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Progress Report

MASTER

Reactor Technology

January—March 1978

University of California

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Edited by

John L. Warren

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REACTOR TECHNOLOGY

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John L. Warren

ABSTRACT

Progress is reported in eight program areas. The nuclear Space Electric Power Supply Program examined safety questions in the aftermath of the COSMOS 954 incident, examined the use of thermoelectric converters, examined the neutronic effectiveness of various reflecting materials, examined ways of connecting heat pipes to one another, studied the consequences of the failure of one heat pipe in the reactor core, and did conceptual design work on heat radiators for various power supplies. The Heat Pipe Program reported progress in the design of ceramic heat pipes, new application of heat pipes to solar collectors, and final performance tests of two pipes for HEDL applications. Under the Nuclear Process Heat Program, work continues on computer codes to model a pebble bed high-temperature gas-cooled reactor, adaptation of a set of German reactor calculation codes to use on U. S. computers, and a parametric study of a certain resonance integral required in reactor studies. Under the Nonproliferation Alternative Sources Assessment Program LASL has undertaken an evaluation of a study of gaseous core reactors by Southern Science Applications, Inc. A formal review was conducted and certain recommendations were made. Independently LASL has developed a proposal for a comprehensive study of gaseous uranium-fueled reactor technology. The Plasma Core Reactor Program has concentrated on restacking the beryllium reflector and redesigning the nuclear control system. The status of and experiments on four critical assemblies, SKUA, Godiva IV, Big Ten, and Flattop, are reported. The Nuclear Criticality Safety Program carried out several tasks including conducting a course, doing several annual safety reviews and evaluating the safety of two Nevada test devices. During the quarter one of the groups involved in reactor technology has acquired responsibility for the operation of a Cockroft-Walton accelerator. The present report contains information on the use of the machine and improvements being made in its operation.

I. EXECUTIVE SUMMARY

This is the fourth Reactor Technology Quarterly Progress Report. There will be no attempt to report progress in every program covered under this heading. A selection has been made on the basis of important progress or continuing effort that deserves recognition. The work reported this quarter is divided into eight program areas. In what follows we will discuss the objectives, significant accomplishments, and, where appropriate, the implications of these accomplishments.

The Space Electric Power Supply (SEPS) program is funded by the Advanced Systems and Material Production Division of Nuclear Energy Programs (NEP) under the Assistant Secretary for Energy Technology (ASET) of the Department of Energy (DoE). The objective of the present program is to determine the need for a nuclear SEPS and evaluate various conceptual designs as a basis for future development. This requires designs for the nuclear reactor, the heat extraction system, the converter from heat to electricity, the waste heat rejection system, and the radiation shielding.

During the last quarter emphasis was given to questions concerning safety of nuclear reactors in space. These questions arose because of the COSMOS 954 incident. There are two keys to the safe use of reactors in space: 1. Keep the reactor in a non-operative mode until it has reached a safe orbit, and 2. operate in orbits whose lifetimes greatly exceed the time necessary for radioactive fission products to decay to safe levels. Areas where safety considerations will set power plant requirements have been identified. Another subject of study this quarter was the influence of electrical conversion efficiency of thermoelectrics on meeting mass limitations for a 100-kW_e power supply. The efficiency depends partly on the temperature difference across the thermoelectric material. The temperature at the hot junction is not limited by the reactor but rather by the high-temperature properties of the thermoelectric itself. The temperature at the cool junction determines the size of the heat rejection radiator. A lower temperature means higher converter efficiency but also a larger radiator area and mass. The efficiency also depends on the thermal conductivity of the material. It was concluded that experimental work on

thermoelectric materials capable of operating at 1375 K or higher and with better efficiency than presently available should be vigorously pursued. It appears that thermoelectric converters can be competitive with other converters for power supplies in the 100-kW_e range. Investigations of the neutronic effectiveness of several neutron reflector materials led to the conclusion that BeO is the best reflector and that ¹⁰B₄C is the worst. Consequently, a rotating-drum control rod assembly based on these two materials should provide the largest reactivity control margin and the smallest size reactor. On the other hand, beryllium and BeO reflected cores would weigh about the same for an equal reactivity control margin. Some consideration was given to ways of connecting heat pipes from the reactor subassembly to those on the thermoelectric or thermionic converter subassembly. A male-female type connection was suggested. The main problem is obtaining a good thermal bond. Several methods are suggested but need experimental evaluation. Another study concerned the consequences of a heat pipe failure in the reactor core and showed that the maximum temperature in the fuel element with the failed heat pipe in the current design can reach unacceptably high levels. There are several ways to reduce this temperature. The reactor can be designed with smaller heat pipes spaced closer together. Better bonding between fuel elements will also help reduce the temperature. Other work included a conceptual design of a heat pipe radiator for a 1.3-kW_e Brayton-cycle isotope power supply and design layouts and calculations on several radiators that might be used with a 100-kW_e Brayton-cycle power supply.

The heat pipe program receives funding from the Power Systems Division of the Fossil Energy Programs under the ASET, and the Division of Basic Energy Sciences under the Office of Energy Research of the DoE. Funding was also received from the Hanford Engineering and Development Laboratory (HEDL). The objective of the program is to develop new heat pipes for special applications. Current heat pipe technology efforts include the ceramic heat pipe development program, demonstration of heat pipe control of solar collector temperature, the development of gas-controlled heat pipes for establishing and controlling the temperature of

