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Progress Report

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Space Reactors

October 1981—March 1982

Los Alamos

Los Alamos National Laboratory
Los Alamos, New Mexico 87545

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Prepared by Donna Stevens, Group Q-13

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SPACE REACTORS
October 1981-March 1982

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ABSTRACT

Progress in design studies and technology for the SP-100 Project--successor to the Space Power Advanced Reactor (SPAR) Project--is reported for the period October 1, 1981, to March 31, 1982. The basis for selecting a high-temperature, UO_2 -fueled, heat-pipe-cooled reactor with a thermoelectric conversion system as the 100-kW_e reference design has been reviewed. Although no change has been made in the general concept, design studies have been done to investigate various reactor/conversion system coupling methods and core design modifications. Thermal and mechanical finite element modeling and three-dimensional Monte Carlo analysis of a core with individual finned fuel elements are reported. Studies of unrestrained fuel irradiation data are discussed that are relevant both to the core modeling work and to the design and fabrication of the first in-pile irradiation test, which is also reported. Work on lithium-filled core heat pipe development is described, including the attainment of 15.6 kW_t operation at 1525 K for a 2-m-long heat pipe with a 15.7-mm outside diameter. The successful operation of a 5.5-m-long, lightweight potassium/titanium heat pipe at 760 K is described, and test results of a thermoelectric module with GaP-modified SiGe thermoelectric elements are presented.

I. EXECUTIVE SUMMARY (W. A. Ranken, Q-13)

The SP-100 Project, a cooperative DOE/NASA program under which the work reported in this document was done, is the successor to the Space Power Advanced Reactor (SPAR) Project, for which progress has been documented in a series of quarterly progress reports dating from February 1979.

The SP-100 space power system is nominally a 100-kW_e power source designed for applications requiring high specific power and long life. In the current reference design concept, heat is generated by a nuclear reactor and converted to elec-

trical power by a thermoelectric (TE) converter/radiator array. The nuclear subsystem of the SP-100 power plant is a UO_2 -fueled reactor designed to supply 1600 MW_t to a TE conversion system by radiation from long, high-temperature heat pipes that emerge from the reactor core.

This document reports progress made in the design of the nuclear subsystem and in developing and demonstrating key technology required for building such a system with high reliability and a 7-yr lifetime.

Developments of particular importance and interest in the past 6 months are as follows:

- The selection procedure for the SPAR reactor design has been reviewed to determine whether the conclusions are still appropriate for the SP-100 Project. A UO_2 -fueled, heat-pipe-cooled reactor remains the choice for the current reference design.
- Design studies of a 1.6-MW_t, individual fuel module reactor are in progress. Fuel modules will be machined, complete with fins, from Mo-13Re bars and welded to Mo-13Re tubing. The current module design includes 12% free volume to accommodate fuel swelling.
- A study of the effect of the number of core heat pipes on reactor design has accentuated the importance of heat pipe wall thickness scaling relations. Of 12 factors that limit how thin the wall can be, it appears that wall material grain growth and pressure potentially exerted by irradiated fuel are the most important.
- A finite element thermal analysis of an individual SP-100 reactor fuel module has shown that fin temperature varies from 1510 to 1730 K under normal reactor operating conditions. An extension of the analysis to groups of fuel modules has shown that steady-state temperatures of fuel surrounding a failed interior heat pipe will reach a maximum value in the range of 2080 to 2180 K, depending on the fin alignment from one module to the next.
- A study has been made of available data on the swelling of unrestrained UO_2 irradiated at high temperature to burnup values in the range of 0.4 to 5.4%. An interpretation of this data has been made that correlates the various sets into a unified whole. Curves of volume increase versus burnup and temperature have been generated for use in the computer programs being developed to analyze fuel behavior in the SP-100 fuel modeling.
- Thermomechanical modeling of SP-100 core behavior is being done for both single fuel modules and a sector of the core. Initial calculations show that the fuel is able to distort molybdenum fins when the two are joined by a perfect bond. The analysis is being refined so that slippage between the fuel and the fin can be included in the model.
- Water immersion safety of the SP-100 reactor has been studied by using the three-dimensional Monte Carlo Neutron-Photon (MCNP) code to determine the nuclear criticality parameters of a number of different configurations in the removable central plug of neutron absorber material. The isotope ^{10}B has proven to be the most effective absorber material. The reactivity of the SP-100 reactor immersed in water with the reflector intact (and the control rods all in the "in" position) has proven to be several per cent higher than the immersion case where water replaces the reflector and control drums.
- Components of the in-pile test capsule, designed to irradiate SP-100 fuel under accelerated as well as nominal operating conditions, have all been fabricated except for the gas-filled heat pipe that regulates the fuel temperature. Molybdenum-13 rhenium material is being procured for the heat pipe to provide low-temperature ductility and eliminate the possibility of heat pipe fracture during handling of the experiment.
- A study of alternative reactor-converter coupling schemes has shown that advantages of the radiatively coupled system--one set of heat pipes, separability of reactor and converter systems, radiative cross-coupling of heat pipes, and avoidance of high-temperature insulators--offset the mass penalty associated with this system.
- SPAR-5, a lithium-filled, annular wick, TZM heat pipe, was filled and pretested on the tilt test system and subsequently operated at 15.6 kW_t and 1525 K in the full-power test chamber. Longitudinal cracking of the wick, thought to be due to a fabrication or testing artifact, was instrumental in preventing long-term testing of this unit. An experimental capsule containing a number of wick specimens has been placed under test in an attempt to identify the reason for molybdenum wick crack formation. Future wicks will be made of Mo-41Re alloy, which has the ductility needed to avoid the cracking problem.
- Thermochemical analysis of sodium and lithium heat pipes with impurities and getter

materials has shown that vapor transport of sodium molybdate can result in a molybdenum transport mechanism that is highly deleterious to the integrity of sodium heat pipes. Titanium, zirconium, and hafnium are all effective in tying up oxygen and preventing this corrosion cycle. This type of cycle does not occur in the lithium/molybdenum system.

- Tests of UO_2 in contact at 1800 K with molybdenum and TZM--with and without a chemically vapor deposited (CVD) tungsten coating--have shown no evidence of reaction for the molybdenum-rhenium and molybdenum specimens, but considerable reaction between UO_2 and TZM. Tungsten, molybdenum, and Mo-46Re all proved to be effective barriers for the diffusion of oxygen from UO_2 to hafnium metal discs.
- A 5.5-m-long, lightweight, potassium/titanium artery heat pipe has been successfully operated at 1.86 kW_t at a temperature of 760 K. Isothermal operation was attained and start-up with solid potassium in the arteries did not prove to be a problem.
- A TE module with GaP-modified SiGe TE elements was tested to a temperature of 975 K. The test results agreed very well with predicted performance. Heater difficulties and a broken current lead limited test temperature and duration.

II. REVIEW OF REACTOR CONCEPT SELECTION (D. Buden, DAD/NP/AR)

With the initiation of the SP-100 Project in October 1981, a review was made of the reactor concept to determine whether or not the reactor design should continue in the directions established by the SPAR Project. The requirements governing the nuclear subsystem selection are

- Power level (kW_t) 1400-1600
- Lifetime (yr)
 - Design 7
 - Overall 10
- Reliability No single-failure points
- Temperature to the converter (K) 1500
- Mass Minimum

The reactor core can be categorized in terms of neutron spectrum and heat-removal methods. A fast reactor remains the choice because of the relatively small amount of burnup experienced over its lifetime, as compared to the higher burnup of a thermal reactor. Fuel candidates include uranium (U), uranium-zirconium hydride (U-ZrH), uranium carbide (UC), uranium nitride (UN), and uranium dioxide (UO_2); the first two are quickly eliminated because of the temperature range of interest, and UN is eliminated because it must have a nitrogen overpressure and would require much greater development. The nitrogen overpressure also introduces a potential single-failure point. Uranium carbide and UO_2 are thus the prime fuel candidates.

The heat-transport candidates include electro-magnetic-pumped liquid metals, fluid turbopumps, and heat pipes. Figure 1 is a schematic diagram of these configurations. All of them can be developed to provide 1500-K temperatures to the converter, but the complexity of the methods is significantly different. None of them are currently developed technologies at 1500 K and 7-yr design life. The figure shows the complexity of pumped loops, whether electromagnetic or turbopump, required to minimize the number of single-failure points. Even then, there are potential single-failure points in the core flow path. Heat pipes, on the other hand, offer many parallel, independent heat-transport paths. Here, the major

Fig. 1. Comparison of heat transport configurations.

reliability problem is guaranteeing that there are no synergistic failures in the design, such as that the heat pipes surrounding a nonworking heat pipe will fail at an unacceptable accelerated rate.

Table I compares the three heat-transport systems. The more complex fluid systems require special coolant loops, valves, instrumentation, and accumulators or expansion tanks. High-temperature refractory metals are needed in all the systems. Pressure requirements are highest in the turbopump configurations, over a factor of 20 higher than a

heat pipe system. Electromagnetic-pumped system pressures are three times higher than heat pipes. The higher pressures mean higher material stresses.

The choice of fuel and the heat-transport system are interactive variables. Significant factors to consider in any decision include the desire to have the maximum feasible uranium density in the core to achieve minimum reactor size and the need to have a compatible barrier between the fuel and heat-transport fluid. The fuel density tends to be lower in fluid-cooled systems

TABLE I
COMPARISON OF THREE HEAT-TRANSPORT SYSTEMS

REQUIREMENTS: <ul style="list-style-type: none"> • TRANSFER HEAT TO CONVERTER AT 1500 K • 7-YEAR LIFETIME • NO SINGLE-FAILURE POINTS 		
GAS TURBINES	ELECTROMAGNETIC PUMPS	HEAT PIPES
FEATURES: <ul style="list-style-type: none"> BEARINGS ROTATING MACHINERY GAS CONTAINMENT LINES BELLOWS VALVES ACCUMULATOR COOLANT LOOP SPEED SENSORS FLOW SENSORS 	<ul style="list-style-type: none"> STATORS COMPLEX FLOW PASSAGES LINES VALVES EXPANSION TANK COOLANT LOOP FLOW SENSORS ELECTRICAL POWER CONDITIONING 	<ul style="list-style-type: none"> TUBING WICK
PRINCIPAL MATERIALS: <ul style="list-style-type: none"> MOLYBDENUM ALLOYS HELIUM STAINLESS STEEL 	<ul style="list-style-type: none"> MOLYBDENUM ALLOYS LITHIUM MAGNETIC ALLOY ALUMINA NICKEL-CLAD SILVER ARGON STAINLESS STEEL 	<ul style="list-style-type: none"> MOLYBDENUM ALLOY LITHIUM HAFNIUM
PRESSURE (N/cm²): 115	15	5
SINGLE-FAILURE POINTS: <ul style="list-style-type: none"> COMPLEX VALVE ARRANGEMENT DOUBLE JACKET LINES REDUNDANT COMPRESSOR 	<ul style="list-style-type: none"> COMPLEX VALVE ARRANGEMENT DOUBLE JACKET LINES REDUNDANT PUMPS AND POWER SUPPLIES 	
SUMMARY: <ul style="list-style-type: none"> HEAT PIPES SELECTED BECAUSE <ul style="list-style-type: none"> • SIMPLICITY • LIGHTWEIGHT 	<ul style="list-style-type: none"> • FABRICABILITY • ELIMINATE MECHANICAL PUMPS AND COMPRESSORS 	

because of the need to maintain fuel structural integrity and to immobilize the fuel against dimensional changes from fuel swelling. The heat-pipe-core configuration can use a higher density fuel because the fuel is allowed to crack or reconfigure during operation, and it can accommodate a

certain amount of swelling. A barrier coating is incorporated into fluid-cooled reactors to protect against fuel erosion and corrosion. The heat pipe walls perform this function in heat pipe reactors.

Table II compares reactor design characteristics, and Table III compares the fuel behaviors.

TABLE II
REACTOR DESIGN CHARACTERISTICS

- REQUIREMENTS:**
- PRODUCE 1600 kWt
 - 7-YEAR LIFETIME
 - NO SINGLE-FAILURE POINTS

FEATURES:

HIGH-PRESSURE CONTAINMENT VESSEL
DIRECTLY COOLED FUEL ELEMENTS
COMPLEX PERIPHERY
HELIUM-COOLED REFLECTOR
FLOW METERING SYSTEM
CORE SUPPORT SYSTEM
INTERNAL INSTRUMENTATION

LOW-PRESSURE CONTAINMENT VESSEL
DIRECTLY COOLED FUEL ELEMENTS
COMPLEX PERIPHERY
LITHIUM-COOLED REFLECTOR
FLOW METERING SYSTEM
CORE SUPPORT SYSTEM
PREHEAT JACKET
INTERNAL INSTRUMENTATION
REQUIRES WARM-UP PROVISION

FUEL TILES SEPARATE FROM COOLANT FLUID
CONDUCTIVE, RADIATIVE-COOLED REFLECTOR
PERIPHERY CONSISTS OF Mo CAN AND INSULATION

SINGLE-FAILURE POINTS:

NONE

LOSS OF FLUID CONTAINMENT

MICROMETEOROID PENETRATION
WELD AND BRAZE FAILURE
CREEP RUPTURE
FRACTURE OF BOLTS IN TURBO-MACHINERY
TURBO-MACHINERY HUB BURSTS
RADIATION-INDUCED MATERIAL DAMAGE
MANUFACTURING DEFECTS
BELLOWS SEAL LEAKS

MICROMETEOROID PENETRATION
WELD AND BRAZE FAILURE
CREEP RUPTURE
RADIATION-INDUCED MATERIAL DAMAGE
MANUFACTURING DEFECTS
BELLOWS SEAL LEAKS
CHEMICAL CORROSION BY COOLANT

LOSS OF PUMPING POWER

STRUCTURAL FATIGUE
TURBINE WHEEL RUBBING
COMPRESSOR IMPELLER RUBBING FROM
MISALIGNMENT OR VIBRATION
BEARING FAILURES FROM FRICTION
DURING STARTUP AND SHUTDOWN
FLOW-INDUCED VIBRATION
EROSION PRODUCTS
SEAL LEAKS

STRUCTURAL FATIGUE
SEAL LEAKS
FLOW-INDUCED VIBRATION

LOSS OF CRITICALITY

CORROSION OF FUEL BY IMPURITIES
(MINOR)

CORROSION OF FUEL BY LITHIUM (MAJOR)

EMERGENCY COOLDOWN AND RESTART

NEED LARGE ACCUMULATOR
IF RESTART DESIRED PLUS BATTERIES
FOR ACTUATORS

NEED POWER RESERVE
FOR PUMPS, LITHIUM WARMING
BLANKETS PLUS BATTERIES FOR
ACTUATORS

NEED BATTERIES FOR ACTUATORS

SUMMARY:

HEAT PIPE REACTOR SELECTED BECAUSE:

- NO SINGLE-FAILURE POINTS
- MORE READILY ACCEPTS RADIATION DAMAGE

- ELIMINATES PRESSURE VESSEL
- MINIMUM FUEL DEVELOPMENT

- SIMPLICITY OF DESIGN

TABLE III
COMPARISON OF UC AND UO₂ FUELS
(1600-kW_t Design)

	UC-10 at.% ZrC	UO ₂ -40 vol% Mo	UO ₂ -20 vol% Mo
<u>FUEL SYSTEMS</u>			
Relative reactor mass	0.94	1.59	1.0
Relative reactor diameter	0.97	1.14	1.0
Relative U-235	1.19	1.68	1.0
<u>FUEL FABRICATION</u>			
Handling	Inert atmosphere	No special problems	No special problems
Method			
Current	Arc melting	Press, sinter, and machine	Coated particles, press, heat, treat, and machine
Future	Extrusion and reaction sintering	Same	Same
<u>DESIGN</u>			
Relative fuel swelling	2.9	0.02	1.0
Mo-fuel compatibility			
Thermal	Mismatched	Matched	Matched
Chemical	Interactions start at 1475 K	None up to 2475 K	None up to 2475 K
<u>TECHNOLOGY</u>			
	Less developed (Rover and LMFBF)	Well developed (LWR and LMFBF)	Moderately developed (LWR and LMFBF)

The design selected is a heat pipe reactor design, irrespective of the electric power converter. Its advantages are

- inherent high reliability because of redundancy in the design;
- elimination of single-failure points;
- the ability to accept material swelling, radiation damage, and other environmental effects without loss of power;
- elimination of the need for a pressure vessel or mechanical pumps;
- minimization of development cost because of the modular configuration;
- reduced susceptibility to fuel erosion and corrosion deterioration;
- capability to supply heat to different conversion systems; and

- elimination of the need for special decay-heat-removal systems.

Fuel development emphasis is on UO₂-20 vol% Mo alloy material. Its advantages are

- minimization of overall program cost;
- compatibility with other core materials;
- more highly developed fabrication processes; and
- processability in air.

III. POWER PLANT SYSTEM

A. Reactor Design (K. L. Meier, Q-13)

First results have been obtained in the design of a 1.6-MW_t, individual-fuel-module reactor. The 120-heat-pipe reactor is shown in Fig. 2. The fins, collar, and core portion of each heat pipe

Fig. 2. Heat pipe space reactor.

are electrical discharge machined (EDM) from solid Mo-13Re bar stock and welded to the 8-m-long container tube of the heat pipe. This fabrication procedure is made feasible by the demonstrated ductility of welds in this alloy. The collars interlock together and are held axially in place by three tiedown bars. Corrugated and bellville spring washers fit between the collars to prevent relative movement and mitigate launch vibration effects. The individual modules have studs at the bottom end that fit into holes in the base of the molybdenum core container. The individual fuel modules are thus held tightly at both ends of the container. The container has a flange at its upper end that is clamped between rigid ZrO_2 insulators, which are, in turn, clamped between portions of the circumferential beryllium reflector. The ZrO_2 insulator reduces core heat loss and protects the beryllium from the very high core temperatures.

A 72-mm-diam safety plug resides in the center of the reactor during launch. This boron or B_4C plug, enriched in ^{10}B , maintains the SP-100 reactor in a subcritical state even when it is immersed in water with all heat pipes broken and filled with water. The plug is removed once a successful orbit

has been achieved and a second plug of BeO is inserted.

The top reflector shown in Fig. 2 consists of BeO spheres packed around the heat pipes outside the core. This is a design alternative that makes use of the region between core and shield and effects a 40- to 50-kg mass reduction by moving the shield closer to the core. Unresolved questions such as containment of the spheres, their packing fraction, and heat transfer have kept this alternative subordinate to the current reference design, shown in Fig. 3, where the BeO is inside the core. The relatively large, solid BeO tiles that form the top reflector in the reference design circumvent the problems of the spheres but increase shield mass.

An important feature of the individual module design is the relative ease with which a damaged heat pipe can be removed and replaced during or after reactor disassembly. Removal of one tiedown bar and a few modules is all that is necessary to replace a defective module and heat pipe.

Fuel dispersal on atmospheric reentry is greatly enhanced by the individual module concept. After the compression bands burn, the beryllium reflector is removed by leaf springs and

Fig. 3. SP-100 modular core reactor.

atmospheric turbulence. The core container then burns, and the core separates into 120 individual pieces. Previous cross-core fin designs held the entire core together, exposing only the periphery to the atmosphere and thus casting doubts as to the ability of the bulk of the fuel to disperse upon reentry.

Details of the fuel/fin/heat pipe region of the individual module design are shown in Fig. 4. The size and shape of the modules vary according to the row; that of the inner row is shown. The area equivalent square of all modules is $x_m = 25.39$ mm on a side. A gap, $x_g = 0.125$ mm, exists between adjacent modules. Fuel tile thickness $\Delta = 2.14$ mm to maintain a 4 to 1 fuel-to-fin volume ratio. Fin thickness $T = 0.5$ mm except for the half-thickness fins bounding the axial fuel swelling gap, which occurs every 10 fuel tiles.

