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Progress Report

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Space Reactors

January—March 1981

Los Alamos Los Alamos National Laboratory
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Prepared by

William A. Ranken
Space Reactors Group Leader

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SPACE REACTORS

January-March 1981

Prepared by

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ABSTRACT

Significant results have been obtained in Space Reactors design and experimental programs. Missions and applications for nuclear reactor space power supplies have been identified through the deliberations of the DOD/NASA/DOE Working Group. Differences in results obtained by two calculational techniques have been resolved for the reactor water immersion criticality studies. The in-pile test capsule is progressing, as is the fabrication of artery heat pipes. Corrosion studies continue on $\text{UO}_2/\text{Na}/\text{Mo}$, TZM systems; one capsule has failed. Potassium/titanium heat pipe corrosion tests continue successfully. Thermophysical measurements of the GaP-bonded SiGe alloy used in the thermocouple work have shown a higher-than-expected figure of merit value.

I. EXECUTIVE SUMMARY (W. A. Ranken, Q-15)

Components for a 10-to 100-kW_e space nuclear reactor electric power plant are being designed, fabricated, and tested. Such a power plant will consist of (1) a nuclear reactor for generating heat, including heat pipes for transferring the energy from the reactor to the power conversion equipment; (2) a radiation shield to attenuate radiation to an acceptable level for the payload; (3) thermoelectric (TE) converters to convert heat to electricity; and (4) a radiator for rejecting waste heat to space. The components being designed and tested, when incorporated into a power plant, will provide a relatively inexpensive, compact power source for large, earth-orbiting satellites that the Department of Defense (DOD) is evaluating for communication and surveillance.

Significant results have been obtained in the design and experimental programs, as follows.

- Through the deliberations of the DOD/ NASA/DOE Working Group, potential DOD missions enabled by nuclear reactor space power supplies have been identified; the need for nuclear electric propulsion for NASA outer planet missions has been delineated; and projections have been made for future capability of both solar photovoltaic and nuclear reactor power supplies.

- Continued analytical investigations of the effect of water immersion on the Space Power Advanced Reactor (SPAR) criticality have resolved the differences in the multiplication constant obtained by Monte Carlo and discrete ordinate transport theory calculations. The latter involve homogenizing the heat pipes with core materials in discrete annular sections of the core, but fail to adequately assess the very significant decrease in neutron leakage from the core that occurs when the vapor spaces of the core heat pipes are filled with water.

- The in-pile test capsule has been extensively redesigned to make greater use of sodium bonding for heat transfer. UO_2 fuel tiles with three different percentages of ^{235}U are in production.

- A new method of sealing sodium/molybdenum heat pipes has been devised that avoids potential cracking problems arising from the current method of flattening and seam welding molybdenum tubing.

- Successful joining of arteries to distribution wicks has been achieved and encouraging results obtained on drawing down the molybdenum heat pipe tube to make close contact with the distribution wick.

- Corrosion studies on $\text{UO}_2/\text{Na}/\text{Mo}$, TZM systems are proceeding. Two capsules have operated for 1215 h at ≥ 1475 K while one capsule, in which the sodium was apparently exposed to air during assembly, developed a hole after only 13.5 h at 1435 K.

- Potassium/titanium heat pipe corrosion tests have shown no sign of interaction during 6511 h of operation above 800 K. For 4797 h at ~ 900 K, the only indication of reaction was a small deposit of material on the wall of the titanium tube adjacent to the top of the potassium pool.

- Measurements at the Purdue Thermophysical Properties Research Laboratory of the thermal conductivity of GaP-bonded SiGe alloy have verified Synchal Corp. measurements below 1000 K and shown that extrapolation to higher

temperatures had been too pessimistic. The result is a 5% higher figure of merit value than previously quoted for optimum material.

II. MISSION REQUIREMENTS (D. Buden, DAD/NP)

A. Space Reactor Power System Working Group

The DOD/NASA/DOE Working Group established in Feb. 1980 to delineate the future needs for power in space, investigate the technology status for reactor power, define the solar/battery system state-of-the-art, and develop a program plan to meet future needs for power in space has completed much of its work. Nuclear reactors are an enabling technology above 25-50 kW_e power, and the Working Group recommended a continued development program for space reactors.

A list of potential DOD missions is shown in Table I. The use of space can act as a military force multiplier--that is, space systems can increase the effectiveness and capability of other military systems. Some of the ways this can be accomplished are tabulated in Table II. In particular, systems that require high power in space--and, thus, will be serious candidates for nuclear reactors--can greatly improve our worldwide surveillance capability without dependence on foreign bases; will improve command, control and communications; and can reduce the effectiveness of enemy surveillance systems. Table III lists the advantages of reactors for these missions. Systems that require more than 25 kW_e are considered power enabled. Survivability is increased not only by having a harder power supply, but by having sufficient power to enable the use of higher power electronics that are harder and also to provide a maneuverability capability through either electric propulsion or more compact spacecraft design. Nuclear electric propulsion not only increases the mass of a spacecraft that can be deployed to high orbits, but also allows low orbit assembly and check-out--a feature that should significantly reduce the cost of developing and deploying future large spacecraft.

The potential use of space reactors by NASA appears to be in two areas of application: power and transportation. Space power reactors providing electrical energy to satellites in Earth orbit or, for surface stations on either the moon or planets, have an advantage over solar electric power generation concepts in that they are compact and independent of lighting conditions. For example, a space reactor as a power source can provide energy to high-capacity communications satellites and to spaceborne manufacturing

TABLE I
DOD POTENTIAL APPLICATIONS

Application	Power Level	Agency
Space-based radar	10 - 400 kW	NAVY (TSS) AFSC/RADC DARPA
Surveillance	30 - 100 kW	DARPA
Communications	100 kW	DARPA
Orbital transfer vehicle (Nuclear electric propulsion)	>100 kW	AFRPL
Space jammer	20 - 200 kW	DARPA

Particle beam ^a	10 - 100s MW	AFWL DARPA AFOSR
Lasers ^a	10 - 100 MW	AFWL, DARPA

^aSPAR technology program--10-400 kW with possible growth potential to 1 MW.

facilities on the surface of the moon or planets. Reactor power for transportation applications requires the integration of the power source with a thruster subsystem to make what is usually called a Nuclear Electric Propulsion System, or NEP. Here again, there are possible applications for NEP in Earth orbit as a payload transport vehicle or on planetary missions as a primary propulsion system needed to effect the heliocentric transfer and orbital maneuvers.

The types of missions and the transportation needs of planetary missions are as follows:

- Reconnaissance missions--flyby class spacecraft of 200-800 kg not including propulsion.
- Exploration missions (planetary orbiters and/or small body rendezvous--orbiter class spacecraft of 750-2000 kg not including propulsion.
- Intensive investigation and sample return missions--multiple vehicles, multiple spacecraft of ≥ 2000 kg not including propulsion.