test capsules for irradiation in the EBR-II, and the development of a mercury heat pipe for removing heat from stored spent fuel elements. During the quarter a new ceramic heat pipe assembly facility was constructed. Protection of the ceramic from the liquid-metal working fluid is provided by a thin layer of refractory metal that is chemically vapor deposited on the interior of the ceramic tubing. Experiments have been performed to determine the temperature dependence of the reaction rate constant between silicon carbide and tungsten. Additional experiments have been performed on methods for sealing the ends of alumina heat pipes. The characterization of the wick structure of an alumina tube lined with a layer of sintered alumina particles has been completed. This is the so-called rough-coat tubing. This structure was able to wick water to a height of 120 mm. The intercluster spacing of the coating particles as observed under a scanning electron microscope gives rough agreement with the prediction of the pyramidal wick model based on the experimental wicking height. The results are promising enough that we are procuring parts to make a heat pipe out of this rough-coat tubing. Ceramic heat pipes designed for use in high-temperature heat recovery units must withstand the high thermal stress induced by startup and also by operating in a temperature gradient. A computer analysis done this quarter shows that the stress due to thermal gradients along the axis of the pipe are negligible compared to the radial stress induced by the temperature difference between the heat pipe working fluid and the external environment. This simplification means that one can develop a simple design chart for determining the ratio of i.d. to o.d. in any given application. We have developed a new application of heat pipes to solar collectors. During the summer many solar collectors are subject to long periods of nonuse during which the temperature rise can cause deterioration of some parts of the system. A heat pipe with radiator fins can be attached to the top of the collector elements to remove the unwanted heat. A working model has been constructed and tested. Two different gas-controlled heat pipes, developed for the establishment and control of specimen temperatures of HEDL irradiation experiments in the EBR-II reactor, have been performance-tested and sent to HEDL in February.

The nuclear process heat program is funded by the Reactor Research and Technology Division of NEP under the ASET of the DoE. The objectives of the program are to provide capabilities for evaluating the international development of very high temperature gas-cooled reactors and for evaluating application systems. This involves development and refinement of computer calculations and systems studies. The activities during the past quarter were mainly concerned with verification of a computer code to calculate the thermal and hydraulic behavior of a pebble bed reactor, and continuation of an effort to evaluate a set of German reactor-design codes called VSOP. The main problem is to get VSOP operative on several U. S. computers. The thermal-hydraulic and life system display package TIK-LSD has been successfully run on the Stanford Linear Accelerator Center (SLAC) IBM computer, and VSOP is now running on the AMDAHL computer at Texas A & M. In addition a parametric study was completed on one of the resonance integrals needed in reactor neutronic calculations. Steady progress is being made in this program.

There is a study in progress under the Nonproliferation Alternative Systems Assessment Program (NASAP) being conducted by Fuel Cycles Evaluation for the Program Director of Nuclear Energy under the ASET in DoE. It is an evaluation of a gaseous-core reactor study being done by Southern Science Applications, Inc. (SSAI). In our first quarterly report, March-June 1977, we reported on our own study of gaseous-core reactor power plants. The knowledge gained in that study is being used to assist DoE in evaluating the SSAI's work. A formal review of the SSAI study by DoE was held during January. SSAI recommended that a steam cycle be used for electrical generation and that the reactor-design study should use features of both the LASL-UTRC Mixed Flow Reactor and the University of Florida's Heterogeneous Gas Core Reactor. DoE accepted the first recommendation but, because of a shortened time schedule for completion of the NASAP study, directed SSAI to assemble the information needed by NASAP using the LASL design and consider only "denatured" cores, i.e., cores containing uranium isotope mixtures which render the fuel useless as weapons material. During the quarter we forwarded to DoE a proposal for a comprehensive

study of gaseous uranium-fueled reactor technology. The proposal defines a 15-year program for development of both a Mixed Flow Reactor and a Plasma Core Reactor.

Funding for the present Plasma Core Reactor (PCR) program comes from NASA. The purpose of the program is to assemble a reactor configuration whose core is a vortex of high-temperature $^{235}\text{UF}_6$ gas and measure its reactivity coefficient as a function of uranium loading and temperature. The program has been delayed recently because of the need to restack the beryllium reflector around the core. The restacking is now complete, and criticality experiments have been done. A critical mass of uranium in the new assembly is 0.66 kg less than in the old assembly. In order to evaluate options for placement of the proposed linear control vanes, we measured the worth of ^{10}B wires and plates in various locations of the reflector. Work yet to be done on the reassembly of the experiment included replacement of the vortex canister, installation of the control system, and check-out of the gas-handling system furnished by United Technologies Research Corp. (UTRC).

Progress is reported on the following critical assemblies: SKUA, Godiva-IV, Big Ten, and Flattop. Funding is mainly from the Division of Military Applications (DMA), under the ASDP of the DoE, and also from NASA. With the completion of mechanical construction and wiring for control circuits, SKUA has been moved into the south end of Kiva III. All the control room panels with their required electrical connections to the Kiva circuits are installed and our initial assembly sequencing tests were performed without difficulty. The initial approach to critical will proceed as soon as the Experimental Plan is approved, probably in early April. There

were a total of 42 Godiva bursts this quarter. That is more than twice as many as in the previous quarter. Central Reactivity Coefficient Measurements in Big Ten and Flattop assemblies continued as part of a program to provide benchmark measurements useful in the validation of neutron transport calculations. Experiments were started at Godiva to establish the feasibility of determining the subcritical multiplication of imploding fissionable material surrounded with low atomic weight substances. The Godiva experiments on static bare plutonium spheres surrounded by various materials established that subcritical multiplication is observable but there may be complications in the interpretation of the data. Computer calculations are in progress to aid in the interpretation of the data and the design of future experiments.

The Nuclear Criticality Safety Program is vital to the operations of many LASL groups and to the Nevada Test Site. Support comes mainly from the DMA under the ASDP in the DoE. During the quarter another nuclear criticality safety course was given, several annual safety reviews were performed, safety evaluations were done on two Nevada test devices, and a calculation was made on the possible criticality of a uranium-loaded graphite annulus subject to hydrostatic pressing. Assistance was rendered to the plutonium chemistry and metallurgy group in connection with their move to the new plutonium processing facility.

In February, Group Q-14 assumed responsibility for operation of the Pajarito Site Cockcroft-Walton accelerator. This machine can deliver up to 700 μA of 350-keV protons or deuterons. During the quarter the accelerator was operated for a total of 44 hours. We are attempting to improve the utility, reliability, and efficiency of the facility.

II. SPACE ELECTRIC POWER SUPPLY (SEPS)

A. Mission Requirements (D. Buden, Q-D0)

Recently, questions have been raised by the COSMOS 954 incident, concerning the safe use of nuclear reactors in electrical power plants in earth orbit. COSMOS 954 was the Russian nuclear-reactor-powered satellite that fell to earth in Northwestern Canada on January 24, 1978. Safety has been and continues to be a major concern of U. S. scientists involved in using reactors in space. Before operation, the reactor and its uranium fuel are safe to handle and touch. There is absolutely no possibility that a nuclear electrical power plant can explode.