Fuel swelling volume is 12% of the total 100% dense UO_2 volume. The actual fuel density is assumed to be 97% of theoretical. The swelling volume is composed of the following:

• Module clearance	2.0%
• Edge chamfer	2.0%
• Top bevel	0.8%
• Heat pipe clearance	0.6%
• Axial fuel gap	3.0%
• Radial fuel gap	3.6%
Total	12.0%

B. Analysis of Effect of Number of Core Heat Pipes on the SP-100 Design (K. L. Meier, Q-13)

An analysis has been made of the effect of the number of core heat pipes on the SP-100 design. The heat pipes in this analysis were designed with the computer code HTPPIPE. They were designed over a broad range of sizes from 5-mm to 23-mm i.d. in both artery and annular wick configurations. HTPPIPE predicted the limit power per heat pipe and the total number of pipes was then calculated for a 1600 kW reactor. A design margin factor of 1.5 is used in this determination, primarily to assure that neighboring heat pipes have the capability

Fig. 4. SP-100 fuel and fin detail.

to carry the extra heat load they would receive if a given heat pipe were to fail. The curves of Fig. 5 show limit power as a function of heat pipe inside diameter and annular gap for an annular screen tube wick heat pipe. Optimum design occurs at maximum power for a sharply peaked curve or slightly to the left of maximum power for a broad-peaked curve. In the latter case, a small power reduction can effect a mass saving by reducing lithium inventory. The limit power vs artery diameter and distribution wick thickness for a 23-mm-i.d. heat pipe shown in Fig. 6 is typical of the plots for an artery pipe. Optimum design for both power and mass is obtained at a wick thickness of 0.6 mm and an artery diameter of 3 mm, assuming a four-artery wick.

The behavior of limit power with heat pipe internal diameter is shown in Fig. 7. The rapid decrease in limit power per heat pipe with decreasing size is sufficiently rapid to give an advantage to larger pipes. The power per unit mass, as shown in Fig. 8, also decreases with size as well as with wall thickness. Vapor area decreasing as the square of the diameter is the primary cause for this effect, but proportionately larger

Fig. 5. Annular wick heat pipe power vs annular gap and heat pipe inside diameter.

Fig. 6. Artery heat pipe power vs artery diameter and heat pipe inside diameter.

Fig. 7. Limit power vs heat pipe inside diameter.

Fig. 8. Heat pipe design power per unit heat pipe mass vs heat pipe inside diameter; constant wall thickness.

fluid boundary layer frictional effects are also important. It is apparent in Fig. 7 that the difference between the artery and annular heat pipe power transport capability is small. This small difference is also apparent in Fig. 9, which is a plot of total heat pipe mass versus heat pipe number. Because of the parity of the two wick designs, no further distinction will be made in this analysis. The artery design, however, has several advantages not apparent in this treatment, such as redundant liquid flow passages, high boiling limit, and lower radial temperature drop.

Table IV shows the relationship of heat pipe inside diameter to design power and number.

1. Mass Analysis. Because system mass is very important in all space satellite applications, the effect of number of heat pipes on heat pipe and reactor mass was examined first. Both the container tube and wick material were taken to be Mo-13Re; the working fluid was lithium at 1500 K. Wick and lithium masses were determined by HTPipe and are a small portion of total heat pipe mass. Wall thickness is the main

Fig. 9. Total 8-m heat pipe mass vs number of heat pipes; constant wall thickness.

TABLE IV
RELATIONSHIP OF HEAT PIPE INSIDE DIAMETER
TO DESIGN POWER AND NUMBER

Inside Diameter (mm)	Design Power (kW)	Number
23	40	40
19	26.7	60
14.1	13.3	120
9	4.0	400
5	1.0	1600

parameter in determining the mass of an 8-m-long heat pipe.

If the criterion is that the container tube wall must be of sufficient thickness to withstand the lithium vapor internal pressure at 1500 K with 1% creep deformation in a 7-yr lifetime, then a 14-mm-i.d. pipe requires a 0.09-mm (0.003-in.) wall. This very thin wall is all that is required because the lithium pressure is only about 0.5 atm.

This extremely thin tube wall is sufficient to withstand the lithium operating pressure but is too thin to withstand fabrication and handling stresses, quality assurance (QA), shuttle emer-

gency conditions, and other factors. Table V lists a number of factors that put a lower limit on wall thickness and gives estimates of the magnitude of these limits. Some wall thicknesses are diameter dependent and are calculated for a 14.1-mm i.d., the SP-100 reference design. Item 13 of Table V is the approximate sum of tube-drawing tolerance, shuttle emergency landing bending stresses, and atmospheric pressure buckling load. This scenario indicates that the heat pipes could bend during an emergency shuttle landing if the wall thickness was not greater than 0.18 mm. Item 9 indicates that Experimental Breeder Reactor-II (EBR-II) fuel capsule assembly experience has shown that, for good reliability and low rejection rate after installation, a wall thickness of 0.38 mm on a stainless steel (SS) tube 10-mm diam is required. The wall thickness required will be greater for molybdenum-rhenium tubing. Because of present uncertainties, a safety factor of 2 was applied to item 9 to derive the reference design wall thickness.

Although item 11 indicates that the wall must be 0.85 mm thick to yield an average grain size after 7 yr equal to the wall thickness, the

TABLE V
MINIMUM WALL THICKNESS CONSIDERATIONS

Factor	Wall Thickness Limit (mm)
1. Atmospheric pressure hoop stress	0.004
2. Lithium internal pressure hoop stress (1500 K)	0.06
3. Drawing tolerance	+0.03
4. Shuttle emergency landing bending load	0.10
5. Atmospheric pressure buckling	0.08
6. Welding QA	0.13
7. Minimum wall thickness presently manufactured	0.25
8. Rocket nozzle tubes, SS	0.25
9. EBR-II fuel capsule cladding, SS (good QA, 10-mm diam)	0.38
10. Bend without solid lithium fill	0.38
11. Grain size ~ wall thickness in 7 yr	0.85
12. Add items 3, 4, 5 (approx.)	0.18
13. Safety factor of 2 on item 9	0.75

equations used to calculate this thickness are strictly generic and are subject to large errors because the items in the equation are not specifically known for molybdenum-rhenium.¹ Specific tests on Mo-13Re are required to determine the necessary parameters and improve the accuracy. Tests are also required to determine if grain size comparable to wall thickness is a stable configuration in terms of containing lithium vapor.

In summarizing the analysis, the range of interest for heat pipe dimensions extends from 9- to 23-mm i.d. and 0.25- to 1.0-mm wall thickness. The heat pipe number for potentially practical systems is below 500. The total 8-m heat pipe mass vs number of heat pipes is shown in Fig. 9 for the aforementioned range. The reference design 120-heat-pipe mass is 450 kg. The curves show an increasing total heat pipe mass with increasing number of pipes and clearly indicate a mass advantage for low numbers.

An analysis was also made of the effect of number of heat pipes on reactor mass. The reactor size and mass were calculated by the computer code SYSTEM and are for a homogenous, two-dimensional core. These calculations are not as exact as a Monte Carlo calculation but are sufficient for an analysis such as this, which is meant to show trends more than absolute values. As can be seen from Fig. 10, reactor mass is a weak function of heat pipe number. The reference design mass is 370 kg.

When the reactor plus total heat pipe mass is determined as a function of number of heat pipes, the result is that shown in Fig. 11. A mass advantage is obtained for low numbers of heat pipes.

It is conceivable that fuel swelling could induce a compressive hoop stress on the heat pipe tube in the core region. The magnitude of any such stresses can only be estimated at this time. This has been done by assuming a range between 3.5 and 14 MPa (500-2000 psi), as shown in Fig. 12. This figure indicates the wall thickness can be reduced for small diameters and large numbers of heat pipes. However, other considerations will dictate a minimum value for wall thickness. The compressive stress increases the required wall thickness for small numbers of heat pipes, reducing the mass advantage that would occur in the

absence of fuel swelling curves. The actual design may follow a compound curve as shown in the shaded region of Fig. 12.

2. Fuel Swelling Analysis. Average reactor temperatures and neutron fluences are predicted by SYSTEM and can be coupled with the fuel swelling data of H. Zimmermann^{2,3} to yield a graph of fuel swelling vs number of heat pipes. As shown in Fig. 13, average fuel swelling for a factor of safety on limit power of 1.5 is below 10 vol% at heat pipe numbers greater than 100. Below 100 the distance between heat pipes increases rapidly. Consequently, average temperature and fuel swelling increase to large values. The reference design allows for 10 vol% fuel swelling (with an additional 2% margin of safety), which precludes the use of less than 100 heat pipes unless additional design changes are made to reduce swelling. Because mass increases rapidly with increasing number of heat pipes and fuel swelling decreases little at high numbers, 200 is a reasonable upper limit.

It is possible to reduce the amount of fuel swelling below 10 vol% for heat pipe numbers less than 100. This is accomplished by using heat pipes that are much larger than the optimum size prescribed by HTPIPE. Although this increases the mass of the individual heat pipes in the reactor, as Fig. 14 shows, a minimum total mass exists in the range of 60 to 90 heat pipes. For these low numbers, the heat pipe diameters required to keep fuel swelling below 10 vol% are 10 to 20% greater than the optimum described by HTPIPE. An obvious advantage to choosing a design in this range is minimum mass, but lower heat fluxes and pressure drops may also produce advantages in greater heat pipe reliability. A disadvantage to using this relatively low number of heat pipes is that the increase in fuel temperature that would occur in the event of a heat pipe failure will become more pronounced. Thermal analysis will be performed to determine the magnitude of this increase. Another disadvantage is that fewer heat pipes mean less radiating area to transport heat to the TE converter array; hence, the required temperature drop will be larger.

3. Handling Stresses. Prediction of heat pipe handling stresses is not straightforward.

Fig. 10. Reactor mass vs number of heat pipes; constant wall thickness.

Fig. 12. Reactor plus total heat pipe mass vs number of heat pipes; constant fuel swelling pressure and wall hoop stress.

Fig. 11. Reactor plus total heat pipe mass vs number of heat pipes; constant wall thickness.

Fig. 13. Fuel swelling volume vs number of heat pipes for various wall thicknesses and factors of safety.

Fig. 14. Reactor plus total heat pipe mass vs number of heat pipes for constant wall thickness and fuel swelling $<10\%$.

Practical experience with materials, lengths, and wall thickness is required. Scenarios of handling can be postulated and stresses calculated, but closer spaced supports or more gentle handling conditions can always be mandated to reduce those stresses. Practical experience with 8-m-long heat pipes will eventually be required to determine proper scenarios. Nevertheless, the stresses in an 8-m heat pipe can be calculated using the scenario of lifting the assembled pipe from both ends. Under the influence of its own weight, the bending stresses developed in an assembled heat pipe are shown in Fig. 15. Because the moment of inertia increases much more rapidly with diameter than weight, large-diameter heat pipes develop low bending stresses for the same wall thickness. A 14.1-mm-i.d. pipe can be handled in this manner without yielding if its wall is greater than 0.4 mm, but a 12-mm-i.d. pipe must have a wall thickness of 0.9 mm. A 9-mm-diam pipe cannot be so lifted without yielding even if it is a solid

Fig. 15. Heat pipe handling stress vs wall thickness and heat pipe inside diameter.

rod. Because the heat pipes are so long, ease of handling will be an important design consideration. The prediction is that there is an advantage to larger, fewer heat pipes.

4. Conclusions. Although as yet incomplete, this analysis has produced several conclusions about the number and size of heat pipes to be considered in the SP-100 design. The number of heat pipes should be between 50 and 200 to keep the total mass near a minimum and fuel swelling below 10 vol%. Fabrication, QA, and grain size considerations suggest wall thicknesses in the range of 0.4 to 0.9 mm. Number of heat pipes and handling considerations favor diameters in the range of 23- to 12-mm i.d. The present reference design of 120 heat pipes, 14.1-mm i.d., 0.75-mm wall thickness is near the middle of these ranges.

C. Thermal Analysis of the Reactor Core (K. C. Cooper and M. B. Wray, Q-13)

A thermal analysis of the 120-heat-pipe, 1.6-MW_t reactor core design was made. The

temperatures in the fins were calculated by the finite element method. Fuel center-plane temperatures were obtained by adding the axial conduction drop of 30 K to the local fin temperature. A typical operating heat pipe was calculated, then the interface resistance between adjacent fuel modules, and finally a failed heat pipe with its surrounding operating modules. The maximum power density location was assumed, with a 1.2 maximum/average power ratio.

Figure 16 shows an axial cross section of a fuel module. Isotherms and heat flux arrows show that the heat flow in the fuel is predominantly to the fins. In the following calculations, the fin conducts the heat generated in the fuel radially to the heat pipe. The radial conductance of the fin is augmented by that of the fuel. The thermal conductivity of the Mo-13Re fin was taken as 83 W/m-K and that of the UO_2 fuel as 2 W/m-K. With the fuel volume about four times that of the fin, the fuel's radial conductance is one-tenth that of the fin. The fin is thermally bonded to the heat pipe, but there is a gap between the fuel and the heat pipe.

The mesh used in the finite element calculation of a typical fuel module is shown in Fig. 17. Isotherms and corner temperatures are shown in Fig. 18. The maximum temperature is 1730 K. The temperature at the junction of the heat pipe and the fin was taken as 1510 K. This allows for a

10-K drop across the heat pipe wall to the design temperature of 1500 K.

Next, a typical heat pipe was assumed to have failed, as shown in Fig. 19. The failed module is surrounded by unfailed heat pipe modules as shown in Fig. 20. The geometry was simplified so that a 90° section of the failed heat pipe module and its surrounding modules could be modeled.

The thermal resistance of the gaps between modules was then calculated. Figure 21 shows the three cases of gap geometry modeled. In all cases, because of the high temperatures in the failed module, the UO_2 was assumed to have evaporated and redeposited so as to fill the gaps between the failed and unfailed modules. (Based on a previous analysis, this migration takes place in the first day after failure.)

The zero-gap geometry represents the nonmodular design, with continuous fins from heat pipe to heat pipe. In the modular design the in-line fin is the most favorable geometry for intermodule transfer of heat in that the temperature drop across the assumed 0.12-mm gap is minimized. The least favorable geometry is where the fins are staggered. The finite element, two-dimensional analysis of the gap conductance includes the effect of two-dimensional heat flow in the UO_2 . With the gap conductance characterized, the temperatures in the fins of the failed module and the surrounding modules were calculated.

Fig. 16. Module cross section through axis.

Fig. 17. Fuel module cross section; finite element mesh.

Fig. 18. Fin temperatures, single module; maximum heating location.

Fig. 19. Failed heat pipe location.

Fig. 20. Failed heat pipe and surrounding modules.

Figure 22 shows the isotherms and significant temperatures in the continuous fins. The outer surface of the neighboring heat pipes was taken as 1530 K. This increase from the normal 1510 K is because the neighboring heat pipes have to radiate one-sixth more heat at the condenser end than they did before the failure occurred. The maximum fin temperature is 1970 K.

The in-line fin temperatures for the modular geometry are shown in Fig. 23. The maximum temperature, 2050 K, is 80 K higher than for the continuous fin. The staggered fin geometry is shown in Fig. 24. The maximum fin temperature has gone up to 2150 K, 100 K higher than for the in-line fin.

The maximum fin temperatures are summarized in Fig. 25.

D. Core Thermomechanical and Swelling Modeling (S. Girrens and C. Anderson, Q-13)

The objective of this task is the development of a modeling capability that is adequate to predict local and overall thermomechanical and swelling behavior of the SP-100 core configuration. The capability will utilize currently accepted models of UO_2 creep and swelling behavior, and will be benchmarked against in-pile test data. During the past 6 months we have been involved in surveying available computer codes, identifying creep and swelling properties of molybdenum and UO_2 for use in the thermomechanical models, carrying out some preliminary creep and swelling calculations on an axisymmetric fuel/fin/heat pipe model, and developing a planar 60° core sector model.

Fig. 21. Gaps between fins of adjacent modules; UO_2 fills gaps after initial transient.

Fig. 22. Failed heat pipe fin temperatures, continuous fin configuration.

Fig. 24. Failed heat pipe fin temperatures, staggered fin configuration.

Fig. 23. Failed heat pipe fin temperatures, in-line fin configuration.

Fig. 25. Summary: maximum fin temperatures (K). (Add 30 K for fuel.)

1. Survey of Codes. We carried out a brief survey of codes capable of predicting creep and swelling; the codes surveyed included SANGRE, ADINA, ABAQUS, and LIFE. It appears that any thermomechanical code that is capable of handling large deformations and nonlinear creep can be modified to predict swelling. (The modification is relatively straightforward and consists of adding another strain increment term to the thermal expansion and creep strain increments.) If the code also possesses the capability to model complex geometries, then it would be adequate to model SP-100 core thermomechanical behavior. All of the codes surveyed possessed a strong material-modeling capability. ABAQUS and ADINA provide the greatest capability for modeling three-dimensional effects, SANGRE is restricted to two-dimensional (plane and axisymmetric) geometries, and LIFE is tied to conventional power reactor fuel pin configurations. Table VI summarizes the capabilities of the codes surveyed. Because of its availability at Los Alamos, we have selected SANGRE for preliminary modeling of SP-100 thermomechanical and swelling behavior. In addition, we plan to purchase ABAQUS for modeling the three-dimensional swelling behavior.

2. Creep and Swelling Data. The swelling behavior of the fuel will dominate the long-term structural behavior of the core, and will have the greatest impact on the core design. At present we have two sets of swelling data that we are using to represent UO_2 swelling. In Fig. 26 we show the swelling data of Zimmermann²⁻⁵ and also data compiled at Battelle Institute⁶⁻¹¹ on UO_2 fuel contained in W-26Re cladding. The latter data

TABLE VI
CAPABILITIES OF CANDIDATE FUEL SWELLING CODES

	ABAQUS	ADINA	LIFE	SANGRE
Dimensionality	3D	3D	1D	2D
Large deformation	Y	Y	N	Y
Nonlinear creep	Y	Y	Y	Y
Fuel cracking	N	N	Y	N
Interface models (sliding, contact conductance)	Y	N	Y	N
Fuel swelling	Y	N	Y	Y

Fig. 26. Volumetric swelling rate vs temperature for UO_2 .

have been corrected for the fact that the reference temperature was taken as the clad temperature, even though the temperature was not uniform as a function of radius.

Creep data for UO_2 and molybdenum are shown in Fig. 27 as a function of stress and temperature. The fuel creep consists of a Nabarro-Herring component, which depends linearly on stress, and another component depending on stress to the 4.2 power; the power dependence of molybdenum creep on stress is 5.7. Both creep relations exhibit an Arrhenius dependence on temperature.

3. Preliminary Calculations and Models.

Figure 28 illustrates the axisymmetric fuel/fin/heat pipe representation that we have used in some preliminary calculations. Of great importance to the results is the assumption that the fins are perfectly bonded to the fuel, and that there is a gap between the fuel and the heat pipe. The outer boundary is restrained against radial motion and at time zero the nuclear heating is turned on, which gives the temperature field shown in Fig. 29 and a stress field (not shown) induced by the

Fig. 27. Creep strain rate vs stress for molybdenum and UO_2 .

Fig. 28. Axisymmetric fuel/fin/heat pipe finite element model.

differential thermal expansion between molybdenum and fuel.

Using the Zimmermann swelling data, we calculated the mechanical response of the system out to 5.3 yr. Figure 30 illustrates the deformed shape at the final time. Because of the greater creep stiffness of the fuel compared to molybdenum and because of the perfect bond at the fuel/fin interface, the fuel drives the fin inward and loads the heat pipe, which also deforms inward as shown. There is an accompanying small amount of axial deformation and bending of the fin, but

Fig. 29. Steady-state temperature distribution in fuel/fin/heat pipe model.

Fig. 30. Fuel swelling calculation, outer boundary constrained (time = 5.3 yr).

virtually no fuel deformation into the gap between heat pipe and fuel. Figure 31 shows the heat pipe and fuel hoop stresses and the heat pipe inner radius as a function of time.

More accurate prediction of fuel motion will require a better interface model for fuel and fin that can reflect sliding, friction, and thermal contact resistance. This code modification is currently being worked on. Further refinements in accuracy will include the use of Mo-13Re creep data and the inclusion of irradiation creep in modeling UO_2 behavior.

Fig. 31. Heat pipe stress, fuel stress, and heat pipe inner radius displacement vs time for axisymmetric fuel swelling calculation.