TABLE II
SPACE SYSTEMS ARE MILITARY FORCE MULTIPLIERS

Systems	Functions	Military Values
Surveillance	Worldwide coverage	Increase quantity and quality of data for C ³
• Space-based radar	Improved detection	Improve aircraft and ship survivability
• Optics	Increased number of objects tracked	Enhance target acquisition and kill probability
Communications	Underwater communications with submarines	Improve C ³
		Increase survivability
Jammers	Reduce enemy radar effectiveness	Increase survivability of fighters, strategic aircraft and cruise missiles
		Add to enemy resource commitment
Nuclear electric orbital transfer vehicle	Open up high orbits to large payloads	Provide active satellite defense
	Provide for maintainable spacecraft	Field survivable command center
		Reduce space system cost

In the early days of space exploration, missions were characterized as reconnaissance flights and typically utilized relatively small spacecraft with masses in the range between 200 and 800 kg. As the exploration of space continued, and owing to its great success, a more sophisticated and ambitious class of spacecraft became necessary to achieve the expanded mission objectives. These were the orbiter class spacecraft with masses in the range between 750 and 1500 kg. Orbiter class spacecraft are appropriate for the exploration class missions.

Shown in Fig. 1 is a performance comparison for the Neptune orbiter mission where Nuclear Electric Propulsion is compared with Solar Electric Propulsion and the ballistic trajectory mode. Performance is characterized by mass in orbit as a function of flight time. From the figure it is apparent that

TABLE III
WHY NUCLEAR?

Advantages	Significance	Benefited Systems
Power enabling	Need not compromise military missions	Space-based radar
		Optics
	Reduced development restrictions on other components	Jammers
	Smaller antennas	Communications
Improved survivability		Orbit transfer vehicle
	Increases electronic components hardness	Space-based radar
	Hardened power supply	Optics
	More maneuverability	Jammers
Low orbit assembly and check out	Intrinsic shielding	Communications
Reduced mass limitation	Increases deployment success	Space-based radar
	Decreased development	Jammers
Reduced mass limitation	Transfer large masses to high orbit using on-board power	Space-based radar
		Optics
	Significant cost benefits	Jammers
Satellite stabilization		Communications
	Improved orientation accuracy	Space-based radar
	Reduced station keeping	Optics

NEP systems, consistent with technology projections, can deliver more mass in less time than any competing propulsion technology or trajectory scheme. The slight spread in performance for the NEP mission mode is a consequence of considering two different NEP systems at different ends of the power range defined by the achievable technologies. The nearly imperceptible difference in performance underscores the insensitivity of NEP to variations in the power

-
- NUCLEAR ELECTRIC PROPULSION ENABLING FOR EXPLORATION OF OUTER PLANETS WITH REASONABLE TRIP TIMES
 - CAPABILITY GREATLY ENHANCED FOR ALL PLANETARY MISSIONS
 - POWER LEVELS 100-200 kWe
-

Fig. 1. Neptune orbiter performance comparison.

rating or technology. Herein lies the principal advantage of using Nuclear Electric Propulsion for missions to the outer planets; namely, nuclear propulsion offers the prospects of executing explorations of these planets in 10 years or less with technology resiliency.

B. Directed Energy Weapon Systems

A preliminary evaluation of the potential use of reactors for directed energy weapon systems was made. A matrix approach was used since the quantity of power required depends on the weapon system, the altitude, and the particular missions. Both SPAR technology and derivatives of SPAR, which might consider a larger sized heat pipe reactor and more efficient converters (such as turbo-alternators) as well as nuclear rocket reactor technology, were considered. The nuclear rocket technology would replace the nozzle with a turbo-alternator and might also incorporate a low electric power conversion loop in a closed loop system, such as was previously proposed using tie-tube heating. The nuclear rocket program had run 20 reactors and demonstrated reactor lifetimes of 2 h at 2450 K. These were solid core, gas-cooled reactor designs. Closed-loop systems appear feasible in the tens of megawatt range--above this,

open-loop systems appear to be the better candidate. Table IV shows the matrix with possible candidate power sources. SPAR-D is a 1-MW heat pipe reactor with a turboalternator converter. The Rover electric is the nuclear rocket reactor with the nozzle replaced by a turboalternator. The Bi-Modal Rover includes both the high-power turboalternator in place of the nozzle, and a separate low-power, closed-loop subsystem. Combinations of power plants may be desirable to meet the total power requirements.

In summary, SPAR can be used for propulsion and station keeping, and derivatives of SPAR satisfy requirements of up to around 1 MW_e. Nuclear rocket technology can be used as a basis for power levels to 100 MW with closed-loop systems feasible in the tens of megawatts.

TABLE IV
REQUIREMENTS MATRIX

Need	Peak Projected Power Levels (MW)				
	0.1	1.0	10	100	1000
OTV (0.1-1 MW days to 2 yr)	SPAR	SPAR SPAR-D	SPAR Bi-Modal Rover	SPAR Bi-Modal Rover	SPAR Bi-Modal Rover
Station-keeping (0.1 MW, 7 yr)	SPAR	SPAR SPAR-D	SPAR Bi-Modal Rover	SPAR Bi-Modal Rover	SPAR Bi-Modal Rover
ASAT, Satellite defense, weapons tests (0.1-1000 MW, short duration)	SPAR	SPAR & electric storage SPAR-D Chemical	SPAR & electric storage Bi-Modal Rover Chemical	Bi-Modal Rover ● open loop Chemical	Bi-Modal Rover ● open loop Chemical
ABM defense (0.1- 1000 MW, to 1 h)	SPAR	SPAR-D Chemical	Rover electric Bi-Modal Rover ● open loop ● closed loop Chemical	Bi-Modal Rover ● open loop Chemical	Bi-Modal Rover ● open loop Chemical

C. Comparison of Nuclear and Solar Power Sources

NASA is currently concentrating on developing a 25-kW_e power system based on "technology ready" components. This includes the use of silicon solar arrays and NiCd batteries. The mass breakout for this system is as follows:

Solar array subsystem	1227
Battery subsystem	4036
Power processing	<u>80</u>
Power subsystem	5343
Ancillary equipment	<u>1171</u>
Total	6514 kg

The system mass is such that it would be very heavy to use in geosynchronous orbit when one considers that the Inertial Upper Stage can deliver but 2270 kg and this system weighs three times that amount without a sensor package. The development and acquisition cost of the power subsystem is estimated to be ~\$120M.

Forecast of solar power performance is shown in Table V. Current technology is based on the use of 11% efficient silicon solar cells and NiH₂ batteries. The advanced technology assumes that by the late 1980s, 18% efficient GaAs and improved NiH₂ batteries will be ready to be fielded. Also, power processing weights will be reduced in half. "Ultra" technology assumes the development of 25% solar cells and high-density rechargeable batteries and an additional 50% reduction in power processing weight. The advanced technology is reasonably developed, but the ultratechnology is in the initial laboratory stage.