The key to safe operation before and during launch is to keep the reactor in a non-operative mode. This is accomplished by adding built-in safety features, such as redundant control elements (only one element is allowed to be unlocked at a time), brakes on the control element actuating mechanisms to prevent movement without two independent signals, and a reactor designed to remain non-operative even with environmental changes, such as immersion in water. Analyses and tests will continue to be used to show safe launch operation in case of launch pad fires, propellant fires, impact at terminal velocity in case of a launch abort or failure to reach orbit, and water impact. Again, the major requirement for safety is not to operate the reactor until the prescribed orbit is reached.

Most applications considered for nuclear reactors are in high orbits, such as geosynchronous (an orbit designed to maintain the satellite at a fixed point above the earth's surface). The higher the orbit, the longer a satellite will remain in orbit. Long orbit times can provide time for radioactive elements to decay. A minimum orbit altitude of about 450 nmi will provide for over a 1000-yr orbit life and thus provide a margin of conservatism in meeting safety criteria. The minimum orbit altitude depends on reactor power level, operating time, and spacecraft configuration. Doubling the orbit to about 900 nmi increases the orbital lifetime to nearly a million years. Satellites in geosynchronous orbit (19 400 nmi), the proposed location of most reactor-powered U.S. satellites, will, for all practical purposes, never reenter the earth's atmosphere.

The only U.S. reactor flown in space, SNAP 10A in 1965, was not operated until its 700-nmi orbit

was achieved. It is estimated that its orbital lifetime is 3800-yr. There are two alternatives for final deposition of the power plant upon eventual reentry. The reactor can be designed to burn up on reentry to the atmosphere, or it can be designed with a heat shield, so that it will impact as a single unit. If designed to break up on reentry, contamination through release of radioactive material in the atmosphere would be limited in quantity and widely dispersed. Potential direct radiation, ingestion, and inhalation exposure would be well below environmental guidelines by many orders of magnitude. The main problem with the burn-up approach is that the reactor is designed to operate at high temperatures making complete burnup difficult. If a reactor with a heat shield should impact as a unit, it would be comparable to a meteorite hitting the earth. The advantage would be that all debris would be in one spot; the disadvantage would be that, like a meteorite, the point of impact would be uncertain.

Safety considerations will establish certain design criteria and influence power plant operation. The major safety-related design goals are:

- (a) to design the reactor to remain subcritical if accidentally immersed in water,
- (b) to avoid accidental criticality resulting from a failure of any element in the power plant,
- (c) to design the reactor to disassemble upon reentry,
- (d) to limit reactor operation during acceptance tests so that fission product build-up causes no problem for ground crew operations prior to launch,
- (e) to restrict reactor operation to sufficiently high orbits so that the radioactivity has decayed to a safe level before reentry occurs.

B. System Studies (D. Buden, Q-D0)

Let us assume a standardized 1000-kW_t reactor design. We now ask how we can optimize the electric power output per unit mass. This depends strongly on the reactor and radiator temperatures. With the use of UC-ZrC or UO₂-Mo fuels, reactors can be designed to operate at temperatures to 1675 K. The prime heat-to-electric converter candidates for a ground demonstration system in the early 80's are

thermoelectrics and Brayton-cycle turbogenerators. The converter is more temperature limiting than the reactor design because of material considerations and is therefore used as a basis for selecting a system operating temperature.

Silicon-germanium is the best developed high-temperature thermoelectric (TE) material. Converters using Si-Ge could operate at 1400 K. System operating temperatures may be reduced to provide allowance for design and manufacturing uncertainties and to provide temperature margin in case of failures in parts of the system. The radiator mass is very dependent on operating temperature; the area requires changes with the fourth power of temperature. The variation in mass as a function of power for various hot-junction thermoelectric temperatures and radiator temperatures is shown in Fig. 1.

The peak output from Si-Ge converters using a hot-junction temperature of 1275 K and radiator temperature of 775 K for a 1000-kW_t reactor is 53 kW_e. The radiator area is significantly reduced at

Fig. 1. Effect of thermoelectric temperature variations on power plant mass.

higher radiator temperatures -- 42% less for 775 K compared to 675 K and 38% less to 875 K compared to a 775 K radiator temperature. The failure mechanism that could limit the hot-junction temperature is in the evaporation of the SiGe material. The limit appears to be around 1400 K but may be higher in a highly compact design of the type we contemplate here. This needs experimental verification. The system mass is a minimum for a radiator temperature between 775 K and 875 K.

Another series of systems were investigated where it was assumed that the SiGe TE efficiency was improved by 40%. Work currently on-going at JPL indicates the near-term attainability of such an improvement by reducing the thermal conductivity of SiGe with the addition of such materials as GaP. Figure 2 is a plot of power plant mass for various power levels with the improved efficiency. Similar trends are seen. Using a hot-junction temperature of 1275 K and cold-junction of 775 K as an example, at 50 kW_e, a 16% power plant weight savings is forecasted with a 40% improvement in thermoelectric efficiency. The peak power for these conditions increased to 74 kW_e from 53 kW_e -- a gain of 21 kW_e. If the TE converter can operate at 1375 K with a radiator of 775 K and the 40% improvement in efficiency is realized, then a peak power of 90 kW_e is possible at a mass/power ratio α of 15.2 kg/kW.

Fuel swelling is the major reactor problem with increases in hot-junction temperature. Figure 3 shows fuel swelling for various power levels. At 1000 kW_t, the swelling for 1400 K reactor heat pipe temperature is about double that at 1300 K but is believed to be acceptable. It appears that a 1375 K hot-junction temperature should be accommodated in the reactor design for thermoelectrics.

Unless the efficiency of the TE conversion can be improved to 10%, it will be necessary to increase the reactor power level to 1500 or 2000 kW_t in order to get 100 kW_e. The reactor core size will have to increase to accommodate these higher power levels while keeping the fuel swelling less than 10%. A goal of 19.1 kg/kW_e must be established so as not to exceed the 30%-of-a-two-shuttle-geosynchronous-orbit-spacecraft mass limitation for the power system. Table I indicates that current Si-Ge TE materials will lead to power plants too heavy to meet our $\alpha \leq 19.1$ kg/kW_e criterion. With improved TE

Fig. 2. Effect of TE temperature variations on power plant mass (efficiencies improved 40%)

materials, there are several ways to meet or come close to meeting the 19.1 design criterion as seen in Table I. Increasing the reactor-power level to 1500 kW_t would lead to an acceptable system mass and provide development leeway in the thermoelectrics. Operating at 1375 K is a decided advantage.

