Vehicle-Scale Investigation of a Fluorine Jet-Pump Liquid Hydrogen Tank Pressurization System

E. C. CADY* AND D. W. KENDLE*
McDonnell Douglas Astronautics Company, Huntington Beach, Calif.

A comprehensive analytical and experimental program was performed to evaluate the performance of a unique, fluorine-hydrogen jet-pump injector for main tank injection (MTI) pressurization of an LH_2 tank. The injector performance during pressurization and LH_2 expulsion was determined by a series of seven tests of a full-scale injector and MTI pressure control system in a 1000 ft^3 flight-weight LH_2 tank. Although the injector did not jet-pump LH_2 continuously, it showed improved pressurization performance when compared to straight-pipe injectors tested previously. The MTI computer code was modified to allow jet-pump injector performance prediction.

Nomenclature

A = injector exit area
f_m = ullage mixing fraction
$\dot{m}$ = mass flow rate
O/F = oxidizer/fuel
P = pressure
$\dot{q}$ = heat-transfer rate
R = gas constant
T = temperature
U = velocity
W = molecular weight
X = distance on the vertical axis
ρ = density

Subscripts

F_2 = fluorine
g = gas
H_2 = hydrogen
if = interface
in = injector
J = injectant jet flow
L = liquid

I. Introduction

On space vehicles using cryogenic propellants, tank pressurization can contribute significantly to the weight, complexity, and cost of the propulsion feed system. This is particularly true for those vehicles that require multiburn operation. The tank pressurization concept known as the main tank injection (MTI) technique can reduce the weight and complexity of the system. The MTI technique calls for injection of a hypergolic reactant into a propellant tank, and the heat released is used to pressurize the tank. This technique has resulted in considerable improvement in performance and lower cost, especially for advanced hydrogen-fueled upper stages.

From July 1966–April 1968, McDonnell Douglas Astronautics Co. (MDAC) conducted an MTI pressurization research program. The tasks were to determine, analytically and experimentally, the feasibility, limitations, and operating characteristics of small-scale fluorine-hydrogen MTI pressurization.[1] After the system feasibility and characteristics had been established, MDAC conducted a comprehensive program to devise an analytical method to predict MTI performance for liquid-hydrogen-fueled space vehicles of any size and to develop and demonstrate a full-scale, flight-type MTI pressurization system.[2]

In this program, conducted from July 1969–June 1970, a computer code was developed to predict the performance and behavior of MTI pressurization of a liquid hydrogen (LH_2) tank through ullage injection of gaseous fluorine (GH_2). Then, a large-scale test apparatus, including MTI injectors, tank pressure control system, and instrumentation, was designed for installation in a 1000-ft^3, flight-weight Thor tank. The MTI injection system was test-fired to insure proper durability and performance and then installed in the 1000-ft^3 LH_2 tank. A series of 17 tests was performed at ullage volumes of from 5% to 90%, LH_2 outflow rates from 5 to 15 lb/ sec, and tank pressures of 25 and 43 psia, utilizing both straight-pipe and diffuser-type injectors. Tank pre-pressurization, constant pressure hold at no outflow, and constant pressure expulsion modes were demonstrated with tank pressure maintained constant to within 1 psia. The data from these tests were analyzed and correlated with the MTI computer code, and empirical factors in the code were determined.

From the MTI test data, it was found that when tank pressurization and expulsion were performed with a nearly full tank (~5% ullage), the GF_2 jet penetrated the LH_2 interface and evaporated sufficient LH_2 to keep the ullage temperature low. As the interface receded during outflow, the LH_2 evaporation ceased and the ullage temperatures rose rapidly. The higher ullage temperatures led to greater tank wall heat-transfer losses, thus to increased GF_2 demand and consumption. It appeared that if controlled ullage mass addition were to occur throughout the expulsion, lower temperatures and reduced fluorine usage could be realized.

Concurrently with this work by MDAC, North American Rockwell-Rocketdyne (NAR) was conducting a program[3] to develop an MTI injector that uses the GF_2 inflow to jet-pump LH_2 from the tank, vaporizes the LH_2, and delivers a controlled flow of hot H_2 into the ullage, regardless of interface location. The NAR injector was hot-fired for short durations and performed well enough to justify pressurization testing in the same 1000-ft^3 tank used for the MDAC tests.

This paper describes the program in which the same test apparatus was used to evaluate the performance of the advanced MTI injector developed by NAR. The tank pressurization performance of the injector was compared with the straight-pipe injector performance and the MTI computer code was modified to allow injector performance prediction.

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* Senior Engineer/Scientist. Member AIAA.

