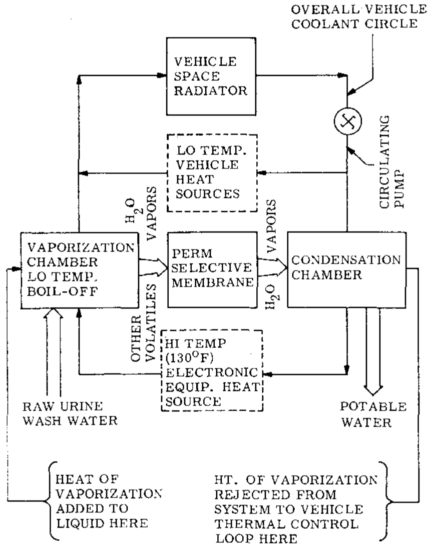

Fig. 4 Flow process: vehicle integrated membrane water recovery system.

vehicle heat injection means, most likely a space radiator. Figure 4 illustrates schematically the integration of a flight optimized system. It is estimated that the power penalty which should be assessed against such a system would total 10–20 w continuous plus control power. The pumps shown would already be required as a part of the vehicle thermal control system, and they would merely have to be increased in capacity to handle the additional pumping load.

As an alternate means, the incorporation of a compressor within the water recovery system was considered which would render the system independent of integrated heat sources or sinks from other systems within the vehicle. An electrical power requirement would exist for the compressor motor, relays, indicator lamps, etc. With this system, final condensation heat would be used directly to supply heat of vaporization to the still pot for initial boiloff. The pressure of the boiled-off vapor out of the vaporization pressure chamber of the system would be increased by the compressor to a sufficiently high level to insure a Δt of at least 10°F in the boiling-condensate heat-exchange relationship. This requires a compressor ΔP of at least 0.35 psi or a compression ratio of 1:1.5 minimum. At a temperature of 110°F, the vapor must be moved at the rate 350 ft^3/lb of water processed, or 290 ft^3/hr. A suitable compressor to perform this pumping rate would have a speed of approximately 1200 rpm, which results in a "piston displacement" of 6.6 $in.^3$ At a compressor efficiency of 0.8, a net "displacement" of at least 8.25 $in.^3$ would be required.

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Volatile Liquid Pressurization Control System

Donald F. Reeves*
The Marquardt Corporation, Van Nuys, Calif.

THE small bipropellant rocket engines being used for the control of aerospace vehicles require a storage and pressurization system that is light, compact, and compatible with high-energy propellants. A common technique employs high-pressure nitrogen or helium, which is separated from the propellants by Teflon or metallic bladders, but storage of these gases may require tank volumes and weights greater than those for the propellants, and gas-flow control devices are required. Several alternate approaches show potential weight and volume savings. For example, the use of a chemical reaction inside the main propellant tanks[1] may be quite advantageous for booster applications where high flow rates are required over a relatively short operational time. The system presented in this note is intended for use primarily in space vehicles having extended periods of on-off operation or for tactical missiles having lower flow rates but more stringent packaging requirements. It uses a volatile liquid, which is stored within the propellant tank and encloses an electrical heating element that maintains the required supply pressure by vaporizing the liquid (Fig. 1). The heat addition can be regulated by the supply pressure itself, the thrust command, or a combination of these. A comparison of pressurization system weight, which includes pressurant, pressurant tank, and propellant tanks, is presented in Fig. 2. The weight of the battery required for the volatile liquid system is excluded, as is the weight for lines, fittings, valves, and pressure regulators necessary to the nitrogen and helium systems. Even a fluorinated hydrocarbon of high molecular weight provides a weight saving over the N_2 system for chamber pressures up to 150 psia. The use of ammonia significantly reduces the weight; such potential weight and volume reductions warrant further investigation of this concept.

Governing Equations

The Laplace transfer function describing the ratio of propellant flow rate $\dot{w}_p(s)$ to the command signal $C(s)$ supplied by the guidance system is given by

$$\dot{w}_p(s)/C(s) = K_1/(s + K_2) \qquad (1)$$

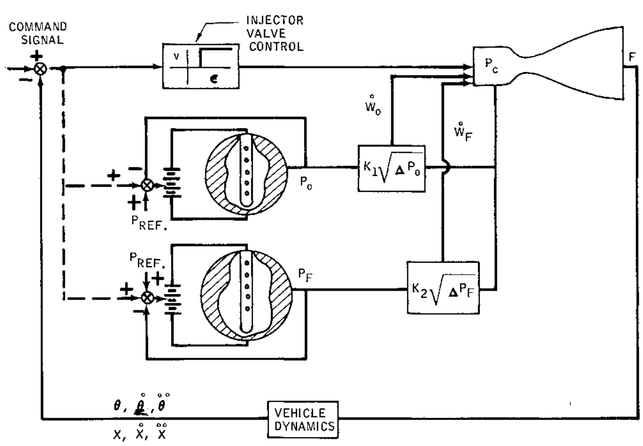

Fig. 1 System block schematic.

