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Heat-Pipe Development for Advanced Energy Transport Concepts Final Report Covering the Period

January 1999 through September 2001

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Heat-Pipe Development for Advanced Energy Transport Concepts

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ABSTRACT

This report summarizes work in the Heat-pipe Technology Development for the Advanced Energy Transport Concepts program for the period January 1999 through September 2001. A gas-loaded molybdenum-sodium heat pipe was built to demonstrate the active pressure-control principle applied to a refractory metal heat pipe. Other work during the period included the development of processing procedures for and fabrication and testing of three types of sodium heat pipes using Haynes 230, MA 754, and MA 956 wall materials to assess the compatibility of these materials with sodium. Also during this period, tests were executed to measure the response of a sodium heat pipe to the penetration of water.

1.0 Sodium Heat-Pipe Safety Tests

The Phase II progress report described tests of water injection into an ambient-temperature sodium-filled heat pipe. Separate tests were performed on heat pipes with different penetration diameters, 0.25 mm and 0.58 mm. In these tests, water was introduced into the heat pipe at 500 kPa absolute pressure. An additional test was conducted with a 4.8-mm-diameter penetration at 77 kPa atmospheric pressure. At no time during any of these tests did the heat-pipe wall temperature rise by more than 50°C. In each test, similar amounts of heat were released—on the order of 10^3 J, ~2% of the total possible heat of reaction from the available sodium inventory. To quantify the effect of penetration size and higher injection pressure on reaction extent, a series of tests was conducted. Two different penetration diameters were selected (0.58 mm and 2.56 mm) between the extremes of the previous tests. Water was injected into the pipe at ~2 MPa. A picture of the apparatus used in these tests is shown in Figure 1. Two heat pipes were made: each is 30 cm long and built of 3/8-inch, Schedule 40, Haynes 230 pipe. Three layers of 100-mesh stainless-steel screen lined the inside of each heat pipe. Each pipe was charged by vacuum distillation with 5.5 g of sodium that, with a saturated wick, left about a gram of free sodium in the vapor space. A complete sodium-water reaction would liberate 112 kJ, sufficient to increase the heat pipe's temperature to 1070 K. The hydrogen pressure generated by a sodium-water reaction that progressed near completion would be more than adequate to breach the wall of the heat pipe.

Figure 1. Sodium heat-pipe apparatus, shown during test at the Minie remote explosives firing area; at this point, the solenoid valve was open, and the sodium-water reaction had already gone to completion.

For each test, a charge volume held 43 ml of distilled water, sufficient to completely fill the heat pipe. Between the heat pipe and the water charge volume was a solenoid valve that isolated the pressurized water from the heat pipe before test and an orifice, which was used to control the size of the simulated leak. Five chromal-alumal thermocouples were attached by spot welding along the length of the pipe. Piezoresistive transducers read the pressure inside the heat pipe, charge volume, and upper reservoir. A thermocouple was used to measure the temperature in the

water bath surrounding the heat pipe. Thermocouple and transducer measurements, taken at 2-s intervals, were recorded by a LabVIEW data acquisition system. Distilled water was introduced into the upper reservoir. The system was then sealed and pressurized. The trigger was activated, and the solenoid valve was opened. The firing area was remotely viewed by video camera, and the response of the data system was monitored. The solenoid valve was kept open until heat-pipe temperatures returned to ambient conditions. Figure 2 shows the pressure measured at the upper reservoir and in the heat pipe for the first 10 minutes after the solenoid valve was opened. Water began flowing into the pipe within seconds of the valve being opened. Gas pressure from the upper reservoir pushed water through the orifice and into the pipe. As water entered the heat pipe, a hydrogen bubble developed from the reaction. The hydrogen bubble raised the total system pressure and limited both the reaction extent and the entry of water into the heat pipe.

Figure 3 shows the temperature response of the heat-pipe wall to the sodium-water reaction. The thermocouple readings moved sharply upward during the first 10 s after water was introduced. A peak temperature of 375 K was reached at the thermocouples ~ 1.5 cm from the end opposite the fill-stem, 10 s into the reaction. Over the next minute, the pipe cooled to 310 K and approached ambient temperature after 2 minutes. Based on wall temperature measurements, ~ 10 kJ of heat were liberated by the reaction.

Figure 2. Pressure versus time measured during test with water injected through a 0.58-mm-diameter orifice (left) and a 2.56-mm-diameter orifice (right).

Figure 3. Temperature response versus time measured for water injection through 0.58-mm (left) and 2.5-mm (right) orifices, demonstrating the insensitivity of the sodium-water reaction to orifice diameter.

Test results were compared with previous results and with predictions from the existing heat-pipe safety-test model. The test results established that the reaction-limiting mechanism applies to high-pressure water injection through a larger penetration. It also established that the reaction extent was weakly dependent on penetration diameter at low driving pressure and was completely insensitive to penetration diameter at higher pressure. The data and model results were used to develop a design equation useful for system safety assessments.

Correlation of Safety Test Results

An equation, suitable for design purposes, was derived to quantify the relation between the injection pressure and energy release by the heat pipe from the sodium-water reaction. This derivation assumes that the hydrogen gas bubble, once formed, remains stationary in the heat pipe and is not expelled. Also, the sodium is assumed distributed along the heat pipe length. The heat energy released by the heat pipe from the sodium-water reaction is

$$Q = \frac{PV}{RT_F} \Delta H, \quad (1)$$

where P is the injection pressure of water into the heat pipe, V is the heat-pipe internal volume, R is the universal gas constant, T_F is the average heat-pipe temperature at the conclusion of the reaction, and ΔH is the sodium heat of reaction. Assuming that the heat pipe is initially at the temperature of its surroundings (non-operating), convection is the predominant heat-transfer mechanism; and using a lumped-parameter treatment, the average heat-pipe temperature as a function of injection pressure at the conclusion of the reaction can be estimated with

$$\frac{R(mc_p + hA\tau/2)}{PV\Delta H} (T_F^2 - T_i T_F) = 1, \quad (2)$$

where m is the total heat pipe mass; c_p is the specific heat of the heat pipe container; h is the heat-transfer coefficient at the heat-pipe surface {Air: $O(10 \text{ W/m}^2\text{K})$, Water: $O(100 \text{ W/m}^2\text{K})$ }; A is the pipe's heat-transfer surface area; τ is the thermal time constant for the heat pipe {surrounded by air: $O(100 \text{ s})$, surrounded by water: $O(10 \text{ s})$; and T_i is the initial heat-pipe temperature.

Figure 4 compares the temperatures measured on the surface of the heat pipe during the five tests conducted during this program with predictions made using Equation (2). The lower value represents the average heat-pipe temperature at the reaction peak; the upper value represents the peak temperature. In Equation (2) the predicted value falls in the temperature range between these values for four out of the five data points. The predicted value falls outside the temperature range at the point furthest to the left (4.8-mm penetration diameter and 77-kPa injection pressure). The poor agreement for this point is believed to be due to the expulsion of hydrogen from the heat pipe that was observed during the reaction. Hydrogen expulsion occurred because of the low injection pressure and violates the stationary-hydrogen-bubble assumption. The prediction passes through the remaining points and accounts for the injection-pressure trend.

Figure 4. Measured peak and average heat-pipe temperatures at the conclusion of the reaction (connected by bars) versus heat-pipe temperature predicted by Equation (2).

This correlation was applied to the case of a leak developing in a non-operating heat pipe surrounded by 10°C water. Figure 5 is a plot of average heat-pipe temperature after the reaction versus injection pressure. The plot indicates that an injection pressure of 40 MPa is required to fully react the ~5 g of sodium contained within the heat pipe. Equation (2) assumes there is a convective boundary condition at the surface of the heat pipe and that the initial temperature of the heat pipe is identical to the background temperature. This relation can be easily modified to include the superposition of a radiation boundary condition, as well as an initial temperature other than the ambient temperature (as would apply to a leak developing in an operating high-temperature heat pipe).

Figure 5. Heat-pipe temperature versus injection pressures that are predicted using Equation (2). Initial temperature assumed is 283 K.

2.0 Superalloy Heat-Pipe Development

Processing Procedures for Haynes 230-Sodium Heat Pipes

Figure 6 shows a metallographic section showing an electron-beam weld joining the Haynes 230 tube to the end cap. The received Haynes 230 materials were chemically cleaned. Each part was then fired at 1200 K and 5×10^{-6} torr for 1 hour and subsequently cooled within the furnace. The end caps were then electron-beam welded to the pipe. The heat pipe was then placed inside a quartz tube with a full-length induction coil. The interior of the heat pipe was evacuated to 5×10^{-6} torr; the outside of the heat pipe was exposed to air at atmospheric pressure. The full-length induction coil brought the heat pipe to 1325 K for 1 hour. The heat pipe was then distillation filled with sodium. The sodium charge was sufficient in all pipes to completely saturate the wick and form a 3-cm-long pool at the evaporator end at the operating temperature. Each pipe was then placed horizontally inside a quartz tube open to the atmosphere and heated inside a furnace to allow the working fluid to wet the wick at 1073 K for at least 24 hours. Before cooling, the heat pipe was tilted to allow any excess sodium to flow to the evaporator end of the heat pipe. The Haynes 230 processing steps are summarized in Table 1.

Processing Procedures for MA 754-Sodium Heat Pipe

The initial results of the electron-beam welding of the MA 754 were generally satisfactory, with the joint coalescing well. However, some voids in the deep penetration region of the joint were observed. The results of a subsequent trial at lower beam current are shown in Figure 7.

The void size in the deep penetration region was reduced at the lower current, and the joint was of sufficient quality to allow proceeding with MA 754 heat-pipe fabrication. Using experience gained from the Haynes 230 heat-pipe development and information obtained from Inco Alloys, heat-pipe processing procedures were established for the MA 754 heat pipe. These procedures are summarized in Table 2. These procedures were intended to (1) remove any stress from working with the material at low temperatures; (2) remove nonmetallic impurities from the interior surfaces of the heat-pipe material; and (3) form a protective oxide layer on the outer surface of the heat pipe.

Figure 6. Metallographic section shows electron-beam weld joining the Haynes 230 tube to end cap.

Table 1. Haynes 230 Processing Procedures

Step	Action
1	Wash piece in Freon TF or equivalent solvent until all signs of grease have been removed.
2	Immerse part over a 3-minute period in a 345 K solution consisting of 17% by volume sulfuric acid, 1% by volume hydrochloric acid.
3	Rinse part in deionized water.
4	Immerse part over a 25-minute period in a 345 K bath consisting of 8% by volume nitric acid, 4% by volume hydrofluoric acid; balance water.
5	Rinse part in deionized water.
6	Remove water from part with ethanol.
7	Vacuum fire part at 1200 K for 1 hour.
8	Place a stainless-steel screen that has been chemically cleaned and vacuum fired into the heat pipe container using standard Los Alamos processing procedures.
9	Fusion-weld end caps to heat pipe in vacuum.
10	Bring heat pipe to 1323 K for 1 hour, exposed to air on its exterior and 10^{-6} torr vacuum on inside.
11	Fill heat pipe with sodium by vacuum distillation.
12	Wet sodium into heat-pipe wick in quartz tube open to atmosphere at 1073 K for 48 hours. Hold the heat pipe in place with alumina rings and air cool.

Figure 7. Metallographic section shows electron-beam weld joining the MA 754 tube to end cap.

Table 2. MA 754 Processing Procedures (see Appendix C for cautionary note)

Step	Action
1	Wash piece in Freon TF or equivalent solvent until all signs of grease have been removed.
2	Rest part on MA 754 spacers inside a clam-shell furnace. Anneal part at 1273 K in air for 1 hour; cool by exposing the part to surroundings.
3	Remove oxide layer from part with Kolene DGS solution at 760 K for 10–15 minutes.
4	Quench and rinse part in deionized water.
5	Immerse part in sulfuric acid, 25% by weight; balance water at 355 K for 5 minutes.
6	Rinse part in deionized water.
7	Immerse part in nitric acid, 1% by weight; balance water at 345 K for 5 minutes.
8	Rinse part in deionized water.
9	Immerse part in bath containing 10 grams of iron per liter of nitric acid, 20% by weight, hydrofluoric acid, 2% by weight; balance water at 125 minutes, initially for 10 s. Repeat 10-s dips until shiny.
10	Rinse part in deionized water.
11	Rinse part in ethanol to remove water.
12	Place a stainless-steel screen that has been chemically cleaned and vacuum fired into the heat-pipe container using standard Los Alamos processing procedures.
13	Fusion weld end caps to heat pipe in vacuum.
14	Evacuate interior of heat pipe to 10^{-6} torr or better inside a clam-shell furnace. Bring the heat pipe to 1223 K for 2 h. This step will (A) partially anneal the material; (B) form a protective oxide layer on the outside of the pipe; and (C) clean the metallic surfaces on the inside of the heat pipe.
15	Fill heat pipe with sodium by vacuum distillation.
16	Wet sodium into heat-pipe wick in quartz tube open to atmosphere at 1073 K for 48 h. Hold the heat pipe in place with MA 754 rings and air cool.

Processing Procedures for MA 956-Sodium Heat Pipe

Before welding, each part was chemically cleaned and vacuum fired, using the procedure summarized in Table 3. Electron-beam welding of the MA 956 material at first met with mixed results using a 110 kV, 10-mA beam and a feed rate of 60 inches per minute. The beam was defocused 0.005 inches above the joint to reduce root cracking. The beam up-slope lasted 1 s, and the down-slope lasted 2 s. Initial joint cross sections appeared unsatisfactory with porosity and agglomeration of dispersoids around the heat-affected zone. A subsequent trial was conducted at lower current: 6 mA. During this later trial, the beam was moved in a circular manner to minimize porosity. The results of this trial are shown in Figure 8. Porosity still remained in the joint and also appeared in the heat-affected zone. The lines through the heat-affected zone and the joint appeared to be cracks. These apparent cracks are actually grain growth in the material. After practice on a trial piece, the heat-pipe fill stem was welded to one end cap, and the end caps were then welded to the gun-drilled tube body. Each weld was inspected under a microscope. No evidence of surface cracking was found. The assembly was then leak tested at room temperature to 10^{-9} torr-l/s.

MA 956 Chemical Clean

Inco Alloys previously provided Los Alamos with pickling steps for the MA 956 material. These steps were incorporated into a procedure consistent with established Los Alamos heat-pipe fabrication practice. A summary of the processing procedure for the MA-956 heat pipe is shown in Table 3. Step 5 calls for a 2- to 5-minute nitric acid-ferric chloride pickle. This is followed in step 7 by a 5- to 10-minute hydrochloric acid-nitric acid bath. These two steps failed to completely strip the surface film present on the as-received material. Increasing the pickle and bath times to 15 minutes each more completely stripped the surface film from the parts. These results prompted us to increase the pickle and bath times in steps 5 and 7 to 15 minutes.