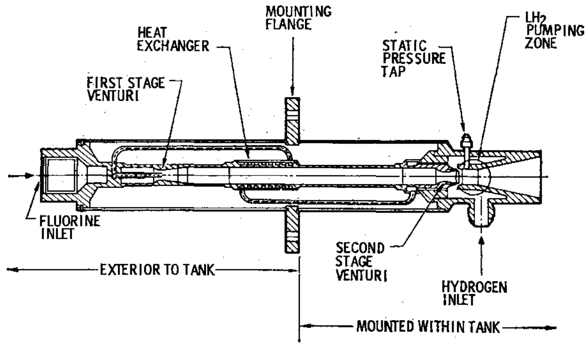

Fig. 1 NAR injector configuration.

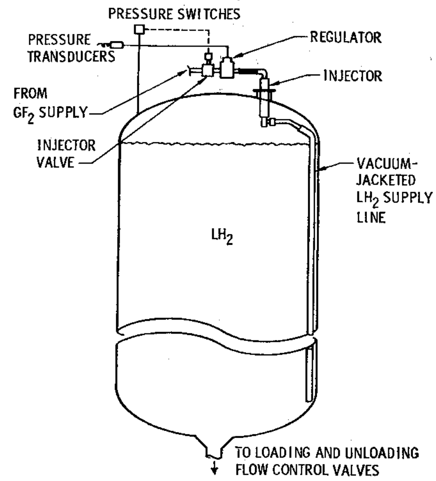

Fig. 3 Test tank injector installation.

II. Injector and Pressurization System Design

The over-all injector configuration is shown in Fig. 1. The injector operates nominally as follows: GF_2 enters the first-stage venturi, a converging-diverging nozzle. The low static GF_2 pressure in the venturi throat pulls H_2 from the vicinity of the second-stage venturi, vaporizes and heats the H_2 in a heat exchanger, and pumps it through the center tube of the first-stage venturi where it combusts with the GF_2 at an oxidizer/fuel (O/F) ratio of about 800 : 1. With ambient-temperature GF_2 inlet conditions, this O/F ratio results in a GF_2 temperature of about 1000° R entering the second stage. This hot GF_2 is again expanded through a converging-diverging nozzle in the second-stage venturi to a Mach number of 1.6, which results in a static GF_2 pressure at the second stage exit of about 16% of the GF_2 inlet pressure. This low static pressure provides a pressure differential across the annulus at the second stage for pumping LH_2 from the tank into the injector. The pumped LH_2 combines with the GF_2 in a downstream combustion zone at a design O/F ratio of 1.8 : 1. The resultant hot hydrogen at 2800° R is used to pressurize the LH_2 tank.

A new injector was designed by using scale factors of about 2 : 1 to create a higher flow-rate version of the original NAR injector. The design fluorine rate was 0.07 lb/sec at 530° R with an inlet pressure of 90 psia. Flow rate is controlled by the first-stage sonic venturi of the injector. The injector detail parts exposed to GF_2 were fabricated from nickel 200 material (which has excellent resistance to hot GF_2) using electric discharge machining for the critical dimensions. The detail parts were welded together; all seams exposed to fluorine were welded by the electron-beam process. The small tubes for hydrogen circulation were attached with a fluorine-compatible braze alloy (gold-nickel-palladium alloy, Palniro No. 7). The completed injector is shown in Fig. 2.

The MTI pressure control and GF_2 supply system was modified to accommodate the NAR injector. The GF_2 supply system was modified by enlarging the injector valve and using a proportional dome-type pressure regulator to control the GF_2 inlet pressure to 2.25 times the sensed tank pressure, so that the proper LH_2 pumping ratio in the injector is maintained. A vacuum-jacketed line was used to supply LH_2 to the injector from the tank bottom. The general configuration of GF_2 supply system, injector, and tank is shown in Fig. 3. The flight-weight LH_2 test tank, instrumentation system, and facility details have been described[2] and were essentially identical for this program.

III. Experimental Results

Prior to its installation in the 1000-ft[3] tank, the injector was test fired in a checkout test fixture to determine structural adequacy under cyclic conditions simulating the tank test environment. The injector expansion cone downstream of the second-stage venturi was damaged by overheating during this test. The damage was attributed to a regulator anomaly (excessive GF_2 pressure overshoot) which led to reduced LH_2 pumping, high O/F ratios and temperatures, and injector melting.

