimental program, and to H. Takimoto, Chemical Propulsion Lab., Aerospace Corp., for his help with the injector clogging problem.

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