Figure 32 illustrates the planar (r, θ) model being developed to study fuel swelling and heat pipe deformation in a 60° sector. The ABAQUS code will be used to carry out the calculation.

E. Neutronic and Safety Analysis (R. J. Bartholomew, Q-13)

The design of the SP-100 reactor requires a neutronic analysis to determine safety-related considerations arising from the measure of time changes in the neutron population of the reactor

core or some of its subassemblies. The safety requirements arise from conditions affecting the public in general or personnel who are in proximity to the core during various stages of fabrication and assembly, transportation, and testing, as well as in possible accident environments such as reactor impact from crashes, water immersion, launch explosion, or atmospheric re-entry.

These safety considerations are included in the discipline of criticality safety, and the analyses that follow involve calculations of k_{eff} (which is a measure of the ratio of the average number of neutrons existing in each "generation" to the average number that existed in the immediately previous generation).

The following definitions are used throughout:

- $k_{eff} < 1$ implies that the reactor or subassembly is subcritical, and the neutron population dies out with time.
- $k_{eff} > 1$ implies supercriticality, and the neutron population increases unboundedly with time.
- $k_{eff} = 1$ implies criticality, and the average neutron population remains exactly the same for all time.
- $\rho \equiv (k - 1)/k$ is the reactivity.

Fig. 32. SP-100 core 60° thermomechanical model.

The constraints on criticality safety that are currently assumed are:

- (1) the core must be capable of achieving a k_{eff} of ~ 1.05 at beginning of life (BOL) so that an excess reactivity ρ_{ex} of ~ 0.05 is available to overcome the negative temperature coefficient of reactivity, the increased neutron absorbing effects of fission product build-up, and the depletion of fissile fuel (^{235}U).
- (2) The control shutdown margin (ρ_{sm}) with absorbing (neutron "poisoning") controls fully inserted must be sufficient to reduce the reactor from its operational critical ($k_{eff} = 1$) condition to definitely subcritical ($k_{eff} < 1$) during its entire operational lifetime (7 yr).
- (3) The control worth ($\Delta\rho_C = \rho_{ex} - \rho_{sm}$) must be sufficient to shut down the reactor or make required power level adjustments during the operational lifetime of the reactor.
- (4) The reactor must be subcritical in all preoperational environments requiring personnel handling or close proximity.
- (5) The reactor must be subcritical on launch until achievement of a stable orbit even in case of accidental geometry deformations that might occur from launch explosion or impact forces, if launch abortive procedures are used.
- (6) The reactor must be subcritical in water immersion accident environments.

In this progress report, the k_{eff} calculations are presented for all constraint conditions except item 5 above. The specific geometries resulting from explosion shock waves and/or impact forces need to be assessed from shock hydrodynamic and structural dynamic models of the accident phenomena before quantitative assessments of k_{eff} can be made.

Because k_{eff} is a statistical (or stochastic) measure of neutronics, it is natural to consider a mathematical model that simulates the stochastic nature of the neutron population, even though a deterministic model can also approximate

the physical process involved. Such a model employs the Monte Carlo (or random walk) stochastic process. The computer code used in the analyses is the MCNP code. This code has features that include

- a three-dimensional geometry (without finite element or finite difference discretization approximations) that allows modeling of core heterogeneities caused by the presence of heat pipes and, thus, allows for neutron streaming;
- utilization of continuous energy cross-section files allowing for good resolution of resonances and not requiring energy grouping or cross-section collapsing as in discrete multigroup calculations; and
- provision of a k_{eff} calculation tool that is well benchmarked by actual experimental critical assemblies.¹²

The specific analyses include annular cores consisting of 84, 120, and 162 heat pipes, initially sized by the deterministic neutronics computer code TWOTRAN, coupled with power level, heat removal, and void fraction parameters in the computer code SYSTEM to give a system designed reactor k_{eff} equal to ~ 1.05 at BOL. SYSTEM also gives an estimate of fuel depletion and swelling volume. The MCNP models take full advantage of the 1/12-sector angular symmetry of the annular core, and they allow for insertion of various materials like a central plug to absorb neutrons in the reactor for criticality safety reasons, and by an operational environment replacement plug to enhance neutron production, thus reducing required core size. The code also allows for easy utilization of different nuclide mixing of various materials that might be considered for heat pipes, working fluids, fuel-fin mixtures and combinations, and for reflector and control absorber materials.

Figure 33 shows the computer geometry for a typical MCNP model of the SP-100 120-heat-pipe annular reactor. Figure 33(a) shows a radial (XY) plane through the center of the core perpendicular to the cylindrical Z-axis, and Fig. 33(b) is the axial (YZ) plane, showing the vertical geometry of the three-dimensional model. The implication of

(a) Radial plane of 3D model.

(b) Axial plane of 3D model.

Fig. 33. SP-100 120-heat-pipe MCNP neutronics model for parametric studies.

the reflecting boundaries is that any neutron that impinges on these boundaries is reflected back into the cell at its incidence angle, without change in velocity, thus exactly simulating the neutron track into material identical with that on its original (unreflected) track. Thus the number and kind of neutron interactions are unchanged from those of a complete (full circle) model, the k_{eff} is unchanged, and we have considerably reduced the number of cells required to model the core.

To maintain required symmetry, we calculated k_{eff} for the rotatable segments of B_4C in the fully out position to get ρ_{ex} and the fully in position to get ρ_{sm} , both at BOL. The MCNP calculations sized the reactor to give $\rho_{ex} = 0.05$ and $\rho_{sm} = -0.08$. The fuel-fin elements that surround the heat pipes are homogenized. The UO_2 fuel is 93 at.% enriched in ^{235}U and has an effective density 85% of theoretical. Its volume fraction is 0.8 of the total fuel-fin volume. The fin material was at 100% theoretical density with a volume fraction of the total fuel-fin volume of 0.2. There are two ring annular cylindrical cans of the same material as the fin comprising the inner and outer walls of the core can as well as the top and bottom surfaces. An annulus of multifoil (0.6 g/cm^3 homogenized molybdenum) is used to represent insulation.

The control plug consists of four wedge-shaped cells that may be varied in relative radial dimension. These cells can be modeled with various materials (H_2O , B_4C , europium, gadolinium, BeO) to determine their effects on k_{eff} .

Because we do not require complete homogenization of the fuel/fin/heat pipe core, as in TWOTRAN, we can account for neutron leakage paths (streaming) along each of the heat pipe longitudinal axes. Streaming effects are expected to become significant as the number of heat pipes is increased (because of the greater leakage). It is also expected that axial and radial power distributions will be affected (since these are spatially dependent) by the heterogeneities of the heat pipes.

The 84-heat-pipe configuration is identical with the 120-heat-pipe configuration except that the outermost row of heat pipes is eliminated from the 120-heat-pipe configuration, and the heat pipe inside diameter is increased to accommodate the

same heat-removal requirement. For the 162-heat-pipe configuration, a sixth, additional row of heat pipes is added to the 120-heat-pipe configuration, with consequent reduction in heat pipe inside diameter.

For all k_{eff} calculations, initial (trial) core diameters were selected to give approximately $k_{eff} = 1.05$ as determined by prior calculations with TWOTRAN. Depending on whether the MCNP k_{eff} was less than or greater than 1.05, an additional new core diameter was then selected and the MCNP run repeated. Assuming that Δk between runs is constant (for small changes in core diameter), the core diameter required to give $k_{eff} = 1.05$ at BOL was obtained by straight line interpolation or extrapolation as required. Table VII presents the effects of heat pipe number on core diameter required and BOL criticality safety margin [$\rho_{csm} = (k_{eff} - 1)/k_{eff}$] for the fully assembled reactor with reflector and controls and with a B_4C control plug inserted for the dry condition. The required core diameter to give $k_{eff} = 1.05$ at BOL was determined for both an operational central void (after the poison plug was removed) and for the case where the poison plug is replaced by a BeO plug to be inserted to reduce the neutron leakage. It is observed from Table VII that required core diameter increases and criticality safety margin decreases with increased number of heat pipes.

The MCNP models for water immersion studies are identical with the dry models except for the addition of 1 m of water surrounding the reactor. (One meter is considered nearly equivalent neutronically to an infinite immersion sink.) Fig. 34(a) shows the radial plane model, and Fig. 34(b) shows the axial plane model. The central plug is always 7 cm in diameter, and various neutron-absorbing materials are evaluated to determine their effects on k_{eff} . The water immersion condition assumes that all void spaces have been filled with ordinary fresh water, including the heat pipes, voids in fuel, and spaces between the fuel and the fins. Two conditions were evaluated:

- the reflector is intact with control elements in the shutdown position (fully inserted absorber), and

(a) Radial section of 3D model.

(b) Axial section of 3D model.

Fig. 34. SP-100 central plug MCNP neutronics model for water immersion criticality safety studies.

TABLE VII
HEAT PIPE NUMBER EFFECTS ON REQUIRED CORE SIZE AND
CRITICALITY SAFETY MARGINS
(DRY CONDITIONS)

Number of Heat Pipes	Core Diameter Required Center Void (cm)	BOL Criticality Safety Margin [(K-1)/K]	Core Diameter Required Center BeO Plug (cm)	BOL Criticality Safety Margin [(K-1)/K]
84	32.64	-0.15	32.08	0.21
120	33.075	-0.15	32.56	-0.168
162	33.5	-0.15	33.14	-0.167

- the reflector is jettisoned so that only the bare (unreflected) core is in contact with the water.

The various materials evaluated as candidate central core plug materials were selected based on their neutron thermalization capability and capture cross section.

One concept involved the utilization of a thin ring of highly absorbing material (gadolinium or europium), inside of which is empty space that fills with water upon immersion. The idea of this concept is that the fast neutrons (averaging 2 MeV) born in the fissile fuel would enter the central region by passing through the thin ring relatively easily because of the low cross section at 2 MeV. These fast neutrons would then be thermalized in the water and subsequently captured because of the higher absorption cross section in the ring material for low neutron energies. The other concept involved the use of a solid material that would have good absorption properties at all energies.

Table VIII presents the values obtained for k_{eff} for an annular core with and without a BeO replacement plug and for water-immersed cores with various arrangements of the central poison plug. The reflector was considered intact with all absorbing elements fully inserted. The calculations were made for a core diameter of 33.3 cm, and the k_{eff} values for the 32.5-cm core were estimated as described above. It is observed from Table VIII that the annular ring idea utilizing gado-

linium or europium is not as good as the solid absorber idea utilizing 90 at.% ^{10}B in B_4C or 100% ^{10}B as a central plug. Also, it is observed that progressively increasing the europium ring thickness from 1 mm to a solid plug makes the criticality safety problem worse. This is probably because of the tradeoff between the thermalization capabilities of water (increasing the capture cross section of europium) and the requirement for enough europium atoms to capture the neutrons. The same effect would probably be observed with gadolinium as an annular ring.

It is also observed from Table VIII that 100% ^{10}B is quite a bit better than B_4C as a plug material and that, when considered with the control absorber, the relative effects of ^{10}B vs B_4C in the control absorber are negligible in the water immersion environment. It is concluded that, of all central plug materials considered, only the ^{10}B central plug is safe when the beryllium and BeO reflectors are intact.

Table IX shows the criticality safety assessment of central plug materials when the reactor is assembled and handled dry. The calculations are made with the beryllium and the BeO reflector in place, and with the control drums in shutdown position. The bare core case is shown for comparison. Also shown are the effects of central plug materials on criticality for water immersion with the beryllium/BeO reflector jettisoned. Cases 6 and 7 were actually calculated (for the 33.3-cm-diam core). This gave a Δk_{eff} variation between

TABLE VIII
REQUIRED CORE SIZES AND CRITICALITY SAFETY ASSESSMENT OF CENTRAL PLUG REPLACEMENT
MATERIALS FOR WATER IMMERSION ACCIDENTS WITH REFLECTOR INTACT, CONTROL IN

CANDIDATE CENTRAL CORE PLUG MATERIAL	MCNP CALCULATION OF K _{EFF} AT CORE DIAMETER OF 33.3 CM	ESTIMATED K _{EFF} AT CORE DIAMETER OF 32.5 CM	CRITICALITY SAFETY ASSESSMENT
<u>SPACE ENVIRONMENT (CONTROL OUT)</u>			
	(1)	(2)	
1. OPERATIONAL BOL	1.0619 ± 0.0034	1.034	NOT APPLICABLE
2. BeO CENTRAL PLUG REPLACEMENT OPERATIONAL BOL	1.0772 ± 0.0046	1.050	NOT APPLICABLE
<u>WATER IMMERSION ENVIRONMENT (CONTROL IN)</u>			
3. H ₂ O (7-CM DIAM)	1.1126 ± 0.0047	1.086	UNSAFE
4. B ₄ C (7-CM DIAM)	1.0244 ± 0.0044	0.997	MARGINAL (2)
5. H ₂ O (6.8-CM DIAM) PLUS Gd ANNULAR RING (1 MM THICK)	1.0661 ± 0.0045	1.040	UNSAFE
6. H ₂ O (6.8-CM DIAM) PLUS Eu ANNULAR RING (1 MM THICK)	1.0511 ± 0.0051	1.025	UNSAFE
7. H ₂ O (6-CM DIAM) PLUS Eu ANNULAR RING (5 MM THICK)	1.0544 ± 0.0042	1.028	UNSAFE
8. Eu (7-CM DIAM)	1.0687 ± 0.0038	1.042	UNSAFE
9. B-10/H ₂ O (7-CM DIAM, 30 v/o H ₂ O-70 v/o B-10)	1.0267 ± 0.0048	1.000	UNSAFE
10. B-10 (6.8-CM DIAM) PLUS Eu ANNULAR RING (1 MM DIAM)	1.0260 ± 0.0048	0.9997	MARGINAL (2)
11. B-10 (7-CM DIAM) B-10 CONTROL IN B ₄ C CONTROL IN	1.0124 ± 0.0040 ---	0.986 0.986	SAFE (2) SAFE (2)

the reflector-intact and reflector-jettisoned cases that was used in conjunction with the intact reflector results to estimate the k_{eff} values for cases 8 through 14 in Table IX.

Table IX shows that the fully assembled reactor is safe for all handling environments and the bare core (no reflector) is safe for all materials considered in water immersion except (6), a central hole filled with water; (7), a 1-mm-thick annular gadolinium ring; and (11), a solid 7-cm europium central plug. The greatest criticality safety margin is obtained from a 100% ¹⁰B solid 7-cm central plug. Even a porous plug to allow for greater neutron thermalization (12) is not as good as the solid ¹⁰B plug.

The effects of various central plug materials on the energy spectrum at the core center are eval-

uated from the MCNP code and are shown in Fig. 35. The nominal void space configuration at the center has a spectrum mainly in the range 10 keV to 3 MeV.

The BeO plug softens the spectrum as indicated, whereas the various water mixtures considered in Tables VIII and IX considerably thermalize and make a more uniform spectral distribution. The boron mixtures harden the spectrum as indicated. BeO neutronic effects are dominated by elastic scattering so that neutrons are mostly scattered back into the fuel regions, where they have a high probability of causing fissions. Thus the decrease of neutron flux (from the BeO neutronic flux effects) over a large portion of the spectrum when boron mixtures replace the BeO is caused by the large absorbing effect of the boron mixtures, because these formerly backscattered neutrons have

TABLE IX
CRITICALITY SAFETY ASSESSMENT OF CENTRAL PLUG REPLACEMENT MATERIALS FOR HANDLING
ENVIRONMENTS AND FOR WATER IMMERSION ACCIDENTS WITHOUT REFLECTOR

CANDIDATE CENTRAL CORE PLUG MATERIAL	MCNP CALCULATION OF K_{EFF} AT CORE DIAMETER OF 33.3 CM	ESTIMATED K_{EFF} AT CORE DIAMETER OF 32.5 CM	CRITICALITY SAFETY ASSESSMENT
<u>HANDLING ENVIRONMENT (DRY-CONTROL IN)</u>	(1)	(2)	
1. BARE CORE	0.70211 $\pm$ 0.0057	0.676	SAFE (1),(2)
2. OPERATIONAL BOL	0.9347 $\pm$ 0.0034	0.908	SAFE (1),(2)
3. BeO CENTRAL PLUG REPLACEMENT OPERATIONAL BOL	---	0.924	SAFE (1)
4. B ₄ C CENTRAL PLUG PREOPERATIONAL	0.8663 $\pm$ 0.0037	0.840	SAFE (1),(2)
5. B-10 CENTRAL PLUG PREOPERATIONAL	---	0.828	
<u>WATER IMMERSION ENVIRONMENT</u>			
6. H ₂ O (7-CM DIAM)	1.0633 $\pm$ 0.0039	1.037	UNSAFE
7. B ₄ C (7-CM DIAM)	0.9923 $\pm$ 0.0040	0.966	SAFE (1),(2)
8. H ₂ O (6.8-CM DIAM) PLUS Gd ANNULAR RING (1 MM THICK)	---	1.008	UNSAFE
9. H ₂ O (6.8-CM DIAM) PLUS Eu ANNULAR RING (1 MM THICK)	---	0.993	SAFE (2)
10. H ₂ O (6-CM DIAM) PLUS Eu ANNULAR RING (5 MM THICK)	---	0.996	SAFE (2)
11. Eu (7-CM DIAM)	---	1.010	UNSAFE
12. B-10/H ₂ O (7-CM DIAM, 30 v/o H ₂ O-70 v/o B-10)	---	0.968	SAFE (2)
13. B-10 (6.8-CM DIAM) PLUS Eu ANNULAR RING (1 MM DIAM)	---	0.968	SAFE (2)
14. B-10 (7-CM DIAM)	---	0.954	SAFE (2)

now been absorbed in the boron. In contrast, the water mixtures tend to simply spread out the neutron spectrum without appreciable absorption, and thermal neutrons that are backscattered into the fuel have a much greater probability of causing fissions.

These effects are further emphasized in Fig. 36. This figure shows the cross sections of the three candidate central plug absorbers: natural gadolinium, 100% enriched ¹⁰B, and natural europium are compared. (The spectra are also shown on the same energy scale.) Both gadolinium and europium have a resonance region (0.1 eV to 1 keV),

whereas ¹⁰B is a 1/v absorber from thermal energy (0.025 eV) to about 10 keV. Boron-10 has about 1/3 to 1/5 the cross section of europium in the spectral region 10 keV to 3 MeV. Its higher neutron absorption capability arises from the fact that the mean number of collisions it requires to thermalize a 2-MeV fission neutron is 77, whereas this number is 1107 for gadolinium and 1143 for europium.

A summary of the mass effects of various core heat pipe and fin materials versus number of heat pipes is shown in Fig. 37. This figure indicates that, from a neutronics consideration, molybdenum

Fig. 35. MCNP neutron flux spectrum at core center during water immersion accidents.

Fig. 36. MCNP cross section for central plug materials and neutron spectra at core center.

is the lightest material, utilization of a BeO plug is worth about 55 kg, and the tungsten-rhenium material results in the heaviest core. All core masses increase with the number of heat pipes in the range considered.

In summary, it has been shown by analysis that an annular reactor with BeO/ ^{10}B replacement plugs at the core center satisfies criticality safety requirements for all preoperational assembly and handling environments and for the water immersion environment for both bare core and with reflector intact with all control segments in shutdown position. Also, the mass savings is about 55 kg with the BeO/ ^{10}B replacement plug concept. It is concluded that

SP-100 CORE MASS REQUIRED FOR $k_{\text{eff}}=1.053$ AT BOL FOR VARIOUS HEAT PIPE AND FIN MATERIALS (CENTRAL HOLE DIAMETER = 7 cm)
CORE MASS INCLUDES 7-cm CENTRAL PLUG OF 90 at. % ENRICHED B-10 IN B₄C

Fig. 37. Core mass variations with heat pipe number for different core materials.

- the annular reactor with BeO/ ^{10}B replacement central plugs is safe for all environments in which core geometry is unchanged or shock hydrodynamic effects are not influential;
- the preferred water immersion configuration would be without reflector intact (jettisoned); and
- BeO/ ^{10}B replacement central plugs are a criticality safety solution, but BeO requires analyses to consider heat-removal requirements.