For nuclear power plant mass (Table VI), in order to compare weights with solar arrays plus batteries, we used as current electric conversion technology the proven SiGe material successfully demonstrated on recent Voyager flights to Saturn. This would provide a 6% efficient power plant. Advanced technology assumed material demonstrated in the laboratory consisting of SiGe alloyed with GaP; we project a 9% system efficiency obtainable for late-1980 power plants. Advanced TE materials, such as boron-carbon and rare-Earth sulfides, are now being investigated that might provide another 30% improvement in system efficiency.

TABLE V
SOLAR POWER SYSTEM PERFORMANCE FORECAST

	Current Technology	Advanced Technology (1987-1990 IOC)	"Ultra" (1990-1995 IOC)
<u>Solar Arrays</u>			
Type	Si	GaAs	Multiband GAP (MBG)
Efficiency (%)	11	18	25
<u>Batteries</u>			
Type	NiH ₂	NiH ₂	High-energy density rechargeable battery (HEDRB)
Weights (WHR/kg)	20	44	110
<u>Power Processing</u>			
Weight (kg/kW)	3.2	1.6	0.8
<u>Performance</u>			
Specific weight (kg/kW)	80	45	21
(W/lb)	5.7	10.1	21.5

TABLE VI
NUCLEAR POWER PLANT MASS (100 kW_e)

	Voyager Material Technology	Improved SiGe	Advanced Thermoelectric Materials
<u>Mass (kg)</u>			
Reactor	445	400	400
Shield (15°)	295	280	265
Converter, radiator, and structure	<u>1255</u>	<u>850</u>	<u>610</u>
Subtotal	1995	1530	1275
Power processor	380	190	95
Specific mass (kg/kW _e)	24	17	14
(W/lb)	19	26	33

The power processing technology is similar for both nuclear and solar power systems. However, the nuclear system requires about half the processing system because the batteries are not present.

Comparing solar and nuclear power plants (Table VII), shows significant weight advantages for nuclear systems. For instance, the ultrasolar system has basically the same specific weight as current nuclear technology. In addition, the solar array systems represent very mature technology, leading to the conclusion that future growth is limited, whereas nuclear technology can grow in specific weight and in power levels to the multimewatt range.

Major size differences exist between solar and nuclear power plants, as shown in Table VIII. A nuclear power plant fits into the shuttle bay in its deployed mode and uses about one-quarter the packaging volume. The solar system using current technology would be the size of half a football field

TABLE VII
COMPARATIVE SPECIFIC WEIGHTS (W/lb) (GEOSYNCHRONOUS ORBIT)

	Current Technology	Advanced Technology	Solar "Ultra"
Solar	6	10	21
Nuclear	19	26	33

TABLE VIII
SIZE COMPARISON (100 kW_e)

	Current Technology	Advanced Technology	Solar "Ultra"
Solar			
• Arrays (m ²)	2500	950	715
• Batteries (m ³)	1.0	0.5	0.2
Nuclear			
• Length (m)	11.3	7.0	5.1
• Diameter (m)	4.5	4.5	4.5
• Length of shuttle bay exclusive of full diameter (m)	4.3	4.3	4.3

after deployment. Improvements may eventually decrease this by a factor of four. Batteries are compact and do not occupy a major part of the shuttle bay. Cost estimates were prepared by the Aerospace Power Division, Air Force Wright Aeronautical Laboratories personnel on solar arrays with battery systems, and by the Los Alamos National Laboratory with the aid of an industrial organization on nuclear power plants. Nuclear power plants have relatively high development costs, but low production costs compared to solar array with battery power plants.

A plot of cost versus number of units purchased for three power levels is shown in Fig. 2. The projected breakeven point considering both development and acquisition cost is 3 units for 100 kW_e, 12 units at 25 kW_e, and 35 units at 10 kW_e.

Fig. 2. Cost comparisons for solar and nuclear power units.

III. POWER PLANT SYSTEM

A. SPAR Water Immersion Studies (R. G. Palmer, Q-15)

Studies of the effect of water immersion on the criticality of the SPAR core and ways of reducing this effect have continued. As mentioned in the Oct.-Dec. 1980 quarterly report, the requirement that the reactor should not go critical if it were to fall into the ocean or a lake is likely to be a mandatory one.

The principal calculational tool used in the study has been the Monte Carlo code MCNP, but the discrete ordinate transport theory code, TWOTRAN has been used for comparison in a few cases. TWOTRAN cannot model the heterogeneous arrangement of heat pipes as explicitly as MCNP, so simplified core models were used in which the heat pipes were homogenized with core materials in discrete annuli.

It was also stated in the last report that the reactivity increase caused by flooding the core heat pipes with water comes from the slowing down of neutrons by hydrogen. Later examination of the neutron spectrum, however, showed the flux of low-energy neutrons in the flooded configuration to be quite small. Less than 6% of the flux at the center of a flooded heat pipe is below 10 keV and only about 1% are below 1 keV. This explains why the introduction of strong resonance absorbers, such as rhenium and gadolinium, are not effective in reducing the k_{eff} on flooding.

SPAR has a very small core, only 29 cm long by 29 cm diameter. It is, therefore, a very leaky core, which is made even more so by the fact that about 22% of the core volume is made up of space containing sodium vapor. This is the volume occupied by 91 core heat pipes. To neutrons, this sodium vapor is virtually voidage, so they find it easier to escape the core than when the heat pipes are filled with scattering material such as water. The MCNP tallies of the neutron current across the core-reflector interface showed about a 30% reduction in neutron leakage on flooding.

The introduction of hydrogen via water does soften the neutron spectrum somewhat and this produces both negative and positive components in the reactivity change. The neutron energy range most important to SPAR neutronics is 10 keV to 1 MeV. In this range the number of fission neutrons per neutron absorbed in the fuel decreases with decreasing energy so that a net movement of the spectrum towards the low-energy end will decrease reactivity. On the other hand, since high-energy neutrons leak from the core more easily than low-

energy ones (the diffusion coefficient increases with energy), a softening of the spectrum will increase reactivity.

The net effect of all of the above mechanisms is for flooding of the core heat pipes to cause k_{eff} to increase from 1.060 ± 0.004 (dry core with all control rods in their outer positions) to 1.185 ± 0.003 , i.e., a reactivity increase of $11.8 \pm 0.7\%$.

Since the production of thermal and resonance region neutrons is so small, we must introduce a neutron poison having good capture cross sections over the range 10 keV to 1 MeV if the reactivity effect of flooding is to be negated. Boron poison in the form of B_4C is the most obvious candidate. Because of the limitations on control rod capability, it does mean that this poison must be removed from the reactor before it can be started up, when orbit is reached.