Figure 8. Metallographic section shows an electron-beam weld joining the MA 956 tube to the end cap.

Table 3. MA 956 Processing Procedures

Step	Action
1	Wash piece in Freon TF or equivalent solvent until all signs of grease have been removed.
2	Rest piece on alumina spacers inside a clam-shell furnace. Anneal piece at 1273 K in air for 1 hour; cool by opening clam-shell furnace to surroundings.
3	Remove oxide layer from piece with Kolene DGS solution at 761 K for 10-15 minutes.
4	Quench and rinse piece in deionized water.
5	Immerse piece in nitric acid, 7% by volume, iron chloride (FeCl_2), 2% by volume, balance water at 339 K for 5 minutes.
6	Rinse piece in deionized water.
7	Immerse piece in hydrochloric acid, 10% by volume, nitric acid, 1% by volume, balance water at 340 K for 5 minutes.
8	Rinse piece in deionized water.
9	Rinse piece in ethanol to remove water.
10	Place a stainless-steel screen that has been chemically cleaned and vacuum fired into the heat-pipe container using standard Los Alamos processing procedures. Place hafnium foil getters in regions near weld joints.
11	Fusion weld end caps to heat pipe in vacuum.
12	Place piece in furnace, air on exterior and evacuate interior to 10^{-6} torr. Bring the piece to 1323 K for 2 h. This step will clean the interior metallic surfaces of volatiles.
13	Fill heat pipe with sodium by vacuum distillation.
14	Wet sodium into heat pipe wick in quartz tube open to atmosphere at 1073 K for 48 h. Hold the heat pipe in place with alumina rings and air cool.

MA 956 Vacuum Fire

The anneal temperature was selected based on KAPL recommendations. Oxide is removed from the metal using pickling procedures recommended by Inco. Note that the pickling procedures are quite different for the two materials. Wrapping the MA 956 in hafnium foil is intended to minimize oxide formation on the metal surfaces during vacuum firing. Hafnium was selected because hafnium oxide has a greater $-\Delta G$ than yttrium oxide. Hafnium foils are placed near fusion weld joints for similar reasons. The processing steps used for the MA 956 heat pipe (which included the use of a hafnium foil getter in the vacuum-firing operation) resulted in a finish that can be described as dark, matte, uniform, and metallic. A series of six trials were conducted to qualitatively assess the effect of the hafnium getter wrap on the post-vacuum fire metal finish. Table 4 summarizes the results of these trials. Of the vacuum-fired parts, those fired

without the hafnium foil getter appeared brighter, based on a blind comparison. The effect of the processing furnace condition (the Centorr furnace is believed cleaner than the quartz-lined furnace) appeared of secondary importance for these trials. Based on these blind trials, no clear benefit was found from the inclusion of hafnium foil in processing step 10. The Centorr furnace is limited to parts no longer than 30 cm. The results from processing samples C and D were not significantly different. These MA 956 processing samples were shipped to Knolls for SEM/EDS analysis on the outer surface. Surface oxygen on the samples fired with chemically cleaned hafnium was about half that of those fired without the cleaned hafnium. However, it appears likely that any oxygen reduction was superficial. The sample fired with cleaned hafnium is hafnium-rich. Hafnium at the surface tends to mask oxide present before firing. Surface oxygen on chemically cleaned and fired MA 956 samples did not greatly differ from MA 956 that was only cleaned. Based on these results, the MA 956 tube and end closures were chemically cleaned but the pre-weld firing step was omitted. After assembly with the fired screen, the parts were electron-beam welded. The inside of the welded heat-pipe assembly was then vacuum fired. The MA 956 heat pipe was processed using procedures summarized in Table 3. The acid pickling (steps 5 and 7) removed significant material from the end cap and fill stem. After pickling, the parts fit poorly. New parts were cut, and the pickling time was again reduced to 5 minutes for each step. The interior of the MA 956 heat pipe was then vacuum fired at 1273 K for several hours. The pipe was then distillation filled with 12.6 g of NIAPURE sodium. The heat pipe was then brought to 1073 K over its entire length for 76 hours, inside a furnace open to atmosphere. This step permitted the sodium to wet the heat pipe's internal surfaces.

Table 4. Qualitative Vacuum Firing Trials MA 956

Sample	Procedure	Result	Rank
A	Fired in quartz-lined furnace with Hf foil as before	Dark uniform matte metallic finish	5
B	Fired in quartz-lined furnace with Hf foil that has been chemically cleaned and pre-fired	Light uneven metallic finish with yellow hued areas	4
C	Fired without Hf foil in metal-lined Centorr	Light uniform metallic finish	2
D	Fired without Hf foil inside quartz-lined furnace	Light uniform metallic finish, slightly darker than Sample C	3
E	Kept unfired for comparison	Bright uniform matte metallic finish	1
F	Kept unfired for comparison	Bright uniform matte metallic finish	1

3.0 Superalloy Heat-Pipe Demonstration

High-temperature, heat-pipe-material failure often results from the concentration of soluble impurities in the evaporator region. To accelerate the rate of impurity accumulation, the evaporation rates per unit area in these tests were scaled to levels 5–10 times that of the reference condition.

Initial Proof Tests of Haynes 230 Heat Pipes

After processing, each Haynes 230 heat pipe was then proof-tested with a 7.62-cm-long induction coil and a water-cooled condenser calorimeter. The proof test consisted of bringing the heat pipe from room temperature to 1173 K in approximately 2 hours. The heat pipe was then operated above 1173 K for several hours. Various heat-pipe orientations were tried. Life-test heat pipe #1 was successfully started in the vertical-assist orientation. Difficulties were experienced starting heat pipes #2 and #3 vertically and at 45-degree gravity assist. Steady-state operation at over 1173 K was obtained for pipes #2 and #3 when oriented with the condensers section 10 degrees above the horizon. A summary of the run-time history of the three heat pipes is given in Table 5. Heat pipe #1 operated vertically at temperatures of up to 1203 K, and a radial heat flux in the evaporator, up to 51 W/cm². During this run, no hot spots were observed in the evaporator, and the heat pipe was shut down in a normal fashion. A subsequent attempt to start heat pipe #1 in a 10-degree orientation resulted in a dry out during startup. Heat pipe #2 was started from the frozen state without difficulty and had operated stably at 1187 K and 42 W/cm² in a 10-degree orientation for 2.5 hours before an evaporator-region leak developed. The condenser temperature data gave no indication regarding whether the leak was initiated from a wall material failure or from an evaporator-region dry out.

Failure Mode Identification

Both wall material failure and evaporator-region dry out were considered as possible leak formation causes. A number of mechanisms were explored. An evaporator dry out in heat pipe #3 pointed toward de-wetting as the cause of the failure of heat pipe #2. The surface temperature of the evaporator during the dry out of heat pipe #3 did not exceed 1273 K. The dry-out condition was well controlled, lasting about 10 s before the rf power was shut off. The outer wall of heat pipe #3 was found to be intact after removal from the test stand. Heat pipe #3 was then placed in an air furnace at 1073 K for 38 hours to allow the working fluid to re-wet the wick. While in the furnace, a leak developed in the heat-pipe evaporator region near the dry-out location. This troublesome finding pointed to possible integrity problems in the gun-drilled wall material. The following point deserves emphasis: *Any stable heat-pipe wall-fluid combination should readily recover from a 100°C over-temperature condition.*

Boiling or de-wetting of the working fluid at the evaporator wall or wick surface could start the dry out in the evaporator region. Boiling is initiated by vapor-bubble nucleation at a site not filled with liquid between the heat-pipe wall and the wick. Alkali metals generally fill all potential nucleation sites, and superheating limits are seldom encountered with these fluids. The nucleation site size required to initiate boiling at these temperatures is over 13 microns, which is quite large for such sites. The boiling limit hypothesis was considered less likely, since boiling limits usually occur almost immediately, not after hours of stable operation. Sodium heat pipes previously built at Los Alamos with spiral-wrapped wicks of similar structure (Kempe 1975)

Table 5. History of Initial Haynes 230-Sodium Heat Pipes

Run	T_{max} (K)	Q_{max} (W)	Q'' (W/cm²)	Run Time >873 K (minutes)	Run Time (minutes)
Life Test Heat Pipe #1					
D9809150730	1073	800	27	90	174
D9809160730	1060	771	26	90	170
D9810010730	1203	1507	51	32	140
D9811200730	1025	810	27	79	159
<i>Total Hours #1</i>				4.9	10.7
Life Test Heat Pipe #2					
D9810050730	981	1186	34	20	185
D9811020730	984	1042	30	60	200
D9811030730	954	962	28	40	126
D9811230730	1187	1465	42	164	284
<i>Total Hours #2</i>				4.7	13.3
Life Test Heat Pipe #3					
D9811160730	1242	1559	45	55	144
D9811180730	1157	1113	32	328	428
D9811200730	964	1186	34	18	130
D9901120730	1202	1100	32	190	294
<i>Total Hours #3</i>				9.9	16.6

also operated at various inclinations, but at peak temperatures 70°C below these heat pipes. A plot of heat-pipe throughput versus evaporator exit temperature is shown in Figure 9 for heat pipe #3. The Kemme data at similar evaporator radial heat flux is also shown for the horizontally and vertically oriented cases. The solid and dashed lines on the right of Figure 9 indicate boiling limits calculated for 12.7-mm and 6.35-mm nucleation site radii, respectively.

Evaporator dry out can also be caused by local de-wetting of the working fluid from surface chemistry changes at the heat-pipe wall or in the wick. The initiation of dry out after several hours of stable operation may suggest a dry out from such a change. Leak development in heat pipe #3 while being rewetted at 1073 K pointed to possible material instability in the gun-drilled material. Several potential material-failure mechanisms have been explored. A mechanism favored by Dwaine Klarstrom at Haynes is stress-induced cracking. Klarstrom (1996) describes

Figure 9. Condenser heat-rejection rate versus evaporator exit temperature measured for startup and operation of heat pipe #3. The condenser is oriented 10 degrees above the horizon. This data is compared to sonic and boiling limits for two nucleation site sizes and data measured by Joe Kemme at Los Alamos during the 1970s.

stress-induced cracking in Haynes 230 alloy during heat treatment from residual stresses produced by cold work during fabrication. The conditions required for crack initiation are (1) residual tensile surface stresses, (2) a strain-intolerant microstructure in the alloy, and (3) the presence of stress raisers. Based on a description of the gun-drilling and honing operations used to form the Haynes 230 tubes, Dwaine suggests that the material may have been weakened during fabrication. Unfortunately, this type of cracking is difficult to detect since it occurs in an intergranular mode during heating. The remaining portion of the heat-treat cycle (in our case dry out and subsequent cooling to room temperature) usually eradicates most of the microstructural evidence. Stress can be produced in the absence of external constraint solely from the incompatibility of the natural expansions or contractions of different parts of a tube. Temperature stresses were calculated for the gun-drilled tube radially heated at 46 W/cm^2 at the inside diameter. Under this condition, the outside tube diameter is in compression, 9.5 ksi, while the inside diameter is in tension, 11.4 ksi. Haynes' literature cites creep data for solution anneal bar stock. A 13.4-ksi initial stress is sufficient to produce creep rupture of the bar material after 10 hours at 1200 K. Haynes personnel suggested that stress-induced cracking could be minimized by heating the material as quickly as possible to 1448 K. The material is then kept at 1448 K for about 10 minutes and then quenched to room temperature as quickly as possible. By induction heating a stainless-steel tube of similar size and then quenching it in helium gas, heating rates of 14°C/s and quench rates of 17°C/s were demonstrated. Liquid metal embrittlement can cause intergranular cracking and fracture in stressed parts of many metals, especially ferritic steels. Hilditch, et al. (1995) report that A508 III, 2Cr–1Mo steel is embrittled by sodium at 423 K–723 K. Liquid metal embrittlement requires effective wetting to establish a true interface between the solid and the liquid. If a specimen loaded in simple tension is embrittled by liquid metal, it will break transverse to the loading axis. The degree of embrittlement is greater for steels that have been heat treated or alloyed to produce high strength

and at higher temperature (in excess of 573 K). Liquid metal embrittlement appears an unlikely failure mechanism for the Haynes alloy. However, embrittlement is a potential stability concern for high-iron-content MA 956 alloy. A comparison of test duration for oxidation-resistant-alloy heat pipes filled with sodium is shown in Figure 10. Plotted on the abscissa are peak operating temperatures; the ordinate gives the test duration. Sodium-Haynes 230 systems at Sandia National Laboratories have generally operated between 1033 K and 1083 K. One test of 3- to 4-hour duration was conducted around 1173 K. Kanthal—an oxidation resistant alloy of similar composition to MA 956—operated without incident for over 100 hours at Los Alamos in 1981. The Sandia systems run typically up to 60 W/cm², but at much lower temperature, while the Kanthal system was operated at 11 W/cm². Some low-heat-flux Thermacore data exists, but the specifics have not been published.

On the heat-pipe dry-out mechanism, the data might be interpreted that one or more minute gaps (on the order of 15 microns thick) between the wick and wall acted as boiling nucleation sites. Most of the bubbles formed at the wall escape into the vapor space. A bubble becomes trapped near the surface and grows, drying out the evaporator. Heat pipes #2 and #3 with nearly identical wick structures dried out in a statistically suggestive manner: Heat pipe #2, operating at 42 W/cm², dried out after 164 minutes at 1187 K. Heat pipe #3, operating at 32 W/cm², dried out after 190 minutes at 1202 K.