UO₂-Mo reactor designs are included in Table I. These generally show an additional 3 kg/kW_e over UC-ZrC-fueled reactor designs. With present converters, material limitations prevent taking advantage of the potentially higher reactor operating temperature of 1675 K associated with UO₂-Mo.

Experimental work on TE converters at 1375 K and with improved efficiency should be vigorously pursued. It appears that TE converters can be competitive in the 100-kW_e range and meet our objective of $\alpha \leq 19.1$ kg/kW_e.

C. Nuclear Fuel Selection (R. E. Riley and K. V. Davidson, CMB-6)

Over 25 000 references have been quickly reviewed and those containing information pertinent

Fig. 3. Fuel swelling (90UC-10ZrC, 7-yr lifetime).

to SEPS have been abstracted and filed. About one third of those selected have been cataloged. Work has started on compiling and documenting the data for uranium monocarbide (UC), uranium-zirconium carbide (UC-ZrC), uranium dioxide (UO_{2+x}), uranium dioxide cermet (UO_{2+x}+M') where M' may be molybdenum, tungsten, etc., and uranium mononitride (UN).
D. Systems Design (D. R. Koenig, K. Cooper, Q-13)

1. Neutronic Effectiveness of Several Reflector Materials. A study of the relative neutronic effectiveness of several reflector materials was made. The materials investigated were BeO, beryllium, graphite, B₄C (natural isotopic composition) and ¹⁰B₄C. One-dimensional neutron reactivity calculations were performed for a fixed core diameter, near the current SEPS reactor-design point. The results of these calculations are summarized in Fig. 4, which shows the change in reactivity k as a function of reflector thickness for reflectors consisting entirely of one of the materials listed above. Sensitivity calculations show that these curves are not sensitive to core radius, that is, changing the radius has the effect of displacing all the curves vertically by the same amount. The core used in the calculations was fueled with 90% UC-10% ZrC,

TABLE i
THERMOELECTRICS FOR 100-kW_e POWER LEVEL

	Current SiGe				Improved SiGe					
	2000 kW _t	2000 kW _t	1500 kW _t	1500 kW _t	1000 kW _t	1000 kW _t	1500 kW _t	1500 kW _t	1500 kW _t	1500 kW _t
	UC-ZrC	UO ₂ -Mo	UC-ZrC	UO ₂ -Mo	UC-ZrC	UO ₂ -Mo	UC-ZrC	UO ₂ -Mo	UC-ZrC	UO ₂ -Mo
TE Temp (K)	1275	1275	1375	1375	1375	1375	1375	1375	1275	1275
Efficiency (%)	5.3	5.3	7.4	7.4	10.4	10.4	9.0	9.0	7.4	7.4
Thermal Power (kW _t)	1887	1887	1351	1351	961	961	1111	1111	1351	1351
Radiator Temp (K)	775	775	675	675	675	675	775	775	775	775
Radiator Power (kW _t)	1787	1787	1251	1251	861	861	1011	1011	1251	1251
Mass (kg)										
Reactor	660	875	525	765	415	610	525	765	525	765
Shield	310	345	260	315	220	270	250	305	260	315
Converter	680	680	350	350	250	250	335	335	485	485
Radiator	760	760	1800	1800	1040	1040	380	380	495	495
Structure	240	265	295	325	190	220	150	180	175	205
Total	2650	2925	3230	3555	2115	2390	1640	1965	1940	2265
α (kg/kW _e)	26.5	29.2	32.3	35.5	21.1	23.9	16.4	19.6	19.4	22.6

whose radial concentration was varied to obtain a flat (to $\pm 5\%$) radial power distribution when surrounded with a 100-mm BeO reflector. The core consisted of a fuel region surrounded by 3 mm of molybdenum foil thermal insulation, a 10-mm gap, and a 2-mm natural B₄C layer to prevent peripheral power peaking by low-energy neutrons. A buckling height equal to 1.2 times the fuel region diameter was employed, which is consistent with previous neutronic calculations.

Figure 4 shows that, of the materials studied, BeO is the best reflector and ¹⁰B₄C is the worst. Consequently, a rotating-drum reflector assembly based on these two materials should provide the largest reactivity control margin and the smallest reactor. A Be-¹⁰B₄C combination would be second best, followed by the C-¹⁰B₄C reflector. The action of rotating control drums which contain sectors of ¹⁰B₄C was simulated in one dimension by moving an annulus of ¹⁰B₄C from the inside to the outside of the reflector region. For reflector assembly thicknesses in the range of 50-150 mm, the maximum reactivity swing was obtained with a ¹⁰B₄C thickness of 25 mm. The maximum obtainable reactivity swing is plotted versus reflector assembly thickness in Fig. 5 for the various reflector materials studied.

In actuality real control-drum systems would have smaller reactivity swing because they would not be as effective as the one-dimensional model assumed in the calculations. Nevertheless, the calculated results should provide a good indication of the relative effectiveness of the reflector assemblies.

A comparison of reactor size was made between two reactors having either BeO or beryllium reflectors, 100 mm thick. The reactors were optimized for heat removal at a power level of 1 MW_t. The BeO-reflected reactor has a diameter of 505 mm and weighs 474 kg. The beryllium-reflected reactor has a diameter of 510 mm and weighs 414 kg (14 kg more than had been estimated previously by straight substitution of beryllium for BeO in the BeO reference design). Although the beryllium-reflected reactor is lighter than the BeO-reflected reactor, it contains 4 kg more U²³⁵, and it is slightly larger. Therefore, its radiation shadow-shield will be a little heavier. Also the beryllium-reflected reactor will have a smaller reactivity control margin.

It may be that a 100-mm beryllium reflector provides sufficient control margin. If so, then one can argue on the basis of the calculations described in Fig. 5 that an 80-mm BeO reflector would provide the same control margin and yield a smaller reactor.

Fig. 4. Reactivity vs reflector thickness for reflector composed of pure material.