Further work is needed in order to evaluate alternative designs with fail-safe separable core segments for accident situations and a consideration of impacted core criticality safety and the structural dynamic and shock hydrodynamic scenarios that produce core geometry changes.

Since the required BOL k_{eff} was taken as 1.05, a fuel depletion model including fission product buildup can and should be set up using MCNP to establish a k_{eff} at end of life (EOL) for a point design.

Further work should be expended on a calculation of radial and axial power distributions that accurately account for the neutron streaming effect in the heat pipes.

Finally, MCNP and TWOTRAN models need to be set up so that intermediate control position effects on k_{eff} can be calculated using fuel depletion and fission product buildup effects.

F. Fuel Swelling (W. A. Ranken, Q-13)

Fuel swelling caused by fission gas generation is the major driving element in the design of high-temperature reactors for space power application. Consideration of this phenomenon was paramount in selecting the fuel type, fuel form, and fuel operating temperature range for the original SPAR reactor design.¹³⁻¹⁶ Estimates of the amount of fuel swelling to be expected as a function of accumulated fission density and fuel temperature were based on data obtained in a program of high-temperature irradiation of refractory nuclear fuels that was conducted by the Battelle Memorial Institute (BMI) from approximately 1964 to 1972.⁶⁻¹¹ At the time the initial SPAR design decisions were made, this was the only comprehensive source of UO_2 fuel irradiation data obtained specifically at high temperatures with the fuel effectively unconstrained by cladding. The conclusion of lack of cladding restraint was reached by the BMI investigators near the close of the program, following a series of experiments in which cladding parameters were varied.¹¹ The conclusion was important to the design of the SPAR system because the UO_2 wafer/molybdenum fin construction of the SPAR core is a low-restraint configuration.

The temperature dependence of UO_2 swelling obtained by the BMI investigation is shown by the solid line in Fig. 38. The dashed lines that flank this curve represent the approximate limits of scatter of the data. The burnup range over which this data was obtained was 0.6×10^{20} to 2.3×10^{20} fis/cm³. In plotting their results, the BMI investigators assumed a linear dependence on burnup so that the ordinate could be expressed in terms of volume percent increase per 10^{20} fis/cm³.

A difficulty that arose in applying the BMI results to the SPAR core was the marked difference in fuel geometry of the test specimens and the reactor core. The BMI data were obtained for cylindrical UO_2 specimens that were 5.3 mm in diameter and usually had a central hole with a diameter of 1.4 or 2.8 mm. These specimens were typically clad with 0.5 mm of W-26Re. Heat flow was radially outward and the radial temperature variation was in the range of 180 to 500 K, depending

Fig. 38. Representation of the volume increase per 10^{20} fis/cm³ vs cladding temperature obtained by BMI investigators for lightly clad cylindrical UO_2 specimens. The dashed lines encompass most of the 36 data points to which the solid line was fit.

on the power density in the fuel, the fuel density, and the diameter of the inner hole. (The high end of the temperature variation range occurred for specimens with no initial central hole.)

In contrast to the BMI specimen geometry, the SPAR core geometry consisted of layers of 2-mm-thick wafers of UO_2 , sandwiched between 0.5-mm-thick molybdenum fins. The latter served to transport heat for the UO_2 tiles in a radially inward direction to the core heat pipes. Heat flow in the UO_2 wafers is primarily from the midplanes of the individual wafers to the surface contacting the molybdenum fins, and the temperature drop in this direction is about 30 K.

Two methods have been used to apply the cylindrical specimen BMI data to the SPAR configuration. One of these simply assumes the equivalence of the heat pipe temperature in the SPAR design to the cladding temperature of the cylindrical

irradiation specimens. With this assumption, the average UO_2 fuel swelling to be expected for a given SPAR heat pipe temperature can be read directly from the curve in Fig. 38. This procedure yielded an expected volumetric swelling percentage of 3% for the 100-kW_e SPAR reactor core over a 7-yr lifetime for an operating heat pipe temperature of 1400 K.

The other method used to adapt the cylindrical specimen results to the SPAR configuration was to plot the swelling as a function of average fuel temperature. Essentially, this means displacing the curve in Fig. 38 toward higher temperatures by 100 to 125 K. This procedure also gave 3% for the average fuel swelling expected under normal operation.

A void allotment of 5% of the UO_2 volume was introduced into the SPAR core design to accommodate this swelling. An additional 5% existed as porosity in the 95% dense fuel but was not deemed to be in a useful form to accommodate swelling in the temperature range of normal operation. It was, however, considered to be effective in accommodating swelling at the higher fuel operating temperatures that would exist in the immediate vicinity of a failed heat pipe. The fuel immediately adjacent to such a heat pipe could reach a temperature of about 1950 K.

The BMI UO_2 irradiation data were very useful in providing a design guideline for the SPAR core and for irradiation test capsules intended to prove the core design concept. Application of the data to the failed heat pipe case was less straightforward, but the uncertainty was ameliorated by the fact that the fuel associated with such pipes would not constitute more than 3% of the total core (because the design criteria called for the capability of tolerating three heat pipe failures). Possible distortion of the failed heat pipe itself was deemed a minimal concern, since the heat pipe was no longer operable in any case and its low strength at high temperature would render it ineffective in distorting the surrounding fuel elements.

The adaptation of the SPAR design into the SP-100 configuration, in which the core heat pipes deliver the reactor heat to the TE array by thermal radiation, has made it necessary

to increase the heat pipe operating temperature to 1500 K. The predicted average fuel swelling at this temperature is 7%, a value sufficiently large to make desirable detailed modeling of expected fuel behavior to determine the adequacy of the amount and placement of fuel void to absorb this amount of volume increase without damaging the reactor core.

For this type of modeling, UO_2 fuel swelling data are needed for unrestrained specimens isothermally irradiated over a temperature range of 1500 to 2100 K to burnups required for the SP-100 reactor (8 to 10×10^{20} fis/cm³). This data, together with a description of UO_2 creep strength under various conditions of irradiation, would permit accurate modeling of the SP-100 fuel configuration for both normal operation and single-failed-heat-pipe regimes.

A recent literature search has revealed data sources that provide information close to that required for meaningful modeling. These sources are a dissertation and subsequent formal publications by H. Zimmermann, which describe a series of UO_2 and mixed UO_2/PuO_2 oxide (MO_2) irradiation experiments.²⁻⁵ The Zimmermann UO_2 irradiation experiments were conducted in both unrestrained and restrained fuel configurations. The unrestrained data were obtained for ring-shaped specimens that were 1 mm high, with an outside diameter of 5.1 mm and an inside diameter of 2.2 mm. These fuel rings were sandwiched between 2-mm-thick molybdenum rings in a stack that was lightly compressed in the axial direction by a spring. The restrained fuel specimens were solid pellets 3 mm in diameter and 5.7 mm long. These fit closely into a thick-walled TZM cylinder in a stack several pellets high, which could be compressed axially to pressures up to 50 MPa (7250 psi) during irradiation.

The Zimmermann fuel specimens were high density (96 to 98% of theoretical) and stoichiometric ($2.0 < \text{U} < 2.005$). They were irradiated to burnups of 1×10^{20} to 3.2×10^{21} fis/cm³ at fission rates of 3.1×10^{13} to 1.4×10^{14} fis/cm³. The specimen temperatures covered a range of 1300 to 2000 K. The quoted temperature values represent temporal and spatial averages. The latter was required because, despite the small

sample size (particularly for the unrestrained specimens), the fission rate was so high that a difference of 150 to 200 K existed from the surface to the midplane of the rings. Zimmermann did not measure changes in physical dimensions of his unrestrained specimens, but rather relied on determining volume changes by immersion of the specimens in carbon tetrachloride or by gas pycnometer measurements. He did not discuss the question of interconnected porosity in the irradiated fuel, so it is possible that his fuel swelling measurements at high burnup will require a correction of several percent in $\Delta V/V$ for use in the SP-100 modeling. This question is currently being clarified.

The unrestrained Zimmermann data are compared to the BMI irradiation data in Figs. 39, 40, and 41, in which volume increase is plotted versus burnup in percentage of heavy metal atoms. Also included in these plots are data obtained by Grando et al.¹⁷ on two cylindrical specimens of

$U_{0.85}Pu_{0.15}O_2$ with an oxygen-to-metal ratio of 1.93; these specimens were 14 mm in diameter and 12.7 mm long. They were isothermally irradiated in an unrestrained configuration to a burnup of 4.9×10^{20} fis/cm³ at temperatures of 1640 and 1855 K. The volume increase results were interpolated to 1750 K and extrapolated to 2000 K, assuming an Arrhenius behavior, for inclusion in the fuel swelling curve.

The specimen temperatures quoted by Zimmermann and Grando are representative of average fuel temperatures, whereas the BMI data were published in terms of cladding temperature. Based on calculations of expected thermal gradients under the BMI irradiation conditions, the temperatures of the BMI specimens have been converted to average temperatures by adding 100 K to the cladding temperature for those specimens having a 2.8-mm-diam central hole and 125 K for those with a 1.4-mm-diam hole. The solid specimens were assumed to be

Fig. 39. Volume increase of lightly clad or unconstrained UO₂ and MO₂ fuel vs burnup. BMI data have been converted from cladding temperatures to estimated average fuel temperature by adding 100 K or 125 K depending on the central hole diameter of the cylindrical specimens. Data from solid cylindrical specimens are not included. M = ⁹²U⁸Pu for BMI data and ⁸⁵U¹⁵Pu for Grando. Numbers accompanying each data point are average fuel temperature in kelvins. Curve is estimated behavior for 1750 K.

Fig. 40. Volume increase of lightly clad or unconstrained UO₂ and MO₂ fuel vs burnup. Numbers accompanying each data point are average fuel temperatures in kelvins. BMI data for cylindrical fuel specimens with central holes have been converted to estimated average fuel temperature. M = 80U20Pu for BMI data and 85U15Pu for Grando. Curve is estimated behavior for 2000 K.

Fig. 41. Volume increase of unconstrained UO₂ fuel vs burnup. Numbers accompanying data points are average fuel temperatures in kelvins. Curve labelled 1500 K is estimated behavior at that temperature. Estimated curves for 1750 K and 2000 K are shown for comparison.

subject to "bladder effect" swelling (accumulation of fission gas in an enclosed central void in between temperature cycles) and hence were not included.

The three sets of data generally fit a pattern of relatively high initial slope of the volume increase versus burnup curve, followed by a pronounced fall-off of this slope as the volume increase tends to saturate. This is particularly apparent at the higher temperatures.

This type of behavior is predicted by Dollins¹⁸ for the temperature range in question where gas bubbles tend to collect in grain boundaries of an equiaxed grain structure, causing swelling until tunnels form throughout the material, which permits fission gas release and results in saturation of fission-gas-induced swelling.

The curves in Figs. 39 to 41 establish an initial basis for proceeding with modeling irradiation effects in the SP-100 reactor core. The task of refining these curves as much as possible is continuing, with more information being sought on the Zimmermann experiments and further effort being made to incorporate the large body of data obtained for mixed oxides under the LMFBF irradiation program.

G. In-Pile Test of SP-100 Fuel (A. J. Giger and J. T. Sena, Q-13)

1. General. The in-pile test of SP-100 fuel planned for the EBR-II reactor at Argonne National Laboratory (ANL), Idaho Falls, is designed to provide data on fuel migration, long-term heat pipe behavior in a radiation environment, fuel swelling, chemical compatibility of fuel and heat pipe materials, and heat transfer between fuel and the heat pipe. The experiment, contained in a MK M-1A irradiation subassembly, will be placed in a 6N3,4 position in EBR-II. Two assemblies are planned.

2. Design. A very detailed, in-depth, design review was presented to the US Department of Energy. The review was not part of nor applicable to the experiment acceptance procedure required by ANL for the EBR-II reactor.

At the time when molybdenum/TZM was still the indicated material for the in-pile heat pipe, a braze-on unit, fin-sleeve design was prepared for heat exchange between fuel and heat pipe. This design was chosen over individual brazing of fins to the pipe to provide more accurate control of fin spacing and to achieve better isolation of braze material from the fuel.

With a subsequent material change to Mo-13Re, the design was changed to a completely integral fin/heat pipe arrangement, as shown in Fig. 42. Six radial slots have been added to the fins of the heat pipe for thermal stress relief.

3. Analysis. The axisymmetric model of the SP-100 fuel test container previously reported was revised to describe test bays 1 and 4. The revision included a single ZrO_2 washer between bays and slightly higher smeared radial fuel conductivities. Two cases of a failed experiment heat pipe were examined. Figure 43 shows this condition with a molybdenum fuel container emissivity of 0.21 (unenhanced). A maximum fuel temperature of 4400^+K indicates fuel melting would occur. Enhancing the container emissivity to 0.72 gave the results shown in Fig. 44. Maximum temperatures of 3400^+K also indicate some fuel melting. Reduction in ZrO_2 radial insulation outboard of the fuel would probably alleviate this in the second case but, during normal operation, would result in less prototypic temperature distribution in the fuel.

Because of the large radial variation in UO_2 fuel heating in the 7N4 position of EBR-II, particularly in ^{238}U , a change has been requested and tentatively approved for use of a 6N3,4 position. Figure 45 gives the ratio of peak fission rates across several EBR-II assemblies and the geometrical relationship of SP-100 fuel. The resulting variation in radial heating is shown in Table X (used in conjunction with Fig. 45).

The safety analysis required for the experiment description includes an analysis of fuel temperatures during an anticipated loss-of-flow

Fig. 42. SP-100 EBR-II fuel test capsule assembly.

Fig. 43. Failed heat pipe; fuel container $\epsilon = 0.21$.

Fig. 44. Failed heat pipe; fuel container $\epsilon = 0.72$.

TABLE X
RADIAL HEATING VARIATION IN SP-100 EBR-II FUEL TEST
(BASED ON 6N1 POSITION AND DATA FROM RUN 118)

TEST BAY	HEAT SOURCE VALUES (FRACTION OF TOTAL)			TOTAL HEATING (W/cm ³)	MAXIMUM VARIATION IN RADIAL HEATING (%)		
	²³⁵ U	²³⁸ U	GAMMA		7N4	6N3,4	6N1
1	0.0905	0.215	0.103	180.3	±17.2	±7.9	±7.8
	0.0845	0.240	0.708				
	0.0529	0.608	0.096				
	0.065	0.49	0.44				
2,3	0.38	0.32	0.30	264	±12.2	±6.6	±6.7
3	0.75	0.11	0.14	586	±6.0	±5.0	±5.3

SUPERSCRIPITS,

0.0905 ← 0.62 x 0.146, 6N1
 0.0845 ← 0.54 x 0.157, 6N3,4
 0.0528 ← 0.54 x 0.098, 7N4
 0.065 ← fission rate variation
 ← geometric factor

(LOF) in EBR-II. An AYER model of the experiment taken from the EBR-II sodium, across the condenser section, down the pipe centerline to the fuel container, and radially outward through test bay 4 to the EBR-II sodium, is shown in Fig. 46.

A preliminary check-out of the model was made for the coolant flow change behavior, shown in Fig. 47, and the loss of power in fuel and structural materials, shown in Fig. 48. The analysis was carried out from the time (t) of pump shutdown and scram (t=0) to t=100 s. The results are shown in Fig. 49. Because of the relatively low SP-100 experiment coolant flow (0.85 ℓ /s sodium), boundary temperatures will be largely determined by adjacent driver fuel. The analysis will be continued to reflect variable boundary temperatures.

4. Development. Since the start of the in-pile test, a materials development has been undertaken for the SP-100 core heat pipe. The in-pile test heat pipe, depending on the same technology, is tied to this material development.

While in-pile heat pipe thermal performance has essentially been met using a molybdenum pipe, the experiment is paced by the production of an operating pipe of acceptable material. Pure molybdenum has been ruled out on the basis of too high a ductile-to-brittle transition temperature (DBTT) and its low (1100°C) recrystallization temperature (RT). It would not be possible to braze fins to a pipe, for instance, without some grain growth and embrittlement with subsequent handling hazards.

Substitution of TZM was seen as a solution; its lower DBTT and higher RT would allow the use of a diffusion alloying braze for fin attachment. However, a test of TZM in contact with UO_2 at 1800 K in vacuum showed the 0.5% titanium and 0.08% zirconium in TZM took oxygen from the UO_2 fuel, reducing the stoichiometry of the test UO_2 from 1.997 to the vicinity of 1.970 and causing the formation of free uranium. This made unprotected TZM unacceptable for either the in-pile or core heat pipe material.

RATIO OF PEAK FISSION RATES
ACROSS EBR-II FUEL ASSEMBLY

POSITION	²³⁵ U	²³⁸ U	GAMMA (URANIUM) ^a
7N4	1.098	2.126	1.096
6N3,4	1.157	1.444	1.078
6N1	1.146	1.347	1.103

^aOver 1.2 span of fuel.

Fig. 45. Ratio of peak fission rates across EBR-II fuel assembly.

Fig. 46. Composite thermal model for SP-100 EBR-II fuel test.

Fig. 47. Primary coolant flow following loss of pumping power.

Fig. 48. Relative subassembly power following loss of pumping power in fuel and structural materials.

Fig. 49. Check-out of composite model response to anticipated LOF and power decay.

The SP-100 core heat pipe reference material is now Mo-13Re. This alloy was selected because it is ductile at low temperatures even after high-temperature operation. An accelerated material test at 1800 K showed that molybdenum-rhenium is a good barrier between the fuel and the lithium (an oxygen getter) in the heat pipe.

Because the experiment is designed in part to provide and verify the heat-transfer mechanisms between the fuel, fin, and heat pipe, a means of measuring maximum temperature is important. We can accomplish maximum temperature measurements by using melt wires. The melt wires that will be used in this irradiation test are contained in small capsule assemblies that, with an orderly arrangement, will provide a temperature profile within the fuel container. To provide a good temperature profile, 34 maximum temperature indicators are placed within the fuel container, shown in Fig. 50. The wires, measuring 0.91-mm diam x 4.45 mm long, are coated with a 0.051- to 0.127-mm-thick yttrium oxide coating and are encapsulated in a rhenium capsule. Fig. 51 shows a melt wire before coating. The capsule and cover and a coated melt wire are shown in Fig. 52. A list of materials used and their approximate melting points is given in the following table:

Code	Material	Nominal Melt Temperature (K)
1	Fe	1802
2	60 Pd/40 Cu	1483
3	60 Pd/40 Ni	1521
4	77 Pd/23 Cu	1566
5	Ni	1726
6	88 Pd/12 Cu	1630
7	Pd	1825
8	50 Pd/50 Pt	1858
9	Rh	2239

Yttrium oxide coating was selected in order to prevent the formation of an unknown eutectic, thus giving an incorrect melting temperature. The first attempt at coating the melt wires by plasma spraying proved unsatisfactory. Plasma spraying generated an excessive amount of heat, and because the wires are so small, they were unable to reject the heat and did not achieve bonding.

Electron-beam (EB) coating provided the best way to coat the wire. We were able to reasonably control the thickness of yttrium oxide coating and to prevent deposition in the melt wire groove.

Data on maximum temperature versus location will be obtained by examining the melt wires after the irradiation test is completed to determine which have melted. Melting can be determined easily and with good precision by radiographing the melt wires through the rhenium capsules. The change in the right angle at the base of the groove in the melt wire is a sensitive indicator of melting. Fig. 53 shows a melt wire that has been radiographed through a rhenium capsule.

5. Fabrication. All SS and niobium parts for the irradiation capsule have been fabricated and are on hand. Design of the Mo-13Re irradiation test heat pipe has been completed and placed for bid. It is expected that the machinability of the Mo-13Re alloy will be comparable to molybdenum. This alloy is currently being produced by an outside vendor.

Fabrication of the melt wires, which have a 0.254-mm-wide x 0.229-mm-deep groove around the center, turned out to be an uncomplicated operation. The critical feature is the 0.025-mm maximum radius at the bottom of the groove. Machining the groove and radius was not as difficult as expected. The wires and outer rhenium capsules will be fabricated by an outside vendor.

While in the transitional period between molybdenum and TZM, four braze-on, fin-sleeve units were fabricated. Two of these units are shown in Fig. 54.