The injector was repaired and installed in the same flight-weight, 1000-ft[3] LH_2 tank. The instrumentation system used for testing the previous MTI injectors was used again. Seven tests were performed at varied LH_2 outflow rates and ullage volumes, and prepressurization, constant pressure hold, and LH_2 expulsion at controlled tank pressures were demonstrated, as described in detail in the MDAC Final Report.[4]

The MTI pressure control system performed in a nominal manner, maintaining tank pressure constant to within ±1 psia, as shown in Fig. 4. The NAR injector performance

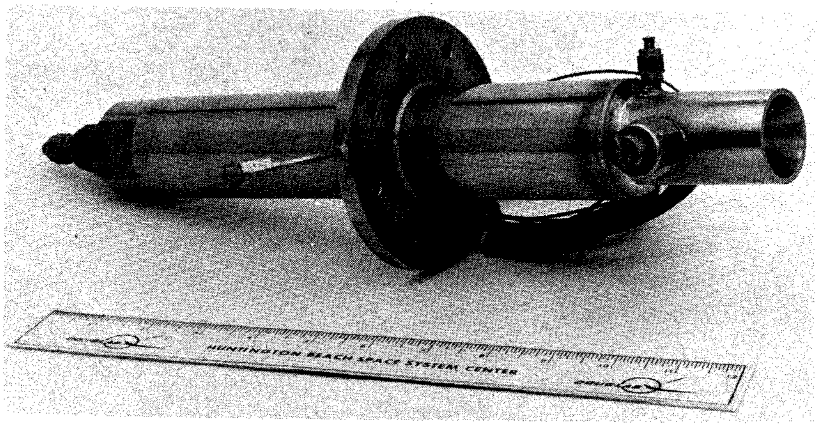

Fig. 2 Assembled NAR injector.

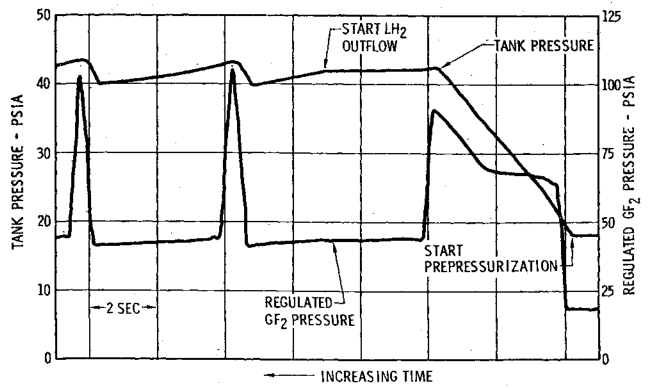

Fig. 4 MTI control system response—5% ullage.

was markedly superior to the performance of the straight-pipe injectors tested previously, in that greatly reduced ullage gas temperatures and GF_2 usage were observed. Test 3 from this program was compared with an essentially identical 50% initial ullage, straight-pipe test (No. 2) from the previous program.[2] The initial ullage gas temperature, that following prepressurization, and that following expulsion are compared for the two tests (Fig. 5). The GF_2 consumption for the two tests is compared in Fig. 6. Although superior performance was observed, it was found following the test that the injector expansion cone, downstream of the second-stage venturi, was again damaged by overheating. Analysis of the data indicated that the injector did pump LH_2 when cold (when the tank was full) during steady operation, but pumped only GH_2 when warm (i.e., during cyclic operation, or during steady-state operation at large initial ullages).

IV. Basic Analysis

The tank, propellant, and ullage are represented by a one-dimensional model. Variations in temperature and temperature-dependent properties occur only along the vertical

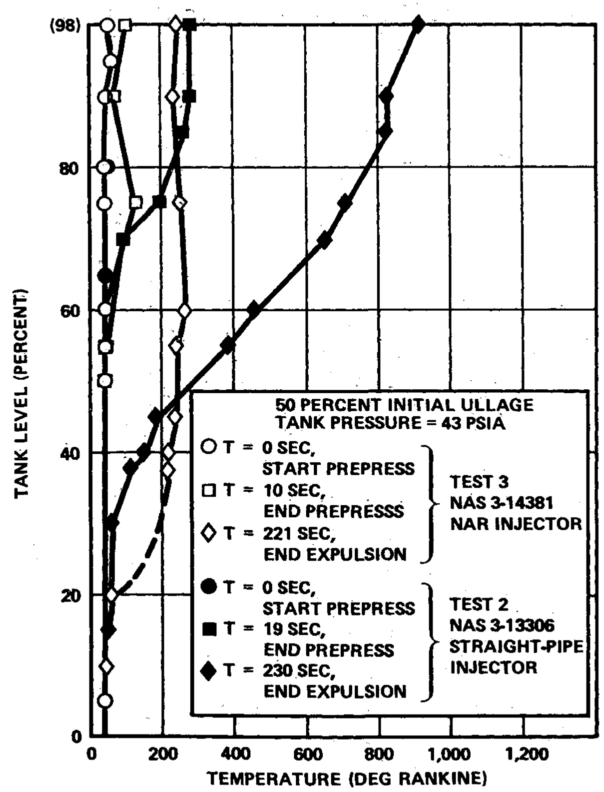

Fig. 5 Ullage gas temperature comparison—50% ullage.