Indeed, such a reactor would have a diameter of 467 mm and weigh 410 kg.

On the basis of the work presented here it seems that "equivalent" BeO and beryllium-reflected reactors weigh about the same, though the beryllium reactor is slightly larger, and that the selection of either reflector material will be governed by considerations of heat transfer, manufacturability, toxicity, safety, etc.

2. Thermal Connection of Heat Pipes. When a heat-pipe-cooled reactor is designed to furnish heat to thermoelectric or thermionic converters, it may be desirable to construct the core as a subassembly, and the converter section as another subassembly, each with its own set of heat pipes. The heat pipes from the core would then have to mate easily with the heat pipes from the converters, with good heat-conducting joints.

A new design approach has been proposed for this heat pipe mating and is shown in Fig. 6. A female socket is required at the end of one heat pipe, into which the other (male) heat pipe fits with adequate assembly clearance. The overlap of the two heat pipes must be sufficient to keep the

Fig. 5. Maximum control margin available by moving a ¹⁰B₄C annulus of optimum thickness (~25 mm) from the outside to the inside of the reflector region.

radial heat flux below an acceptable level; the amount of overlap will be about the length of the reactor core.

A metal sheath is fitted over the end of the male heat pipe before assembly. The sheath is designed to swell radially when it is heated to operating temperature, bridging the gap between the two heat pipes and providing good thermal conduction.

Three approaches to the design of the swelling sheath have been proposed:

- 1) A structure with many small bimetallic fins which would lay flat at low temperature, and curl outward at high temperature, contacting the mating heat pipes.
- 2) An expanding metal. This would have small seed particles encapsulated in a matrix of ductile metal. The seed material would vaporize or decompose to a high-pressure gas on heating, expanding the ductile metal.
- 3) Tubes containing a liquid metal, e.g., sodium, which has a high volumetric expansion on heating.

Fig. 6. Male-female heat pipe coupling.

There are disadvantages in this scheme for mating heat pipes: the temperature drop across the joint can still be as high as 100 K, depending on the design; the female end on the heat pipe is complex to fabricate; the joints occupy space and add weight; and development is required. An alternate approach might be feasible. The heat pipes to be coupled would be sealed and tested as part of the radiator and core assemblies. Before mating the two subassemblies, the end caps on the heat pipes would be removed. The heat pipes would then be welded together as a single unit. The major problem in such a coupling scheme would be obtaining good continuous wick structure.

3. Effect of Heat Pipe Failure on Fuel Temperature. If a core heat pipe fails, the heat from its fuel element will flow out through neighboring fuel elements to their heat pipes. The resulting increase in maximum fuel temperature has been calculated for a reactor with $\text{UO}_2\text{-Mo}$ fuel. For a design where the temperature difference, maximum to minimum, in the fuel was 240 K at normal operating conditions, it increased to 620 K at the location of the failed heat pipe.

This calculation was made with the ISAAS code.¹ Perfect thermal contact was assumed between adjacent fuel elements. With imperfect thermal contact, where some of the heat had to radiate across the interface, an additional temperature rise would occur. If pressure alone were used to obtain a good thermal bond between fuel elements, a correlation found in Ref. 2 gave an additional temperature rise across the interface of 100 K, subject to large variations because of the uncertainty of actual conditions.

For a reactor heat pipe normal operating temperature of 1675 K, the maximum fuel temperature with a failed heat pipe can exceed 2300 K (3690 °F), which

is probably unacceptable. This temperature can be reduced by designing the reactor with more and smaller heat pipes, spaced closer together. The temperature rise in the fuel varies approximately as the square of the heat pipe spacing. Bonding the fuel elements to one another or eliminating interfaces by improved design will also help reduce the fuel temperature rise.

E. Reject Heat (G. Bennett, D. L. Ingwersen, J. V. Repa, WX-4)

1. Brayton Isotope Power System (BIPS) Radiator Design. Work was performed on the conceptual design, layout, and calculations of a heat pipe radiator for the 1.3-kW electrical BIPS in order to determine if heat pipe concepts offered weight and redundancy advantages for isotope power supplies. The working fluid used in the BIPS ground demonstration system is a mixture of xenon and helium, with a molecular weight of 83.8. The conductivity and viscosity, as a function of temperature, have been calculated using the methods recommended in The Properties of Gases and Liquids, by R. C. Reid and T. K. Sherwood. A literature search was made to locate experimental thermodynamic data for Xe-He gas mixtures.

Five radiator designs were considered. Mechanical design of the radiator focused on obtaining a configuration that combines reliability, use of known acceptable fabrication techniques, and minimum mass. All of the designs considered are based on machining or extrusion of aluminum. Ammonia and n-pentane were selected as the heat pipe working fluids because of their operating temperature range and compatibility with aluminum. Configurations 1, 2, and 3 were designed to use individually extruded tube and finned heat pipes joined to each other at the fin edges and then joined to the separately constructed gas manifold. The heat pipes and fins were designed consistent with today's extrusion technology for 6063 alloy aluminum. Concepts 4 and 5 were designed to be constructed from monolithic slabs of aluminum drilled by well-established, deep-hole drilling techniques. The panels would be thinned by machining or chemical-metal-removal techniques to reduce their mass. Individual panels would be mechanically joined, and the gas plumbing would be welded together. It was judged that this concept would contain more joints than necessary,

and construction and testing of the heat pipes would be complicated.

The design shown in Fig. 7 was selected as the most promising. This radiator has a calculated mass of 79.6 kg, was 2.11 m high and 1.47 m in diameter, and has 153 heat pipes. The details of attaching this radiator to the BIPS support structure have not been worked out.

A calculational sequence was outlined to balance the radiator size and heat pipe operating temperatures and to calculate the pressure drops and heat transfer coefficients in the xenon-helium gas mixture. The manifold heat exchanger calculations were done for several sizes of circular and rectangular flow passages to pick a reference size and shape based on flow and thermal considerations. A plot of the gas temperature and the heat pipe operating temperature as a function of distance along the circumference for configuration 3 is shown in Fig. 8. All five concepts resulted in radiators the same physical size or shorter than the pumped-fluid reference radiator for the BIPS flight system.

Fig. 7. BIPS radiator design concept, configuration 3.