To accelerate reaction mechanisms, these heat pipes were tested at radial heat fluxes of between 30 and 50 W/cm² in the evaporator. Since the reference condition is 5 W/cm², the materials-related problems experienced should be less important. However, orientation effects, whether from boiling or slugging, must be taken into account since they may influence reference heat-pipe performance. The experience gained from the Haynes alloy heat pipe may eventually be useful in design of a Haynes 230 panel heat pipe. Further work was performed on the heat

Figure 10. Test duration versus temperature for Haynes 230-sodium system (Merrigan data is for Kanthal—an iron-based oxidation-resistant alloy similar to MA 956).

pipe tests with emphasis placed on understanding the anomalous behavior observed in the Haynes 230-sodium system at temperatures above 1173 K. Table 6 summarizes the heat pipes tested, as well as several heat pipes—5, 6, and 7—that were eventually proof tested. Heat pipe #1 (schedule 40 Haynes 230-sodium) was operated for an additional 8 hours. A longer induction coil was used than in previous tests, one of sufficient length to produce 15 W/cm^2 evaporator radial heat flux at 1173 K. A hot spot developed under the induction coil during startup in a region that, during previous tests, had not been covered by an induction coil. The evaporator-region hot spot was first observed when the evaporator was at 800 K. This behavior suggests that the metal surface was not completely wetted by the sodium during startup. The wetting of solid metals by a liquid metal can be sensitive to small changes in the nonmetallic impurity content of both metals. Wetting normally occurs above a “critical wetting temperature,” where impurities are reduced by the liquid sodium to expose a pure solid-metal surface. The wetting behavior of liquid metals on complex alloys containing chromium, molybdenum, and tungsten can be quite different from the wetting behavior on pure metals. In the presence of impurities, these alloys can produce ternary compounds that are stable in sodium. Partial wetting has been observed between sodium and 316 stainless steel (Longson and Prescott 1973). This wetting behavior has been attributed to the formation of a ternary oxide film. In this case, the sodium rather than wetting the solid metal partially wets the ternary oxide film (Addison 1984).

Heat pipes #2 and #3 dried out suddenly after 4 hours of stable operation at around 1170 K. It appeared that boiling at the heated surface might have caused this dry out. However, boiling normally occurs soon after heat is applied. Instances of boiling onset long after stable operation has been established are absent from the literature. The nucleation-site size required for boiling onset is quite large. In addition, following dry out, the heat pipe #3 wall developed an external leak when placed inside a processing furnace. This indicates possible integrity problems in the gun-drilled wall material. Dry-out onset after ~ 4 hours of operation indicates, for both pipes #2 and #3, a rate-dependent mechanism. Calculations from the thermochemical model indicate that impurities carried by the recirculating working fluid move toward the evaporator region in about this time scale. The accumulation of nonmetallic impurities in the evaporator zone may be sufficient after 4 hours to form a thin nonmetallic layer that is not broken down by the sodium. Candidate compounds for this nonmetallic layer include Cr_xC_y , NaCrO_2 , and Cr_xO_y . Such an accumulation of impurities in the evaporator may have caused either local de-wetting or a boiling onset. Clearly, de-wetting was observed during the startup of heat pipe #1. The situation with heat pipes #2 and #3 is less clear. The effect of impurity concentration on the incipient boiling superheats of liquid metals is mostly qualitative. A consistent trend has been established in the literature, showing that increased impurity concentration significantly diminishes incipient boiling superheats (Dwyer 1976). Changes in oxygen concentration from 3 to 9 parts per million have been shown to reduce the incipient boiling superheat from 3.5°C to 0.8°C on stainless steel with 0.4-mm-diameter cavity at 1073 K (Schultheiss and Schmidt 1969). The finding of significant oxygen in the distillate contained within the test capsule answered many questions raised from the failure of heat pipes #1–#3. Significant oxygen concentration in the heat pipe from the distillation process is consistent with the impurity-induced de-wetting hypothesis that had been proposed. Table 7 shows a summary of potential failure causes that were considered. Note that the thermochemical model developed for the Haynes 230 system was useful as a

Table 6. Summary of Life-test Heat Pipes

Heat Pipe	0	1	2	3	4**	5	6	7	units
Wall material	Haynes 230	S40 Haynes 230	Haynes 230	Haynes 230	Haynes 230	Haynes 230	MA 754	MA 956	
wall fabrication technique	gun drilled	seamless	gun drilled	gun drilled	gun drilled	seamless	gun drilled	gun drilled	
wick material	—	ss	ss	ss	ss	ss	ss	ss	
wick type*	5/8-18 UNF	7 layer wrap	7 layer wrap	7 layer wrap	7 layer wrap	2 layer wrap	2 layer wrap	2 layer wrap	
mesh	—	150	150	50	100	150	150	150	
porosity	—	0.65	0.65	0.6	0.65	0.65	0.65	0.65	
getter	none	none	Zr wire (~2 g)	none	none	none	none	none	
operation	air	air	air	air	air	air	air	air	
pre-test radiography	no	yes	yes	yes	yes	yes	yes	yes	
post-test radiography	no	yes	yes	yes	yes	yes	yes	yes	
end cap weld method	tig	e-beam	e-beam	e-beam	e-beam	e-beam	e-beam	e-beam	
end cap material	230 bar	230 bar	230 bar	230 bar	230 bar	230 bar	754 bar	956 bar	
wire diameter	—	0.066	0.066	0.066	0.066	0.114	0.066	0.066	mm
wire diameter	—	2.60	2.60	2.60	4.50	2.6	2.60	2.60	mil
wall inside diameter	1.45	1.24	1.45	1.45	1.45	1.24	1.45	1.45	cm
outside diameter	1.91	1.71	1.91	1.91	1.91	1.71	1.91	1.91	cm
inside diameter	1.45	1.25	1.45	1.45	1.45	1.24	1.45	1.45	cm
wall thickness	0.23	0.23	0.23	.23	0.23	0.23	0.23	0.23	cm
total length	91.4	45.7	45.7	45.7	45.7	45.7	45.7	45.7	cm
cond+adiab length	83.8	35.6	35.6	35.6	—	25.4	25.4	25.4	cm
adiabatic length	60.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	cm
evaporator (coil) length	5.1	7.6	7.6	7.6	—	17.8	17.8	17.8	cm
pool length	2.5	2.5	2.5	2.5	2.5	1.3	1.3	1.3	cm
wick thickness actual	—	1.26	1.48	1.50	2.01	0.36	0.42	0.43	mm
wick thickness theoretical	—	0.92	0.92	0.92	1.60	0.26	0.26	0.26	mm
charge	10	14	29	20	26	10	12	12	g
maximum throughput	1030	1520	1456	1560	—	1530	1718	1621	W
maximum evap rad. flux	45	51	42	45	—	16.0	16.2	15.2	W/cm ²
temp at maximum flux	1200	1203	1187	1242	—	1213	1219	1200	K

*Wick held to wall by internal tension of material

**not tested

Table 7. Proposed Haynes 230-Sodium Heat-Pipe Failure Mechanisms

Mode	Mechanism	Source	Evidence	Significance/Solution
Chemistry	De-wetting	1. Impurity movement from condenser to evaporator through liquid space	[S] Failure after three-hour operation points to rate-dependent mechanism consistent with impurity transport to the evaporator. [S] Thermochemical model indicates impurity transport to evaporator time constant on the order of hours. [S] Thermochemical model indicates production of chromium carbides. [S] Dewetting during startup of heat pipe #1.	This problem might be solved with revised processing and fill procedures.
	De-wetting	2. Impurity movement by diffusion from wall to evaporator surface	[S] Thermochemical model indicates chromium carbide production. [?] Confirmation by metallurgy.	Difficult problem to solve...additives to inhibit diffusion?
	De-wetting	3. Formation of unstable compounds in evaporator wall at the heat pipe operating temperature	[S] Development of hole in wall of heat pipe #3 while inside processing furnace at 1073 K. [S] Mn and Cr have relatively high vapor pressure at the operating temperature. [S] Thermochemical model indicates chromium carbide production. [?] Confirmation by metallurgy	No solution, indicates material instability.
	Corrosion	Oxygen induced in evaporator wall	[R] Thermochemical model gives no corrosion reaction with oxygen concentrations up to 1800 ppm. [S] Jacobson and Want (1984) experience and failure analysis of sodium/Inconel 617 heat pipe at 1035 K. [?] Metallurgy	This problem might be solved with revised processing and fill procedures.
	External reaction	Formation of eutectic compounds between Haynes and aluminum	[S] Development of hole in wall of heat pipe #3 while inside processing furnace at 1073 K. [R] Results from subsequent tests of samples inside processing furnace were negative.	Unlikely in light of tests
Design	Wick detachment from wall	Softening of wick near wall at operating temperature	[R] Repeated x-rays have yielded no evidence. [R] Very little stress placed on wick during operation	Considered unlikely
	Boiling	1. Superheat at surface of evaporator wall associated with thick liquid layer	[R] Delayed dry out onset not characteristic of boiling [R] Nucleation site size required is huge. [R] Dry out occurred on top-half of heat-pipe evaporator.	Considered unlikely
	Boiling	2. Collaps of condenser pool	[R] Dry out occurred on top half of heat-pipe evaporator [R] Force balance [S] Oscillation in temperature data	Unlikely for operation at 10° positive assist. Solution might be to reduce charge or to operate at horizontal.
	Boiling	3. Oxygen concentration changes in evaporator	[S] Three-hour operation points to rate-dependent mechanism consistent with impurity transport to evaporator. [S] Change in oxygen concentration from 1 to 10 ppm in evaporator could be sufficient to initiate. [?] Formation of stable oxygen compound can be detected by metallurgy.	Source similar to dewetting. This problem might be solved with revised processing and fill procedures.
Material	Stress induced cracking	Residual surface stress + stain intolerant microstructure + stress raisers	[S] Cold work of gun-drilled material [S] Geometry of cracks in failure surfaces of pipes #0 and #2 [?] Hardness measurements	This problem might be solved with revised heat treat and processing procedures
	Thermal stresses	Incompatibility in natural expansion of material	[S] Stress calculations [S] Creep rupture data for Haynes 230	Thermal stresses for unconstrained material are believed to be self relieving.
	Intergranular cracking and fracture	Liquid metal embrittlement	[S] Jacobson and Wang (1984) experience and failure analysis of sodium/Inconel 617 heat pipe at 1035 K. [S] Operating temperatures more than adequate to produce	Can be viewed as the opposite of de-wetting.

[S] tends to support hypothesis; [R] tends to refute hypothesis; [?] possible test of hypothesis.

diagnostic tool. The thermochemical model established diffusion and mass transport rates of impurities inside the heat pipe. This data helped identify impurity transport as the heat-pipe failure mode. The measurement of oxygen concentration in the distillate of over 1500 parts per million leads us to conclude that the source of the de-wetting observed in heat pipe #1 was oxygen contamination inside the heat pipe. The failure of heat pipe #2 after about 5 hours operation above 873 K was likely caused by an evaporator dry out initiated by oxygen-induced de-wetting. This finding is consistent with the results of the thermochemical model that indicate oxygen transport toward the evaporator with a time constant on the order of hours. The material failure of heat pipe #3 inside the processing furnace is believed due to sodium-oxygen attack on the Haynes 230 wall material. These findings were supported by metallurgical examination of the heat pipes conducted by Knolls.

Proof Tests of Haynes 230-Sodium Heat Pipe

The Haynes 230 heat pipe #5 was distillation-filled with 10.5 g of NIAPURE sodium. The heat pipe was then brought to 1073 K over its entire length for 48 hours inside a furnace open to atmosphere. This step permitted the sodium to wet the heat pipe's internal surfaces. The heat pipe was placed vertically inside a test fixture with a 16-cm-long induction coil. Power was increased to the heat-pipe evaporator over a 2-hour period until the condenser thermocouples read 1173 K. As the condenser thermocouples read 1098 K, a region, covering 30 degrees of heat-pipe circumference and spanning the evaporator length, appeared. This region, about 100 K above adjacent heat-pipe surfaces, persisted at the operating temperature, but did not otherwise affect heat-pipe performance. When the condenser thermocouples read 1173 K, sodium saturated the wick with less than a 2.5-cm-long evaporator pool. Non-condensable gas was completely absent from the heat pipe. The Haynes 230 heat pipe was then operated on 3 successive days, for a total of 18 hours of operating time with the condenser thermocouples reading 1173 K. Figure 11(a) shows the Haynes 230 heat pipe operating on the fourth day with the condenser thermocouples reading 1273 K. The heat pipe was then operated for another 40 hours over 6 days with the condenser thermocouples reading 1223 K.

Figure 11. Haynes 230-sodium heat pipe (a) and MA 754-sodium heat pipe (b). The corrected surface temperatures of the heat pipes pictured are 1273 K for (a) and 1223 K for (b).

The total condenser heat-rejection rate was estimated using wall temperature data. The radiation component to condenser heat-rejection rate was found using the following relation:

$$\dot{q}_{rad} = \sigma \epsilon \pi d_o \sum_{i=1}^{N_c} (z_{i+1} - z_i) \left(\frac{T_{i+1}^4 + T_i^4 + T_{i+1}^3 T_i + T_i^3 T_{i+1} + T_{i+1}^2 T_i^2}{5} - T_\infty^4 \right) . \quad (3)$$

The total hemispherical emittance of the Haynes 230 was estimated to be 0.8, based on various data taken for nickel-chromium-alloy surfaces oxidized above 1173 K found in Gubareff (1960). Note that the unity coefficient in the $T_{i+1}^2 T_i^2$ term in Equation (3) corrects Equation (9) in the 1998 progress report (Reid 1999) that had a coefficient of 2 leading the same term. The total heat transfer rate from the condenser is then the sum of the radiation and free convection components as follows:

$$\dot{q} = \dot{q}_{rad} + 1.42 \pi d_o \sum_{i=1}^{N_c} \frac{(z_{i+1} - z_i)}{(z_{i+1} - z_i)^{1/4}} \left(\frac{T_{i+1} + T_i}{2} - T_\infty \right)^{5/4} , \quad (4)$$

where SI units apply to all terms, and the second term uses a free convection relation for vertical surfaces in air found in Holman (1981). There was also heat loss from the 0.25-mm-diameter thermocouple wires that had an uninsulated length of about 3 mm away from the heat-pipe surface. An analytical calculation of a thermocouple wire attached to a semi-infinite solid surface indicates that thermocouples under-predicted the actual surface temperature by 8 K when the condenser temperature was 725 K. Because the 8-K under-prediction is larger than the uncertainty for Type-K thermocouples, ± 3 K, we elected to correct the temperature data for this offset. The left panel on Figure 12 shows the heat-transfer rate from the condenser to the surroundings versus evaporator exit temperature for the Haynes 230 heat pipe. Also plotted on Figure 12 are the sonic and viscous heat-transfer limit curves. At 650-K evaporator exit temperature, the heat-transfer rate from the condenser exceeds the viscous limit by about 25 W. Heat conduction along the wall near the evaporator exit accounts for this difference. As the temperature gradient near the evaporator exit diminishes and the evaporator exit temperature approaches 725 K, the data draws closer to the viscous limit curve.