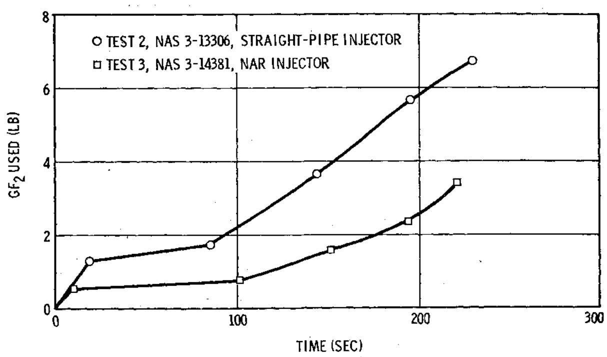

Fig. 6 Accumulated fluorine usage.

tank axis with no radial or circumferential variations. This model is the basis for several analyses of conventional heated-gas tank pressurization[5-7], and its validity is well established. Buoyant forces caused by gravity or vehicle acceleration tend to produce a stable thermal stratification in the gas and liquid, resulting in a temperature distribution that is essentially one-dimensional.

The computations are based on a finite-difference representation of the physical system. The tank wall, internal hardware, propellant, and ullage are each divided by horizontal planes into a number of nodes, as shown schematically in Fig. 7, with the properties within each node being uniform. The axial thickness and location of the gas and liquid nodes can vary during the solution, while the wall and hardware nodes are of equal thickness and fixed.

Ullage mixing is a key feature of the MTI analysis. The injectant inflow causes gas mixing in the region near the the injector, resulting in a nearly uniform temperature in the top part of the ullage. This mixed zone is represented by the large, single, upper gas node shown in Fig. 7. Nonuniformities exist directly in the injectant flow path, particularly with the MTI flame; however, in the vicinity of the wall heat-transfer surface, a nearly uniform temperature is maintained in the mixed zone.

The extent of the mixed ullage region is directly related to the depth of penetration into the ullage of the downward-flowing injectant jet. The velocity of this jet decreases with distance from its origin due to buoyancy because it is at higher temperature and lower density than the ullage and because of viscous mixing with the surroundings. These processes slow the jet to a zero axial velocity at some point, which is the jet penetration limit. An analysis for predicting this jet penetration depth was developed initially for nonreacting jets[8] and was extended to the reacting MTI case.

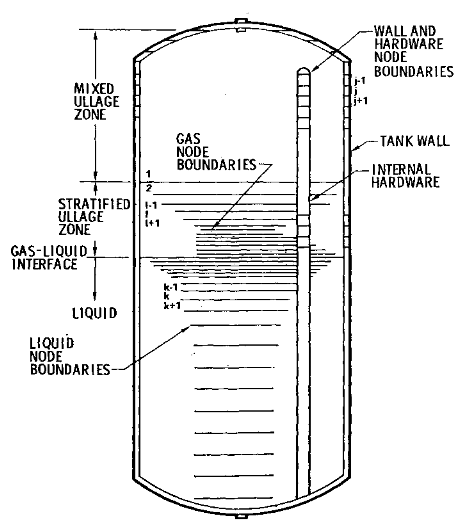

Fig. 7 Finite-difference node system.

The complete derivation of the MTI pressurization analysis has been previously presented.[2]

The jet penetration and ullage partial-mixing models are of primary importance to the over-all MTI pressurization analysis. All heat released by the flame goes into the mixed zone because this is the region directly affected by the injectant flow. Forced convection heat transfer to the tank wall occurs in this region due to the agitation caused by the gas mixing. The heat and mass transfer at the gas-liquid interface are determined by the injectant jet penetration because the dominant mode of interface heat-transfer results from direct impingement of the injectant flow upon the liquid surface. Other aspects of the analysis are similar to those of a conventional heated-gas pressurization system.

The MTI pressurization computer program[2] incorporating these features includes essentially all capabilities of existing pressurization programs: variable properties for the gas, liquid, wall, and internal hardware; unrestricted tank configuration; injectant supply system computations; operating parameters specified by either time-variable tabular inputs or internal calculations; and a number of different operating modes. In normal usage, the fluorine supply system and propellant outflow rates are specified, and the computer program calculates the temperature distributions in the wall, hardware, liquid, and gas, as well as the liquid vaporization rate and tank pressure, all of which vary with time during the solution. These data may be output from the program as frequently as desired.

V. Analysis of Experimental Results

Two of the most important parameters in the prediction of MTI injector performance are GF_2 usage and the temperature of the ullage gas (which directly affects both tank wall heating and GF_2 usage). These parameters are directly related to the degree of ullage mixing. With good ullage mixing, the ullage gas temperature is lower, heat transfer to the wall is lower, and GF_2 requirements are therefore minimized. The reverse is also true: poor mixing results in higher ullage temperature, higher heat transfer, and greater GF_2 usage. The effectiveness with which the injectant jet mixes the ullage is expressed analytically by the mixing fraction factor, f_m. If the ullage is completely mixed to the depth of the predicted injectant penetration, f_m equals 1.0. If f_m is less than 1.0, the injectant jet penetration itself is not affected, but the mixed depth is less, and thus the temperature in the mixed region is higher.