Fig. 8. Gas and heat pipe operating temperatures.

2. Brayton-Cycle System. Work during this quarter was devoted to the conceptual design layout and calculations for a 100-kW_e Brayton-cycle system pumped-fluid radiator for use in spacecraft integration studies. The assumptions used in the design require the use of four independent radiator panels, any three of which must have a 99% probability of surviving a 7-yr mission.

Several design concepts were considered. Figure 9 shows a telescoping cylinder design sized to fit into the space shuttle and capable of expansion when it is deployed in space. The smallest cylinder is 1.5 m in diameter and fits snugly around the power supply. Additional cylinders 3.6 m long and each 0.2 m larger in diameter than the other are added until the required surface area is obtained. The cross section of the extruded aluminum

Fig. 9. Telescoping cylinder radiator for 100-kW electric Brayton System.

Fig. 10. Tube and fin cross section.

tube and fin elements is shown in Fig. 10. Mass of the radiator, including 15% for structure support is 527 kg using aluminum and 354 kg using beryllium.

The most promising design concept is shown in Fig. 11. The radiator consists of four individual panels hinged to a support structure that is attached to the power supply framing. The radiator panels are designed to fold together alongside the power supply for transport. The panels would be deployed by tensioned coil springs designed to open the panels in approximately 20 s. The residual kinetic energy of the opening process would be absorbed by crushing a stainless steel honeycomb. This would prevent damage to the structure.

Preliminary structural analyses were completed to verify the feasibility of torsional spring

Fig. 11. Flat plate radiator.

radiator deployment. Position, velocity, acceleration, time, and maximum kinetic energy relationships were determined as shown in Fig. 12. Relationships are based on the deployment of a single 7.0- by 3.2-m panel with a mass of 90 kg. The zero gravity environment allows radiator deployment using very small force, energy, and stress.

Preliminary calculations were made to determine the size and mass of the folding-plate radiator with fin and tube geometry. The effect of changing the ratio of fin length to tube radius on the system mass is shown in Fig. 13. System size and mass calculations were done for panels with three different aspect ratios. A plot of the system mass as a function of the radiator panel width in Fig. 14 shows that long narrow panels result in the lightest radiators.

Fig. 12. Energy, velocity, acceleration deployment time as a function of spring torsion.

F. Materials (L. Lundberg, 9-13)

A paper was prepared describing the molybdenum and tungsten brazing work that was completed with SEPS support last Spring. The paper is entitled "Brazing Molybdenum and Tungsten for High-Temperature Service," and it will be presented to the American Welding Society sponsored Ninth International Brazing Conference in New Orleans on April 4, 1978. The paper summarizes investigations conducted to develop vacuum brazes for molybdenum and tungsten which can be used in seal-joint applications up to 1870 K. Joints were attempted in molybdenum, tungsten, and tungsten-molybdenum. The braze materials included: Ti-10Cr powder, Ti-30V wire, Ti-64V wire, V wire, Ni electroplate, MoB-50MoC powder mixture, V-50Mo powder mixture, Mo-15MoB₂ powder mixture, and Mo-49V-15MoB₂ powder mixture. Braze temperatures ranged from 1900 to 2530 K. Leak-tight joints were made with all braze materials except Ti-10Cr. After heat treatments up to 1870 K Kirkendall voiding was found to cause leakage of some of the joints made with only substitutional alloying elements, but adding base metal powders to the braze or narrowing the root opening eliminated this problem. Kirkendall voiding was shown not to be a problem when interstitial elements were a major ingredient

in the braze material. Shear testing of Ti-6V, V, MoB-50MoC, and V-50Mo brazed molybdenum at 1670 K indicated strengths equal to or better than the base metal.

G. Reliability Analyses (H. Martz, 9-12)

A preliminary systems reliability analysis was performed for both gas-cooled and heat pipe reactor designs coupled to a Brayton-cycle power conversion system. The preliminary results were based on power system component reliability projections and estimates. It was found (Table II) that, by use of sufficient redundancy, the gas-cooled reactor could achieve a projected overall system reliability of approximately 0.92 while a heat pipe reactor design could attain a reliability of roughly 0.95.

H. Publication

- I. L. B. Lundberg, W. C. Turner, C. G. Hoffman, "Brazing Molybdenum and Tungsten for High-Temperature Service," American Welding Society Ninth International Brazing Conference, New Orleans, April 4, 1978.

Fig. 13. FL/R vs system mass.

Fig. 14. System mass vs radiator width.

TABLE II
OVERALL SYSTEM RELIABILITY FOR SEVERAL ALTERNATIVE GAS-COOLED/HEAT PIPE BRAYTON PCS DESIGNS

<u>DESIGN</u>	<u>EOM RELIABILITY</u>		
	<u>BRAYTON PCS</u>	<u>OTHER</u>	<u>SYSTEM</u>
Gas-cooled (1 loop), single ducting welds, 12 drums/12 actuators	0.955	0.960	0.917
Gas-cooled (1 loop), double ducting welds, 12 drums/12 actuators	.955	.967	.923
Heat pipe (1 loop), single ducting welds, 12 drums/12 actuators	.955	.956	.913
Heat pipe (1 loop), double ducting welds, 12 drums/12 actuators	.955	.962	.919
Heat pipe (2 loops), single ducting welds, 12 drums/12 actuators	.998	.943	.941
Heat pipe (2 loops), double ducting welds, 12 drums/12 actuators	.998	.956	.954
Heat pipe (2 loops), double ducting welds, 12 drums/6 actuators	.998	.954	.952
Heat pipe (2 loops), double ducting welds, 6 drums/6 actuators	.998	.949	.947
Heat pipe (1 loop), single ducting welds, 6 drums/6 actuators	.955	.949	.906
Heat pipe (1 loop), single ducting welds, 12 drums/6 actuators	.955	.954	.911

III. HEAT PIPE TECHNOLOGY

Current heat pipe technology efforts include the ceramic heat pipe development program, demonstration of heat pipe control of solar collector temperature, the development of gas-controlled heat pipes for establishing and controlling the temperature of test capsules for irradiation in the EBR-II, and the development of a mercury heat pipe for removing heat from stored spent fuel elements. Progress in these projects is reported below.