Fuel slot dimensions are given in the following table:

Part No.	Material	Method	Fuel Slot Width (mm)	
			Specified	Delivered Range of Values For 8 Slots
-2	Mo	turned	+0.013 1.024 -0.000	1.016 -1.034
-2	TZM	ground	+0.013 1.024 -0.000	1.019 -1.029
-3	TZM	turned	+0.025 1.011 -0.000	1.019 -1.021
-4	TZM	turned	+0.013 2.052 -0.000	2.040 -2.052

Fig. 50. Thermal capsule locations in fuel container.

Fig. 51. Melt wire before coating.

Fig. 52. Yttrium-oxide-coated melt wire and rhenium capsule.

Fig. 53. Radiograph of coated melt wire taken through rhenium capsule.

Fig. 54. Fin-sleeve units for irradiation capsule.

The 0.013-mm total tolerance could probably be achieved with very tight fabrication controls. It is expected that this information will apply to the fabrication of Mo-13Re.

6. Posttest Diagnosis. The disassembly and examination of the experiment will be done in a hot cell facility. Neutron radiographs will be taken of the fuel container prior to irradiation and after removal from EBR-II, for use in making comparisons in the posttest examination. Radiography will be used extensively to provide accurate records of the posttest conditions of each wire.

H. Comparison of Reactor-Converter Coupling Schemes (D. R. Koenig, Q-13)

The merits of several concepts for coupling the reactor to TE converters have been compared. These include the closely coupled, compact TE converter SPAR design; the radiatively and conductively coupled, distributed TE converter concepts, and concepts employing an intermediate heat ex-

changer. An example of the latter is shown in Fig. 55. Here, the heat carried by the core heat pipes is transferred to a large ring assembly of planar TE converters through an intermediate heat exchanger, which consists of a layered stack of "pancake" heat pipes. The principle of the pancake heat pipe is described in Fig. 56. The pancake heat pipes have penetrations or sleeves that allow the core heat pipes to pass through. A thermal bond is established at the sleeves by brazing. The flow of heat in the pancake heat pipes is from the central bundle of core heat pipe sleeves to the pancake periphery, as shown in Fig. 55, or possibly to another set of sleeves penetrated by converter heat pipes, as shown in the top right side of Fig. 56. There are several significant advantages to the use of such a heat exchanger. The reactor is physically decoupled from the converters. The core heat pipes are short (<2 m), straight, and therefore identical; and their number is dependent only on the

Fig. 55. Heat exchanger planar TE converter design.

Fig. 56. Pancake heat pipes.

constraints of the core design. These features greatly simplify assembly of the reactor. The strong thermal coupling of core heat pipes outside the core reduces the consequences of a core heat pipe failure and lessens the power flattening requirements. The radiation shield design is simpler and easier to cool than concepts where the core heat pipes skirt around the shield. These attractive features are offset by a heat exchanger temperature loss on the order of 25 to 50 K and by a mass penalty of several hundred kilograms. The latter is caused in part by the need for additional shielding against radiation streaming through the core heat pipes. The mass penalty was judged severe enough to abandon the heat exchanger concept for the time being.

The radiatively and conductively coupled, distributed TE converter concepts have been described earlier.¹⁹ For completeness in presenting this reactor-converter coupling comparison study, a description of these two concepts is included again here.

The radiatively coupled concept is depicted in Figs. 57 and 58. The core heat pipes fan out around the radiation shield, then extend for several (6 to 8) meters in a conical bundle, which is surrounded at some distance by the TE converter assembly. The heat pipes transfer the core power by thermal radiation to the TE hot shoes. The reject heat is radiated to space directly by the TE cold shoes. Thus, the need for a separate radiator assembly, as in the SPAR design, is eliminated. The major advantages of this concept are that it requires only one set of heat pipes, and the reactor and converters are physically separate systems that can be assembled and tested separately, then mated without stringent tolerances during final assembly. Another significant advantage is that the TE converter assembly is similar in concept to existing radioisotope TE generator technology, and it does not require a hot shoe electrical insulator. Radiative cross-coupling of the heat

Fig. 57. SP-100 nuclear power system radiatively coupled design.

Fig. 58. Radiatively coupled system design TE module cross section.

pipes reduces the consequences of a heat pipe failure and lessens the power flattening requirements. And finally, the power plant is insensitive to meteorite damage.

This attractive concept is not without drawbacks. It requires long (>8 m), bent core heat pipes that are difficult to fabricate, complicate assembly of the reactor core, and may have inferior thermal performance. The nature of radiative heat transfer is such that there will be a large (>100 K) temperature differential between core heat pipes and converter hot shoes, even if high-emissivity ($\epsilon=0.85$) surfaces can be developed. This loss of temperature results in a proportional reduction in electrical conversion efficiency of the power plant. This efficiency loss can be offset by raising the reactor power level or the core temperature, both of which increase plant size and weight. Finally, the concept requires the develop-

ment of high-emissivity surfaces that are stable at 1400 to 1500 K.

The last concept studied is the conductively coupled, distributed TE converter system described in Figs. 59 and 60. In this case, the core heat pipes extend out along the conical surface described by the TE converter panels in the preceding (radiative) concept. TE converters are clamped, and brazed if necessary, directly onto the core heat pipes, as shown schematically in Fig. 60. The cold shoes of the converters consist of many small heat pipes that radiate directly into space. This cold shoe design makes possible a radiating area many times greater than the cross-sectional area of the TE couples without incurring the temperature drop of a conventional fin. The cold shoes are segmented at the thermocouple junctions to permit elimination of a cold junction electrical insulator. The current is carried by the cold shoes, obviating the need for additional electrical straps. The core heat pipes and the thermocouples are thermally insulated with multifoil, wrapped as shown in Fig. 60. The multifoil provides good meteoroid protection for the heat pipes, enabling substantial size and mass reductions in this configuration by radiating power from both sides of the cold shoes.

One of the disadvantages to this coupling scheme is, again, the use of long (5- to 6-m) core heat pipes. These are somewhat shorter than in the radiatively coupled system, but they will probably have converters attached before reactor assembly, making assembly more difficult. Also, the converters require a hot shoe electrical insulator. Cross-coupling the core heat pipes to reduce the consequences of heat pipe failures will require an additional set of heat pipes, as indicated in Fig. 59. Finally, the loss of a core heat pipe results in the loss of a TE converter string and its radiating area.

A mass comparison for the radiatively and conductively coupled systems has been performed for the conditions listed in the top half of Table XI. The bottom half of Table XI shows differences in the resulting operating parameters for the two systems. The conditions chosen in Table XI are representative of the best performance that may be expected from the SiGe-GaP converter technology.

Fig. 59. Conductively coupled, distributed TE converter design.

Fig. 60. TE module for conductively coupled, distributed TE converter system.

System masses are compared in Table XII, together with a mass estimate for the SPAR design. The conductive system is the lightest, weighing approximately 200 kg less than the radiative system. The radiative system is significantly heavier than the conductive system in the areas of heat pipes, TE elements (including electrical straps and cold side attachments), and multifoil insulation, and notably lighter in the cold shoe radiator. The SPAR system is competitive with the other systems and would be even more so if the radiator weight could be reduced. The comparison was performed for specified hot and cold junction temperatures. A similar analysis was performed for specified core heat pipe and radiator temperatures, as shown in Tables XIII and XIV. The mass of the radiative system under these conditions is nearly 400 kg greater than the conductive system mass, which has hardly changed. This result is largely a consequence of the greater hot side temperature loss (because of the nature of radiative heat transfer) and the greater cold side temperature loss (because of the cold shoe electrical insulator) for the radiative system.

This mass comparison was extended to other hot junction temperatures and values for the assumed TE

TABLE XI

SYSTEM CHARACTERISTICS AT POINT OF COMPARISON
(SPECIFIED HOT AND COLD JUNCTION TEMPERATURES)

Power output (kW_e)	100	
Hot junction temperature (K)	1415	
Cold junction temperature (K)	340	
TE figure of merit (K^{-1})	0.001	
TE efficiency (%)	9.05	
TE heat flux (W/cm^2)	45	
Emissivity, all surfaces	0.85	
Cone half-angle ($^\circ$)	15	
Shielding criteria at 25 m	10^{12} nvt/ 10^4 Gy	
	<u>Radiative</u>	<u>Conductive</u>
System efficiency (%)	7.48	7.74
Reactor power (MW)	1.34	1.29
Heat pipe temperature (K)	1550	1512
Radiating temperature (K)	795	822
Heat pipe added length (m)	7.13	5.26
Number of converters	2×10^5	1×10^4

TABLE XII
100 kW_e SYSTEM WEIGHT SUMMARY (kg)
FOR CONDITIONS IN TABLE XI

	Distributed Converters		Compact Converters
	Radiative Coupling	Conductive Coupling	SPAR
Reactor	415	410	410
Shield	760	755	755
Heat exchanger	---	65	65
Heat pipes	325	230	---
TE converters			
Clamp or hot shoe	85	60	
TE elements	145	55	
Insulation	175	105	
Radiator/cold shoe	70	160	530
End thermal shield	95	65	80
Structure (10%)	<u>210</u>	<u>190</u>	<u>215</u>
	2280	2095	2340

TABLE XIII

SYSTEM CHARACTERISTICS AT POINT OF COMPARISON
(SPECIFIED CORE HEAT PIPE AND RADIATOR TEMPERATURES)

Power output (kW_e)	100	
Core heat pipe temperature (K)	1500	
Radiating temperature (K)	800	
TE figure of merit (K^{-1})	0.001	
TE heat flux (W/cm^2)	45	
Emissivity, all surfaces	0.85	
Cone half-angle ($^\circ$)	15	
Shielding criteria at 25 m	10^{12} nvt/ 10^4 Gy	
	<u>Radiative</u>	<u>Conductive</u>
TE efficiency (%)	8.15	9.29
System efficiency (%)	6.78	7.94
Reactor power (MW)	1.47	1.26
Hot junction temperature (K)	1352	1404
Cold junction temperature (K)	850	818
Heat pipe added length (m)	7.37	5.24
Number of converters	2×10^5	1×10^4

figure of merit. The general trend of the study was that the mass difference between the radiative and conductive systems decreases as the hot junction temperature and the figure of merit increase and vice versa. The mass difference is also

TABLE XIV
100 kW_e SYSTEM WEIGHT SUMMARY (kg)
FOR CONDITIONS IN TABLE XIII

	Distributed Converters	
	Radiative Coupling	Conductive Coupling
Reactor	430	400
Shield	790	745
Heat exchanger	---	65
Heat pipes	370	255
Thermoelectric converters		
Clamp or hot shoe	115	60
TE elements	165	55
Insulation	195	105
Radiator/cold shoe	75	160
End thermal shield	95	65
Structure (10%)	<u>225</u>	<u>190</u>
	2460	2070

sensitive to the heat pipe mass. Although the radiatively coupled system appears from this study to have a significantly greater mass than the conductive system, it is the recommendation of this study that its advantages are so great that demonstration of its feasibility should be pursued vigorously.

IV. CORE HEAT PIPE DEVELOPMENT (M. A. Merrigan, Q-13)

A. Heat Pipe Fabrication

1. SPAR-5 Fabrication (H. E. Martinez and E. S. Keddy, Q-13). SPAR-5 is an annular wick, TZM heat pipe that uses lithium as its working fluid. Its configuration is illustrated in Fig. 61. The container tube is 2.04 m long, with a 15.88-mm outside diameter, and 15.00-mm inside diameter. A TZM calorimeter flange was EB-welded to the container tube 660 mm from the end of the heat pipe tube. The TZM fill tube and end cap welds were also done by EB. The 6.35-mm-diam TZM fill tube was EB-welded to the fill tube end cap; the other end was zirconium-brazed to a niobium 6.35-mm-diam tube; a 9.53-mm-diam molybdenum sleeve backs up the braze joint. The molybdenum sleeve was also brazed to the TZM and niobium

tube. The zirconium brazing was performed in two steps; TZM was vacuum-brazed to niobium at 2040 K and molybdenum to niobium at the same temperature. The TZM-to-molybdenum brazing was performed at 1880 K. The braze temperatures were chosen to be slightly higher than the eutectic temperatures for the niobium-zirconium and molybdenum-zirconium systems.

The annular wick was formed from 150 mesh molybdenum screen by rolling on a mandrel and covering with a C1015 carbon steel sheath. The assembly was drawn to 13.56 mm. The mandrel and sheath were etched away in HCl, and the screen tube was washed in distilled water and rinsed in ethanol. It was then sintered in a vacuum furnace at 1710 K for 2 h. Pore measurements were performed to characterize the sintered wick. The largest pore was 78 μ m in diameter and the largest pore in the evaporator area was 55 μ m in diameter. The outside diameter was 13.13 mm and the inside 12.70 mm.

A low-carbon, arc-cast (LCAC) molybdenum plug was fitted and zirconium-brazed to one end of the screen tube; pore tests were repeated to check the braze. Twenty dimpled hafnium discs, 0.13 mm thick, were assembled on the screen tube end plug to complete the subassembly. The screen tube was assembled into the container tube and the final closure weld made.

The heat pipe was attached to the lithium fill and test system shown in Fig. 62. The lithium distillation and transfer part of this apparatus is illustrated in more detail in Fig. 63. The fill/test system has been modified from that used on SPAR-1B and SPAR-4 to incorporate the following improvements:

- An intermediate loading pot of a calibrated volume is used, as opposed to distilling directly into the heat pipe. The lithium is then transferred as a liquid to the heat pipe. Distillation into the calibrated volume avoids distilling to dryness, improving the lithium purity.
- Water chills are provided below and above the loading pot to act as valves in the transfer.

Fig. 61. SPAR-5 heat pipe configuration.

Fig. 62. Lithium fill and test system.

- The heat pipe and connecting Swagelock joint are held within the vacuum container as protection against heat pipe contamination if a leak should occur. The entire external system is a welded SS assembly.
- Two vacuum systems are used. The lower system shown in the figure is used to evacuate the assembly within the quartz vacuum chamber. The upper vacuum system acts on the distillation pot, loading pot, and the inside of the heat pipe. A residual gas analyzer is connected to this vacuum system to determine the species of gas evolving from it. The vacuum pumpout line is fitted with a screen tube and a calorimeter to condense and return lithium vapor to the loading pot. This system maintains the core of the pump lines open for continuous pumping on the distillation pot during filling.
- Hafnium and yttrium are used as getters during the fill operation. The hafnium is located in the loading pot and the yttrium is in the 6.35-mm-diam tube near the heat pipe.

With the system in the vertical position, the heat pipe was heated and outgassed at 1500 K using the 125-kW rf power supply. The loading pot was then outgassed at 1050 K using the clam shell

Fig. 63. Lithium distillation and transfer detail of heat pipe fill/test system.

resistance heater. The lithium in the distillation pot was heated and outgassed at 1050 K. The temperature in the loading pot was then decreased, and the water chill below the loading pot was activated. The temperature in the distillation pot was slowly increased to 1125 K, and the lithium was transferred to the loading pot. The heat pipe temperature was reduced when the lithium transfer began. When the loading pot was full, the lower chill was deactivated, and the contents of the loading pot were pushed into the heat pipe by the vapor pressure in the distillation pot. The chill was then reactivated, and a second distillation into the loading pot was initiated. In filling the SPAR-5 heat pipe, the quantity of lithium moved into the upper pump system exceeded predictions, leaving an insufficient quantity of lithium to fill the loading pot a second time, so the distillation operation was terminated.

The SPAR-5 heat pipe was then tilted horizontally, and an rf heating coil was used to heat the full length of the heat pipe, wetting it in at 1000 K. In subsequent testing of the heat pipe by

heating over a length of 850 mm, it appeared that there was some noncondensable gas in the heat pipe. Measurement of the temperature at the liquid/gas-vapor interface verified the presence of gas in the heat pipe. The loading pot was removed from the system and replaced with a dump tank, which was connected to the vacuum system and the residual gas analyzer. The heat pipe was set up vertically, and an rf coil was used to heat the surface of the liquid pool at the lower end of the heat pipe. A second rf coil was used to heat the 6.35-mm-diam tubing that connected the heat pipe to the vacuum system, while an attempt was made to remove noncondensable gas from the heat pipe. After outgassing, the heat pipe was retested on the test stand. It appeared that the annulus still contained gas, causing the evaporator to dry out. It was believed that gas was held in solution in the cold liquid and appeared when the liquid was heated.

In an attempt to fill the annulus and capture and hold the soluble gas, a chill was assembled on the 6.35-mm-diam niobium tube to form a liquid metal seal. The heat pipe was set up vertically, with the evaporator end down. An rf coil heated the heat pipe just above the lithium pool. Another rf coil heated the upper end of the heat pipe to evaporate any lithium that might be sealing the gap between the annulus and vapor passage. The upper rf coil was shut off and removed, and the heat pipe was slowly moved to the horizontal position. After steady-state operation in a horizontal position to getter the gas in the liquid pool, the heat pipe was tilted to work against gravity. The heat pipe performed successfully without dryout at 1270 K and 50° tilt from the horizontal.

The heat pipe was also heated to 1500 K in the horizontal position; the annulus appeared full, and the gas was apparently held in the cold lithium liquid at the condenser end of the heat pipe. Therefore, the decision was made to operate the heat pipe for performance testing with the condenser-end chill in place. The heat pipe was removed from the fill test setup, radiographed, and prepared for performance test. Although the radiographs showed longitudinal cracks of unknown origin along the screen tube length, it was

decided, based on the tilt test performance, to continue the test.

2. SPAR-6 Fabrication (J. Runyan, Q-13).

Figure 64 illustrates the basic differences between the SPAR-6 artery heat pipe and the SPAR-4 artery heat pipe. For SPAR-4, contact between the container and the distribution wick was achieved by drawing the container down onto the distribution wick. Contact pressure in the SPAR-6 design was achieved by forming by internal pressurization. This is achieved by applying hydraulic pressure to a bladder, which was used as a mandrel for wrapping the distribution wick. Four hundred mesh molybdenum-rhenium screen from Drener Metalltuch and a TZM container tube were used in the construction of SPAR-6.

A flow sheet of the steps involved in the fabrication is shown in Fig. 65. The dual arteries are contained in a wrap, with spot welds joining the assembly to the expanded distribution wick. This screen-wrapping technique eliminates the sintering operations previously required and provides rigidizing of the multiple screen layers to allow handling. Securing the edges as shown in Fig. 66 provides alignment of the screen on the mandrel and anchors the leading and trailing edges to the wrap to prevent separation after dissolving

the mandrel. The spot welds were made with a Unitek 500 welder supply using a series handpiece to increase pressure sensitivity. The standard 20N minimum triggering force was reduced to 4N for welds on 0.025-mm-diam wires.

Samples of distribution wick were made up using 400 mesh molybdenum-rhenium screen and tested for permeability prior to fabrication of the complete assembly. Measured values were in the 10^{-12} to 10^{-11} m² range, in good agreement with the heat pipe design requirements.

The bladder (mandrel) was drawn from 15.88-mm-o.d. x 0.89-mm stock SAEC1015 Shelby steel tubing to 13-mm o.d. Wrapping the 305-mm stock width screen on this mandrel gave a wick with 6-3/4 wraps. Expansion of the bladder and wrap into the TZM container was accomplished by pressurizing to 73 MPa. Radiographs were taken of samples to evaluate the contact between wick and container. Expansion of the distribution wick was controlled to preclude tension fracture of the screen while providing clearance for assembly. The limit of the expansion was determined with a sample using an intentionally undersized mandrel of 12.65-mm o.d., wrapped with 400 mesh screen to a screen outside diameter of 13.36 mm. The sample was expanded to a container bore of 14.22 mm, for a diametral expansion of 0.86 mm. A longitudinal crack in the wick, resulting from overexpansion, measured 0.13 mm circumferentially. Subtracting this amount from the diametral expansion gave the point at which the screen reached its expansion limit, i.e., 0.82 mm in diameter.

For SPAR-6, the 2-m bladder was drawn to 13.0-mm outside diameter and the wick wrapped as above so that, when expanded, the screen was at 62% of the expansion limit. Samples of the 400 mesh molybdenum-rhenium screen tested under tension showed a 5.9% elongation at failure, which is in fair agreement with the 6.3% of the sample.