Tests of MA 754-Sodium Heat Pipe

The MA 754 heat pipe #6 was distillation filled with 11.5 g of NIAPURE sodium. The MA754 heat pipe was brought to 1073 K over its entire length inside a furnace open to the atmosphere for 55 hours to wet the heat pipe's internal surfaces. The heat pipe was then placed vertically inside a test fixture with a 16-cm-long induction coil. Power was increased to the evaporator over a 2-hour period until the condenser thermocouples indicated 1223 K. The right side of Figure 12 shows condenser heat-rejection rate (based on thermocouple measurements) versus evaporator exit temperature. A hot region similar to that observed with the Haynes 230 heat pipe formed. At the 1223-K temperature measured by the condenser thermocouples, sodium saturated the wick with no more than a 2.5-cm-long evaporator pool. Non-condensable gas was completely absent from the heat pipe. Figure 11(b) shows the MA 754 heat-pipe operation on the second day of test.

Figure 12. Typical throughput measured versus evaporator exit temperature data during startup of the Haynes 230-sodium heat pipe (left) and MA 754-sodium heat pipe (right). The solid line represents the sonic limit curve.

Corrected Heat-Pipe Surface Temperature

Pyrometer measurements were made on the MA 754 heat-pipe surface when the thermocouple at the evaporator exit read 1223 K. These measurements were used to estimate the actual heat-pipe surface temperature using Planck's Law, radiosity observed by the pyrometer ($T_{observed}$), and a 0.65-micron wavelength:

$$T_{actual} = \frac{C_2}{\lambda \ln \left[\epsilon_\lambda \left(e^{C_2 / \lambda T_{observed}} - 1 \right) + 1 \right]} \quad (5)$$

MA 754 contains 78-percent nickel by weight. The lack of measured radiation property data for MA 754 prompted us to use published radiation property data for pure nickel. Gubareff (1960) gives a spectral emittance of 0.84 at 0.65 microns for nickel oxidized in air at 1256 K. The Equation (5) result indicates that a 1223-K condenser reading corresponds to a heat-pipe surface temperature of 1273 K. Another analytic calculation was made of thermocouple lead-wire loss to confirm this observation. The heat loss from an uninsulated thermocouple wire (3 mm long and 0.5 mm in diameter) attached to a semi-infinite solid surface was computed. The calculation indicates that when the thermocouples read 1223 K, the actual condenser surface temperature was about 1270 K. The interpretation of the pyrometer measurements prompted us to lower the MA 754 heat-pipe operating temperature so that the actual heat-pipe condenser temperature reflected the 1223-K target. Haynes 230 contains 57-percent nickel by weight, so the spectral emittance of oxidized nickel might also be assumed for the emittance of the Haynes 230 surface. Given this assumption and the similar thermocouple attachment technique, the temperature correction applied to the MA 754 heat pipe seems equally warranted for the Haynes 230 heat pipe.

Results of Haynes 230- and MA 754-Sodium Heat-Pipe Tests

Corrected condenser temperature data for the Haynes 230 heat pipe is shown in Table 8. For the first 18 hours of operation, the corrected heat-pipe condenser temperature was about 1223 K. The evaporator radial heat flux at this temperature was about 20 W/cm². The heat pipe was then

Table 8. History of Haynes 230-Sodium Heat Pipe #5 Demonstration

Run	Corrected T _{condenser} (K)	Maximum T _{evaporator} (K)	Evaporator Q'' (W/cm ²)	Duration at T _{condenser} (minutes)	Comments
W0003030730	1073	1073	~ 0	2880	Wet in at 1073 K for 48 h in air
D0003060730	1223	~1323	20	360	On startup evaporator develops hot spot at about 1100 K.
D0003070730	1223	~1323	20	360	Accumulation of liquid at condenser end
D0003080730	1223	~1323	20	360	Condenser end liquid pool dissipates and is distributed to other regions of heat pipe.
D0003090730	1273	~1373	22	420	No pool at condenser end
D0003100730	1273	~1373	22	360	Stable operation
D0003130730	1273	~1373	22	450	Stable operation
D0003140730	1273	~1373	22	450	Stable operation
D0003150730	1273	~1373	22	240	Stable operation
D0003160730	1273	~1373	22	480	Stable operation
W0003170730	1073	1073	~ 0	3270	After heat pipe was shut down, it was wet in at 1073 K for another 54.5 h in air.
Total Hours Haynes 230				18 h (1223 K) 40 h (1273 K)	Heat pipe was then x-rayed and delivered to Knolls.

brought to 1273 K and 22 W/cm² and operated there for another 40 hours. After the Haynes 230 tests were completed, the heat pipe was x-rayed. Not surprisingly, wick shrinkage was found in the evaporator, consistent with the evaporator temperature distribution observed during operation. No evidence of corrosion was detected in the condenser or the evaporator regions of the Haynes 230 heat pipe.

The MA 754 heat pipe operated on 5 successive days at the corrected 1223-K condenser temperature. At this operating temperature, the radial heat flux at the outside surface of the evaporator was 20 W/cm². The operating history and corrected temperature data for this heat pipe are summarized in Table 9. After tests were completed on the MA 754 heat pipe, it was x-rayed. Near the evaporator axial midpoint, two low-density spots, about 0.5 mm in diameter, were apparent. These low-density spots may indicate the onset of corrosion. Some shrinkage of the inner screen layer was also seen in the x-ray of the evaporator region. The screen shrinkage observed in the x-rays of both the Haynes 230 and the MA 754 heat pipes may be attributed to the evaporator of each heat pipe operating at temperatures over 1300 K.

Results of MA 956-Sodium Heat-Pipe Tests

The MA 956 heat pipe was placed vertically inside a test fixture with a 16-cm-long induction coil. Power was increased to the heat pipe evaporator over a 2-hour period until the condenser thermocouples indicated 1173 K (1223 K corrected). A small region, about 50 K above adjacent heat-pipe surfaces, similar to but less pronounced than that observed on the Haynes 230 and MA754 heat pipes, developed. This region did not otherwise affect heat-pipe performance. Non-condensable gas was completely absent from the heat pipe. Figure 13 shows the MA 956 heat pipe operating on the second day near 1223 K (corrected) at the evaporator exit. The heat pipe was then tested for another 40 hours over 5 successive workdays with the condenser near 1223 K (corrected). Test history for the MA 956 heat pipe is shown in Table 10. The evaporator radial

Table 9. History of MA 754-Sodium Heat Pipe #6 Demonstration

Run	Corrected T _{condenser} (K)	Maximum T _{evaporator} (K)	Evaporator Q'' (W/cm ²)	Duration at T _{condenser} (minutes)	Comments
D0003180730	1073	1073	~ 0	3300	Wet in at 1073 K for 55 h in air
D0003210730	1273	~1323	22	60	Initial startup to 1273 K and then correction
	1223	~1323	20	420	to 1223 K
D0003230730	1223	~1323	20	420	Stable operation
D0003240730	1223	~1323	20	390	Stable operation
D0003270730	1223	~1323	20	450	Stable operation
D0003280730	1223	~1323	20	450	Stable operation
D0003290730	1223	~1323	20	450	Stable operation
W0003300730	1073	1073	~ 0	3360	After heat pipe was shut down, it was wet in
					at 1073 K for another 56 h in air.
Total Hours MA 754				1 h (1273 K) 43 h (1223 K)	Heat pipe was then x-rayed and delivered to Knolls.

Figure 13. MA 956 heat pipe tested near 1225 K (corrected) at the evaporator exit. This photograph was taken on the second day of test.

Table 10. History of MA 956-Sodium Heat Pipe #7 Demonstration

Run	Corrected $T_{\text{condenser}}$ (K)	Maximum $T_{\text{evaporator}}$ (K)	Evaporator Q'' (W/cm ²)	Duration at $T_{\text{condenser}}$ (minutes)	Comments
W0007120730	1073	1073	~0	4560	Wet in at 1073 K for 76 h in air
D0007250730	1223	~1273	20	120	Stable operation
D0007260730	1223	~1273	20	480	Stable operation
D0007310730	1223	~1273	20	450	Stable operation
D0008010730	1223	~1273	20	450	Stable operation
D0008020730	1223	~1273	20	510	Stable operation
D0008030730	1223	~1273	20	510	Stable operation
W0008070730	1073	1073	~0	2880	After heat pipe was shut down, it was wet in at 1073 K for ~48 h in air.
Total Hours MA 956				42 h (1223K)	Heat pipe was then x-rayed and delivered to Knolls.

heat flux at 1223 K was about 20 W/cm². After the MA 956 heat-pipe tests were completed, the heat pipe was x-rayed. Wick shrinkage was found in the evaporator, similar to that observed in the Haynes 230 and MA 754 heat pipes. This shrinkage appears consistent with repeated exposure of unsupported stainless-steel screen to temperatures above 1200 K. The x-rays gave no evidence of corrosion in the condenser or the evaporator of the MA 956 heat pipe. The heat pipe was then placed inside a furnace (in air) and re-wet at 1073 K over a 48-hour period. After re-wet, it was shipped to Knolls.

4.0 Gas-Loaded Heat-Pipe Development

A 1-m-long active gas-pressure-controlled sodium heat pipe was built using low carbon arc cast (LCAC) molybdenum as a wall material and Mo-41%Re 400-mesh screen wick. Tests characterizing heat-pipe operational transients from the frozen and non-frozen states were conducted at various heating rates, thermal histories, and non-condensable gas loading. Gas-pressure control allows rapid transition from full power to low power at various temperatures. Thermal power was supplied to the heat-pipe evaporator by induction heating. The heat pipe was instrumented to allow measurement of heat-pipe surface temperatures, heat-pipe internal pressure, and thermal energy rejected from the condenser section. The heat pipe was designed to the specifications given in Table 11.

Gas-Control System Design

Figure 14 shows a schematic drawing of the annular variable conductance heat pipe with its set-point temperature controlled by gas injection. The gas-control system consists of two gas reservoirs: the first, marked *I*, was maintained at a higher pressure than the heat-pipe saturation pressure; the other reservoir, marked *II*, was kept at vacuum pressure. Throttles, marked 2 and *I*2, and solenoid valves, marked 3 and *I*3, separate each reservoir from the heat-pipe vapor space. A pressure transducer continually monitored heat-pipe internal pressure. A gas feed tube, marked 8, extends a sufficient distance (15 cm) into the heat pipe to prevent the tube from being blocked by the condenser liquid sodium pool. The gas feed tube consisted of a 0.953-cm-o.d.,

Table 11. Gas-Loaded Heat-Pipe Design Values

Item	Value
Orientation:	Horizontal
Overall Length:	100 cm
Evaporator Length:	30 cm
Condenser Length:	70 cm
Outside Diameter:	1.91 cm
Inside Diameter:	1.60 cm
Capillary Structure:	Annular Gap
Gap Thickness:	0.064 cm
Wick Material:	400-mesh molybdenum screen
Non-condensable Gas:	Neon
Nominal Temperature:	1225 K
Evaporator Radial Flux:	5 W/cm ²
Gas Control Mechanism:	Controllable Gas Charging

Figure 14. Schematic of molybdenum-sodium gas-loaded heat pipe: 1—high-pressure reservoir; 2—throttle; 3—solenoid; 4—induction coil; 5—gas interface low power; 6—gas interface, high power; 7, 8, 9—gas supply tube; 11—low-pressure reservoir; 12—throttle; 13—solenoid.

0.79-cm-i.d., Nb-1%Zr tube zirconium-brazed to the molybdenum condenser end cap. Niobium-zirconium alloy was selected for the gas supply tube because it is less susceptible to brittle fracture near room temperature than LCAC molybdenum. The zirconium braze provides a stable high-temperature joint and avoids problems associated with fusion welding of dissimilar materials.

To produce a sharp gas-vapor interface, the molecular weights of the non-condensable gas and the working fluid were selected for molecular weight similarity. The inert gas neon, molecular weight 20.2, closely matches sodium's molecular weight of 23. The heat pipe can operate with its condenser cool or at the evaporator exit temperature along its length. At near-zero condenser power, the gas-vapor front hovers about the position marked 5 in Figure 14, blocking vapor transport to the condenser region. While in this condition, the evaporator maintains its nominal temperature. The heat-pipe transitions to full power by removing non-condensable gas from the condenser to the low-pressure reservoir through the solenoid valve marked 13. The neon mass that must be removed from the heat pipe to bring the condenser to full power is

$$\delta m = \frac{P_v}{RT_c} \frac{D_i^2}{4} \frac{\dot{q}_c}{D_o \epsilon \sigma T_v^4}, \quad (6)$$

where P_v is the vapor pressure at the evaporator exit, R is the neon gas constant, T_s is the sink temperature, D_i is the vapor space diameter, $\dot{q}_c$ is the full-power condenser throughput, D_o is the outside diameter of the heat pipe, ϵ is the condenser emittance, and T_v is the evaporator exit temperature. As neon moves to the low-pressure reservoir, the vapor front advances and the temperature at 6 will begin to rise, signaling the closure of solenoid valve 13. At full power, the gas-vapor front remains at the position marked 6 in Figure 14.

Figure 15 shows various heat-pipe-performance limits versus evaporator exit temperature for the annular gap LCAC molybdenum-sodium gas-loaded heat pipe. The nominal temperature for this heat pipe is 1225 K. Heat-pipe-performance limits are not an issue during the operation of this heat pipe. Bare molybdenum has insufficient emittance for the heat pipe to benefit fully from the use of the high-performance annular wick structure. The annular-gap design features was incorporated into this heat pipe to verify processing procedures intended to prevent annulus blockage by the control gas in a high-performance heat pipe.

Figure 15. Performance limits versus evaporator exit temperature for the annular LCAC molybdenum-sodium gas-loaded heat-pipe design. The radiation limit curves are for two active lengths, and the viscous limit curve is the limit encountered during startup.

Preliminary Heat-Pipe Simulation

A numerical model characterizing the heat-pipe response to transitions from a fully active condenser to a fully inactive condenser was written using the SINDA lumped-capacitance differential analysis code. This transient thermal model divides the condenser into 12 nodes and the gas-supply-tube and end-cap region into 5 nodes. The effects of axial conduction and heat capacity in the heat-pipe wall and wick, radiation to the environment (0.12 emittance), and heat transport by condensation were considered. The movement of the non-condensable gas region is treated as a step-change in condenser region heat flux. The hydraulic response of the heat-pipe working fluid or the non-condensable gas is *not* directly considered in this model. A simulation was run with the boundary at the evaporator exit kept at 1200 K. Before shutdown, the gas-vapor front resided 25 cm from the condenser end of the heat pipe. The condenser end of the heat pipe was kept near 800 K. Figure 16 shows typical results from this model—the axial temperature distribution beginning from a steady-state condition where the temperature at the evaporator exit is 1200 K. Gas enters the heat-pipe condenser in response to a step reduction in evaporator heating rate. This transition is modeled by a step change in heat flux to the condenser region nodes. The heat pipe cools to 750 K over a 5-minute period. The simulation shows that a transition from 850-W condenser-heat-rejection rate at 1200 K to low-power operation (about 100-W condenser-heat-rejection rate at 750 K) should be possible over a 5-minute period, while the heat-pipe evaporator remains near 1200 K. Keeping the evaporator near 1200 K allows the heat-pipe condenser to be rapidly brought back to the full-power condition as non-condensable gas is expelled to the low-pressure reservoir. Figure 17 shows the results for the heat-pipe restart following a 5-minute cooling period. Note the axial temperature distribution hysteresis near the heat-pipe condenser end.