A possible position for this temporary poison material is just inside the core container, as shown in the scalloped configuration (Fig. 3), where 30 half rods of B_4C are placed. A total of about 2 kg of B_4C (enriched to 90% in the B_4C isotope) reduced the flooded k_{eff} from 1.184 ± 0.003 to 1.140 ± 0.003 . The introduction of 16 new geometric cells to describe the 30 half rods in the MCNP model resulted in the capacity of the code being exceeded. To reduce the model size below the limit of 175 geometric cells required the replacement of the radial reflector and control drums by a simple radial reflector. The value of k_{eff} for the flooded configuration with all of the control rods in their innermost position has to be inferred to previous results. This inference yields a k_{eff} of about 1.08. To try to reduce this to below unity, the bottom reflector was replaced with B_4C . This configuration gave a k_{eff} of 1.102 ± 0.003 ; the inferred value with control drums in would be about 1.04.

It appears that the k_{eff} of the flooded configuration would be reduced to below unity by having a combination of a B_4C ring between the core and radial reflector and a B_4C block replacing the lower reflector, or by distributing about 2 kg of enriched B_4C within the main body of the core where the higher neutron importance makes it more effective. This latter approach has more impact on the design of the core.

Fig. 3. Temporary poison half rods at core interface.

B. In-pile Test of SPAR Fuel (A. J. Giger, E. S. Keddy, Q-13, R. G. Palmer, and J. T. Sena, Q-15)

1. General. The SPAR fuel in-pile test is planned for ANL's EBR-II reactor at Idaho Falls. It will consist of probably two capsules to be placed in row 7 (possibly 6) of the reactor. These capsules will each be ensconced in an ANL MK M-1A irradiation subassembly. The experiment is designed to provide early data on SPAR fuel swelling under fission power density ratios up to 5, while maintaining similar temperature conditions to SPAR. Also to be examined are heat transfer mechanism between fuel, fin, and heat pipe; fuel migration, if it occurs; heat pipe performance; and chemical compatibility of fuel, fin, and heat pipe materials.

2. Design. The design of the fuel irradiation capsule has been extensively modified. It is shown as Fig. 4. Experimental fuel is contained in four insulated bays on the evaporator end of the molybdenum heat pipe, which uses sodium as its working fluid and neon as a buffer gas. Table IX lists the salient features of the fuel. The small fuel container is held in position on the 15.9-mm-diam heat pipe by the molybdenum heat transfer fins.

The fins, which are interleaved with the UO₂ fuel wafers, are attached to the pipe in the same manner as in the SPAR reactor. A detent prevents unintentional slippage of the container.

TABLE IX
SPAR UO_2 FUEL IN EBR-II IRRADIATION EXPERIMENT

Parameter	SPAR Reactor	Fuel Container Experiment Bay on Heat Pipe			
		I	II	III	IV
Fission power density ratio	1.0	1.0	1.9	5.2	1.9
Fuel enrichment (%)	93	0.7	6	27	6
Fuel thickness (mm)	2	1.5	1.0	1.0	2.0
Ideal $\Delta T_{\text{max fuel}}$ to HP (K)	253	259	254	303	303
Δr_{fuel} (mm)	8.1 (equiv.)	7.4	7.4	5.4	5.4

Fig. 4. SPAR EBR-II irradiation experiment capsule.

A brazed or threaded molybdenum sleeve is planned for the condenser section of the heat pipe. This threads into a thin-wall niobium cylinder, a part of the first complete encapsulation of the fuel container and heat pipe. A seal separates the vacuum region surrounding the fuel container from the condenser section of the heat pipe. This allows a low gas pressure in the condenser region and ensures heat transfer through the threaded section.

Between the inner niobium shell and an outer 316 SS shell, a sodium fill bonds the experiment thermally to the flowing sodium of the EBR-II host. This fill permits good thermal control

- (a) at the bottom of the niobium vacuum chamber in case of a fuel melt,
- (b) on the outside of the fuel container during normal operation,
- (c) through the condenser section, allowing a controllable transfer of heat from the experiment to the EBR-II coolant, and
- (d) of the heat pipe in the buffer gas region (upper end).

Fill/evacuation tubes are located at appropriate places in the assembly from the heat pipe outward.

At the condenser section, an impedance hexagon forces reactor sodium flow inboard slightly to provide proper cooling for this section.

3. Analysis. A two-dimensional heat transfer equation yielding the difference in temperature between the hottest portion of the fuel and the heat pipe wall was used as a design tool to establish experimental fuel configurations. The equation was

$$\Delta T = \frac{1}{2} \frac{fg_u + hg_u}{kh} \left[x_2^2 \ln \frac{x_2}{x_1} - \frac{1}{2} (x_2^2 - x_1^2) \right] + \frac{g_u}{2k_u} \frac{f^2}{2} ,$$

where f = UO_2 fuel thickness

h = moly fin thickness

k = fin conductivity

k = fuel conductivity

g_u = γ and fission heat generation in
fuel

g_u = γ and heating in fin

x_1 = minimum fin radius

x_2 = maximum fin radius.

Solutions for this, programmed on a HP 67, provided an "ideal" basis for comparing SPAR conditions with the planned test (Table IX). Inasmuch as perfect thermal contact between fins and fuel and fins and heat pipe was assumed, actual temperatures will be higher than those calculated.

Fission and gamma heating from the experiment fuel are calculated at 6.0 kW; another 1.5 kW will be added to the heat pipe load from gamma heating in the molybdenum parts.

At 100% release, fission gas pressure at the end of 1 yr would be $4.8 \times 10^4 \text{ N/m}^2$ in the 243 cm^3 container. Considerably less than 100% release is expected, however.

Figure 5 shows the results of a preliminary calculation of the temperature regulation provided by the heat pipe in the experiment; the longest pipe with the best regulation will be used.

For the condenser section, Fig. 6 shows the ideal calculated temperature profile from the inside wall of the heat pipe to the reactor sodium coolant. At this gradient and with perfect thermal contact in the threads, 8.2 kW are transferred per 25.4 mm of length. Thermal resistance is expected in the threads; this will necessitate a larger condenser section and modify the temperature regulation somewhat. Calculations indicate that the expected EBR-II sodium coolant flow rate of 0.82 l/s will provide a satisfactory heat transfer coefficient of $5.1 \text{ W/cm}^2 \text{ K}$ on the outside of the condenser section.

At the maximum temperature indicated for the sodium bond region of the condenser section, sodium vapor pressure is expected to be $3.2 \times 10^4 \text{ N/m}^2$.

4. Fabrication Procurement. An order has been placed internally (CMB-6) for sufficient experimental SPAR fuel for three capsules. Samples to test the braze joint between the threaded sleeve and the heat pipe have been made up.