Artery development involves varying compression and number of screen layers to achieve the desired pore size and permeability. Table XV gives the parameters used to produce the final effective pore size of 45 μ m in the artery and artery wrap.

The artery wrap is used as a means of attaching the dual arteries to the distribution wick

Fig. 64. SPAR-6 and SPAR-4 configurations.

Fig. 65. SPAR-6 fabrication.

without the necessity of welding directly to the arteries. The central trough of the artery wrap provides the location for a hafnium ribbon getter (see Fig. 65). The wrap is sized to tightly capture the dual arteries and has a pore size equal to the arteries to provide capillary pumping continuity. The pore size is controlled by compacting in drawing through a die. The artery

wrap is preformed to provide parallel lobes to accept and locate the dual arteries. Preforming is accomplished in a half-shell fixture, which provides a cylindrical supporting surface the length of the wrap. A forming mandrel aids the preforming operation and is attached to the arteries so they may be pulled into the wrap. Final forming of the wrap with arteries in place is

TABLE XV
ARTERY MANDREL AND SHEATH DIMENSIONS

	Artery				Artery Wrap			
	Mandrel		Sheath		Mandrel		Sheath	
	o.d. (mm)	i.d.	o.d. (mm)	i.d.	o.d. (mm)	i.d.	o.d. (mm)	i.d.
Predraw	1.6	0.25	4.0	2.97	5.97	4.19	9.53	7.75
Postdraw	1.6	0.25	3.81				8.08	

accomplished by pressing a blade tool into the trough of the artery wrap while it is supported in the half-shell fixture.

The artery wrap is attached to the inside of the distribution wick by spot welding. The welds are performed in an inert atmosphere to prevent embrittlement. The welding head pictured in Fig. 67 was developed for these welds. It incorporates a tungsten roller electrode with alumina-coated sides, which provide abrasion resistance and electrical insulation. Electrical isolation of the head allows the sheath copper wand, the plastic-coated brass head, the roller axle, and the roller to be used as conductors. The electrical resistance of the container afforded by the screen of the artery wrap and the grounded distribution wick provides the necessary weld

heat. Weld pressure is achieved by a cantilevered leaf-spring, with the force adjustable by a set screw at the midpoint of the spring. The free end of the spring houses a second axle with free-wheeling dielectric rollers to provide the force opposing the electrode. These rollers bear on the distribution wick straddling the opposing artery wrap.

The container tube served to exclude air during the welding operation. A steady flow of argon provided an inert gas shroud for the weld. Following the welding of the artery wraps, the same welding head is used to attach the hafnium getter ribbons.

Hafnium and zirconium getter discs are used at the ends of the heat pipe. At the condenser end, the discs are captured between the fill plug and the end of the distribution wick. A hole in the center of each disc provides passage for fluid during the fill operation. At the evaporator end, a screw attaches discs to the end plug. Shallow dimples in each disc provide spacing for fluid contact.

THE LEADING EDGE OF THE SCREEN IS SPOT-WELDED TO THE MANDREL

A FULL TURN OF SCREEN PLUS AN ADDITIONAL 10° IS ADDED AND SPOT-WELDED TO ITSELF, JUST PAST THE FIRST WELD

THE TRAILING EDGE IS SPOT-WELDED TO THE UNDERLAYER

Fig. 66. Securing the edges of multiple-layered screen.

Fig. 67. Spot welder roller head.

The SPAR-6 core artery heat pipe, as a second generation design, provides a number of design improvements to the SPAR-4 configuration. Particularly important among these is the use of 400 mesh screen, which requires less compression to achieve the permeability and pore size needed for the arteries and distribution wick. Improved welding techniques used for SPAR-6 include a method of attaching arteries that obviates direct welding through the wick.

B. Core Heat Pipe Testing

1. SPAR-4 Performance Testing (F. C. Prenger, Q-13). Results of the SPAR-4 heat pipe tilt test discussed in the last report were used to determine the performance characteristics of the dual-artery design. Analysis of the tilt test data indicated an effective pore size of $80\text{ }\mu\text{m}$ and a screen permeability of $6.5 \times 10^{12}\text{ m}^2$. These parameters were used to predict heat pipe performance in calorimeter testing. The predicted limits are shown in Fig. 68 for both single-artery and dual-artery operation. Experimental data points from calorimetric testing of SPAR-4 are also included in this figure. The arteries in the SPAR-4 heat pipe were attached to the distribution wick with spot welds on 8-mm centers. Radiographic

analysis after assembly showed irregular contact between the arteries and distribution wick, especially in the first 30 cm of the evaporator, where one of the arteries was separated from the wall wick.

The lower curve in Fig. 68 represents data collected when the evaporator or heating zone contained the detached portion of the artery. In this case, only one artery appeared operational. When the heat-addition zone was moved 30 cm from the evaporator end, away from the detached portion of the artery, improved performance resulted, as shown by the single data point near the upper curve in Fig. 68. This curve corresponds to two arteries operating.

Test data are for two heat pipe orientations. One orientation has the arteries located one above the other, with the detached artery lowermost, and the second orientation has both arteries at the same elevation. For the latter case the detached artery failed to work at all, and the data follow the single-artery prediction curve at temperatures below 1208 K. At higher temperatures an abrupt increase in performance was observed. This behavior is similar to that of other heat pipes operated under similar conditions,²⁰ with the evaporator slightly lower than the condenser. Excess liquid can be held at the elevated condenser end at low temperature but can return by puddle flow to the evaporator; at higher temperatures liquid/vapor shear is reduced by diminishing vapor velocity.

Testing with the arteries located at the top and bottom of the heat pipe cross section produced a different performance characteristic. In this case the defective artery contributed to the performance at low temperature but gradually isolated itself as the temperature increased. With increasing temperature, the performance approached that of the single-artery case. This transition behavior can be explained by assuming a geometry modification within the heat pipe that alters the liquid pressure drop with increasing temperature. One such mechanism may be an increase in the artery-distribution-wick gap due to radial growth of the heat pipe with temperature. The increase in the gap would create discontinuities in the artery-distribution-wick contact area, resulting

Fig. 68. Correlation of heat pipe test data and prediction.

in an increased flow path for the liquid when wetting the evaporator wall.

2. SPAR-5 Performance Testing (H. E. Martinez and E. S. Keddy, Q-13). Following the filling and tilt testing operations described in the Fabrication and Assembly section, the SPAR-5 heat pipe was assembled on a gas-gap-controlled calorimeter. Design of the calorimeter was modified from that used for SPAR-4 to improve seal cooling. With operating temperatures to 1500 K planned, additional cooling was required to maintain the silicon rubber sealing gasket from overheating. The modified heat pipe calorimeter seal is shown in Fig. 69. The condenser end of the heat pipe was chilled to maintain a vacuum seal during operation by the chill arrangement shown in Fig. 70. The design allows for axial growth of the heat pipe at the condenser end. The heat pipe was instrumented with chromel/alumel and W/W-26Re thermocouples, and the heat pipe/calorimeter assembly was loaded

into the vacuum chamber as shown in Fig. 71. After chamber assembly and check-out, the heat pipe was slowly heated to outgas the system while maintaining chamber vacuum at less than 5×10^{-4} torr. At start-up it was necessary to load a small quantity of gas in the calorimeter to maintain the stem temperature near the chill at the condenser area less than 360 K. This loading in the condenser forced the heat pipe to operate on the sonic limit during warm-up. At about 1250 K the power input could be increased without any further increase in the stem temperature. Figure 72 shows the first exercise of the heat pipe under power. It was held at 8.2 kW and 1450 K for 1 h in accordance with the threshold value specified in the plan. Figure 73 shows a subsequent test where the heat pipe was operated at 15.6 kW and 1525 K for 5 min, exceeding the design goals of 15 kW and 1500 K. The wicking limit curves shown in Figures 72 and 73 for both laminar (on the left) and turbulent flow are based on a maximum pore

Fig. 69. Modified calorimeter/heat pipe arrangement.

Fig. 70. Design of calorimeter for chilling stem.

Fig. 71. SPAR-5 heat pipe experimental setup.

size in the evaporator of $143\text{ }\mu\text{m}$. This pore size limit was calculated from the results of the tilt test, in which a tilt angle of 55° was attained for an operating temperature of 1340 K and a heat throughput of 2500 W . Because this was a

stable operating point rather than a performance limit, the actual maximum pore size is an unknown amount less than $143\text{ }\mu\text{m}$ and, hence, performance limit curves plotted in Figs. 72 and 73 represent minimum values.

Fig. 72. First exercise of SPAR-5 heat pipe under power.

With the preliminary heat pipe tests completed, the system was shut down to prepare for a 100-h test at 15 kW and 1500 K for 100 h. The heat pipe was x-rayed, reinstrumented with additional W/W-26Re thermocouples, and reassembled in the vacuum chamber. In continued testing, a heat pipe limit was established at 12 kW at 1500 K by the evaporator dryout. After repeated dryout the limit decreased to about 4 kW. It appeared that cycling of the heat pipe further damaged the wick, probably through increase in the width of the cracks observed during the tests. Also, during the two dryouts at 12 kW, gas may have been released into the annulus. Testing was terminated and the heat pipe was removed from the calorimeter, examined, and x-rayed. The x-ray films showed an increase in width of the longitudinal wick cracks observed after testing in the tilt apparatus. Very few additional defects were observed. Final disposition of the heat pipe is pending further

evaluation. An investigation of possible causes of the wick cracking is now being conducted.

3. Thermochemical Analysis (M. Merrigan, Q-13; E. Martinez, Q-13; R. Feber, CMB-8). Instabilities in the internal chemical environment of high-temperature heat pipes are of concern in long-term operation because of potential effects on the heat pipe operation through changes in fluid surface tension, wick pore size or permeability, surface wetting characteristics, noncondensable gas generation, or low volatility fluid accumulation. In addition, complete loss of performance through penetration of the heat pipe envelope is possible. Some of these effects have been seen in prior SP-100 project heat pipes, leading to an intensified effort to identify, predict, and control sources of chemical instabilities within the heat pipes. Steps in the program for thermochemical control have included posttest analyses of heat pipe chemistry and

Fig. 73. SPAR-5 under test at 15.6 kW and 1525 K for 5 min.

metallurgy, chemical characterization of heat pipe materials and processes throughout the fabrication and assembly cycle, improved material preprocessing for cleanup, in situ cleaning during heat pipe filling and processing by hot and cold trapping, and gettering during heat pipe operation. Each of these steps has been conducted to some extent on prior heat pipe efforts. Improvements in the control of these process steps in current heat pipes are the result of additional understanding gained from the posttest analyses and from the development of detailed chemical models of the heat pipes. These models permit the evaluation of alternative gettering materials and other changes in heat pipe chemistry without test operation, thereby accelerating the development process. The starting point for the thermochemical review in this reporting period has been the posttest analysis of the SPAR-1B heat pipe, a sodium, annular wick, molybdenum heat pipe.

The SPAR-1B 2-m, molybdenum-sodium, annular heat pipe was disassembled after testing and metallographic, chemical, scanning electron microscopy (SEM), and physical measurements were made on both the screen tube and the getters from the heat pipe.

Metallographic examinations of the evaporator and condenser portions of the screen tube showed that changes in the grain size occurred; typically, grains were smaller in the evaporator. The molybdenum grain structure was single phase in all cases and free of precipitates. Optical microscopic examinations of the zirconium and hafnium getters showed some contaminants, which appeared to be hydrides or other low-atomic-number elements.

Chemical analysis for oxygen, carbon, iron, and titanium was performed on portions of the screen tube at the evaporator and condenser. Analysis showed as much as 2500 ppm oxygen in the evaporator, as opposed to 310 ppm in the condenser. Carbon was high at the inlet to the evaporator,

176 ppm, but highest at the liquid-sodium-pool area of the condenser, 245 ppm. Levels in between were lower. Titanium contamination was similar to the carbon except that the results were higher in the evaporator inlet, 160 ppm, and lower at the condenser pool area, 80 ppm. Iron analysis showed the highest quantity at the condenser pool area, 130 ppm, with a steady decrease to the evaporator inlet of 54 ppm. Analysis of the hafnium getter material for oxygen indicated 1200 ppm oxygen. The zirconium getter metal contained 41 ppm hydrogen and 690 ppm oxygen.

SEM and x-ray microanalyses were performed on sections of the screen tube in the evaporator and condenser. The SEM and x-ray analyses were performed on the inside and outside of the screen tube sections. The SEM showed the outside of the screen in both the evaporator and condenser relatively clean; that is, the wire mesh was typical of a new sample. The inside of the screen tube in the evaporator was highly contaminated with foreign material; x-ray analysis showed relatively large amounts of sodium, silicon, and smaller amounts of aluminum, calcium, and titanium. The inside of the screen tube in the condenser section was slightly contaminated, and traces of aluminum, titanium, and silicon were found by x-ray analysis.

Pore measurements performed on sections of the screen tube showed that smaller pores and fewer of them existed in the evaporator compared to measurements made prior to testing. The condenser sections of the screen tube had larger pores and more of them.

The examinations show that impurities present in the heat pipe, probably in the form of oxides of aluminum, titanium, and silicon, are concentrated at the inside of the screen tube in the evaporator, and that the liquid pool area of the heat pipe serves as a cold trap, especially for carbon. Both hafnium and zirconium appear to be good getters in the molybdenum-sodium system.

As a result of the chemical phenomena observed in the SPAR-1B heat pipe, particularly the silicon and titanium deposits on the inner surface of the wick, chemical analysis of heat pipe materials as-received and at each stage of processing was undertaken in an attempt to identify the source of the contaminants. This chemical analysis was

concentrated on the screen wick materials because prior analyses had shown that the tube material was of good quality and, because the fine-mesh wick materials have a large surface area, a slight surface contamination can lead to high levels of heat pipe contamination. The chemical analyses conducted included quantitative spectrographic analysis for the 9 elements listed in Table XVI, plus wet chemistry for oxygen, carbon, and sulfur. The results to date are summarized in the table. The tests have shown that no single process was the major source of contamination. As anticipated, the Oakite cleaning agent used on the molybdenum screens was a source of silicon, but not a major one. The same cleaning process was shown to be effective in reducing carbon levels in the screens and is being considered as a final process step after vacuum sintering of the screen materials. The tests also showed that the screen material (150 mesh) being used at present is essentially the same chemically as that purchased in 1977 and that used in the life-test heat pipe, which has operated successfully for more than 20 000 hours. Review of the results indicates that just one monolayer of oxygen on the surface of a 0.025-mm-diam molybdenum wire would yield a bulk oxygen measurement of 16 ppm. Thus, it is apparent that surface contamination of the molybdenum screen by oxygen is responsible for a large part of the observed oxygen impurity levels. Because protection of the wire from atmospheric contamination throughout the assembly process is impractical, it can be concluded that final cleanup of the heat pipe materials must take place

TABLE XVI
ELEMENTAL COMPOSITION OF THE LITHIUM-TZM SYSTEM

Element	Amount, moles
Lithium	3.85
Molybdenum	9.65
Carbon	0.008 (0.1 wt% in Mo)
Nitrogen	0.002 (30 ppm in Mo)
Oxygen	0.006 (100 ppm in Mo)
Titanium	0.094 (0.5 wt% in Mo)
Zirconium	0.007 (0.07 wt% in Mo)
Hafnium	variable
Inert gas	variable

during the filling and sealing operation to eliminate the possibility of surface contamination following the cleaning operation. In the lithium heat pipes now being fabricated, this has taken the form of a hot and cold trapping operation intended to remove contaminants from the lithium pool during refluxing of the lithium vapor over the heat pipe interior surfaces. This technique is particularly effective with lithium because of the high chemical affinity of lithium for oxygen, nitrogen, and carbon.

The selection of gettering materials to be used for continuous cleanup during heat pipe operation and during the hot trapping operation, and the selection of parameters for cold trapping, are based on the results of chemical modeling of the heat pipe as a function of temperature and species present. This modeling has been greatly improved in accuracy in the last 6 months through the use of a chemical equilibrium computer program. The computer program, SOLGASMIX, obtained from T. M. Besmann of the Oak Ridge National Laboratory, has been used to (1) predict or assess mechanisms for mass transport of molybdenum between the evaporator and condenser sections of a heat pipe, and (2) evaluate methods for control of oxygen, nitrogen, and carbon by gettering or cold trapping.

SOLGASMIX uses a free-energy-minimization method to calculate equilibrium compositions in systems that may contain a gaseous phase, multiple condensed phase solutions, and additional condensed phases of invariant or variable stoichiometry. Ideal solutions and unit activities for condensed phases are assumed. The condensed phases are also assumed to have invariant stoichiometry for present purposes. The calculations require knowledge of the free energies of formation of pertinent and possible compounds that can be formed from the chemical elements in the system. The JANAF Thermochemical Tables were used as the starting point for the thermodynamic data, but were supplemented with other data compilations, a literature search to the present, and a recalculation of data when necessary to ensure an internally consistent set of free energies of formation.

Calculations have been done for both lithium and sodium heat pipes with molybdenum or TZM walls. Table XVI shows the amounts of which elements were

assumed in baseline calculations for the lithium/TZM system. The elemental composition was based on the composition of the TZM alloy, the major impurities, provision for a hafnium getter, and the inclusion of an inert gas for convenient adjustment of the relative amounts of gaseous and condensed-phase solutions. The amounts were based on the composition of an experimental heat pipe.

Table XVII lists the species for which thermodynamic data are currently included in the calculations of lithium systems. An analogous, but not identical, set of species has been incorporated in the calculations of the sodium systems; e.g., the solubility of nitrogen in sodium does not appear to have been measured. In some cases available data do not extend to temperatures of interest; the solubility of oxygen in lithium has been measured only to 1007 K, and that of oxygen in sodium has been reported only to 828 K. The existing data are used to establish a phase boundary at the lower temperatures. Above the temperatures of the experimental data, the phase boundaries are somewhat arbitrarily extrapolated to the melting points of the oxides (1843 K for Li_2O and 1405 K for Na_2O), with guidance from experimental data for the solubility of nitrogen in lithium. The latter has been measured continuously to the melting point of

TABLE XVII
SPECIES INCLUDED IN LITHIUM SYSTEMS

Gas Phase		Liquid Lithium Solution
Li	Li_2O	Li
Li_2	LiN	C (as in Li_2C_2)
O_2	Li_2MoO_4	N (as in Li_3N)
LiO	N_2	I (as Li_2O)
MoO	CO	
MoO_2	NO	
MoO_3	Inert	

Other Solid/Liquid Phases			
Li_2MoO_4	Li_2C_2	Li_3N	Li_2O
Mo	Mo_2C	Mo_2N	MoO_2
C			
Ti	TiC	TiN	TiO
Zr	ZrC	ZrN	ZrO_2
Hf	HfC	HfN	HfO_2

Li_3N (1086 K). Implicit in this procedure is the assumption that the liquid oxides are completely miscible in the liquid alkali metals.

No data were found for the solubility of alkali molybdates in the liquid metals. The species tabulation in Table XVII corresponds to the assumption that condensed-phase lithium molybdate (melting point, 974 K), if formed, would be insoluble in liquid lithium. In order to bound the uncertainty, calculations were also made assuming that liquid lithium molybdate would be completely miscible in liquid lithium. A corresponding pair of assumptions was made for the behavior of liquid sodium molybdate (melting point, 962 K) in liquid sodium.

An attempt was made to include all species that would be significant for the objectives of the calculations, and for which thermodynamic data existed at temperatures of interest or could be extrapolated with reasonable confidence to such temperatures. One interesting species in the lithium system that has not yet been included in the absence of thermodynamic data is dilithium cyanamide (Li_2NCN). This compound has been isolated from lithium systems containing nitrogen and carbon. The extent of its solubility in liquid lithium is unknown.

Table XVIII is a shortened version of a typical output from a calculation in the lithium-TZM system at 1500 K and 1 atm total pressure; gaseous species having a partial pressure less than 10^{-15} atm are not listed. In this particular example, the gas volume corresponding to the amount of inert gas is such that 0.038 mole of the initial 3.85 moles of lithium is in the gas phase. In none of the calculations for either sodium or lithium systems are the gaseous molecules MoO , MoO_2 , or MoO_3 of importance.