Figure 16. Numerical simulation of the gas-loaded molybdenum-sodium heat-pipe response to a pressure-regulated shutdown of the condenser region.

Figure 17. Numerical simulation of the gas-loaded molybdenum-sodium heat-pipe response to a pressure-regulated condenser restart following a 5-minute shutdown.

Gas-Loaded Heat-Pipe Fabrication

A trial sample for the LCAC molybdenum end cap-niobium fill tube with prototypic dimensions was fabricated. The end cap fill tube pictured in Figure 18 was assembled in preparation for a zirconium-braze trial. These two parts were cleaned and vacuum fired using standard Los Alamos molybdenum and niobium processing procedures. These parts were then placed inside the Centorr furnace with a zirconium wire, which was placed into a groove counter bored into the molybdenum end cap. The furnace temperature was then raised above 2125 K to allow the braze material to melt and join the parts. Cross sections of the part were then cut, and the braze joint was examined for integrity.

Condenser end parts consisting of a LCAC molybdenum end cap and niobium gas supply tube were cut. The gas feed consisting of a 7.9 mm–o.d., 6.4 mm–i.d., Nb-1%Zr tube was chemically cleaned using standard Los Alamos processing procedures. The niobium tube was joined to a molybdenum end cap under vacuum near 2125 K, using zirconium as a braze material. The braze joint, similar to the one shown in Figure 18, was examined and found to be satisfactory. The LCAC molybdenum tube was cut, chemically cleaned, and vacuum fired near 1300 K. Originally, 150-mesh molybdenum screen was proposed as a material for forming an annular wick for this heat pipe. Several trials failed to bond pure molybdenum screen sheets together so that it could be placed between a steel mandrel and sheath in preparation for a sheath-drawing operation. Therefore, the use of pure molybdenum screen was abandoned. In its place, a remnant of 400-mesh Mo-41%Re screen was found in stock sufficient to form an annular wick consisting of 6 layers. This screen was tacked to a 1020 mild steel mandrel, wrapped, and tacked again on its outer layer. The screen-covered mandrel was then inserted into a mild steel sheath and drawn to diameter. The sheath-drawing step compresses and sizes the screen to fit inside the LCAC tube to form a 0.635-mm annular gap. The drawn assembly was placed inside a heated hydrochloric acid bath to dissolve the mandrel and sheath. A radio-frequency heating system was assembled in preparation for a wick sintering operation and subsequent zirconium end plug braze.

Figure 18. Niobium-molybdenum transition end cap used at condenser end of Mo-Na gas-loaded heat pipe.

Molybdenum-Sodium Heat-Pipe Processing Procedures

After fabrication, 37.0 g of sodium were introduced into the heat pipe by vacuum distillation. The vacuum distillation assembly was then pressurized with argon. The heat pipe distillation assembly was then placed inside an inert gas glove bag. The fill stem was then disconnected from distillation hardware and checked to see if sodium blocked the gas tube. The fill stem was then attached to the end flange valve assembly. The valve was then connected to the vacuum system. The valve was opened, and the heat pipe was evacuated. The valve was then closed, and the heat pipe was attached to the gas-handling system.

The horizontally oriented heat pipe was then placed with attached gas-handling system inside a quartz tube covered with an rf coil that covered the pipe evaporator and condenser up to 10 cm away from the gas supply tube. A low-temperature wet-in was performed by bringing the heat pipe to 583 K in vacuum for 4 hours. The heat pipe was then cooled to room temperature.

Gas-Control System Characterization

A gas-control subsystem was built, and its transient pressure response was characterized. The system consisted of a pressurized gas reservoir, a vacuum reservoir, pressure transducer, loading can, and a closed-loop pressure controller. The controller's output pressure is proportional to an electrical signal input. Gas is supplied to the heat pipe through two solenoid valves. One valve supplies gas from a reservoir at a pressure much higher than the heat-pipe saturation pressure. The other valve exhausts gas from the heat pipe to vacuum. A transducer sends a pressure signal to electronic controls. This feedback signal is compared against the command signal input. A difference between the two signals causes one of the solenoid valves to open or close, allowing flow in or out of the system. Accurate pressure is maintained by active control of these solenoid valves. Figure 19 focuses on the gas-control system. Two reservoirs are shown: the first, marked P, is maintained at a higher pressure than the heat-pipe saturation pressure; the other, marked V, is connected to the dynamic vacuum. Solenoid valves, marked SP and SV, separate each reservoir from a container, R, and the heat-pipe vapor space. The pressure transducer, T,

Figure 19. Schematic of molybdenum-sodium gas-loaded heat pipe showing control system detail: P—high-pressure reservoir; C—proportional-pressure controller assembly; SP—solenoid to P; R—gas container; T—transducer; SV—solenoid to V; V—vacuum reservoir.

continuously monitors working pressure. The gas feed tube extends a sufficient distance into the heat pipe to prevent the tube from becoming blocked by the condenser sodium pool. The reservoir, marked R, functions as a cold trap and provides compliance for an auxiliary control mode.

The heat pipe may operate in any of three distinct modes. Startup is identical to a conventional variable conductance heat pipe. The heat pipe is first charged with a fixed quantity of neon (about 50 torr). The valve connecting the heat pipe to the can, R, is closed during this start-up period. The initial neon charge is sufficient to keep the gas front away from the gas-tube entrance at a predetermined operating temperature. Evaporator power is increased until the heat pipe reaches a nominal operating temperature and active length. Once the heat pipe reaches the nominal operating state, the gas-control-system set point is adjusted to match the heat-pipe saturation pressure. A path is then opened between the heat pipe and the gas-control system, C. This action engages the active control mode. Evaporator power can then be reduced or the temperature set point changed. During power reduction, the gas front moves toward the evaporator exit as the control system holds the heat-pipe internal pressure and temperature constant. Figure 20 gives the response of the gas-control system to various pressure transients. A gas can with internal volume of 754 cm^3 was connected to the gas-control system (shown as R in Figure 19). The can has about three times the internal volume of the molybdenum heat-pipe condenser. The high-pressure reservoir, P, is filled with nitrogen at 425 kPa absolute pressure through all transients shown in Figure 20. The vacuum reservoir, V, was kept around 50 microns of pressure using a vacuum pump.

The first curve in Figure 20(a) shows the response of the can to a step pressure increase from 158 kPa to 326 kPa. During the 2.5-s transition, the high-pressure source was kept at 425 kPa. This step increase corresponds to a change in heat-pipe saturation temperature from 1225 K to 1300 K, shown on the first curve in Figure 20(c). The response throughout the transition was smooth. No oscillations occurred once the target pressure was reached. The second curve in Figure 20(a) shows the response of the controller with a 158-kPa set point after it is opened to the 754 cm^3 can containing 425 kPa of nitrogen. Four seconds are required for the controller to bring the can from 425 kPa to 158 kPa. There is no noticeable overshoot as the pressure approaches the set point. The third curve in Figure 20(a) shows the controller (with set point 158 kPa) response after being opened to an evacuated can. After several low-amplitude oscillations, the controller brings the can to the 158-kPa target pressure. The total excursion time is less than 4 s. The fourth curve in Figure 20(a) shows the controller response to a step decrease in pressure set point from 158 kPa to 66 kPa. The 4-s transition corresponds to a heat-pipe saturation temperature change from 1225 K to 1140 K, shown on the first curve in Figure 20(c). The response throughout the transient was smooth. No oscillations occurred once the target pressure was reached. The curve marked 5 in Figure 20(b) shows the response of the can to a change in controller set point from 66 kPa to 158 kPa. During the 3-s transition, the high-pressure source was kept at 425 kPa. This step increase corresponds to a change in heat-pipe saturation temperature from 1140 K to 1225 K, shown on the first curve in Figure 20(d). The response throughout the transient was smooth. Little oscillation occurred once the target pressure was reached.

Figure 20. Response of gas-control system to various pressure transients: (a) shows system response from 160 kPa: 1—step pressure increase; 2—high-pressure excursion (supply pressure 425 kPa); 3—low-pressure excursion (exhaust pressure 50 microns); 4—step pressure decrease; (b) shows system response from 66 kPa: 5—step pressure increase; 6—high-pressure excursion (supply pressure 425 kPa); 7—low-pressure excursion (exhaust pressure 50 microns); 8—step pressure decrease; (c) and (d) show the sodium equilibrium saturation temperatures that correspond to (a) and (b), respectively.

The curve marked 6 in Figure 20(b) shows the response of the controller with a 66-kPa set point after it is opened to a 754-cm³ can filled with nitrogen at 425 kPa. It requires 10 s for the controller to bring the can from 425 kPa to 66 kPa. Other than a brief small amplitude oscillation at 13.5 s into the transient, the can pressure remains stable once the target pressure is reached. The curve marked 7 in Figure 20(b) shows the controller response after it is opened to an evacuated 754-cm³ can. After several low-amplitude oscillations, the controller stabilizes within 4 s after the valve connecting it to the can is opened.

The curve marked 8 in Figure 20(b) shows the controller response to a step decrease in pressure set point from 66 kPa to 25 kPa. The 9-s transition corresponds to a change in heat-pipe saturation temperature from 1140 K to 1040 K, shown on the first curve in Figure 20(d). The response during the initial transient was smooth. Some undershoot is seen near the target pressure. The can has three times the volume of the heat-pipe condenser. These tests demonstrated that the pressure controller is stable even for volume and pressure changes larger than anticipated for the gas-loaded heat pipe. The gas-system response time was typically less than 10 s. Since the thermal time constant of the heat-pipe condenser is on order of minutes, changes in heat-pipe boundary conditions from the gas system can be treated as instantaneous compared with changes induced by heating or cooling of the condenser.

Besides functioning as a cold trap, the container, R , serves as compliance for manual shutdown of the condenser region. In an operational system, such a pre-charged container might serve as a manual override feature in case of power loss to the pressure control system. By pre-loading the container, R , with gas, closing the valve between the container and the control system, C , and then manually opening the valve between the container, R , and the heat pipe, the heat-pipe condenser can be shut down. The ratio of the can's initial pressure to the can's final pressure can be found from mass conservation and gas state information:

$$\frac{P_{R1}}{P_{R2}} = 1 + \frac{T_{R1}}{T_{H2}} \frac{V_{H2}}{V_R} - \frac{T_{R1}}{T_{H1}} \frac{V_{H1}}{V_R}, \quad (7)$$

where P , T , and V represent gas pressure, temperature, and volume. The subscripts R and H apply to the can and heat pipe, respectively. Numerical subscripts apply to the system condition before, 1, and after, 2, the valve connecting the heat pipe and can is opened. For a thirty-to-one, can-to-condenser operating gas volume, $P_{R1}/P_{R2} = 1.13$ is sufficient for a condenser gas volume ratio of 3-to-1 and a heat-pipe condenser temperature transition from 1000 K to 775 K.

5.0 Gas-Loaded Heat Pipe: Results

Initial Heat-Pipe Operating Sequence

The full-length rf coil was replaced with a 25-cm-long rf coil located at the evaporator end. The heat-pipe was partially started so that the active region resides 10 cm from the entrance of the gas supply tube. The heat pipe evaporator exit temperature was about 700 K in this state. This partial startup was done slowly to ensure that the vapor region did not over shoot into the gas supply tube. The heat pipe was operated with active region 10 cm from gas tube for a few minutes. The rf power was turned off in single step, and the heat pipe was allowed to cool to room temperature.

A second partial heat-pipe startup was performed so that the active region resides 10 cm from the entrance of the gas supply tube. Once the active region reached 10 cm from gas tube, the pressure controller was switched to 3 kPa, and the line connecting the pressure controller to the heat pipe was opened to introduce neon gas into the heat pipe. Power was added to heat-pipe evaporator until the active region came within 10 cm of the gas tube. Gas was then added to the heat pipe to increase its operating temperature and move the gas front near the evaporator exit.

The heat pipe was shut down by reducing generator power gradually until heat-pipe wall temperatures were in the 573 K-to-673 K range. Lines were opened between the heat pipe and vacuum system to purge the heat pipe of neon. The valve was closed, and the heat pipe was cooled to room temperature.

Neon Evacuation Procedure

Heat pipes with low-impedance flow paths, such as arteries or annuli, are prone to loss of capillary pressure head from absorption of non-condensable gas in gas-loaded heat pipes. Control gases can become trapped in the flow path, seriously degrading performance. To avoid such problems, we developed an evacuation procedure to prevent trapping gas in such flow paths. The key idea is to remove any non-condensable gas below the temperature where the heat-pipe working fluid has appreciable vapor pressure, and above the temperature where the working

fluid de-wets the heat-pipe wick. For the 1-m Mo-Na heat pipe, we selected 623 K as the vapor temperature limit and 473 K for the de-wet temperature.

This section summarizes tests of the 1-m-long Mo-Na active gas-controlled heat pipe. Figure 21 shows a photograph of the system operating at 1150 K, with the gas front extended midway along the available active length. Significant accomplishments with this heat pipe include startup of a gas-controlled refractory metal-sodium system in less than 5 minutes and demonstration of the active pressure control principle applicable to advanced refractory metal power conversion systems. The Mo-Na heat pipe was first started without gas to check that the capillary limit was not reached at low power. No evaporator hot spots—evidence of such a limit—were observed. Throughout testing, the heat pipe remained horizontal with the evaporator and condenser at the same height. As anticipated, the sodium liquid tended to collect at the location that minimizes surface energy at the condenser end of the heat pipe, on the cold outside diameter of the gas supply tube, and the inner diameter of the heat pipe.

Figure 21. Photograph of active pressure-controlled, gas-loaded heat pipe during operation.