IV. CORE HEAT PIPE DEVELOPMENT

A. Heat Pipe Fabrication

1. SPAR-1B Fabrication and Assembly (H. E. Martinez and E. Keddy, Q-13).

SPAR-1B is an annular screen tube wick heat pipe similar to SPAR-1, with three exceptions: (1) the wick is centered in the evaporator by spiral wound spacer wires to provide a higher boiling limit; (2) the plug in the evaporator is zirconium brazed to the wick to prevent its detachment; and (3) the getter disks are zirconium, dimpled to increase surface area exposed to liquid sodium. SPAR-1B was fabricated according to the sketch of Fig. 7. After

Fig. 5. SPAR EBR-II capsule temperature regulation.

Fig. 6. SPAR EBR-II capsule condenser section.

Fig. 7. SPAR-1B heat pipe configuration.

radiograph inspection, it was filled with sodium by distillation. Pinch-off was accomplished by mechanical flattening of the fill tube followed by an EB closure weld. The heat pipe was then placed in a quartz vacuum tube and the wet-in process started using an rf coil. Upon heating to 1000 K, a sodium vapor leak was immediately detected in the mechanically flattened portion of the molybdenum fill tube at the closure weld. Detection of the presence of gas inside the heat pipe necessitated disassembly and sodium removal. Inspection of heat pipe components showed no damage by oxidation. The porosity of the wick was nearly the same as before the test, 30 μm in the evaporator. Several molybdenum heat pipes with identical fill tubes had been pinched off previously by the same technique without difficulty. The cause of this failure is not completely understood, but can be attributed to a faulty weld, an excessive mechanical flattening of the fill tube, a defect in the molybdenum tube, or any combination of these.

An effort to develop a better mechanical flattened pinch-off has commenced. A tool is being designed with rounded edges to reduce stresses in the region of transition from flat to tube. A hydraulic press will be used to achieve a

slow, steady rate of flattening with a measurable and reproducible amount of force. Initial experiments will be performed on 6.4-mm- ϕ TZM tubes.

SPAR-1B was refilled by distillation and pinched off using the standard method previously mentioned because the new hydraulic press technique had not yet been implemented. The pipe was wet-in isothermally at 1000 K. An attempt was made to operate the pipe with an rf coil heat source, but the presence of a large quantity of noncondensable gas was detected. Because no sodium leaked from the pipe, it was suspected that the removed portion of the fill tube harbored the leak. This proved to be true. The mechanical pinch-off had caused stresses in the fill tube that produced a crack. Figure 8 shows bubbles emerging from portions of the crack. Although this portion of the fill tube was removed by the EB closure weld, gas had already entered the pipe before it was inserted into the EB welder chamber. SPAR-1B was disassembled and the sodium removed. No damage to the wick or other parts of the pipe were evident.

An alternate heat pipe closure technique was devised to eliminate the pinch-off. It consisted of a back distillation of sodium into the chill-block-cooled fill tube after the heat pipe had been filled. A solid sodium plug was created in the fill tube as was verified by radiography. The heat pipe was enclosed in an argon-filled bag and the molybdenum fill tube cut off with a small hand saw. The heat pipe was transported to the EB welder in an

Fig. 8. Bubbles emerging from crack in fill tube.

argon-filled bag that protected the exposed end of the sodium plug from oxygen. The sodium plug preserved the heat pipe vacuum until two protective cover caps were welded over the fill tube. The heat pipe was wet-in at 900 K for 44 h. It was placed in an evacuated quartz tube and heated with a 30-cm-long RF coil. The heat pipe functioned properly at temperatures up to 1000 K and radiated about 1 kW of power. Although many problems were encountered in producing another heat pipe similar to SPAR-1, SPAR-1B has now been successfully fabricated and is free of cracks or sodium leaks. The pipe initially appears to be gas free, although tilt tests and high-power tests will be required for verification.

2. Production of Fine Molybdenum Screen (L. B. Lundberg, Q-13). The development of artery wicks for core heat pipes would be facilitated by the availability of 325 mesh screen. Use of this fine mesh screen would obviate the need for screen compaction and eliminate the accompanying embrittlement. Requests for bids were issued and received from two vendors. After a successful trial weave, one vendor subsequently submitted a relatively high bid. A considerably lower bid was received later from the second bidder. The second bidder requested 2 km of 25- μm - ϕ molybdenum wire to perform a trial weave. This trial weave has not yet been completed but, if successful, an order for 3 m² of screen will be issued.

B. Core Heat Pipe Analysis (F. C. Prenger, Q-13)

Development of a successful wick design for the core heat pipes has been an evolutionary process. The process started with the screen-covered grooves, which represented a known technology, and progressed to an annular wick design that was used to obtain needed test data on the effects of long adiabatic sections and bends. Finally, boiling limitations and lack of redundancy have indicated an artery design to be more desirable. Subsequent tests will show if the artery design is adequate.

Experience has been gained with three heat pipe wick designs, although testing of the artery design has not been accomplished. During testing, poor performance due to impurities was experienced with the grooved-wall configuration. Also, potential superheat problems due to the relatively thick heat pipe walls contributed to development of the annular wick and artery designs subsequently pursued.

Performance limits for the annular wick and artery designs are similar. A comparison between the two designs is shown in Table X. Only two significant

drawbacks exist for the artery design: difficulty of fabrication, especially if bending of the heat pipe is required, and filling uncertainties associated with the amount of working fluid and proper filling of the arteries. The annular wick design suffers from excessive superheat, which lowers the boiling limit, and no redundancy in the liquid flow passage.

A continuing experimental test program is required to better understand the vapor pressure drop limitations in high-performance heat pipes containing bends and long adiabatic sections. Preliminary test results have indicated that a transition to turbulent flow of the vapor occurs in the adiabatic section. Further, this transition occurs at Reynolds numbers based on tube diameter of approximately 4000. What effect bends will have on this transition is of paramount interest.

Additional testing with a stainless steel sodium heat pipe using a radiant furnace for heat addition has been accomplished to evaluate the vapor pressure drop in the heat pipe evaporator. An increased pressure drop has been observed

TABLE X
COMPARISON BETWEEN ARTERY WICK DESIGN AND ANNULAR WICK DESIGN

	Annular Wick	Artery Wick
Advantages	<ul style="list-style-type: none"> ● Ease of fabrication and assembly. ● No circumferential distribution of working fluid required. ● Simple geometry enabling accurate calculation of liquid inventory. 	<ul style="list-style-type: none"> ● Redundant liquid passages. ● Impurities collect in distribution wick causing only local hot spots. ● More tolerant of wick damage.
Disadvantages	<ul style="list-style-type: none"> ● Sensitive to boiling limits. ● No redundant liquid passages. ● Collection of impurities in evaporator may result in single point failure. 	<ul style="list-style-type: none"> ● Difficulties in assembly (intimate contact with wall required). ● Provide excess working fluid to fill artery (fillets require excess working fluid).

in previous heat pipe tests that incorporated induction heating of the heat pipe evaporator. By using radiant heaters, interference with the thermocouple leads is eliminated, thereby enabling accurate measurement of the small temperature differences.