A. Ceramic Heat Pipe Technology Development (W. A. Ranken, Q-13)

The ceramic heat pipe involves the use of ceramic tubing as the basic structural material for high-temperature heat pipes. Protection of the ceramic from the liquid-metal working fluid is provided by a thin layer of refractory metal that is chemically vapor deposited on the interior wall of the ceramic tubing. The main application envisioned for the ceramic heat pipe is the construction of very high temperature heat recuperators for various industrial processing furnaces.

During the past quarter a ceramic heat pipe assembly facility has been built and tested, work has continued on liner/ceramic compatibility and braze seal development, and a start has been made on using finite element stress analysis methods to predict the behavior of ceramic heat pipes during start-up and normal operating conditions.

1. Ceramic Heat Pipe Assembly Facility (E. S. Keddy, Q-13). In order to fabricate prototype ceramic heat pipes it was necessary to construct a ceramic heat pipe assembly facility (Fig. 15). This facility provides a vacuum and/or inert gas environment for the loading of either sodium or lithium into the heat pipe and the subsequent insertion and high-temperature sealing of the ceramic end plug.

The facility consists of a water-cooled fixture with a vacuum and inert gas manifold, and an O-ring joint for mounting a quartz envelope. Centered in the fixture is a base plate that holds an adjustable tube to position the ceramic heat pipe. Also attached to this base are rods to support a molybdenum collar, which acts as a guide to center the ceramic heat pipe and supports the radio frequency (r.f.) susceptor. An r.f. susceptor is a piece of metal in the r.f. field that converts the r.f. energy into heat. In this case, it is a metal ring around the ceramic

Fig. 15. Vacuum/inert gas facility for loading and sealing 610 mm ceramic prototype heat pipes.

heat pipe. Three 2.5-mm-diam tungsten pins are used to support the 100-mm-long molybdenum r.f. susceptor thus limiting the thermal conductance from the heat zone to the support collar. The assembly is enclosed in a quartz envelope which fits into the water-cooled fixture. Access to the heat pipe is provided through a ground-glass joint on the top of of the quartz tube. Heat is applied by r.f. induction into the susceptor by positioning a 100-mm-long r.f. coil around the quartz envelope.

Typical loading and final closure of a ceramic heat pipe utilizing this facility will be as follows:

1. A ceramic heat pipe will be mounted in the facility positioned such that the top end is centered within the r.f. susceptor. The quartz envelope is emplaced and the system is alternately evacuated and purged with argon.
2. With a continued flow of argon through the chamber, a funnel-shaped capped container, with a predetermined amount of working fluid under a cover gas of argon, is positioned on top of the heat pipe and within the heat zone. The protective caps are removed from the container and the quartz envelope is closed and evacuated to $<10^{-5}$ torr.

3. The working-fluid container is radiation-heated by the molybdenum susceptor. The molten fluid is thus introduced into the ceramic heat pipe by gravity flow. Additional degassing of the working fluid can be accomplished by heating the pool with clam shell heaters wrapped around the quartz envelope.
4. After vacuum degassing and cooling to ambient temperature, the chamber is back-filled with argon and continually purged while the working-fluid container is replaced by the ceramic end plug. The end plug, with the sealant material (ceramic or braze) is inserted into the end of the heat pipe. A W-W26%Re thermocouple is placed through the access port and positioned inside the end plug. The quartz envelope is evacuated to 10^{-5} torr and the joint area is induction-heated to the sealant melt temperature to make a vacuum-tight final closure.

Although the facility was designed for the final assembly of 25-mm-o.d. prototype heat pipes 610 mm long, shorter test specimens, to evaluate ceramic seal techniques, can be accommodated. Special refractory fixtures, adapted to fit on the support tube, are used to center the test specimens in the heat zone.

Thus far only one reaction-type bond has been tried with the assembly unit. This was the yttria-alumina powder mixture that had been successfully demonstrated earlier in the resistance-heated, high-vacuum furnace. No problems were encountered with the control and operation of the new facility, and reaction bonding of two alumina cylinders, mating with a 5° taper, was successfully accomplished. However, insufficient thickness of the bonding mixture prevented the achievement of a leak-tight joint.

2. Compatibility and Sealing Studies (L. B. Lundberg, Q-13). Experiments to determine the kinetics of the reaction between β -SiC and tungsten at high temperatures are continuing. We are also interested in determining the kinetics of the high-temperature reaction between the composite β -SiC/graphite/tungsten as fabricated by Thermo Electron

Corporation. These studies should generate the type of data needed to establish operating and braze temperature limits for the silicon carbide heat pipe.

As mentioned last quarter, chemical vapor deposited (CVD) silicon carbide was found to react with CVD tungsten only at 1875 K or above. The 1875 K data for reaction layer growth fit a parabolic law of the form $X^2 = 2kt$, where X is the layer thickness, k is the rate constant, and t is the time. A rate constant of $3.5 \times 10^{-10} \text{ cm}^2/\text{s}$ was calculated from the data. Additional experiments have been performed to determine the temperature dependence of the reaction rate constant. Tungsten-coated silicon carbide tube sections were heated for 1, 4, and 16 h at 2005 and 2075 K in a vacuum to develop these data. The metallography is in progress, so the reaction layer thicknesses have not yet been measured. The rate constant is expected to vary exponentially with the inverse of the temperature.

Although direct lining of silicon carbide with tungsten is the simplest configuration for fabricating silicon carbide heat pipes, the possibility exists that better long-term stability and accommodation of the slight thermal expansion mismatch between silicon carbide and tungsten could be achieved by interposing a barrier layer between these two materials. This approach has been taken by Thermo Electron Corporation investigators in developing emitter assemblies for flame-heated thermionic converters. An assembly of this type, wherein graphite is used as an interlayer between tungsten and β -SiC, has been obtained from Thermo Electron. Sections of this assembly have been vacuum-fired for 1, 4, and 16 h at 1910, 2005, and 2075 K. The metallography is in progress, so the reaction rate data are not yet available. Thermo Electron has had good success with the maintenance of the bond between the composite components during thermal cycling.

Tubes of silicon carbide have been received from two companies, Carborundum and Hague International, to evaluate their potential for heat pipe application. The Carborundum tubing is made from their sintered α -SiC, which is impermeable, while the Hague tubing is made from an $\alpha + \beta$ mixture and is permeable. The tubes are not large enough for fabricating a heat pipe, therefore the evaluations will only include tungsten-coating studies, material characterization, and joining studies.