From results such as those in Table XVIII, conclusions may be made concerning the efficacy of titanium, zirconium, or hafnium as getters for oxygen, nitrogen, and carbon in lithium or sodium systems. The following conclusions refer to gettering from largely liquid systems in which vaporization is minimal.

a. Lithium Systems. Titanium, zirconium, and hafnium are effective getters for carbon and nitrogen (500 to 1500 K). Molybdenum is a less effective getter for carbon and becomes worse

TABLE XVIII
ABBREVIATED SAMPLE OUTPUT FOR THE
LITHIUM/TZM SYSTEM

1500 K

1 atm total pressure

Gas Phase	Moles	Partial Pressure
Li	3.11 E-02	0.368
Li_2	3.40 E-03	4.02 E-02
LiO	1.59 E-16	1.88 E-15
Li_2O	1.99 E-09	2.36 E-08
CO	9.43 E-15	1.11 E-13
N_2	7.24 E-16	8.56 E-15
Inert gas	5.00 E-02	0.592

Liquid Li Solution	Moles	Mole Fraction
Li	3.80	0.9984
Li_2O	6.01 E-03	1.58 E-03
Li_3N	1.06 E-08	2.79 E-09
Li_2C_2	8.19 E-17	2.15 E-17

Other Phases	Moles
Mo	9.65
Ti	9.10 E-02
TiC	3.00 E-03
ZrC	5.00 E-03
ZrN	2.00 E-03

with increasing temperature. Molybdenum does not getter nitrogen at any temperature. Hafnium is the only one of the metals showing any gettering of oxygen, and that is only partial at intermediate temperatures (~50% of oxygen in the calculation at 800 K).

b. Sodium Systems. Titanium, zirconium, and hafnium are effective getters for carbon, nitrogen, and oxygen (800 to 1400 K), although there is some deterioration of oxygen gettering at higher temperatures. Molybdenum is a less effective getter for carbon and becomes worse with increasing temperature. A small amount of nitrogen is gettered by molybdenum at 800 K.

It is instructive to consider the effect of vaporization on the distribution of oxygen-containing species between the liquid and gas phases. Figure 74 is a plot of the log of the

Fig. 74. Vaporization in lithium-oxygen-molybdenum system at 1500 K.

partial pressure of Li_2O versus the percent of lithium in the vapor phase at 1500 K. Lithium oxide is calculated to be the only species of significance in the gas phase other than lithium and Li_2 . The amounts of lithium molybdate calculated to be present are so small that uncertainty about the miscibility of the liquid molybdate and lithium is of no importance. With the amount of oxygen assumed to be in the system (Table XVI), and in the absence of gettering agents for oxygen, solid Li_2O is calculated to form above ~98% lithium vaporization. However, oxygen gettering by titanium, zirconium, or hafnium, although not extensive, is sufficient to suppress precipitation of Li_2O in this situation.

A similar plot for the effect of vaporization in sodium systems at 1400 K and in the absence of oxygen gettering is shown in Fig. 75. The rapid increase in the total partial pressure of gaseous molybdates (Na_2MoO_3 plus Na_2MoO_4) is apparent as the system approaches dryness. If liquid sodium molybdate is assumed to be insoluble in liquid sodium at the level of oxygen contamination assumed, liquid molybdate is calculated to separate as a phase at ~99.5% vaporization of the sodium. If, on the other hand, the liquid molybdate is assumed to be completely miscible in liquid sodium, no molybdate separates out and the liquid sodium becomes progressively richer in molybdate as dryness is approached. Thus, in the

Fig. 75. Vaporization in sodium-oxygen-molybdenum system at 1400 K.

absence of effective gettering of oxygen, possible mechanisms exist for the mass transport of molybdenum in systems approaching dryness that involve gaseous or liquid molybdate or molybdate dissolved at relatively high concentration in liquid sodium. Contact of any of these forms of molybdate with "clean" liquid sodium can be expected to reprecipitate molybdenum. Gettering of these highly vaporized systems with titanium, zirconium, or hafnium should eliminate the features shown in Fig. 75 and thereby eliminate the transport of molybdenum. At some temperature below 1200 K, the material that is calculated to separate from the sodium system at high vaporizations and without gettering becomes Na_2O rather than sodium molybdate.

It is concluded that distributed getters will have merit in both sodium and lithium systems, particularly in regions of the heat pipe likely to approach dryness, so problems involving transport of molybdenum or plugging of wicks by precipitated oxides will be minimized.

The results of the SOLGASMIX analysis may be used to interpret the corrosion patterns seen in the SPAR-4 heat pipe. The corrosion pattern seen in the radiographs of SPAR-4 is illustrated in Fig. 76 and consists of a line of wick corrosion approximately centered between the arteries in the evaporator region of the heat pipe. This corrosion pattern is consistent with the analysis results, indicating a vapor phase mechanism for

Fig. 76. SPAR-4 corrosion pattern.

molybdenum transport that is dependent on the oxygen level in the sodium. The mechanism is shown schematically in Fig. 77, wherein the molybdates serve as vapor transport carriers for molybdenum. The SOLGASMIX analysis shows no similar transport mechanism for the lithium system, indicating that the control of oxygen in the lithium heat pipe will not be as critical as in the sodium system.

C. Heat Pipe Analysis

1. Performance Analysis (F. C. Prenger, Q-13). The design of the SPAR-6 artery heat pipe is sensitive to the location of the arteries within the heat pipe, the thickness of the distribution wick, and the artery diameter. The optimum design is achieved when a specified combination of these parameters results in the minimum dynamic pressure drop within the heat pipe.

Two bends are required in the heat pipes for the SP-100 application. The bends are coplanar

Fig. 77. Vapor phase mechanism for molybdenum transport.

and, therefore, occur such that the same neutral axis exists for each bend.

To minimize the impact the bends have on the artery integrity and attachment to the distribution wick, the arteries are placed on the neutral bend axis of the heat pipe. The number of arteries is dictated by redundancy considerations. Four arteries, arranged in pairs and diametrically opposed, were chosen for the SPAR-6 design. This arrangement allows each artery to feed a quarter-segment of the heat pipe circumference. In the event of an artery failure, the remaining good artery must feed half the heat pipe circumference, but with no increase in flow path length.

With the number of arteries and their location chosen, the ratio of liquid-to-vapor flow area was established. Figure 78 shows the heat pipe performance as determined by the artery diameter, with the wick pore size as a parameter. Each curve shows a broad maximum between 1.8- and 2.8-mm inside diameter. Artery diameters below 1.8 mm reduce the performance because of insufficient liquid flow area, while diameters above 2.8 mm cause excessive pressure drop in the vapor, a result of the arteries occupying a larger fraction of the heat pipe cross section. The effect of a reduced pore size and, consequently, an increased capillary pressure differential is clearly shown. However, continued reduction of the pore size incurs pressure drop penalties in the distribution wick, suggesting that the wick pore size and its permeability must also be optimized.

In artery heat pipe design, the distribution wick serves to distribute the liquid around the circumference of the evaporator and to collect the liquid in the condenser. The permeability of the wick is, therefore, important in determining the liquid pressure drop. The wick permeability, k , is an empirical factor defined by

$$k \equiv \frac{\dot{m}L}{\rho A \Delta p}, \quad (1)$$

where μ = the fluid viscosity,

$\dot{m}$ = mass flow rate,

L = flowpath length,

ρ = fluid density,

A = cross-sectional area, and

Δp = pressure drop.

Fig. 78. Heat pipe performance as determined by artery diameter.

Application of the momentum equation for low-Reynolds-number flows leads to the following relationship for the permeability:

$$K = \frac{\epsilon r_c^2}{b} \quad (2)$$

where ϵ = porosity,
 r_c = pore radius, and
 b = tortuosity.

This expression for permeability contains only geometry terms that describe the wick and is independent of the fluid used for permeability measurements. The terms in Eq. (2) are related, and it is difficult to modify any one term without affecting the others. For example, reducing the pore size, r_c , usually increases the tortuosity, causing a decrease in the permeability. Figure 79 shows the effect of permeability and distribution wick thickness on the heat pipe performance. Higher permeability materials give better performance. As the thickness of the distribution wick increases, the liquid pressure drop is reduced, but as with the artery size, the thicker wick reduces the vapor flow area and increases the vapor

Fig. 79. Heat pipe performance as a function of distribution wick thickness and permeability.

pressure drop. Hence, each curve in Fig. 79 has a maximum that changes with wick permeability. The results also illustrate the need for exceeding a minimum permeability. Wicks with permeabilities less than $1.0 \times 10^{-12} \text{ m}^2$ limit performance to below the design goal of 15 kW at 1400 K.

The performance degradation resulting from a failure of one or more arteries is shown in Fig. 80. If at least two arteries are located on each side of the heat pipe on the neutral bend axis, the loss of two opposite arteries has minimal influence. Figure 80 also shows the effect of losing two arteries on the same side, which is a more severe case. Under this condition, the longer flow path in the distribution wick significantly increases the liquid pressure drop.

2. Stress Analysis of SPAR-5 Wick Cracking
(M. Merrigan, S. P. Girrens, Q-13). During the SPAR-5 tilt testing, radiographs were taken that disclosed cracking in the wick. These cracks increased the effective pore size of the wick, limiting the performance capability of the heat pipe. An initial review of possible causes of the cracking led to the conclusion that thermal stressing

Fig. 80. Effect of artery failure on heat pipe performance.

of the wick during warm-up, while the lithium was still in the solid state, was a probable cause. Detailed review of the radiographs showed that the cracking was primarily along a single circumferential location over the length of the heat pipe. The cracks appeared to be tensile failures, without evidence of buckling or distortion of the crack edges. Radiographs taken after continued testing of the heat pipe did not show any new cracks or any linear growth of the existing cracks, although the cracks had grown in width. An examination of a Nb-1Zr-wick, lithium heat pipe, and a SS, lithium heat pipe did not reveal similar cracks, despite a history of repeated cold starts with these two heat pipes. The tentative conclusion from this examination was that the cracks were caused by tensile stresses in the wick and were more likely to occur in low-ductility wick materials.

A program was initiated to identify the cause of the cracking, to verify the mechanism, and to develop and test design or procedural changes to eliminate the problem. The steps in this investigation were to conduct a structural analysis of

the wick/lithium matrix in warm-up, to determine the conditions that could produce tensile stresses capable of cracking the wick, to conduct stress-deflection tests of screen wick samples, to assemble a heat pipe for a test demonstration of cracking, and to simultaneously test the resistance to cracking of some alternative wick configurations. The first of these investigative tasks, the structural analysis of the wick/lithium matrix under a variety of boundary conditions, had as its basic objective the identification of conditions that could cause tensile loading of the wick to levels capable of causing failure. In all calculations for identifying a possible tensile failure condition, a plane strain analysis was performed on a finite element model, as shown in Fig. 81. Two rows each of elements were utilized to describe the molybdenum tube wall, the lithium layers, and the equivalent solid properties of the screen wick. Material properties used in the model are summarized in Table XIX.

The first set of boundary conditions considered simulated a heat pipe evaporator warm-up condition where the lithium-molybdenum matrix is heated from the outside with a solid layer of

Fig. 81. Finite element model for inside heating of SPAR-5 with solid lithium in annulus.

TABLE XIX
SPAR-5 WICK-CRACKING ANALYSIS MATERIAL PROPERTIES

	Molybdenum	Lithium
Elastic modulus, MPa	1.93×10^5	5.0×10^3
Thermal expansion coefficient, mm/mm-K	4.0×10^{-6}	5.6×10^{-5}
Thermal conductivity, W/mm-K	0.0981	0.071
Specific heat, J/kg-K	350	3330

lithium in the annulus between the screen wick and tube wall. Results from this calculation indicated peak compressive stresses in the wick material at 145 MPa. A second model was constructed considering an inside heat source, assumed to be provided by a condensing lithium layer, which, upon solidifying, would stress the lithium/molybdenum matrix (see Fig. 82). A transient analysis of this condition was performed, again assuming a solid layer of lithium in the annulus between the wick and tube wall. Calculated results showed compressive stresses, increasing with time to a maximum of 174 MPa at steady state for a 0.076-mm lithium layer thickness. It was determined from this calculation that only when the wick was unconstrained and the lithium in the annulus incapable of supporting a load, could the screen develop tensile stresses.

The third model studied thus assumed the condition of inside heating and a nonconstraining annular lithium layer for various thicknesses of lithium inside the screen wick. The results of this calculation are plotted in Fig. 83. The tensile stresses obtained increased with increasing thickness of the lithium layer. The maximum lithium thickness assumed in the calculations was 0.3 mm, which developed a peak tensile load in the wick material of 134 MPa.

The conclusion reached from this analysis was that tensile failure of the wick because of stressing by thermal expansion of the lithium prior to melting could occur, but required a particular combination of conditions. The conditions included heating at the surface of a solidified lithium layer on the inner wick surface and the

Fig. 82. Finite element model for inside heating of SPAR-5 with solid lithium in annulus.

Fig. 83. Tensile stresses vs lithium thickness for inside heating, unconstrained screen wick model of SPAR-5.

existence of an unrestrained outer boundary on the wick such as would result from melting the lithium in the annulus. The situation that could produce these conditions is shown in Fig. 84. The heat pipe, in this hypothesis, is heated by rf induction in the tube wall, with conduction of the heat axially through the tube occurring more rapidly than the free molecular transport of heat in the interior vapor space of the heat pipe. The lithium in the annulus, therefore, melts before the lithium in the inner surface of the wick. After the lithium in the annulus has melted, and before the inner layer of lithium has melted, a thermal pulse is introduced at the inner surface. The mechanism conjectured for this sudden increase in inner surface heat input is the transport of liquid lithium along the lower inner surface of the wick, either by gravity during tilt tests or by entrainment in the high-velocity lithium vapor stream. If this hypothesis is correct, the solution to the cracking problem may be procedural, consisting primarily of ensuring that the heat pipes in test are heated to above lithium melting temperature (approximately 180°C) before tilting, or by rapidly increasing the evaporator heat input. It is also possible that the use of more ductile wick material, such as the Mo-41Re alloy, 400 mesh material, will eliminate the cracking, although yielding of the wick material may still

Fig. 84. Lithium heat pipe wick-cracking model.

occur because of distortion of the wick structure in lithium expansion. Verification of these hypotheses is the objective of the second part of the investigation, which consists of the fabrication and operation of a test heat pipe.

The configuration of the wick-cracking experimental heat pipe is shown in Fig. 85. The experimental wick samples in the heat pipe include a duplicate of the SPAR-5 wick, a sample of the wick planned for use in SPAR-6, and a number of samples of reinforced wicks and ductile materials and configurations, as listed in the figure. After this heat pipe is filled, the test plan includes wet-in in a horizontal position with a full-length rf coil, cycling through a number of rapid melt cycles, start-up and cooldown with a half-length rf coil, and melt-tilt testing with the half-length coil. Radiographs will be taken before the start of the testing and at the conclusion of each test phase. If no cracking is induced by these tests, the experimental program will be expanded to include dry-out of the wick over a short length of the pipe and other tests as necessary.

At the conclusion of the wick-cracking test program, the results will be reviewed, analytic models correlated, and fixes for the problem selected. These fixes will then be implemented in the SPAR-6 heat pipe or in its test procedures. It is not anticipated that a similar problem will occur with the SPAR-6 heat pipe because of the more ductile wick materials used and the fact that the wick in SPAR-6 is directly supported by the wall, rather than being spaced away as in the

SAMPLES

1. SPAR-5 CONTROL, 150 MESH, SINTERED LCAC MOLYBDENUM
2. 20 MESH INTERNAL REINFORCED 150 MESH LCAC MOLYBDENUM
3. 20 MESH ANNULAR REINFORCED 150 MESH LCAC MOLYBDENUM
4. SPIRAL WRAP 150 MESH ANNULUS
5. HIGH-DUCTILITY SAMPLE (SS)
6. ARTERY WICK--400 MESH, Mo-41 RE DISTRIBUTION WICK AND ARTERY

Fig. 85. Wick-cracking experimental heat pipe.

annular configuration. In addition, the performance of the SPAR-6 configuration will be less sensitive to cracking than that of SPAR-5 because of the arterial configuration. However, despite these reassurances, assembly of the SPAR-6 heat pipe will be delayed for incorporation of the wick-cracking fixes.

D. Alternative Wick Fabrication (W. J. McCreary, D. W. Carroll, and W. H. Zimmer, CMB-6)

Development tests were reported in the July to September, 1981 Progress Report¹⁹ on a 1000-mm-long, 2.5-layer tube, rolled from 150 mesh molybdenum screen, that was CVD-tungsten-coated to reduce the effective capillary pore size. The results showed that CVD tungsten could uniformly reduce the effective pore size of the wrapped tube.

During the current reporting period, a 6.5-layer-thick screen tube of the same 150 mesh molybdenum screen was formed by lightly drawing it within a mild steel sheath. After removal of the steel by dissolution in HCl, the screen tube was rinsed in (1) tap water for 2 h, (2) deionized water, and finally (3) absolute alcohol.

The CVD apparatus shown schematically in Fig. 86 was used to coat the 1000-mm-long, drawn molybdenum screen tube. The screen was mounted on a rotating shaft, and the reactant gases were metered through small holes in several 4.76-mm-diam tubes. Four 30.5-cm heating zones were controlled by thermocouples located at the midpoint of each zone.

One thermocouple located at the midpoint of the screen cylinder inside the rotating shaft indicated that the cylinder was at the furnace temperature. Typical coating conditions were

Zone temperature: 780 K

H₂ flow: 250 cm³/min

WF₆ flow: 2 x 45 cm³/min (45 cm³/min each tube)

System pressure: 13 torr

Shaft rotation: 18 rpm.

The screen tube was coated during four 1-h runs and two final runs of 30 min each. Three runs (nos. 3, 4, and 5) were made using the alternate WF₆ single delivery tube in attempts to even out the coatings by admitting WF₆ into the center of the reactor. This was not particularly effective. In the final run, the temperature in the zone at one end of the furnace was increased to 600°C (873 K) and resulted in an increased coating rate at that end. The final coating results, listed in Table XX and shown in Fig. 87, appear to be satisfactory.

Figure 87 also shows that the uniform screen structure was distorted by the initial drawing process.

The final results appeared satisfactory and this wick may be used to fabricate a smaller test heat pipe.

Scale-up of the present coating apparatus to accommodate wicks 2000 mm long is a reasonable

Fig. 86. CVD coating apparatus for 1-m-long heat pipe.

TABLE XX
COATED WIRE DIAMETER VS LOCATION ON
CVD-TUNGSTEN-COATED SCREEN (1.0 m)

Location:	Heavy End	+ 30.4 cm	+ 50.8 cm	+ 91.4 cm	Light End
Coated Diameter:	76.7 μm	66.7 μm	66.3 μm	61.2 μm	59.3 μm

goal and would not require major design changes. Lengths greater than 2000 mm would require a substantial engineering effort and construction of a new coating system.

In a related effort, the feasibility of attaching an annular screen tube wick to the inside diameter of a heat pipe by CVD tungsten deposition was evaluated. A prototype was fabricated by placing a mild steel tube inside a tube of 5 to 6 wrapped layers of 400 mesh Mo-41Re screen. The screen was then inserted into a 1.4-cm-i.d. TZM tube and the steel tube pressurized, forcing the screen wick radially outward into contact with the TZM tube.

The mild steel was subsequently dissolved, and the remaining TZM-wick assembly was sectioned to provide substrates for CVD attachment coating experiments. Tungsten hexafluoride and hydrogen were admitted into a 7.5-cm-long tube sample and tungsten deposition occurred under the following conditions:

Zone temperature: 450°C (723 K)

WF₆ flow: 20 cm³/min

H₂ flow: 1000 cm³/min

System pressure: 20 torr

Time: 12 min.

Examination of the coated sample indicated that the deposition of tungsten had occurred only in the innermost one-half of the screen layers, with no coating apparent at the screen-TZM interface. This indicates that the deposition rate was too high.

Future experiments will vary the coating conditions to increase diffusion through the screen structure and produce uniform coating.