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* Member of the Advanced Technical Staff.

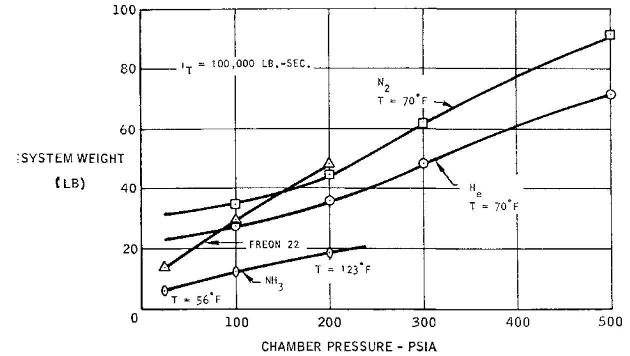

Fig. 2 Pressurization system weight vs chamber pressure.

where K_1 and K_2 are determined by the rocket engine valve and chamber dynamics. As propellant is expelled, the change in volume occupied by the volatile fluid increases, thus resulting in a decrease in pressure. If this process is one of constant specific enthalpy, the incremental change in pressure is given by

$$\Delta P_1(s) = K_3\, \dot{w}_p(s)/s \tag{2}$$

where K_3 is determined from the slope of a constant-enthalpy line in the liquid-vapor equilibrium phase.

The ΔP for constant-volume heat addition is

$$\Delta P_2(s) = K_4\, \dot{Q}(s)/s \tag{3}$$

where K_4 can be determined from the slope of a constant-volume line in the same liquid-vapor equilibrium phase. The heat flow rate $\dot{Q}$ is regulated by either ΔP_s or by $\Delta P_2(s)$ plus a direct signal from the guidance system

$$\dot{Q}(s)/\Delta P_s(s) = K_5/(s + K_6) \tag{4}$$

where $\Delta P_s(s) = \Delta P_1(s) + \Delta P_2(s)$, and

$$\dot{Q}(s)/\epsilon(s) = K_5/(s + K_6) \tag{5}$$

and where $\epsilon(s) = \Delta P_2(s) - K_7\, C(s)$, and K_5, K_6, and K_7 are determined by the heat of vaporization required as well as the allowable pressure errors. Combining (1), (2), and (4), the closed-loop transfer function corresponding to Fig. 3 is given by

$$\frac{\Delta P_s(s)}{C(s)} = \frac{K_1K_3(s + K_6)}{(s + K_2)(s^2 + K_6 s + K_4K_5)} \tag{6}$$

and the closed-loop transfer function corresponding to the alternate control system of Fig. 3 is given by

$$\frac{\Delta P_s(s)}{C(s)} = \frac{s(K_4K_5K_7 + K_1K_3) + (K_2K_4K_5K_7 + K_1K_3K_6)}{(s + K_2)(s^2 + K_6 s + K_4K_5)} \tag{7}$$

The relative merits of these systems are compared in the following section by determining the system gains and response times necessary to maintain a minimum pressure deviation.

System Application

For the reference system, the thrust was set at $F = 5$ lbf, and only the fuel tank was considered; a bipropellant reaction system of this size can operate from a command signal $C = 24$ v and have a time constant of 10 msec, i.e., $K_2 = 100\ \text{sec}^{-1}$. Thus, from Eq. (1)

$$K_1 = K_2\, \dot{w}_p/C = K_2\, F/(I_{sp})(1 + O/F)/C$$
$$= 0.031\ \text{lb/sec}^2\text{-v}$$

where O/F = oxidizer-fuel ratio = 1.6, I_{sp} = specific impulse = 250 sec, and $\dot{w}_p$ = fuel flow rate, lb/sec.

A 1022-in.^{3} tank will hold sufficient fuel and volatile liquid for 1 hr of run time under the following conditions: temperature, 62°F; pressure, 184 psia; densities of saturated liquid and vapor (Freon 13B-1), 0.054 and 0.00272 lb/in.^{3}, respectively; weight of fuel, 31 lb; weight of Freon 13B-1, 2.81 lb; and heat of vaporization, 35 Btu/lb. For this system,