Sodium Blockage of Gas Supply Tube

Sodium vapor entry and solidification in the gas supply tube can block it. To minimize the chance for tube blockage, it was decided to keep the temperature of the thermocouple outside the gas supply tube below 673 K at all times. This approach keeps the sodium vapor pressure at the tube entrance at a negligible level. On several occasions when sodium vapor in that region exceeded that temperature, sodium entered the gas supply tube, solidified, and blocked it. Blockage occurred only when the gas pressure was suddenly reduced at the pressure source, forcing vapor into the tube while the generator power remained constant. On the few occasions when a sodium blockage did occur, a heat gun was focused to raise the temperature of the metal surrounding the suspected blockage-point above the sodium melting point. With a little patience, this practice was found to work well. In an operational system, a small heater, suitably placed, would be sufficient to entirely avoid sodium blockage of the gas supply tube. In the reported system, by keeping the supply gas at an elevated pressure when the generator power was changed, gas-supply-tube blockage was entirely avoided.

Condenser Heat-Rejection Rate

The condenser heat-rejection rate was estimated based on thermocouple data. The net radiant energy rate from a quartz-enclosed condenser surface to its surroundings can be written

$$\dot{Q}_c = \sum_{i=1}^{N_c} \sigma P_i \left\{ \epsilon_m \int_a^b T^4 dz - (b-a) \left[\epsilon'_q F'_q T_q^4 + \epsilon''_q (1-F'_q) T_q^4 + \tau'_q F'_0 T_0^4 \right] \right\}_i, \quad (8)$$

where the condenser surface is divided into segments between temperature measurement locations. The boundaries of each segment are $[a, b]$; where a is the left boundary, and b is the right boundary. Calculating the net energy rate emitted from the heat-pipe condenser requires integration of the temperature distribution along the heat-pipe surface. The heat-pipe surface temperature varies axially between points $[a, b]$ but can be normally assumed uniform about its circumference.

The emitted energy rate is proportional to the fourth power of absolute temperature, so for some arbitrary temperature distribution

$$J \propto \int_a^b T(z)^4 dz. \quad (9)$$

Given two discrete temperature measurements at adjacent locations $[a, b]$ along the length of the heat pipe, a linear relation is assumed for all points between

$$T(z) = T_a \left(\frac{z-b}{a-b} \right) + T_b \left(\frac{z-a}{b-a} \right). \quad (10)$$

Integrating the linear temperature distribution between $[a, b]$ and using Equation (8) gives the emitted-energy-rate proportional result for each condenser segment.

The emittance of molybdenum without a surface oxide layer has a strong spectral dependence at wavelengths below 3 microns. To simplify computation, the heat-pipe surface is treated as a body with temperature-dependent emittance. A polynomial curve fit is calculated using the total hemispherical emittance data for unoxidized molybdenum as a function of surface temperature found in Gubareff (1960)

$$\varepsilon_m \equiv \varepsilon_m(T_A) \quad (11)$$

Total hemispherical emittance of the molybdenum requires computation of a segment-average heat-pipe-surface temperature

$$T_A = \sqrt[4]{\frac{1}{b-a} \int_a^b T^4 dz} \quad (12)$$

During testing, the heat pipe was enclosed in a quartz vacuum tube. Thermal power was rejected from the heat-pipe condenser by radiant exchange with the quartz tube and the surfaces surrounding it. Quartz is effectively transparent to infrared radiation below a 4-micron wavelength and is effectively opaque above 4 microns. For reference, data for molybdenum spectral emittance are also plotted. To account for the spectral properties of quartz, a two-waveband approximation is used to determine the coupling between the condenser, the quartz, and the surroundings. The two-waveband approximation is especially important as the peak of the black body spectrum approaches the quartz-transmittance-transition wavelength, just above 900 K.

To establish a radiation background condition for the heat-pipe condenser, measurements were previously taken, during a startup from the frozen state, of the temperature at the heat-pipe surface and the quartz tube that surrounds it (Reid 1999). The quartz temperature is related to the temperature of the adjacent heat pipe. To track the various twists and turns, a fifth-degree polynomial is fitted to this data

$$T_q \equiv T_q(T_A) \quad (13)$$

The quartz temperature is then fixed for a given heat-pipe-surface temperature. This approach to specifying the condenser boundary condition avoids the complexity associated with characterizing the coupled radiation and natural-convection boundary conditions on the outside surface of the quartz tube.

The two-band calculation requires the integration of Planck's radiation law. The result of the integration can be approximated using a relation suggested by Wiebelt and found in Siegel and Howell (1981)

$$F' = \frac{1}{\sigma T^4} \int_0^\lambda e_{\lambda b} d\lambda \approx \frac{15}{\pi^4} \sum_{m=1,2,\dots} \frac{e^{-v}}{m^4} \left\{ [(mv+3)mv+6]mv+6 \right\}, \quad (14)$$

where $v = C_2/\lambda T \geq 2$, and C_2 is the second constant in Planck's spectral energy distribution. For the conditions here, use of the first three terms under the summation produces a discrepancy with the exact result of less than one percent. This relation is evaluated at a 4-micron wavelength at the quartz and the surrounding temperatures.

Gradual Startup and Shutdown: Test D0108091150

Figure 22 shows the temperature versus time response during startup and shutdown of the gas-controlled heat pipe. Initially the heat pipe was evacuated and was started as a conventional gasless heat pipe. At the end of the conventional start, the heat pipe was isothermal at 720 K, with an active length of 81 cm and an estimated condenser heat-rejection rate of 60 W.

Neon was then introduced into the heat pipe at 3.4 kPa, and the heat-pipe active region approached equilibrium at 700 K in a single step. A vertical sodium vapor-neon gas front formed across the diameter and was pushed by the gas source until it hovered near the evaporator exit. The vapor-gas front was sharp with little stratification observed across the heat pipe diameter.

Figure 23 shows the condenser heat-rejection rate versus time response during startup and shutdown of the gas-loaded heat pipe. The solid curve traces the power throughput estimated at the evaporator exit assuming a linear temperature versus time slope during the 4 h start up (positive) and shut down (negative). The broken line traces the estimated net power emitted from the condenser. Once the vapor-gas front reached the evaporator exit, the generator power setting

Figure 22. Temperature versus time measured at various locations on the 1.0-m Mo-Na heat pipe during a gradual startup and shutdown.

Figure 23. Condenser-heat-rejection rate estimated for the gradual startup and shutdown of the 1.0-m Mo-Na heat pipe. The solid curve traces the power throughput estimated at the evaporator exit. The broken line traces the estimated net power emitted from the condenser.

was increased at 5-minute intervals while the pressure in the heat pipe was held constant at 3.4 kPa. Neon pressure versus time is plotted in Figure 24. Increasing the generator power setting moved the vapor-gas front away from the evaporator and toward the condenser end of the heat pipe as the heat-pipe temperature remained constant at about 870 K. The generator power setting was increased at 5-minute intervals until the gas front resided at 67 cm. Concurrently, the estimated condenser heat-rejection rate reached 120 W. Gas pressure was then increased at 5-minute intervals until reaching 20 kPa, while the generator power setting was kept constant. This brought the heat-pipe evaporator to about 1000 K and moved the vapor-gas front back toward the evaporator, cooling the condenser and keeping the condenser heat-rejection rate at about 120 W.

The generator power setting was then increased at 5-minute intervals while pressure was held constant at 20 kPa. This moved the vapor-gas front toward the condenser and at the same time raised the condenser heat-rejection rate to about 300 W. Gas pressure was then increased at 5-minute intervals until it reached 110 kPa, while the generator power setting was kept constant. This brought the heat-pipe evaporator to about 1150 K and moved the vapor-gas front back toward the evaporator, cooling the condenser but raising the condenser heat-rejection rate to about 450 W. The generator power setting was then increased at 5-minute intervals while pressure was held constant at 110 kPa. This moved the vapor-gas front toward the condenser and at the same time raised the condenser heat-rejection rate to about 550 W. The heat-pipe active region was thus brought to 1150 K along the entire length of the heat pipe in this manner and kept at that temperature for about an hour.

Figure 24. Gas pressure and radio-frequency generator settings measured versus time during gradual startup and shutdown of the 1.0-m Mo-Na heat pipe.

The heat-pipe-cooling profile mirrored the heating ramp, producing the pleasingly symmetrical temperature versus time plot shown in Figure 22. Generator power setting was reduced at 5-minute intervals that moved the vapor-gas front toward the evaporator. Pressure was then reduced at 5-minute intervals, moving the vapor-gas front toward the condenser, increasing total condenser surface area, and lowering the heat-pipe surface temperature. The pressure and power were again reduced in a similar manner, and then the generator power was shut off in a single step, concluding the test.

Accelerated Startup and Shutdown: Test D0108271053

Figure 25 shows a similar start-up and shut-down sequence, but at an accelerated pace. Changes were made at one-minute rather than five-minute intervals. Another view with a different horizontal scale can be seen in Figure 26. As before, the heat pipe initially contained no neon and started in a conventional gas-less condition. At the conclusion of the conventional startup, the heat pipe was isothermal up to the thermocouple 81 cm from the evaporator entrance. Figure 27 shows gas pressure and generator settings versus time for the accelerated startup. The gas-control system was engaged, and neon was then inserted into the heat pipe at 3.4 kPa. The heat-pipe active region reached equilibrium at 850 K in a single step. At that point, the gas-vapor front resided near the evaporator exit. The generator power setting was then increased at one-minute intervals, while neon pressure was held constant. This moved the vapor-gas front away from the evaporator and at the same time raised the condenser heat-rejection rate to a little over 250 W. Once the vapor-gas front reached its furthest extent, neon pressure was increased while the generator power setting was held constant. This moved the vapor-gas front away from the condenser and toward the evaporator end of the heat pipe as the heat-pipe temperature remained constant. The heat-pipe active region was brought to 1150 K along the entire length of the heat pipe in this manner and kept there for about 10 minutes.

Figure 25. Temperature measured versus time at various locations on the 1.0-m Mo-Na heat pipe during an accelerated startup and shutdown, at same scale of preceding temperature profile graph.

Figure 26. Temperature measured versus time at various locations on the 1.0-m Mo-Na heat pipe during an accelerated startup and shutdown.

Figure 27. Gas pressure and radio-frequency generator settings measured versus time during an accelerated startup and shutdown of the 1.0-m Mo-Na heat pipe.

The power-down ramp mirrored in a reversed fashion the power-up ramp, producing the symmetrical temperature relation previously noted. Figure 28 shows condenser heat-rejection rate versus time for the accelerated startup. Generator power was reduced, moving the vapor-gas front toward the evaporator at one-minute intervals. Pressure was then reduced moving the vapor-gas front toward the condenser, increasing total surface area and lowering the heat-pipe surface temperature. The pressure and power were again reduced, and then the generator power was shut off in a single step, concluding the test. Keeping gas inside the heat pipe while the generator was shut off avoided problems with sodium blockage of the gas supply tube.

Cyclic Operation Demonstration: Test D0109201109

Figure 29 shows the effect of a series of cyclic maneuvers on the performance of the gas-loaded heat pipe. The heat pipe was brought to 700 K in about 10 minutes in a gas-less condition. Neon gas was then introduced into the heat pipe over the next 5 minutes, leveling at 120 kPa, while concurrently, the generator power setting was increased for both actions, bringing the heat pipe to its nominal 1150-K operating temperature. The heat-pipe boundary conditions were then left unchanged for about 10 minutes. Generator power was then cycled eight times, from 2.85 to 2.45 at 5-minute intervals while holding pressure constant. Figure 30 shows the condenser heat-rejection rate for the gas pressure and generator power settings versus time shown in Figure 31. One-hundred data points separate each symbol. The temperature plot clearly shows effect of the power cycling, with a regular cyclic pattern emerging once the power cycles become regularly periodic. At 7200 s into the test, the input-power pattern is changed. Instead of oscillations between 2.85 and 2.45 at 5-minute intervals, the generator-power input is cycled 3 times between 2.85 and zero at 2-minute intervals. The heat-pipe response during this and a later series of three, 1-minute cycles between 2.85 and 1.85 was completely normal— evaporator hot spots were not observed nor was there evidence of sodium blockage in the gas control tube.

Figure 28. Condenser heat-rejection rate estimated for the accelerated startup and shutdown of the 1.0-m Mo-Na heat pipe. The solid curve traces the power throughput estimated at the evaporator exit. The broken line traces the estimated net power emitted from the condenser.

Figure 29. Temperature versus time measured at various locations on the 1.0-m Mo-Na heat pipe during a cyclic operation.

Figure 30. Condenser heat-rejection rate estimated during a cyclic operation of the 1.0-m Mo-Na heat pipe.

Figure 31. Gas pressure and radio-frequency generator settings versus time during a cyclic operation of the 1.0-m Mo-Na heat pipe.

Rapid Gas Loaded Startup: Test D0110091107

Figures 32, 33, and 34 show the startup of the gas-controlled sodium heat pipe in less than 5 minutes. The heat pipe is preloaded with neon gas at 110 kPa. The generator was brought from zero to 3.25 power setting in a single step. The heat-pipe evaporator exit temperature immediately responded to the generator change with a temperature-over-time slope of 9°C/s . During this and all subsequent periods during startup, the heat-pipe evaporator remained isothermal, with no developed hot spots. The melt front continued to progress down the heat-pipe length with thermocouple 4 reaching 1200 K about 5 minutes into the startup. Axial conduction to the thermocouple locations 5 and beyond contributes only modest heating. The heat pipe is clearly divided into an active region on the evaporator side of thermocouple 4, and an inactive region toward the condenser end side of thermocouple 5. Figure 33 indicates a transition in condenser heat rejection rate from near zero watts at 150 s to 600 W at 450 s. The heat pipe responses to the step power input from the generator with no apparent problems. The heat pipe is held in this condition for about 20 minutes, and the power is then shut off while neon pressure is held constant.

Table 12 summarizes the tests conducted on the 1-m Mo-Na active control gas loaded heat pipe. This test series demonstrated the use of active pressure control in the design of a high performance heat pipe without problems such as artery blockage. This result is significant. High power density devices may achieve rapid transient response by incorporating alkali metal heat pipes with this design feature.

Figure 32. Temperature versus time measured at various locations on the 1.0-m Mo-Na heat pipe during a rapid startup.

Figure 33. Condenser heat-rejection rate estimated for a rapid startup of the 1.0-m Mo-Na heat pipe.

Figure 34. Gas pressure and radio-frequency generator settings versus time during a rapid startup of the 1.0-m Mo-Na heat pipe.