E. Heat Pipe Test Facilities (E. A. Brown, Q-13)

Test facility development in support of the core heat pipe test operations during the current reporting period has included development of an improved control for the 125-kW rf induction power supply and continuation of investigations of alternatives to rf heating, such as the use of

a. Heavy end +6.3 cm.

b. +24.1 cm.

c. +50.8 cm, center.

d. +76.2 cm.

e. Light end, +95.2 cm.

Fig. 87. Tungsten-coated, 1-m-long molybdenum wick (68X). Locations are from the heavy end.

resistance heating elements or direct resistive heating of the heat pipe wall.

Closed-loop control of heat pipe temperature is complicated by the large change in heat pipe thermal time constant with operating level. Preliminary data indicate that the time constant changes from 1 or 2 min at very low power to a few tens of milliseconds at high power. This time constant has a direct effect on the dynamic response and stability margin of the temperature.

The present investigation of high-power rf induction heating control systems has as an objective the development of a closed-loop system with stability independent of power setting. Another control problem with the rf heaters has been a shift of operating temperature with the power supply operating at steady state. Transients as high as 60 K have been observed, and 20- to 30-K transients and static offsets occur fairly frequently. Constant monitoring is required to make the power adjustments needed to correct the temperature shifts. Recent data indicate that the major cause of the shifts is fluctuation in heat pipe input power, caused by supply line voltage changes. A secondary factor may be fluctuation of the calorimeter cooling water flow rate. A new high-voltage control system was installed on the 125-kW rf generator after completion of the SPAR-5 tests to eliminate these fluctuations.

A heat flux of 100 W/cm² is required for the heat pipe operating at 15 kW. Reasonable design margins require a heat source and heater capacity of approximately 150 W/cm² plus an allowance for furnace heat losses. Radio-frequency induction heaters are the only currently available commercial systems that will satisfy the power requirements in a vacuum with the heat pipe operating at a temperature of 1500 K.

The rf induction heating systems currently used for heat pipe tests have the following disadvantages:

- They require approximately 8 m² of floor space.
- Their efficiency is only 10 to 15%, which translates to a line requirement of 1.5 MW for the nine generators required for verification testing.

- Acquisition cost is approximately \$60,000 each, and service costs are very high.
- Radio-frequency pickup causes serious instrumentation problems.
- A safety hazard is associated with their 12-kV operating level.

Several alternative heating systems are being investigated. One promising system involves connecting electrical leads to the ends of the evaporator section of the heat pipe and passing current through the sheath. The electrical operating levels would be approximately 6.25 V and 2400 A for 15 kW of power. Except for frequency, this type of heating is identical to the inductively coupled current heating achieved with rf systems.

A second alternative heating system involves the use of two 30-cm-long molybdenum blocks, which are clamped to the evaporator section of the heat pipe. Six electrical heaters are clamped into the molybdenum blocks to provide heat. Two types of heaters are being developed, one sheathed and one unsheathed.

The sheathed heater, which is on hand, is shown in Fig. 88. The compacted BeO insulation used in this configuration is the best material available in terms of good thermal conductivity and electrical resistivity at high temperatures. Its questionable features are the integrity of the joint between the 0.5-mm, W-5Re heater wire and the 2.5-mm tungsten lead, and the small amount of surface area of the heater wire for heat transfer.

The outer component of the unsheathed heater is a hard-fired ceramic cylinder of alumina or boron nitride. A W-5Re heater wire, wound in the form of a helix, is fitted into a hole in the cylinder. The hole is then filled with an alumina slip and the element is baked. The advantages of this design are low cost and increased heater wire area. The disadvantage is a poorer insulator from the standpoint of thermal conductivity and electrical resistivity.

The third alternative heating system being considered requires a layer of plasma-sprayed alumina on the evaporator section of the heat pipe. A heater is then wound or deposited on the alumina, and a second coat of alumina is applied. This design eliminates the molybdenum blocks but is not without problems. The integrity of the

Fig. 88. High-temperature electric heater.

relatively thick layer of alumina on the molybdenum heat pipe is questionable. Also, there are inherent scheduling problems and damage hazards associated with fabricating heaters directly on the heat pipe.

F. Materials

1. Molybdenum Heat Pipe Corrosion Studies (G. W. Fly, Q-13). A series of tests is being run to obtain information on the corrosion of molybdenum/sodium and TZM/sodium heat pipe materials in contact with the UO_2 fuel. To chemically simulate the SP-100 reactor core environment, TZM and molybdenum capsules containing UO_2 are placed inside TZM/sodium and molybdenum/sodium heat pipes. Empty capsules are placed in heat pipes as control samples.

Total test hours vary with individual samples to a current maximum of 9200 h. Table XXI gives the test conditions, and Table XXII shows the current or maximum number of hours and mode of failure where applicable. Two of the samples failed because of leaks in the fill tube, and two samples failed because of a leak in the wall after only a few hours operation. One heat pipe, Sample A, failed after 4475 hours because of a leak in the wall, while three other heat pipes are still operating. The failure mechanisms in all failed heat pipes do not involve fuel interaction.

Posttest examination of Sample 1, a Mo/Na/TZM/ UO_2 pipe, has been completed. This pipe failed because of corrosion through the heat pipe wall after 14 h of operation. The TZM fuel container showed no increase in oxygen concentration nor any oxygen gradient across the wall; thus, it

appears that this pipe failed for reasons other than oxygen transfer from the fuel. (There is a high probability that oxygen was introduced into this unit at the time of assembly because of difficulties encountered in the capsule sealing operation.) Posttest analysis of Sample A showed little or no corrosion of the heat pipe wall. The leak appeared to have been caused by a thermal cycling crack, which occurred during a restart after a radiographic examination of the pipe. Some minor corrosion of the screen wick was evident. However, no reaction between the molybdenum fuel container and the UO_2 was noted in metallographic examination.

2. Molybdenum-Rhenium Development (M. Merrigan and G. W. Fly, Q-13). Molybdenum-rhenium alloys are being developed for use in the core heat pipes for the SP-100 reactor. As a result of a thorough literature search, which has indicated that Mo-13Re would be the optimum alloy composition for our application, we are currently producing an ingot of this alloy by EB melting. The material used was previously arc-cast into a 4-in. ingot and hot-extruded into a 2-in. round at Oak Ridge National Laboratory (ORNL). Chemical analysis was performed on the extrusion and a high carbon level was detected, leading to the decision to EB melt to purify the material. The extrusion was sent to Rocky Flats Nuclear Plant for the EB melting. Rocky Flats melted the extrusion into two 2-in. ingots, which were intended as feed stock for producing a 4-in. ingot. However, the Rocky Flats EB melter did not have sufficient power to melt the

TABLE XXI
CORROSION TEST MATRIX

Sample No.	Container Material	Working Fluid	Capsule Material	Fuel Present	Test Temp (K)
1	molybdenum	sodium	TZM	yes	1425
3	molybdenum	sodium	TZM	yes	1400
4	molybdenum	sodium	TZM	no	1500
5	molybdenum	sodium	TZM	yes	---
6	TZM	sodium	molybdenum	yes	1380
7	molybdenum	sodium	molybdenum	no	1475
8	TZM	sodium	TZM	no	---
A	molybdenum	sodium	molybdenum	yes	1500

TABLE XXII
MOLYBDENUM/SODIUM/UO₂ CORROSION TESTS
STATUS AND AVAILABLE RESULTS

Sample No.	Description	Failure Mode	Hours
1	Mo/Na/TZM/UO ₂	leak in wall	14
3	Mo/Na/TZM/UO ₂		4400
4	Mo/Na/TZM/unfueled		9200
5	TZM/Na/TZM/UO ₂	leak in fill tube	0
6	TZM/Na/TZM/UO ₂		4400
7	Mo/Na/Mo/unfueled	leak in wall	24
8	TZM/Na/TZM/unfueled	leak in fill tube	0
A	Mo/Na/Mo/UO ₂	leak in wall	4475

4-in. ingot because of beam dispersion problems, which led to electrode failures in the melter.

A small (37-lb) 4-in. ingot was obtained from the Rocky Flats melt. This ingot has been shipped to ThermoElectron Corporation for ingot conditioning and canning. The canned ingot will be extruded and returned to ThermoElectron for machining. This material will then be used for initial characterization of the alloy and may be used to make EBR-II heat pipes.

The remaining material from Rocky Flats, along with enough additional material to produce 150 lb of the alloy, was shipped to GNB Corporation. GNB has a large EB melter capable of producing a 4-in. ingot in molybdenum-rhenium. They have sufficient pumping capacity in their melter to work in the

10⁻⁵-torr range and are currently modifying their power supplies to allow them to use 110 kW on each of three EB guns. With this modification complete, they anticipate no trouble melting the 4-in. ingot.

A specification for molybdenum-rhenium alloy tubing is being developed. The specification addresses chemistry, grain size, mechanical properties, weldability, fabrication procedures, inspection procedures, surface finishes, and dimensional tolerances. This specification is being developed with input from the prospective producers of molybdenum-rhenium alloy tubing.

3. Fuel Compatibility Studies (E. K. Storms, CMB-3). Accelerated studies have been conducted to determine the compatibility between the SP-100

UO₂ fuel and various structural materials. These materials consist of molybdenum, Mo-46Re, tungsten-coated TZM, and uncoated TZM.

Two separate studies were made. The first study involved heating UO₂ in contact with TZM for 287 and 428 h at 1800 K in vacuum. The second study was made using a stack of materials in which TZM coated with a 0.001-in.-thick layer of tungsten, molybdenum, and Mo-46Re were heated in contact with UO₂, as shown in Fig. 89. Test time was 262 h. After heating, the oxygen content of the UO₂ was determined, and the metals were examined metallographically.

Analysis of these accelerated tests required that the oxygen content of the UO₂ be determined with higher precision than can be done chemically. A technique was developed based on a measurement, using a mass spectrometer, of the ratio between the gaseous species UO(g) and UO₂(g) that vaporize from solid UO₂ at high temperatures when the pulverized solid is contained in a Knudsen cell and heated to about 1800 K. This ratio is directly proportional to the oxygen

Fig. 89. Fuel compatibility test.

Fig. 90. Pressure ratio of UO/UO₂ vs UO₂ stoichiometry.

activity, which in turn, is very strongly influenced by the oxygen concentration in the solid, as shown in Fig. 90. The figure is based on previous direct measurements of the UO/UO₂ ratio and oxygen activity.

The assembly consisted of a ring of UO₂ sandwiched between two massive pieces of TZM. Based on the UO/UO₂ ratio, the initial composition of the UO₂ was UO_{1.995+0.0001}, as indicated in Fig. 91. After 287 h, the average composition had changed to 1.977, and free uranium had formed at the interface. Further heating for 141 h caused the composition to move slightly to 1.976 and more uranium liquid to form. The small difference between the measured composition and the value at the phase boundary (1.970) is caused by the concentration gradient between the surface and the interior of the UO₂ disc. We can conclude that TZM is not stable in contact with any composition in the UO₂ system. The titanium and zirconium contained in the alloy can reduce the UO₂ to free metal. Photomicrographs show that titanium and zirconium were converted to their oxides and that some uranium had diffused from the interface.

Fig. 91. Oxygen loss from UO₂ in contact with TZM at 1800 K.

Next, a study was made to determine if TZM could be protected by a tungsten coating and if molybdenum and Mo-46Re were unreactive as expected. A stack was made, as shown in Fig. 89, that placed one surface of the UO₂ discs in contact with each of the materials. A hafnium foil was placed behind each metal disc to act as an oxygen sink. Heating was continued for 262 h. During this time the temperature of the TZM went from 1769 K to 1839 K, with the molybdenum-UO₂ perhaps 300° lower.

No reaction was found at the molybdenum-UO₂ or the Mo-46Re-UO₂ couples. Not only did these materials not react, but they prevented diffusion into the hafnium sink. A liquid formed at the hafnium interface in both cases, which ensured good contact. The couples lost weight because of the vaporization of UO, UO₂, and MoO₂.

The tungsten-coated TZM, on the other hand, did react. The UO₂ changed composition from 1.995 to 1.989 as shown in Fig. 92, but no free uranium was evident. This change in composition was less than that expected based on the experi-

Fig. 92. Oxygen loss from UO₂ in contact with tungsten-coated TZM at 1800 K.

ence with the uncoated TZM. However, because of the uncertain temperature, a quantitative comparison is not possible.

The study has given the following information:

- TZM can reduce UO₂ to free uranium;
- a coating of tungsten on TZM reduces the diffusion rate for oxygen;
- molybdenum and Mo-46Re are nonreactive to UO_{1.995} and may be used as coatings on TZM to reduce the oxygen diffusion rate; and
- a very sensitive method has been developed for determining the composition of UO₂ and the results of compatibility studies.

V. RADIATOR HEAT PIPE TESTING (S. P. Girrens, Q-13, and D. M. Ernst, Thermacore)

The filling and testing of RAD-3, a 5-m-long, dual-artery, titanium/potassium heat pipe with a D-shaped cross section and very lightweight

structure, was completed at Thermacore in October 1981. The 5.5-m heat pipe was filled with 260 g of potassium, and heat pipe testing was performed under space-simulating conditions in a water-cooled vacuum chamber at 10^{-7} torr. Heat input to the evaporator was provided by rf heating. Figure 93 shows the 5.5-m heat pipe constrained in the support apparatus before assembly into the vacuum tank.

Tests on the 5.5-m heat pipe were designed to obtain low-power start-up characteristics and achievement of design power transport at an operating temperature of 775 K. The testing of high-temperature performance limits was not possible because the heat pipe was uncoated (a high-emissivity coating is required for radiative heat rejection at these higher powers). Figure 94 illustrates the variation in axial temperature profiles as power is increased during start-up. The good correlation between analytical prediction and start-up power measurements, shown in Fig. 95, indicates that the start-up capability of the heat pipe is very stable and predictable in the lower power range. As shown in Fig. 96, data were obtained covering a wide operating temperature range of 180 K.

Deviation of measured operating powers from the predicted performance levels might be a result of the assumption of the shot-blasted distribution surface being equivalent to a 0.127-mm thickness of 50 mesh screen in the analytical calculations. The maximum heat pipe power attained was 1.86 kW at an average operating temperature of 760 K. This data point, 63% of design power, was a significant result because the heat pipe reached a heat input dryout limitation. This limitation was a result of the shot-blasted distribution wick's inability to distribute enough working fluid for evaporation. This result, then, indicates the necessity for wire screen supplementation in the evaporator section of the heat pipe to increase the fluid distribution capability. A comparison between the analytically predicted axial-temperature variation and the measured profile is shown in Fig. 97. The heat pipe was also operated successfully against gravity (pipe tilted so that condenser section is lower than evaporator), thus demonstrating artery fill and operation.

The successful design, fabrication, filling, and testing of the 5.5-m heat pipe represent an important technological advance in the development

Fig. 93. Titanium/potassium heat pipe before assembly into vacuum tank.

Fig. 94. Axial temperature profiles of RAD-3 during start-up.

Fig. 96. Measured operating powers of RAD-3.

Fig. 95. Measured start-up powers of RAD-3.

Fig. 97. Comparison of analytical and measured temperature profile at maximum operating power.

of very long, lightweight, high-temperature heat pipes for space applications. Its length of 5.5 m makes it the longest alkali metal heat pipe ever tested. Further experiments on a similar heat pipe design utilizing a wire screen for the evaporator distribution system and a high-emissivity coating in the condenser would allow testing of heat pipe performance limits.

VI. THERMOELECTRIC MODULE DEVELOPMENT (G. W. Fly, Q-13)

During this reporting period, the tests on the thermoelement were continued for a short while. Difficulties with the heating assembly caused a premature shutdown of the test. A new heater was designed and constructed and a new method of attaching lead wires to the thermoelement was developed, but no additional thermoelements were completed because of lack of funding for this effort.

The data from the tests that were run were reduced during this period. The results indicate that, even with the limited amount of data available, the three basic test objectives were met. These were the determination of the interface bonds, the measurement of the interface temperature drops, and the performance of a primary evaluation of the electrical characteristics. While no long-term module performance can be inferred from such short-term data, these tests did demonstrate the basic integrity of the module, and both the thermal and electrical performance were close to the predicted values for the operating points reached during the test.

Some modifications were made to the baseline design to facilitate testing. Figure 98 shows a cross section of the tested thermocouple with all of the various bonding layers marked. Figure 98 also shows the numbered instrumentation thermocouple locations for the test. The basic alterations to the module design that were incorporated into the test apparatus were a change from 0.025-cm molybdenum to 0.1-cm tungsten insulator backings and thermocouple interconnects, a change from silicon to SiGe-GaP hot and cold shoes, and the elimination of the curved surfaces on the hot side. The first two changes were made to allow instrumentation of the insulator backings and interconnects, and to provide a better expansion match between these parts and the thermocouple.

Fig. 98. Cross section of modified thermocouple.

The improved expansion match was required because of the increased thickness of the pieces. The final change simplified construction of the thermocouple. Only the change to SiGe-GaP for the hot and cold shoes significantly altered the performance of the thermoelement, but this change was factored into the performance predictions.

Because of the heater problems during this test, the maximum hot shoe temperature achieved was 1070 K, and this was reached just prior to heater failure. The highest steady-state temperature was 975 K, and this operating point is used for all of the performance calculations. The heat fluxes for this operating point are approximately 60% of the heat fluxes to be used in the module, so the measured temperature drops should adequately represent the bond integrity and interface conductivity. Neither the increase in temperature nor heat flux should dramatically alter the integrity or conductivity of the interfaces. Table XXIII shows a listing of both the calculated temperature drops and the measured temperature drops. The calculated values in this table are based on the actual tested configuration. These data indicate that the bond integrities are good and that the interface conductivities are close to the predicted values.

The thermocouple was delivered with the instrumentation holes 7 through 10 on the n leg missing. Because of the risk of drilling these holes after the unit was bonded to the niobium upper plate, the test was put together without these instrumentation thermocouples. The overall output voltage could be measured by comparing thermocouples 17 and 18. Unfortunately,

TABLE XXIII
INTERFACE TEMPERATURE DROPS

Interface	Measured	Calculated
T11-T6	30	30.4
T6-T5	63	73.0
T5-T4	82	80.5
T4-T2	242	246.0
T2-T1	28	31.2
T1-T17	24	19.8
T17-T14	33	28.9

thermocouple 18 broke during the test and no data from the n leg could be obtained while current was flowing. All of the p-leg instrumentation remained intact, and some useful electrical performance data were derived from the test.

Voltages at the hot shoe and output leads were available, so the p- and n-leg Seebeck coefficients could be found separately. The Seebeck coefficients were close to the previously measured values ($S_n = 200 \mu\text{V/K}$, $S_p = 245 \mu\text{V/K}$). By measuring the voltages at the p-leg hot and cold shoes and measuring the current, the p-leg resistivity was determined. The integrated average value, $2.56 \text{ m}\Omega\text{-cm}$, was slightly lower than previous measurements for this operating condition. Because we could not measure the voltage drop on the n leg, its resistivity could not be determined directly. However, if we assume that its resistivity is the same as previously measured (since its Seebeck coefficient is the same, this is a reasonable assumption), the internal resistance of the module can be calculated. This calculated value is $9.5 \text{ m}\Omega$. The external resistance can be altered, but for this test, the minimum external resistance (because the output lead resistance was high) was $61 \text{ m}\Omega$. Consequently, the peak power condition could not be reached. This point can, however, be extrapolated from the available data, as shown in Fig. 99. In addition, the output power can be predicted for the given external resistance and compared to the measured value. The predicted power for this operating condition would be 0.250 W , while the measured power is 0.256 W . The calculated peak power for this operating point would be 0.508 W , while the peak power extrapolated from the measured voltage-current plot would be 0.525 W . Clearly, the electrical performance of the thermoelement appears to be quite good.

While this test was short lived and the prescribed operating point was not reached, the objectives of the test were achieved. In addition, a method for attaching leads has been developed that will reduce the output lead resistance to a few milliohms, and a new heater has been constructed that will survive the testing. These alterations need to be incorporated into a new test device, and testing needs to be done to confirm these results, but these preliminary data

Fig. 99. Extrapolated peak-power condition.

indicate that such tests should be quite successful.

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