In the previous investigation[2], for those tests with large initial ullages (no interface mass transfer) and high tank pressures, where the GF_2 requirements demanded large injector on-time fractions, the ullage gas temperatures were correlated by assuming f_m to be 0.8. But for the low-pressure cases, where smaller GF_2 requirements were satisfied by small on-time fractions, temperatures were correlated by assuming f_m to be 0.9. The difference was possibly due to an ullage circulation flowfield which occurred with long on-time fractions and led to temperature stratification in the tank and reduced ullage mixing. The tests with the NAR injector were all characterized by very short injector on-time fractions and deep, uniform temperature profiles, so that the correlation was done with an assumed f_m of 0.9.

The processes occuring in the NAR injector are much more complex than in the simple straight pipe previously tested. The injectant gas flow at the injector exit is likely to have nonuniform distribution of temperature, composition, and velocity, and it is not obvious how the jet penetration process should be modeled. It was first assumed that the mising between the GF_2 stream and the aspirated GH_2 in the second-stage diffuser was no different than that occurring for GF_2 flow into an unconfined ullage. The jet-exit parameters were set for the conditions at the second-stage venturi exit. This high-velocity, small-diameter jet did not give adequate ullage penetration for any reasonable GF_2 inlet temperature. It was then assumed that the GF_2 flow from the second-stage venturi went through a normal shock to become subsonic and then filled the entire 1.3-in. diam injector width, with negligible GH_2 flow. The analysis again predicted insufficient ullage penetration depth and excessive ullage temperature even with f_m equal to 1.0. It seemed clear that the injector was pumping enough hydrogen to affect the behavior of the jet.

If the injector was assumed to be pumping GH_2 at a large O/F ratio (for example, 36), the weight of pumped H_2 was small, but the molar flow of the low-molecular-weight H_2 was nearly half that of the GF_2 and was enough to confine the GF_2 flow effectively, thus giving higher injectant velocity and penetration. The following equations indicate this effect:

$$U = \dot{m}_J / \rho_J A \tag{1}$$

Assuming perfect gas

$$\rho_J = \frac{P}{(R/W_J)T_J} \tag{2}$$

Substituting in Eq. (1)

$$U = \dot{m}_J R T_J / W_J P A \tag{3}$$

and

$$\frac{\dot{m}_J}{W_J} = \frac{\dot{m}_{F2}}{W_{F2}} + \frac{\dot{m}_{H2}}{W_{H2}} = \frac{\dot{m}_{F2}}{W_{F2}} + \frac{\dot{m}_{F2}}{(O/F)W_{H2}} \tag{4}$$

Substituting in Eq. (3)

$$U = \left(\frac{1}{W_{F2}} + \frac{1}{(O/F)W_{H2}}\right)\frac{\dot{m}_{F2} R T_J}{PA} \tag{5}$$

In addition, some of the GF_2 combusts inside the injector and thus contributes to increased injectant velocity by raising the mean effective jet temperature [T_J in Eq. (5)]. This does not mean that the flow is mixed and at an average temperature, but rather that the mean velocity of the jet corresponds to the mean effective temperature. For example, if 9% of the GF_F combusts with pumped GH_2 in the injector, then T_J would equal 2000° R, assuming the GF_2 from the first stage is at 600° R.

The O/F ratio and mean effective temperature determine the jet-exit velocity. The jet-centerline properties for the penetration depth analysis are determined by the unreacted core of the jet-pump flow; that is pure GF_2 at 600° R. The jet-pump outflow is presumed to be a central core of GF_2 surrounded by an outer sheath of pumped GH_2 which join in a mixing/reaction zone. Extremely high temperatures occur in the reaction zone, resulting in a higher mean temperature for the total flow although most of the GF_2 core will be unaffected. The GF_2 temperature is that resulting from combustion in the first stage of the injector. The design $O/F = 800$ with a temperature of 1000° R assumes LH_2 enters the heat exchanger in the first-stage feed line. Experimental data indicate that GH_2 entered the heat exchanger, which gives a much higher GH_2 inlet temperature to the first-stage combustor, higher O/F ratio, and lower combustion product temperature; therefore, the first-stage temperature was set at 600° R.