It has been suggested, in collaboration with European investigators, that the vapor-liquid, equilibrium model used in the analysis may not be valid for the heat pipe evaporator where superheated vapor may exist. Use of a perfect gas model may be more suitable and may provide better agreement with the experimental data. More testing will be conducted to evaluate these effects.

C. Core Heat Pipe Artery Wick Development

1. SPAR-4 Fabrication (J. R. Runyan, Q-13, and K. L. Meier, Q-15). The development of an artery wick core heat pipe, SPAR-4, is being undertaken in an effort to avoid the working fluid superheat problem and to reduce the probability of a vapor dryout of the wick. The wick for this heat pipe is constructed of 150 mesh molybdenum screen compacted into arteries and a separate distribution wick. The arteries are joined to the wick by spot welding. Anticipated difficulties in fabrication of this type of heat pipe have been largely overcome through a series of trial experiments. Table XI is a synopsis of these artery and distribution wick fabrication experiments. Arteries 1.5 mm ϕ and 2 m long (MARS 1, artery A and B in Table XI) have been successfully fabricated, as has a distribution wick 13.2 mm ϕ and 2 m long, consisting of five and three-quarter wraps of 150 mesh molybdenum screen (MARS 1, wick #2). The arteries consist of three and one-quarter wraps of 150 mesh molybdenum screen lightly drawn and swaged to a porosity of 50 μm in the evaporator. The evaporator porosity is about 70 μm .

The arteries and distribution wick were successfully spot welded together to produce the completed wick, MARS-1, shown being welded in Fig. 9. A twin-electrode series hand piece welding apparatus was used because the mandrel was made of lucite and provided alignment of the arteries inside the wick but not a current path. This nonconductive mandrel prevented alloying of the wick and mandrel material. After spot welding, a porosity test showed essentially no change in porosity of the wick which is shown in Fig. 10 in its completed state.

One advantage of an artery wick over a screen tube annular wick is that the former is in intimate contact with the wall, thus mitigating the problem

Fig. 9. Welding the MARS-1 wick.

Fig. 10. The completed MARS-1 wick.

TABLE XI
CORE ARTERY HEAT PIPE ARTERY TEST SAMPLES

Sample No.	Artery/wick o.d. (mm) i.d.		Screen No. of Layers	Sheath o.d. (mm)	Effective Pore Size, μm	Remarks
1			4			Sheath and wrapped
2			3			Screen radial
3			5			Clearance too small
4E	2.08	1.50	4	DR. 4.04 DR. 3.91	88	
5F1	1.70 min. 1.83 max.	1.50	4	SW. 4.01		HEX shaped
5F2	2.07		4	DR. 3.86	94	Elongated screen
5F3			4	SW. ?	22	
6A	2.02		4	DR. 4.58		
	2.04		4	DR. 3.81	81	
6B	2.09		4	DR. 4.58		
	2.11		4	DR. 3.95	83	
6C	1.79		4	DR. 4.58		
	1.82		4	SW. 3.98	32.5	
7A	1.93		3	DR. 4.57		
	1.98		3	DR. 4.32 SW. 3.96	83	Mandrel and sheath vacuum annealed 2 h at 900°C
7B	1.79		3	DR. 4.57		
	1.81		3 1/4	SW. 3.96	50	
8A			4 1/4	SW. 15.11		Separation
B			4 1/4	DR. 15.19	90	
C			4 1/4	SW. 15.11		Separation
9A	13.06	12.47	5 1/4	SW. 15.06	70	
			5 1/4	DR. 15.19	76	
			5 1/4	SW. 15.06	80	
10A		12.39	6 1/4	SW. 15.06	66	
		12.39	6 1/4	SW. 15.06	68	Shim added to swage die
11	13.05	12.48	5 1/4	SW. 15.06	70	
12	13.05	12.51	5 3/4	SW. 15.06	70	Trailing edge screen tack w/Fe snippets
13 Artery Wick	1.71	1.43	3 1/4	DW. 4.58	85	
	13.04	12.82	5 3/4	SW. 15.06		Leading edge screen up 45°
MARS 1 Artery A	1.73	1.45	3 1/4	DW. 4.58		
Artery B	1.69	1.42	3 1/4	SW. 3.96	53	
				DW. 4.58 SW. 3.96	70	
MARS 1 Wick #1			5 3/4	SW. 15.06		Separation and flaw in weave
Wick #2	13.02		5 3/4	SW. 15.06	78	

of vapor bubble dryout of the sodium-filled annulus in the evaporator. The intimate contact will be obtained in SPAR-4 by drawing the molybdenum container tube through a die so as to reduce its inside diameter to that of the outside diameter of the wick. The first drawing experiment consisted of a 4-cm-long trial draw of an empty tube at 500 K to determine the inside diameter reduction and the necessary die size to achieve contact with the SPAR-4 wick. The second trial draw consisted of drawing a 30-cm tube with a 20-cm section of MARS-1 inside through the die size determined by the first trial draw. The reduction of the tube inside diameter proved to be excessive, causing a buckling of the wick as shown in Fig. 11. The reason for the excessive inside diameter reduction of the molybdenum tube is not fully understood. Perhaps a drawing length phenomenon exists, because the first draw was over a 4-cm length, whereas the second was over a 30-cm length. A third trial draw will soon be performed, using the parameters of the second draw to determine proper die size and achieve intimate contact with, but not buckling of, the wick.

The SPAR-4 container tube, end caps, plug, getter, and fill tube have all been fabricated. All parts are similar to SPAR-1B except the plug. Unlike its predecessor, SPAR-4 will use a plug fabricated from compacted 150 mesh molybdenum screen. This will allow better flow of liquid sodium into the getter region because some vaporization will occur through the plug into the

Fig. 11. Buckled wick after tube i.d. reduction.

evaporator vapor passage. Consequently, this will require a small flow of the most contaminated sodium from the arteries into the zirconium/hafnium getter region.

2. Porous Metals for Artery Wicks (K. L. Meier, Q-15, H. Sheinberg, CMB-6). Porous niobium 85% and 70% of theoretical density was received from a vendor for evaluation as a wick material. Initial measurements showed an effective pore diameter of 33 μm for the 85% dense sample, which was fabricated by powder metallurgical techniques from 200- μm average-sized particles. Bend tests of 1-mm-thick disks at temperatures of up to 500 K showed this material to be brittle and unable to withstand a 5° bend angle prior to fracture. A scanning electron microscope image (SEM) of the fracture surface, Fig. 12, showed that only a small bonding area between particles existed. The 85% dense sample was re-sintered at 2100 K for one and one-half hours to increase the particle bonding area. The improvement in bonding area is shown in Fig. 13. Subsequent bend tests at room temperature evinced a marked increase in bend ductility with a bend angle of 75° required to produce fracture. Research and testing of this material will continue.

Fig. 12. SEM (100X) of fracture surface of porous niobium.