Table 12. Gas-Loaded Heat-Pipe Test Summary

Run	Maximum Condenser Temperature (K)	Test Duration (s)	Comments
D0107301010	740	5960	Initial frozen startup. Operated as fixed conductance heat pipe. No hot spots.
D0107311430	930	10267	Fixed conductance startup. Generator fault at 3600 s. Power ramp resumed. Transition to active control. No hot spots.
D0108071328	1142	22741	Active control demonstrated. Shutdown due to generator fault. No hot spots.
D0108091150	1151	33362	Active control demonstrated during startup and shutdown. Changes in pressure and power made at 5-minute intervals.
D0108161125	1152	26522	Gas supply tube initially blocked with sodium. Startup in variable conductance mode. Unblocked gas tube during operation, normal shutdown.
D0108231057	1151	17618	Normal restart. Changes in pressure and power made at 2-minute intervals. Power reduced at maximum gas pressure.
D0108271053	1152	23689	Normal restart. Changes in pressure and power made at 1-minute intervals. Pressure reduced, followed by shutoff of generator power.
D0108301503	1177	15557	Normal restart. Gas front position kept fixed 6 inches from evaporator exit on startup. Changes in pressure and power made simultaneously at 1-minute intervals.
D0109201109	1150	14400	Normal frozen restart. Cyclic operation demonstrated. Ten cycles between 100% and 67% condenser throughput with 7.5- minute period. Four cycles between 100% and 20% condenser throughput with 7.5-minute period.
D0108301503	1200	3600	Rapid gas-loaded startup. Front reached mid-axial location in condenser within 300 s.

6.0 Gas-Loaded Heat Pipe: Transient Response Approximation

Three conditions control the heat-transfer rate to a heat-pipe condenser during alkali metal heat-pipe frozen startup. Early on, the heat pipe is viscous limited: low-saturation pressure constrains working fluid circulation. As vapor pressure increases with temperature, the sonic flow or the condenser coupling controls the heat-transfer rate to the condenser. When noncondensable gas is introduced into the heat pipe, the evaporator saturation temperature corresponds to the gas pressure.

The unsteady one-dimensional diffusion equation with a heating source term and radiation to the surroundings can be written as

$$C \frac{\partial T}{\partial t} = k \frac{\partial^2 T}{\partial x^2} + \dot{q} - G_R (T^4 - T_\infty^4) . \quad (15)$$

Writing Equation (15) in difference form about the evaporator node:

$$C_1 \frac{T_1 - T_1^p}{\delta t} = \dot{q}_{RF} + G_C(T_2 - T_1) - G_{R,1}(T_1^4 - T_\infty^4) - \dot{q}_{LIM} . \quad (16)$$

The first term on the right side of (17) is the power applied to the evaporator, estimated with condenser temperature data. The axial conductance, G_C , is calculated at a cross-sectional area of the wick and wall and the spacing between node mass centers. The minimum of the radiation, sonic, and viscous limits: $\dot{q}_{LIM} = \min(\dot{q}_R, \dot{q}_S, \dot{q}_V)$, determines the heat transfer-rate $\dot{q}_2$ to the condenser. A power balance about the four condenser nodes is of the form

$$C_i \frac{T_i - T_i^p}{\delta t} = \dot{q}_i + G_C(T_{i+1}^p - T_i) - G_{R,i}(T_i^4 - T_\infty^4) \quad i = 2 \dots N_s \quad (17)$$

In the absence of external heat or work transfer into the node, thermodynamics requires $T_i \geq T_i^p$. When $T_i > T_i^p$ is computed, T_i assumes the value T_i^p , or the saturation vapor temperature based on the applied gas pressure. The power transferred to the next node is

$$\dot{q}_{i+1} = \dot{q}_i + G_C(T_{i+1}^p - T_i) - \frac{C_i}{\delta t}(T_i - T_i^p) - G_{R,i}(T_i^4 - T_\infty^4) . \quad (18)$$

The Newton-Raphson method is used to solve Equations (16) through (18) for the respective node temperatures. The radiation coupling and the heat capacity of the last node are adjusted for the presence of the cold liquid pool that accumulates at the condenser end of the heat pipe.

The minimum of the radiation, sonic, and viscous limits: $\dot{q}_{LIM} = \min(\dot{q}_R, \dot{q}_S, \dot{q}_V)$, is the heat transfer rate $\dot{q}_2$ to the condenser during startup. A heat balance about the evaporator establishes the heat transfer rate to the condenser:

$$\dot{q}_R = \dot{q}_{RF} + G_C(T_2^p - T_{sat}^p) - \frac{C_1}{\delta t}(T_{sat} - T_{sat}^p) - G_{R,1}(T_{sat}^4 - T_\infty^4) . \quad (19)$$

The condenser starts at x_2 and continues to the edge of the liquid pool, x_{POOL} . The modeled heat pipe was not sonic-limited during startup. For brevity, sonic-limit calculations are omitted from this description.

At low temperature, surface forces are undeveloped, and evaporator vapor pressure circulates sodium through the vapor space and wick. A pressure balance at this condition is described by

$$P_{sat} = \Delta P_{EVAP} + \Delta P_{COND} + \Delta P_{LIQ} . \quad (20)$$

The analytical pressure drop relations used in this paper are listed in Woloshun (1988). Vapor pressure drop in the evaporator and condenser is divided into viscous and inertial terms. Radial mass injection stabilizes evaporator vapor flow. The laminar flow friction factor is $f=16/\text{Re}_D$, where Re_D is the axial Reynolds number, $\text{Re}_D = 4\dot{q}_2/(\pi d_v h_{fg} \mu_v)$. The inertial vapor plus viscous pressure drops for laminar incompressible flow in the evaporator region of a cylindrical heat pipe is

$$\Delta P_{EVAP} = \left(\frac{\dot{q}_2}{h_{fg}} \right)^2 \frac{16L_e f}{\pi^2 \rho_v d_v^5} [1 + \Psi] ; \quad (21)$$

the velocity profile correction factor, Ψ , a function of radial Reynolds number, $\text{Re}_{r,e} = -\dot{q}_2/(2\pi L_e h_{fg} \mu_v)$, is

$$\Psi = 0.61 \text{Re}_{r,e} \left(1 + 1/(3.6 + \text{Re}_{r,e}) \right) . \quad (22)$$

Condenser vapor flow is often turbulent from mass removal. Turbulent onset correlates with radial Reynolds number $\text{Re}_{r,c} = -\dot{q}_2/(2\pi L_c h_{fg} \mu_v)$. If $\text{Re}_{r,c} < -2.25$, the viscous vapor pressure drop is calculated with $f=16/\text{Re}_D$ for laminar flow, $f = 0.079 \text{Re}_D^{-0.25}$ for $2,000 < \text{Re}_D < 20,000$, and $f = 0.046 \text{Re}_D^{-0.20}$ for $\text{Re}_D > 20,000$. For uniform heat removal and radial Reynolds number in the condenser, $\text{Re}_{r,c} > -2.25$, the inertial pressure recovery for laminar incompressible flow in the condenser of a cylindrical heat pipe was given by Busse (1967):

$$\Delta P_C = P_{x=L_C} - \frac{64\mu_v \dot{q}_2 L_C}{\rho_v h_{fg} \pi d_v^4} \text{Re}_{r,c} \left(\frac{7}{9} - \frac{8}{27}\alpha + \frac{23}{405}\alpha^2 \right) \left(1 - \frac{x-x_2}{L_C} \right)^2 ; \quad (23)$$

the velocity profile correction is $\alpha = 15/22 \left(5 + 18/\text{Re}_{r,c} + \left((5 + 18/\text{Re}_{r,c})^2 - 44/5 \right)^{0.5} \right)$.

In the 1970s, Joe Kemme, at the Los Alamos Scientific Laboratory, correlated cylindrical heat-pipe-condenser inertial-pressure recovery for $\text{Re}_{r,c} < -2.25$ to be a fraction of the inertial pressure at the condenser entrance (Woloshun, 1988):

$$\Delta P_C = \frac{(-\text{Re}_{r,c} - 2)}{\rho_v (-1.23 \text{Re}_{r,c} + 2L_E/L_C)} \left(\frac{4\dot{q}_2}{h_{fg} \pi d_v^2} \right)^2 . \quad (24)$$

The annulus provides the primary liquid-sodium return path. Liquid pressure drop in the annulus is found using the Poiseuille flow equation based on hydraulic radius.

Figure 35 compares the measured temperature versus time profile to the calculated profile using transient response approximation at various locations on the 1.0-m Mo-Na heat pipe during a gradual startup and shutdown. Agreement between the calculated and measured profiles is good during the frozen start up period. Calculated and measured profile agreement is fair as gas is introduced into the heat pipe. Considering that the power applied to the evaporator is estimated based on measured wall temperatures, the predictive capability of the approximation is surprising.

Figure 35a. Temperature versus time measured at various locations on the 1.0-m Mo-Na heat pipe during a gradual startup and shutdown.

Figure 35b. Temperature versus time calculated using transient response approximation at various locations on the 1.0-m Mo-Na heat pipe during a gradual startup and shutdown.

Appendix A: Approaches to the Vacuum Distillation of Sodium

Existing Sodium Distillation Procedure

Charging a heat pipe with sodium consists of up to seven separate operations as follows: (1) the movement of sodium from a storage container to a loading pot of a known volume; (2) the transfer of sodium from the known volume to the distillation pot; (3) the vaporization of sodium in the distillation pot and condensation of the sodium in a line that allows the distillate to gravity-feed into the heat pipe; (4) the formation of a sodium freeze plug in the heat-pipe fill stem; (5) the cutting away of the fill stem inside an inert-gas atmosphere; (6) the movement of the heat-pipe end cap assembly to the electron-beam welding machine; and (7) the attachment of the end cap to the heat pipe under vacuum inside the electron-beam welder. All parts that are exposed to sodium during charging are degreased and vacuum-fired before use. This seven-step procedure was performed to charge the sodium-test capsules. The super alloy heat pipes were charged using the first four steps. During the test of the heat pipes, a freeze plug was maintained at the fill stem using a chill block.

The heat pipe is charged with sodium using the hardware shown schematically in Figure 36. A stainless-steel known volume is attached with compression fittings to a sodium storage container. A positive pressure of argon is maintained inside the storage container to prevent air from entering it. The known volume is then evacuated. The fill of the known volume with sodium from the storage container is depicted in Figure 36. The sodium inside the storage container is melted with resistance heaters, and the known volume is heated above the melt-point temperature of sodium. The argon above the sodium-free surface in the storage container is pressurized, forcing sodium from the bottom of the storage container into the known volume. The known volume and the sodium storage container are then separated under inert gas, and both are sealed with compression fittings. The known volume is then attached to the distillation pot. This distillation configuration is shown in Figure 37. The known volume container is then

Figure 36. Fill of known volume container with sodium from storage container.

Figure 37. Sodium distillation apparatus used to fill heat pipes #1–#4. The inside diameter of the transfer line was about 0.5 cm.

brought to the sodium melting point, and the sodium is pushed into the distillation pot with argon gas. A positive argon pressure is maintained inside the distillation pot as the known volume container is removed, and the pot is sealed with compression fittings. The distillation pot is leak-tested to verify the integrity of all weld and compression joints. The internal volume of the heat pipe and distillation hardware is attached to a vacuum pumping system as a means of degassing the system before distillation. This same pumping system was used to remove off-gasses from the sodium during the fill process. The distillation container is then evacuated to 10^{-6} torr and brought to temperature with heater tapes. Under the existing distillation procedures, the distillation pot is heated to 500°C. Sodium vapor moves at sonic velocity up the transfer line and condenses on the far side of the bend. The condensed sodium then flows into the heat pipe by gravity. Figure 38 shows a temperature history of the distillation fill of heat pipe #2. The numbers associated with each temperature curve reference the thermocouple locations shown in Figure 37.

Filling continues until the distillation pot is dry, indicated by the increase in the temperature versus time slope read at thermocouple location #1. A water-cooled chill block on the heat-pipe fill stem is then used to form a plug of frozen sodium in the fill stem. The sodium plug seals the heat-pipe interior during testing or until a more permanent cover tube can be welded over the frozen sodium seal. To attach a more permanent cover tube to the heat pipe, the fill stem is cut near the heat pipe under inert gas. The cover tube is then placed over the fill tube in an inert gas bag and then taken to the electron-beam welder, where the assembly is evacuated, and the cover tube electron beam is welded to the body of the heat pipe.

Figure 38. Temperature versus time measured during the vacuum distillation of sodium into heat pipe #2. The total sodium transferred during this fill operation is on the order of 10 g.

Evaluation of Existing Fill Procedure

During distillation, the sonic velocity of the vapor flowing through the transfer line limits the sodium mass transport rate between the distillation pot and the heat pipe. The data shown in Figures 3 and 4 can be used to check this. Under the existing distillation procedures, there are several potential oxygen contamination sources that could contribute to high (or apparently high) oxygen concentration inside the capsule after the fill. These sources include the following: (1) formation of a surface film of Na_2O on the exposed region of the solid sodium during the transfer of the capsule between the inert-gas bag and the electron-beam welder; (2) oxygen diffusion and leakage into the sodium storage box and access valves; (3) low inert gas purity in the inert gas bag; (4) oxygen contamination in metallic surfaces of the capsule or distillation hardware; (5) trapping of air inside the end cap during electron-beam welding; (6) leaking of air into the distillation apparatus during or following fill; and (7) production of oxygen from the sodium during the distillation procedure.

The formation of sodium-oxide film on exposed solid sodium is not a credible mechanism for producing apparent oxygen concentrations of over 1000 ppm in the filled capsule. Exposure time to air was on the order of seconds, and the solubility of solid sodium at room temperature is quite small. Oxygen diffusion and leakage into the sodium storage container and access valves are entirely possible given the age of the sodium storage container (about 15 years) and the probable leakage rates through even high-vacuum valves. However, air leakage into the source alone does not explain the presence of oxygen in the sodium after vacuum distillation. Low inert gas purity in the argon source is considered unlikely. Our argon gas supplier quotes an oxygen concentration of less than 10-parts-per-million oxygen. Pure argon from the bottle still does not eliminate the possibility of oxygen entering the gas lines feeding into the distillation system and

argon glove bag. Before distillation, any oxygen-bearing argon is evacuated from the distillation pot. Once distillation is complete, the exposure of sodium to any cover gases is comparatively brief and occurs near room temperature. Oxidation of metallic surfaces of the capsule or distillation hardware is a possibility over time. Oxygen concentration was measured in the empty capsule last November and again in April, with the capsule filled with sodium. Meanwhile, the capsule material could have absorbed oxygen. However, oxidation is strongly temperature dependent, and the capsule was stored from November to April at room temperature in a reduced-oxygen atmosphere. The possibility of metal oxidation sufficient to produce 7 mg of oxide under these conditions appears remote. Air trapped inside the end cap during electron-beam welding also seems unlikely. Before closure, the electron-beam welder was evacuated to below 10^{-6} -torr pressure. The oxygen mass contained in this volume at room temperature and pressure is much less than 7 mg. The residual oxygen remaining in the gap between the fill stem and end cap once inside the welder should be very small.