Based on the preceding assumptions ($O/F = 36$; $T_J =$ 2000° R), the ullage gas mixed-zone temperature history was predicted for the large ullage volume tests as shown typically in Fig. 8 for Test 3. The lines shown indicate the predicted mixed-zone depth and temperature (the stratified ullage below the mixed zone is not shown) compared to the experimental points. The agreement is excellent; however, for Test 2 (a warm initial ullage case with small on-time fractions),

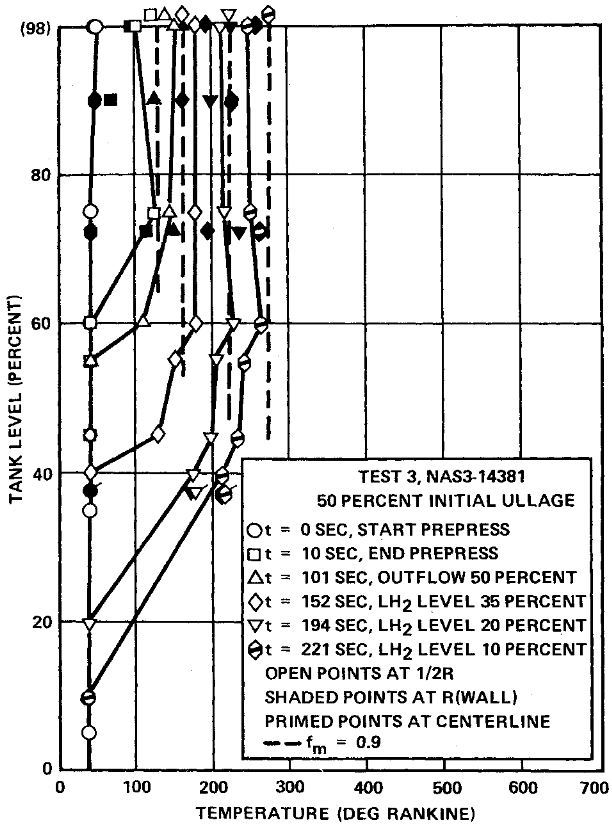

Fig. 8 Temperature correlation for Test 3.

f_m equal to 0.9 gave excessive ullage temperatures and GF_2 usage. The same situation was encountered in Test 13 from the previous program[2], also a warm initial ullage test with small on-time fraction, and the data from Test 13 were best correlated with f_m equal to 1.0. It was postulated at the time that the warm initial ullage resisted establishment of the ullage circulation field, but substantiation of this thesis was not possible. However, Test 2 from this program was also well correlated with f_m equal to 1.0, so that an ullage temperature effect does in fact seem to act in these cases.

Following the successful correlation of the large-ullage tests (which verify the jet-penetration and heat-transfer models), the small-ullage tests were analyzed. In order to predict ullage temperature history, GF_2 usage, and LH_2 evaporation, the LH_2 interface mass and heat-transfer processes must be modeled.

The correlation established in the previous investigation[2] for interface heat and mass transfer was expressed by

$$\dot{q}_g = 0.6\, X_L^2$$
$$\dot{q}_L = 0.2\, q_g \qquad (6)$$

for the gas-to-interface and interface-to-liquid heat-transfer rates, respectively. The difference $\dot{q}_g - \dot{q}_L$ is the heat input rate to liquid vaporization. These same equations were retained initially in the 5% ullage computations for the jet-pump injector tests, but gave much less vaporization than indicated by the mass balance. The initial liquid penetration depth, X_L, with the NAR injector was similar to that with the straight-pipe injector, although the injector-to-liquid distance was more than three times as great with the jet pump. It is apparent that the injector-to-liquid distance is an important parameter in determining the heat-transfer and evaporation rates. The total heat rate was assumed to be proportional to the area of the jet impingement (proportional to the square of the injector-to-liquid distance) as well as the intensity of the convective disturbance (proportional to the square of the liquid penetration depth). The resulting correlation of this form is

$$\dot{q}_g = 0.15(X_{if} - X_{in})^2 X_L^2 \qquad (7)$$

where the coefficient value of 0.15 was determined from the NAR injector test data. This correlation is effective for reasonably large injector-to-liquid distances. However, the predicted heat-transfer rate would approach zero as the injector exit location approaches the interface (as $[X_{if} - X_{in}]$ approaches zero). Since this is not physically valid, a minimum value of $(X_{if} - X_{in}) = 1.0$ ft is maintained in the computer code for this relationship. Of the total heat transferred from the gas, 20% is lost to liquid heating and 80% vaporizes liquid as in the previous correlation.

With this interface model, the ullage gas temperatures were predicted accurately for the small ullage tests as shown in Fig. 9 for Test 1. When compared to the 5% ullage tests from the previous program[2], the new correlation gave conservative temperature prediction at the previously assumed $f_m = 0.8$.