$$Q = (2.81\ \text{lb})(35\ \text{Btu/lb}) = 98.5\ \text{Btu}$$

Based on the definition of enthalpy, $H = U + PV$, it can be shown that complete expulsion of the fuel with no heat addition would produce $P = 9.2$ psia and $T = -92$°F, and thus ΔP_1/lb fuel expelled is 5.65 psia/lb. Although this pressure change is not linear, a linear approximation is very close, and $K_3 = -5.65$ psia/sec-lb/sec in Eq. (2). Similarly, with these end conditions plus the heat of vaporization, constant-volume heat addition will give $K_4 = 1.78$ psia/sec-Btu/sec. The time constant for a heating element of this size was taken to be approximately 10 sec, i.e., $K_6 = 0.1\ \text{sec}^{-1}$. (This parameter can be regulated if required):

$$\dot{Q}_{\text{required}} = 0.0255\ \text{Btu/sec}$$

therefore, $K_5 = K_6/K_4 = 0.056$ Btu/sec^2-psia. On the basis of the system response to flow rates other than the reference level, K_5 may also be changed. Similarly, $K_7 = 0.5$ psia/v is based on the given flow rates but can be increased if the error criterion so requires.

Equations (6) and (7) can now be rewritten with constants included:

$$\frac{\Delta P_s(s)}{C(s)} = \frac{-0.175(s + 0.1)}{(s + 100)(s^2 + 0.1s + 0.1)} \tag{8}$$

$$\frac{\Delta P_s(s)}{C(s)} = \frac{-0.125(s - 40)}{(s + 100)(s^2 + 0.1s + 0.1)} \tag{9}$$

The servo portion of the system (Fig. 4) can be analyzed by considering ΔP_1 to be a ramp input which corresponds to the case where no direct control is included. The closed-loop transfer function is given by

$$\Delta P_2(s)/\Delta P_1(s) = 0.1/(s^2 + 0.1s + 0.1) \tag{10}$$

Since the response of ΔP_1 to a step input in $\dot{w}_p$ will be a negative ramp of the form $-A/s^2$, the velocity error is a direct measurement of the steady-state ΔP_s due to the increased flow rate. The forward loop transfer function is $KG(s) = 0.1/s(s + 0.1)$, and the velocity-error constant is $K_v = -\lim sKG(s) = -1$, $s \to 0$, where the negative sign occurs because of the negative ramp input. The steady-state velocity error is thus $\epsilon_v = 1/K_v = -1$ psia. Thus, the tank pressure will be kept within 1 psia of the design value.

The servo control can be further investigated in terms of center of gravity of its roots and the asymptotic lines for a root-locus plot:

$$\text{c.g.} = \Sigma\text{poles} - \Sigma\text{zeros}/(\#P - \#z) = 0.05 \tag{11}$$

$$\text{Asymptotes} = (2K + 1)\ 180°/(\#P - \#z) = 90°, 270° \tag{12}$$

$$\text{Gain}\ (K) = 1/|GH| = 0.1 \tag{13}$$

Regardless of gain increases, the system will remain stable since the set of root locations does not cross the j axis (asymp-

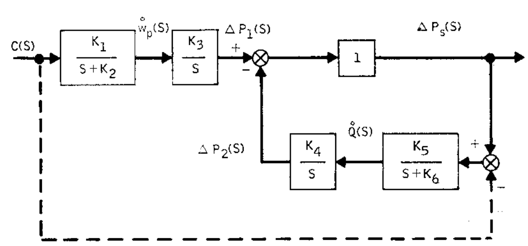

Fig. 3 Pressure feedback control system.

totes at 90° and 270°). If the gain were reduced to zero, the system would remain stable, but the steady-state error would be infinite, which is actually the case where no control is provided. A Bode diagram (Fig. 5) shows that the frequency response characteristics are poor because of the large time constant of the heating elements.

In order to compare the two control approaches that were proposed, the closed-loop transfer functions have been inversely transformed to the time domain. The tank pressure for the system presented in Fig. 4 where the input is a step of 24 v is of the form

$$\frac{\Delta P_s(t)}{A_1} = \frac{a_0}{\gamma(\alpha^2 + \beta^2)} + \frac{(\gamma - a_0)e^{-\gamma t}}{\gamma[(\alpha - \gamma)^2 + \beta^2]} + \left\{\frac{(a_0 - \alpha)^2 + \beta^2}{(\alpha^2 + \beta^2)[(\alpha - \gamma)^2 + \beta^2]}\right\}^{1/2} \cdot \frac{e^{-\alpha t}\sin(\beta t + \psi)}{\beta} \quad (14)$$

where $\psi = \tan^{-1}[\beta/(\alpha - \gamma) + \tan^{-1}(\beta/\alpha) + \tan^{-1}\beta/(a_0 - \alpha)]$, $A_1 = -4.2$, $a_0 = 0.1$, $\gamma = 100$, $\alpha = 0.05$, and $\beta = 0.312$. Substituting these values,

$$\Delta P_s(t) = -4.2[0.0001 - 0.0001e^{-100t} + 0.03e^{-0.05t}\sin 0.312t] \quad (15)$$

The theoretical steady-state pressure deviation is −0.0004 psia, and thus the control system provides extremely tight control. The size of the heater could undoubtedly be reduced in an actual system unless higher flow rates were anticipated.