The leakage of air into the distillation apparatus during or after fill is possible. However, before fill, the distillation apparatus is checked for integrity with a helium-mass-spectrometer leak detector. Also, the assembly is only opened under an inert cover gas at room temperature, which minimizes the potential for sodium oxidation.

The production of oxygen from the sodium during the distillation process at first appears unlikely. Sodium monoxide remains stable well above the temperatures reached in the existing fill process. However, the peroxide of sodium begins to dissociate within the range of temperatures measured during the existing fill process. Figure 39 compares the vapor pressure of sodium to the oxygen dissociation pressure above sodium peroxide versus temperature. The measurement of oxygen concentration in the sodium at 1530 parts per million offers a clue to a possible oxygen-contamination mechanism. The solubility of oxygen in sodium is strongly temperature dependent. Figure 40 plots oxygen solubility in sodium with temperature. Assuming that sodium peroxide exists in the sodium in sufficient quantity to saturate the distillate with oxygen, the oxygen mass in the distillate can be estimated with

$$m_O = A \int_{\tau} \rho_{sat}(T(t)) [\gamma RT(t)]^{1/2} [O](T(t)) dt . \quad (25)$$

Performing this integration using the data plotted in Figure 40 yields an oxygen concentration in the distillate of 1692 parts per million. Thermocouple #2 data is used as the temperature of the vapor flowing through the transfer tube and the temperature of the condensate. This estimated concentration value is in good agreement with the neutron activation measurement. The small difference between measurement and calculation can be accounted for by considering that sodium most likely condenses at a slightly lower temperature than measured by thermocouple #2.

Based on the above analysis, a plausible oxygen-contamination mechanism emerges. Ambient air gradually diffused through the sodium storage container walls and leaked through connecting valves over a period of some 15 years, producing various forms of sodium oxide. These oxides, having a higher specific gravity than pure sodium, accumulated at the bottom of

Figure 39. Oxygen solubility measured in sodium liquid versus temperature (left curve); sonic-limited sodium vapor mass flux versus temperature (right curve).

Figure 40. Dissociation pressure of oxygen measured above sodium peroxide (solid curve) and sodium saturation pressure (dashed curve) versus temperature.

the storage container near the sodium intake line. Using our existing fill procedures, sodium and its oxides are transferred into the distillation pot. During fill, the distillation pot is heated to temperatures sufficient to produce significant dissociation of sodium peroxide. The temperature gradient and the pressure gradient produced by the vacuum system carry free oxygen up the transfer line along with sodium vapor. As sodium condenses on a cold surface on the inside diameter of the transfer line, it becomes saturated by the oxygen gas stream as the stream passes over the condensate.

Revised Fill Procedure

To reduce the oxygen content in the sodium distillate, the sodium fill procedure will be revised. Passing the sodium through a 10-micron filter as it moves between the known volume pot and the distillation pot will reduce the concentration of Na_2O and Na_2O_2 from the sodium source. Minimizing the distillation temperature reduces the dissociation pressure of oxygen above Na_2O_2 . Reducing the distillation temperature will also reduce the sodium condensation temperature and will lower the oxygen solubility of the distillate. A target value for distillation pot temperature of 450°C will be used in the revised procedures. These revised procedures will considerably reduce the mass flux of sodium vapor to the heat pipe. To reduce the time required to fill a heat pipe at these temperatures, the cross-sectional area of the transfer line will be increased by a factor of four.

Appendix B: Oxygen Gettering with Hafnium

Heat pipe #2 contained a getter to remove impurities from the sodium. A natural question thus arises. If impurity concentration induces dry out, should a pipe with a getter behave differently from a pipe without a getter? Figure 41 gives some insight into getter effectiveness. After 4 hours at 1200 K, the oxygen concentration in a hafnium getter is still far away from stoichiometric proportions. The curves in Figure 41 are based on temperature-dependent binary diffusion data found in Vykhodets and Kurennikh (1993) for an oxygen-hafnium system (oxygen-hafnium and oxygen-zirconium diffusion rates are assumed here to be similar). This suggests that at 1200 K and at short time-scales, getters should not significantly affect impurity concentration in sodium.

Figure 41. Diffusion (dimensionless concentration difference) of oxygen into a 0.15-mm hafnium foil versus time at various temperatures.

Appendix C: Incident Attributed to MA 754 Processing Procedures

In October 1999, an incident occurred at Los Alamos that was attributed to chemical waste generated during the processing of the MA 754 heat pipe. The pickling procedure recommended by Inco Alloys for MA 754 involves mixing iron filings with dilute nitric and hydrofluoric acids. Waste from this procedure was poured into an acid-compatible glass bottle and stored inside a cabinet. About 2 weeks after the glass bottle was placed inside the cabinet, a deflagration occurred within the cabinet that caused a shelf to be displaced and several bottles to be broken. The source of this deflagration is now believed to have been the combustion of hydrogen gas formed from the iron-dilute nitric-acid mixture inside the sealed glass bottle. An internal Los Alamos report detailing the incident was submitted.

Appendix D: NIAPURE Sodium Analysis

The NIAPURE sodium received from DuPont was produced by the Downs electrolytic process and is among the purest commercially available stock. Table 13 compares impurity concentrations quoted by DuPont for various elements in NIAPURE to that of standard sodium. Omitted from Table 13 are values for oxygen concentration. DuPont has not recently measured the oxygen concentration in its NIAPURE sodium. However, based on historical production performance, Vern Markant, a DuPont sodium specialist, believes that the sodium sent to Los Alamos should contain oxygen at the 50- to 100-ppm level. The lack of recent oxygen measurements for the DuPont sodium prompted us to make an independent measurement. SteriGenics, the commercial laboratory that we had used for neutron activation analysis, experienced an equipment malfunction, which made it unprofitable for them to continue their service. Dr. Dennis James of Texas A&M University offered to perform a similar analysis. Several 2.54-cm-o.d. by 5-cm-long stainless-steel vials were built to hold 13 g of sodium from

Table 13. Impurity Concentrations in NIAPURE Sodium Given by DuPont.

Symbol	Element	Regular Grade (ppm)	NIAPURE Grade (ppm)
Ag	Silver	<28	14
Al	Aluminum	4	1.6
Ba	Barium	2	2
Be	Beryllium	<0.5	<1
B	Boron	2	<2
Ca	Calcium	330	4
C	Carbon	ND	1.7
Cl	Chlorine	30	<5
Cr	Chromium	<5	0.8
Cu	Copper	<18	1.7
Fe	Iron	2.4	0.9
K	Potassium	300	300
Li	Lithium	<10	<1
Mg	Magnesium	9	4
Mn	Manganese	<1.2	0.4
Mo	Molybdenum	<6	0.4
N	Nitrogen	ND	<3
Ni	Nickel	<9	0.9
P	Phosphorous	<20	6
Si	Silicon	5	7
Sr	Strontium	21	<1.3
Sn	Tin	<12	<25
S	Sulfur	10	0.4
Ti	Titanium	3.6	<6
V	Vanadium	8.2	0.02
Zn	Zinc	30	<6.3
Zr/Hf	Zirconium/Hafnium	ND	2

different stages of the fill operation. These capsules were cleaned and vacuum-fired to remove nonmetallic impurities from the metal surface and then filled using standard Los Alamos heat-pipe processing procedures. These samples were sent to Texas A&M University for oxygen measurement by neutron activation analysis. Sample #1 contains NIAPURE sodium in the received condition. Sample #2 was filled with vacuum distilled NIAPURE sodium using standard Los Alamos heat-pipe processing procedures. Sample #3 was an empty stainless-steel vial—identical to those used to contain the sodium in the first two samples. Sample #1 was found to have average oxygen content of 55.7 ± 2.4 ppm. This is based on the weight of the container plus the sodium it contained. The value reported is an average value of two independent analytical runs. The two replicate results were 53.3 and 58.0 ppm. The weight of the sample was 143.1 g, giving a total mass of oxygen present of 7.966 mg.

Sample #2 was found to have an average oxygen content of 46.4 ± 6.9 ppm. In this case, the result is an average of three runs; the results of which were 44.3, 39.2 and 55.6 ppm. The weight was 143.5 g, giving a total oxygen mass of 6.655 mg. The oxygen content measured in unfilled sample #3 was 56.7 ± 10.6 ppm. That the unfilled container has a higher oxygen concentration than the filled container appears at first to be surprising. However, the difference between 55.7 ppm measured in sample #1 and 56.7 ppm measured in sample #3 is not statistically significant. Wilcoxon statistical two-sample analysis was used to determine the significance of these results that involve a small number of tests with large standard deviations (Walpole and Myers 1978). The results of this analysis indicate a 0.8 level of significance for the proposition that the concentration of oxygen in sample #1 is greater than that in sample #2. If sample #2 is considered to be a container filled with oxygen-free material, then the difference in oxygen masses is 1.311 mg, and the oxygen content of the sodium in sample #1 is 1311 micrograms/12.4 g = 105.7 ppm. The 12.4-g-sodium weight is the average difference of the weights of the two sodium-containing containers to the empty container. The measured 105.7 ppm for sample #1 is consistent with the DuPont oxygen-concentration estimate. This suggests that the oxygen concentration in the distilled sodium in sample #2 is less than 105.7 ppm. Although used primarily to measure oxygen concentration, neutron activation can also be used to measure the concentration of other nonmetallic impurities (James 1999). Fast neutron activation analysis (FNAA) has been used for nitrogen measurement by a (n,2n) reaction on N-14, producing the positron emitter, N-13. Nitrogen concentration is identified by way of a 511-keV annihilation peak. Nitrogen measurements by this method are only good at percent levels in most materials. Phosphorus can also be measured by a similar technique, but since it also produces a positron emitter, the two elements interfere with each other. If both are present at percent level, the decay curves can be resolved (10-minute versus 2.5-minute half-lives, respectively). Carbon cannot be measured with neutron activation analysis. Silicon can be measured with an (n,p) reaction at about the percent level. Calcium and copper can be determined most sensitively with reactor-based thermal neutron activation analysis (NAA) [generally called instrumental neutron activation analysis (INAA)]. At higher concentrations, copper can be measured with FNAA. As far as simultaneous measurements, oxygen is always separate. Nitrogen, silicon, copper, and, potentially, phosphorus could be measured in a single FNAA run, although it would be a complicated one. Copper and calcium could be determined in a single INAA run.

Symbols

Alphabetic

A	area
c_p	specific heat capacity
C_i	heat capacity of heat pipe
C_2	Second Planck's constant
d	diameter
$G_{R,i}$	radiation coupling of surface nodes to surroundings
h	heat transfer coefficient
m	mass
N	number
N_s	number of heat pipe segments
O	oxygen concentration
P	pressure
Q	energy transfer rate
$\dot{q}_i$	heat transfer rate between nodes
$\dot{q}_c, \dot{q}_R, \dot{q}_S, \dot{q}_V$	conduction, radiation, sonic, and viscous heat transfer rates
R	universal gas constant
T	temperature
T_i, T_i^p	temperature at present time, preceding time
T_∞	radiation background temperature
t	time
V	volume
x_i	axial position from evaporator entrance
z	axial position

Greek

ΔH	heat of reaction
$\Delta P_C, \Delta P_E, \Delta P_L$	condenser, evaporator, and liquid pressure drops
ε	total hemispherical emittance
γ	specific heat ratio
λ	wavelength
ν	kinematic viscosity
ρ	density
σ	Stefan-Boltzmann constant
τ	thermal time constant

Subscripts

a	actual
c	condenser or cross-sectional
F	at conclusion of reaction
I	initial condition
o	outside
O	oxygen
rad	due to radiation
RF	radio frequency
v	vapor
∞	background

References

- Addison, C. C. (1984) *The Chemistry of the Liquid Alkali Metals*, (Wiley, New York), p. 262.
- Busse, C. A. (1967) "Pressure Drop in the Vapor Phase of Long Heat Pipes," in *Proceedings of the 6th Thermionic Conversion Specialist Conference*, published by the IEEE, pp. 391–398.
- Dwyer, O. E. (1976) *Boiling Liquid Metal Heat Transfer*, (ANS, Hinsdale, IL), p. 84.
- Gubareff, G. G. (1960) "Thermal Radiation Properties Survey," Honeywell Research Center, Minneapolis, Minnesota.
- Hilditch, J. P., Hurley, J. R., Skeldon, P., and Tice, D. R. (1995) "The Liquid Metal Embrittlement of Iron and Ferritic Steels in Sodium," *Corrosion Science*, Vol. 37, No. 3, pp. 445–454.
- Holman, J. P. (1981) *Heat Transfer*, Fifth Edition, McGraw-Hill, New York.
- James, W. D. (1999) personal communication with Robert S. Reid.
- Klarstrom, D. L. (1996) "Heat Treat Cracking of Superalloys," *Advanced Materials and Processes*, April 1996, pp. H-2108–H-2111.
- Kemme, J. E., Deverall, J. E., Keddy, E. S., Phillips, J. R., and Ranken, W. A. (1975) "Performance Tests of Gravity-Assist Heat Pipes with Screen Covered Structures," *Progress in Astronautics and Aeronautics*, Vol. 39, pp. 201–213.
- Longson and Prescott found in Hamer, A. N. et al. (1973) *Proc. Int. Conf. On Liquid Alkali Metals* (British Nuclear Energy Society), p. 171.
- Reid, R. S., Sena, J. T., Merrigan, M. A., Elder, M. G. and Martinez, A. L. (1999) *Heat Pipe Development for Advanced Energy Transport Concepts Phase II Final Report Covering the Period October 1, 1997 to September 30, 1998*, Los Alamos National Laboratory Report LA-13549-PR.
- Schultheiss and Schmidt (1969) *J. Heat Transfer*, Vol. 91, p. 198.
- Siegel, R. and Howell, J. R. (1981) "Thermal Radiation Heat Transfer," Hemisphere Publishing Corporation, Washington, DC, p. 769.
- Vykhodets, V. B. and Kurennykh, T. Y. (1993) "Oxygen Diffusion in Hafnium," *Fizika Metallov Metallovedenie*, Volume 76, Number 1, pp. 174–176.
- Walpole, R. E. and Myers, R. H. (1978) *Probability and Statistics for Engineers and Scientists*, Macmillan, New York, pp. 473–487.
- Woloshun, K. A., Merrigan, M. A., and Best, E. D. (1988) *HTPIPE: A Steady-State Heat Pipe Analysis Program*, Los Alamos National Laboratory Manual, LA-11324-M.

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