The correlation was not successful for Test 5, which was a low-pressure, low-LH_2-outflow-rate, long-duration (800 sec) test. In the previous program, Test 7 was the same kind of test and also was not correlated by Eq. (6). The reason advanced at that time was that the long duration of the test allowed external heat leak to cause a layer of saturated LH_2 to build up at the interface, so that all heat input went to evaporation. Further, it was postulated that the evaporation would not necessarily depend on X_L because of the very short on-time fraction and very transient LH_2 penetration. It was assumed that evaporation would only depend on the available energy in the ullage, or simply as a fraction of q_c. Thus

$$\dot{q}_g = 0.25\, q_c$$
$$\dot{q}_L = 0 \qquad (8)$$

was the assumed correlation, which, together with $f_m = 0.9$, gave good agreement with temperature history, GF_2 usage,

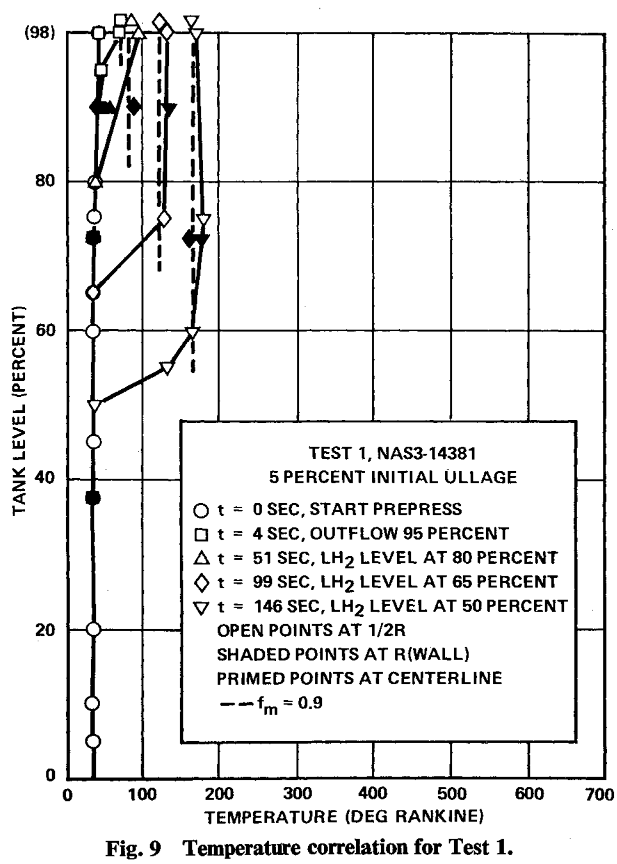

Fig. 9 Temperature correlation for Test 1.

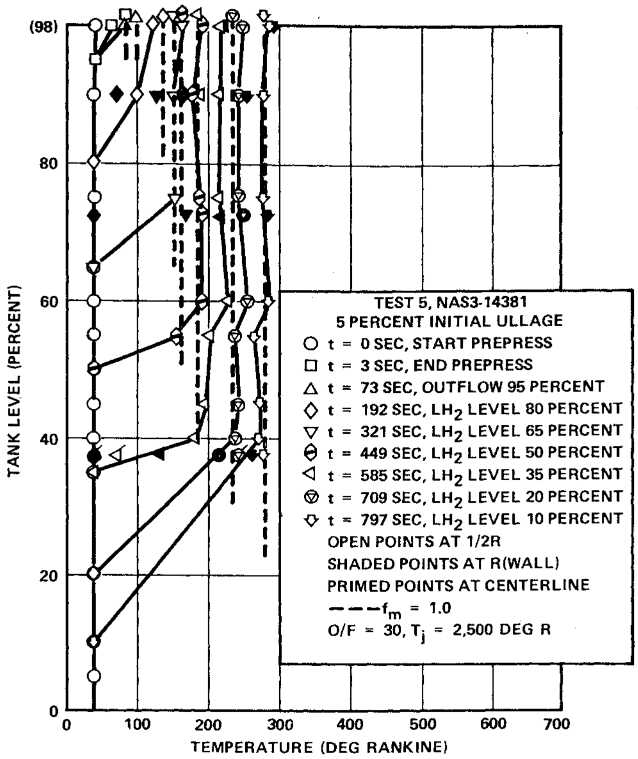

Fig. 10 Temperature correlation for Test 5.

and evaporation for Test 7, as described in Ref. 2. Because of the similarity of Test 5 from this program to the previous Test 7, the same correlation was used, together with $f_m = 1.0$ and the NAR injector model. This gave fair agreement, and conservatively predicted the temperature history and GF_2 usage. The correlation was improved by assuming an O/F ratio of 30 and T_J equal to 2500° R, which gave good final temperature correlation, as shown in Fig. 10.