The second control presented in Fig. 3 includes a direct lead term input to the heating element and is also of the form of Eq. (14) where the constants are $A_1 = 5.0$, $a_0 = -40$, $\gamma = 100$, $\alpha = 0.05$, and $\beta = 0.312$. Evaluating the pressure response for the same system,

$$\Delta P_s(t) = 5[4 - 0.00014e^{-100t} - 4.06e^{0.05t}\sin(0.312t + 1.5)] \quad (16)$$

The steady-state pressure deviation would be 20 psia and thus the system is over-compensated. In order to apply this concept to this particular system, the gain K_7 of the lead term would have to be decreased and again the size of the heating element could be reduced.

Concluding Remarks

This volatile-liquid pressurization system can reduce system weight and required packaging space and eliminate gas flow control elements. Its disadvantages include the need for more electrical power, an electronic control package, and more complex propellant tank construction. Specific conclusions are as follows. 1) The simple pressure feedback control provides sufficient response and accuracy for low-thrust systems. 2) The direct heat control technique is required only for high-thrust or low-total-impulse systems where instantaneous pressure drops might occur; its need also depends upon the size of the tank or total impulse as well as the propellant flow rate. 3) A more detailed dynamic analysis is required in which the effects of heat transfer are considered as well as the nonlinear characteristics of the system; for zero-g applications, this analysis will be very difficult, hence a full-scale simulation of this system is required. 4) This technical approach can also be used in the study of volatile fluid transient behavior during storage in and expulsion from

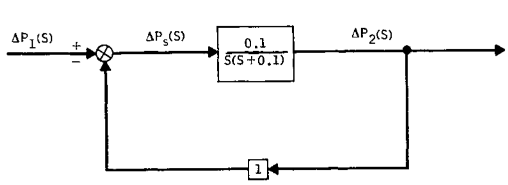

Fig. 4 Pressure control servosystem.

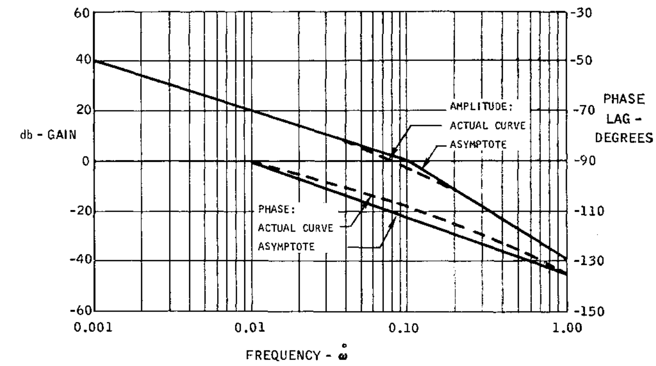

Fig. 5 Bode diagram.

fixed or variable volume tanks, e.g., in a monopropellant propulsion system.

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Parachute-and-Retrorocket Landing System for Vertical Descent

Kenneth E. French*
Lockheed Missiles and Space Company, Sunnyvale, Calif.

Nomenclature

c = rocket effective exhaust velocity, fps
C_D = parachute drag coefficient, dimensionless
D = parachute diameter, ft
g = acceleration of gravity, ft/sec²
k_j = const, dimensions as appropriate (j = 1–7)
m_b = mass of rocket propellant, slug
m_i = mass of rocket subsystem inert components, slug
m_0 = mass of payload, slug
m_p = mass of parachute, slug
m_r = mass of complete parachute subsystem, slug
M = total landing system mass, slug
t_b = rocket burning time, sec
T = rocket thrust, lb
v = specified touchdown velocity, fps
v_p = descent velocity on parachute, fps
π = numerical constant (= 3.141 . . .)
ρ = atmosphere mass density, slug/ft³

Introduction

A PROBLEM frequently encountered in recovery system design is that of providing a soft landing for a payload of known mass and strength. The landing systems used employ many basic types of deceleration devices; e.g., parachutes, retrorockets, crushable material, inflatable bags, and nose spikes. This paper considers a landing system that uses a parachute and a retrorocket in sequence, with particular attention to determination of a minimum weight system. Only vertical descent is considered. The vehicle is brought to an intermediate, stabilized rate of descent on the parachute, which is then cast free, and the retrorocket is ignited. Touchdown occurs at a specified velocity at the end of rocket burning. The parachute subsystem consists of the parachute assembly (canopy, lines and links, riser, pack), the

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* Research Specialist. Member AIAA.