Fig. 13. SEM (100X) of porous niobium after sintering 1.5 h at 1850°C vacuum.

3. CVD Artery Wicks (K. L. Meier, Q-15, and W. McCreary, CMB-6). Preliminary experiments were conducted to determine the feasibility of using chemical vapor deposition to reduce the porosity of wicks made from 150 mesh molybdenum screen and to bond arteries to the wick in the same process. Successful CVD would allow the use of readily available 150 mesh screen without compaction as well as obviate the need for the spot welding of arteries to wick. Although spot welding has produced a successful artery wick, this procedure is extremely tedious and harbours the possibility of producing an artery-destroying hole at each spot weld.

Initial CVD experiments were performed on 150 mesh molybdenum screen tightly wrapped over a steel mandrel. The reacting gases, MoF_6 and H_2 , to deposit molybdenum, were passed over the mandrel and screen, which were induction heated. The initial CVD apparatus produced neither uniform gas flow nor uniform heating of the screen. Hence, the coatings were both axially and circumferentially nonuniform. Although it was demonstrated that the screen pore size could be reduced to the desired size, no bonding of screen layers

occurred. In addition, a solid "skin" of CVD molybdenum covered the mandrel and inner layer of screen precluding the use of the screen as a wick. Similar experiments using WF_6 and H_2 to produce tungsten coatings resulted in somewhat more uniform coatings, but no bonding between screen layers.

An analysis of the CVD apparatus resulted in a redesign to produce both more uniform heating and gas flow. The former was achieved by the use of an rf susceptor around the wick to effect a uniform radiant heating. The latter was produced by increasing the distance between the gas entrance and the experiment, inserting flow-straightening tubes in the between space. Mandrels will be eliminated in future experiments, thus avoiding the skin formation problems. The modified apparatus will soon be tested on artery wick samples of about 10-cm length.

D. Facilities (H. E. Martinez, N. L. Vikdal, Q-13, and K. L. Meier, Q-15)

The south bay heat pipe test vacuum chamber has been fitted with a Brew radiant heater. This heater supersedes the rf coil and provides greater long-term reliability, increased electrical efficiency, and less control circuit interference. Tests were performed using the stainless steel heat pipe SEPS-5. An input power of 5 kW to the pipe was achieved during these tests. In no-load, sans-heat-pipe tests, the Brew heater reached input power levels of 15 kW with an element temperature in excess of 2900 K. Although minor insulator, control circuit, and chamber cooling problems occurred, the heater performed satisfactorily. Higher power heat pipe tests are forthcoming.

E. Materials

1. Molybdenum Heat Pipe Corrosion Studies (L. B. Lundberg, and L. A. Geoffrion, Q-13). The SPAR reactor design results in UO_2 being in very close proximity to, and under some conditions potentially in intimate contact with, the molybdenum core heat pipes that contain sodium as a working fluid. Because oxygen in sodium/ molybdenum heat pipes is known to produce high corrosion rates, it is necessary to understand the behavior of the $UO_2/Mo/Na$ system in order to assure that oxygen transport from UO_2 to sodium will not occur in sufficient magnitude to adversely affect heat pipe lifetime. To this end, an experimental program is under way to simulate the thermochemistry of molybdenum/sodium heat pipes in a reactor core by sealing UO_2 under vacuum inside a tight-fitting molybdenum or TZM molybdenum alloy capsule which is, in turn, sealed inside a molybdenum/sodium heat pipe. The wick in the heat pipe is a loosely wrapped molybdenum screen of 150 square mesh, and the tube

containing the UO_2 is centered in the heat pipe bore by a mating recess in the blind end cap and by wire supports near the fill end cap.

The filled and sealed heat pipe is placed in a quartz tube, which is evacuated and sealed. The heat pipe is heated inside the quartz tube by rf induction during the evacuation process to degas its outer surfaces. Up to three heat pipes, each sealed in their own individual quartz tubes, are heated by rf induction in a three-hole concentrator that is 25 mm long. Heat is applied near the blind end cap of the heat pipe, and its temperature is measured optically.

Thus far, three molybdenum/sodium heat pipe corrosion tests have been run. One heat pipe that contains a molybdenum tube filled with UO_2 has operated for over 1215 h at 1475 K without failure, and another heat pipe that contains an empty TZM tube has operated for the same period at 1500 K without failure. One heat pipe that contained a TZM tube filled with UO_2 operated for only 13.5 h at 1435 K before it failed by developing a perforation in the tube wall in the heated region, i.e., the evaporator.

The hole that developed in the wall of the molybdenum heat pipe container tube can be seen on the inside of the evaporator section in the photograph given in Fig. 14. This photograph shows the extent of the metal removal from the evaporator section of the container tube and the screen wick. A river-like pattern of metal was removed from the length of the evaporator section in both the container tube wall and the screen on the bottom side of the heat pipe. Dark gray crystallites were found at both ends of the evaporator attached to the screen on the side of the heat pipe that was oriented downward during the test. Also, a few crystallites were found under a fold of screen in the vicinity of the hole. Some of these crystallites were collected and analyzed by x-ray diffraction techniques. The powder pattern indicated that the crystallites were pure molybdenum.

Estimates of the amount of molybdenum removed and the sodium circulation rate indicate that the sodium must have contained more than 90 ppm of oxygen in the evaporator. This result is based on an assumption that 2.5 or more oxygen atoms are required to remove a molybdenum atom, either in the form of an oxide or as sodium molybdate.

The most obvious source of oxygen was a small leak that developed in the first weld of the fill tube during initial startup. When the heat pipe was removed from the evacuated quartz tube and rewelded with the electron beam

Fig. 14. Evaporator section of a $\text{UO}_2/\text{TZM}/\text{Na}$ corrosion test molybdenum heat pipe after 13.5 h at 1435 K.

welder, it was assumed that the leak had been sealed temporarily with sodium when the heat pipe cooled. Restarting the heat pipe after it had been repaired gave no clear evidence that any air had leaked in. However, quantities of air in the vicinity of 1 cm^3 or less probably could not be detected by observing the operating heat pipe, but this quantity of air is sufficient to cause the observed corrosion.

Posttest analysis of the wick from the capsule heat pipe indicated that a segment of wick very close to the evaporator contained 260 ppm of oxygen, while a segment of wick from the condenser contained 160 ppm of oxygen. If it is assumed that the difference represents the amount of oxygen leaked out of the condenser wick by the sodium, this amount is sufficient to cause the corrosion. However, the credibility of this explanation of the oxygen source suffers from the fact that two sister capsules, presumably containing the same oxygen level in the wick, are still operating after 1215 h.

Either of the above sources of oxygen could mask any contribution of oxygen by the UO_2 in the TZM container. Diagnostic measurements on the oxygen activity of the UO_2 and of the mechanical properties and chemical composition of the TZM are in progress to determine whether any interactions occurred that could supply oxygen to the sodium.