In these tests, as in the previous ones, heat-transfer data taken at the tank wall locations in the mixed ullage region indicated heat-transfer rates in excess of those accounted for by free convection. This excess was assumed to be the forced convection contribution. Using an established equation for forced convection to a vertical flat plate[9], the gas velocity was determined, which gave the experimental coefficient value. This velocity at the wall was related to the injection velocity in a manner similar to that used previously[2] but slightly modified to improve computer modeling.

With the revised injectant penetration, heat-transfer, and interface models, the GF_2 usage was generally predicted within 20% for these tests.[4] The GF_2 usage from the previous straight pipe tests was also accurately correlated by the revised models.[4]

An ullage mass balance and ullage gas and tank wall enthalpy balance were computed for each test. The ullage mass was calculated from the measured pressure and local temperature conditions measured at the sensor locations; the temperature was assumed to vary linearly between the measured points. When conditions in the ullage changed slowly, the temperature sensors were able to respond adequately, and the mass balance gave reasonable results. However, when the ullage temperatures changed rapidly, as during large ullage prepressurization, the response lag of the platinum temperature sensors gave erroneous results for the mass balance. Under these conditions, the ullage was actually warmer than the sensors were recording, so that the mass computed from the "colder" temperatures was larger than the actual mass. This effect also occasionally occurred due to temperature extrapolation caused by mssing sensors, especially near the LH_2 interface. However, for the large ullage tests, the ullage mass stayed constant within 8% or less, which confirmed that ullage mass addition did not occur, as predicted by the analysis.

The mass balances for the small ullage cases gave generally good results because of slower changes in temperature. Again, the revised models also accurately predicted the ullage mass for the previous tests.[4]

As in the case for the previous tests, the enthalpy balances were rather imprecise, because of temperature sensor lag and the requirement for linear interpolation among a relatively few wall temperature sensors.

VI. Conclusions

The conclusions from this analytical and experimental program are as follows:

1) A unique jet-pump injector was designed and fabricated by North American Rockwell-Rocketdyne (NAR) and tested in an MDAC 1000-ft³, flight-weight LH_2 test-tank system. The MTI control and injection system was modified with the addition of a specially designed proportional GF_2 pressure regulator, enlarged injector valve, and vacuum-jacketed injector LH_2 feed line, and performed in a nominal fashion, controlling the tank pressure to within 1.0 psia under essentiall all conditions.

2) The NAR injector was able to jet-pump LH_2 only under steady-state conditions when completely chilled (with a full LH_2 tank). During large ullage tests and during all cyclic operations of the MTI system, the injector pumped only H_2 vapor, which resulted in damage to the LH_2 pumping annulus from overheating.

3) The first-stage preheating of the GF_2 injectant and H_2 vapor pumping resulted in increased jet penetration and ullage mixing for the NAR injector, which gave improved MTI pressurization performance (reduced ullage temperatures and GF_2 consumption) compared to that of the straight-pipe injector tested previously.[2] The MTI pressurization computer code was used successfully to correlate the data from the NAR injector tests with only minor modifications.

References

[1] Cady, E. C., "An Investigation of Fluorine-Hydrogen Main Tank Injection Pressurization," *Journal of Spacecraft and Rockets*, Vol. 6, No. 11, Nov. 1969, pp. 1248–1253.

[2] Cady, E. C. and Kendle, D. W., "Vehicle-Scale Investigation of a Fluorine-Hydrogen Main Tank Injection Pressurization System," *Journal of Spacecraft and Rockets*, Vol. 9, No. 3, March 1972, pp. 158–164.

[3] Barber, H. E., "Hydrogen Tank Pressurizing Gas Injector," CR-72998 (R-8713), Oct. 1971, NASA.

[4] Cady, E. C. and Kendle, D. W., "Vehicle-Scale Investigation of a Fluorine Jet-Pump Liquid Hydrogen Tank Pressurization System," CR-120947 (MDC G2763), Feb. 1972, NASA.

[5] Roudebush, W. H., "An Analysis of the Problem of Tank Pressurization During Outflow," TN D-2585, Jan. 1965, NASA.

[6] "FORTRAN Program for the Analysis of a Single-Propellant Tank Pressurization System," Rocketdyne Rept. S&ID IDWA 5835, June 15, 1964, North American Rockwell Corp., Canoga Park, Calif.

[7] Kendle, D. W., "A Tank Pressurization Computer Program for Research Applications," Rept. DAC 63076, Dec. 1968, McDonnell Douglas Astronautics Co., Huntington Beach, Calif.

[8] Kendle, D. W., "Ullage Mixing Effects on Cryogenic Tank Pressurization," Rept. DAC 63168, March 1968, McDonnell Douglas Astronautics Co., Huntington Beach, Calif.

[9] McAdams, W. H., *Heat Transmission*, McGraw-Hill, New York, 1954.