2. Molybdenum-Rhenium Alloy Development (L. B. Lundberg, Q-13). Because of conflicts in the use of the electron beam melter at ORNL, it was necessary to arrange to have the melting of our Mo-13 wt% Re alloy performed in the electron beam melter located at the Cabot Corporation in Kokomo, Indiana. Two electrodes will be double melted in this facility to produce 10-cm-diam ingots for extrusion. The electrodes are presently being assembled at the Metals Division of Thermoelectron Corp. by welding the rhenium plates we provided to our round molybdenum bar. The ORNL people do not foresee any significant delays in the production of alloy wire, rod and tubing once the alloy ingots are produced.

V. RADIATOR DEVELOPMENT

A. Radiator Heat Pipe Testing (S. P. Girrens, Q-13 and D. Ernst, Thermacore)

The task of filling and testing RAD-3, the 5.5-m titanium/ potassium radiator heat pipe, was given to Thermacore under JPL contract in February 1981. In March, a meeting was held between Los Alamos and Thermacore to discuss the test program for RAD-3. Test objectives agreed upon include the following: (1) successful fill and wet-in with arteries down orientation; (2) successful startup; (3) successful operation at 500°C in vacuum at low power (run at maximum uncoated power) with measurements of axial ΔT s; (4) successful operation at 500°C at maximum uncoated power in a low-pressure inert gas atmosphere (to increase heat transfer); (5) variation of evaporator area and/or input heat flux to obtain dry-out limits on the shot blasted distribution wick.

Modifications are currently being made to Thermacore's vacuum fill and test chamber to accommodate the 5.5-m heat pipe. Input heat to the evaporator will be provided by a rf pancake coil to allow visual identification of evaporator hot spots. It was decided to fill RAD-3 with 250 g of potassium and not allow operating temperatures in excess of 525°C. In a low-pressure, inert gas atmosphere, the heat transfer should be enhanced sufficiently by natural convection to allow uncoated operating powers in the 3-kW range. Thermacore will commence filling RAD-3 as soon as test facilities are modified.

B. Titanium-Potassium Compatibility (L. B. Lundberg, L. A. Geoffrion, Q-13)

The two titanium/potassium heat pipe corrosion tests have been discontinued because of concern for imminent failure due to reaction with the copper heater block strapped to their evaporators.

One heat pipe operated with an evaporator exit temperature ranging from 806 to 828 K and a condenser inlet temperature range of 802 to 824 K for a period of 6511 h without a failure. There was a temperature drop through the condenser of about 10° . The power output from this heat pipe ranged from 157 to 192 W. As can be seen in Fig. 15, there was significant interaction between the heater block and the heat pipe. In a vacuum, these metallurgically bonded areas are expected to be transferring all but a small fraction of the heat into the heat pipe. Therefore, assuming the input power is approximately equal to the output power, the input radial heat flux is calculated to have ranged from 724 to 886 kW/m^2 during the test. The output radial heat flux was calculated to have ranged from 26 to 32 kW/m^2 during the test. No indications of corrosion inside the heat pipe were noted when it was cut open after the test.

The other heat pipe operated with its evaporator exit temperature ranging from 895 to 906 K and its condenser ranging from 883 to 895 K for a period of 4797 h without failure. This heat pipe had a temperature drop through the condenser of about 5° . The power output for this heat pipe ranged from 631 to 1013 W with a tendency toward increased output with time of operation. A reaction product was found similar to that seen on the lower temperature heat pipe in Fig. 15. Using the assumptions stated previously, the input radial heat flux was calculated to have ranged from 121 to 195 kW/m^2 , and

Fig. 15. Titanium/niobium heat pipe after 6511 h at $\sim 815\text{K}$ in a vacuum.

the output radial heat flux was calculated to have ranged from 77 to 124 kW/m . There was a slight indication of corrosion on the inside of the tube as manifested by a deposit found at what had been the top of the potassium pool during the test. It is not clear where this material came from because there was no wall pitting or thinning at any other location inside the heat pipe. Further analysis is under way to help deduce the nature and perhaps the cause of the deposit.

VI. THERMOELECTRIC MODULE DEVELOPMENT (G. W. Fly, Q-13, E. A. Brown, Q-15, V. Raag, Synical Corp.)

A. Materials Research

Synical Corp. has continued work on improving the figure of merit of p-type SiGe/GaP by varying the dopant concentration level. The majority of the work in the past quarter in this area has been in developing a model of the Seebeck coefficient, electrical resistivity, and thermal conductivity as a function of carrier concentration. Presently, analytical models for Seebeck coefficient and electrical resistivity have been completed but work is continuing on correlating thermal conductivity to carrier concentration.

Synical has continued the annealing studies on both p- and n-type samples. These annealing studies have now reached almost 5000 h and there has been very little change in any of the properties during the past 3000 h. The change in electrical properties in these tests occurs primarily because of dopant precipitation. Since the addition of GaP enhances the solubility of both boron and phosphorus, the electrical properties have not degraded as much during these tests as standard silicon germanium. This should have a positive impact on the long-term performance of the thermoelectric module.

Some additional samples of n- and p-type material were submitted to Purdue's Thermophysical Properties Research Laboratory for evaluation of their thermal conductivity. The samples were measured at Synical from 20°C up to 700°C. At Purdue, the samples were measured from 20°C to 1000°C. Purdue's measurements showed thermal conductivities below those normally quoted by Synical and the Laboratory. This decreased thermal conductivity improves the figure of merit of the material. Based on Purdue's thermal conductivity data and Synical's electrical measurements, this material exhibits a figure of merit approximately 5% higher than previously quoted for optimum material.

B. Module Design and Testing

A committee established to review the design of the bonded module came to the conclusion that, though the present module design might be satisfactory, construction of an entire module of ring elements as a first step would be risky, and evaluation of the results, should a premature failure occur, would be difficult. Consequently, in conjunction with the design review committee, we have decided to first test individual thermocouples that retain the basic configuration of the thermocouples in the ring elements. The thermocouple test apparatus shown in Fig. 16 has been designed and is currently being built. This design is similar to a comparative thermal conductivity apparatus. The outer foil maintains a temperature profile similar to that within the thermocouple, thus reducing heat losses out of the element.

The heat losses in this module are still too large to make effective efficiency measurements, but this thermocouple will provide information on interface temperature and electrical drops, material compatibilities, material degradation, and sublimation characteristics.

The test apparatus will use a separate heater and a radiation heat rejection system that will simplify testing. Since efficiency measurements will

Fig. 16. Thermocouple test apparatus.

not be made, extensive instrumentation can be provided to measure drops across each interface. Detailed metallography after test will allow determination of the reaction rates and failure mechanisms.

PUBLICATIONS

L. B. Lundberg, "A Critical Evaluation of Molybdenum and Its Alloys for Use in Space Reactor Core Heat Pipes," Los Alamos National Laboratory report LA-8685-MS (Jan. 1981).