Additional experiments have been performed on sealing methods for alumina heat pipes, and the characterization of the alumina rough-coated tubing was completed. Experimental activities have included brazing bare alumina tubes with both the Al_2O_3 -25mole% Y_2O_3 and Ti-21V-25Cr, and brazing tungsten metallized alumina with V-34Nb. The braze was tried on a 7° taper joint without success due to buckling of the tube column at the joint. This taper angle seems to be less satisfactory than the standard taper that has yielded a leak-tight joint in the past with this braze material.

The Ti-21V-25Cr alloy that was prepared at LASL was not ductile enough for rolling, so it was made into powder via the hydride process. The powdered braze material was placed at the root of an AD-998 alumina straight joint with a root opening of about 0.16 mm, and the joint was brazed at 1875 K for about 1 min. The braze did not flow freely and there was evidence that bubbling of the braze material during the braze cycle prevented the formation of a leak-tight joint. Discussions with D. Cannonico at ORNL indicated the bubbling was probably due to chromium evaporation from the melt and their experience indicated that a hold time of about 10 min at the braze temperature was required to eliminate the bubbles. He also indicated that this treatment virtually eliminated the chromium from the braze joint. The ORNL procedure will be followed in future experiments with this braze material and standard taper joints will be used.

Tungsten metallized standard taper alumina mating pieces have been obtained for braze trials using V-34Nb as the braze material. Poor braze flow was observed when one of these joints was taken to 2160 K with a V-34Nb foil preplaced in the joint, even though the braze temperature was 25 K above the reported melting point of this braze material. Ti-65V, which has been used successfully in brazing tungsten,³ will be tried in subsequent tests.

The characterization of the wick structure of an alumina tube lined with a layer of sintered alumina particles has been completed. This is the so-called rough-coat tubing. The scanning electron micrograph seen in Fig. 16 shows the rough coat to be a porous structure of relatively large clusters of agglomerates of fine particles. As reported last quarter, this structure was able to wick water

Fig. 16. Scanning electron micrograph of alumina particulate layer sintered on interior wall of smooth-bare alumina tubing.

to a height of 120 mm. This is in rough agreement with the pyramidal wick model for which the wicking height is expressed as

$$h = \frac{2.23 \gamma}{b \rho g},$$

where γ is the surface tension of the working fluid, ρ is its density, g is acceleration due to gravity, and b is the width of the bases of the pyramids, or in this case, the intercluster spacing. Substitution of the observed wicking height into this equation gives a value of $b = 0.14$ mm, whereas the average intercluster spacing of the wick structure lies in the range of 0.11 to 0.15.

Steps are presently being taken to procure the parts necessary to assemble bare alumina heat pipe with this rough coat on the inside walls.

3. Thermal Stress Analysis of Ceramic Heat Pipes (C. A. Anderson, Q-13, and F. Ju, Dept. of Mech. Eng., UNM).

a. Introduction. Ceramic heat pipes designed for use in high-temperature heat recovery units must withstand the thermal stresses induced by heat pipe start-up and also those caused by operating temperature gradients. Although metal heat pipes would be capable of supporting these stress fields, ceramic heat pipes are more prone to fail from thermally induced stresses because of the brittle nature of the material. Thus, careful evaluation of thermal stresses in ceramic heat

pipes must be carried out in order to assure that the stresses do not exceed the ultimate tensile strength of the material at the given temperatures.

For purposes of calculation we have taken the situation of heat pipe of length 2% with an evaporator section ($-2 \leq x \leq 0$) in which a heat flux Q is being absorbed from a hot gas stream, and with a condenser section ($0 \leq x \leq 2$) where the flux Q is being returned to a cold gas stream. Figure 17 shows the situation being modeled. For normal operation the working fluid in the heat pipe will be at nearly constant temperature, which we assume to be the value at the inner wall of the heat pipe. Two-dimensional steady-state temperature and stress distributions were calculated using the finite element TSAAS code¹ for a pipe with 19.0-mm i.d., 25.4-mm o.d., and length 2% equal to 76 mm. It turns out that the maximum stress does not occur near the boundary between the evaporator and condenser but rather in the uniformly heated sections. This means that one can replace the two-dimensional analysis (radial and axial) with a one-dimensional analysis (radial alone). These two analyses are discussed below in separate subsections.

b. Two-Dimensional Finite Element Thermal Stress Analysis. Figure 17 illustrates the finite element mesh of the reference design alumina heat pipe (19.0-mm i.d., 25.4-mm o.d., and 76-mm long). The heat flux at the outer wall was taken to be 2 W/cm^2 (inwardly directed) for $x < 0$ and -2 W/cm^2 for $x > 0$. No axial motion or shear stress was allowed at the ends of the pipe. The inner surface of the heat pipe was taken to be at 800°C . Material properties for 99.8 percent pure polycrystalline alumina (Al_2O_3) at 800°C (Ref. 4) were used in all calculations and are shown in Table III.

TABLE III

MATERIAL PROPERTIES OF 99.8% PURE POLYCRYSTALLINE
 Al_2O_3 at 800°C

Young's modulus	370 000 MPa
Poisson's ratio	0.3
Ultimate tensile strength	220 MPa
Coefficient of thermal expansion	$9 \times 10^{-6}/\text{K}$
Thermal conductivity	0.008 W/mm-K

Fig. 17. Finite element mesh for alumina heat pipe reference design.

The resulting steady-state contour plots of temperature, transverse shear stress, radial stress, and circumferential (hoop) stress are shown in Fig. 18. The axial discontinuity in heat flux actually causes a reduction in the hoop stress in the region of the evaporator-condenser interface with an attendant development of relatively small transverse shear stress. The most severe stress situation (tensile hoop stress and no transverse shear) is that caused by the radial temperature gradient alone. For this reason ceramic heat pipes at operating conditions can be designed using an analytically based radially symmetric thermal stress field.

Fig. 18. Temperature and stress contour plots.

c. Design Chart Approach. Reference 5 gives formulas for stresses in a long, thick-walled elastic cylinder under uniform heat flux Q on the inner or outer surface. For a heat flux directed inward at the outer surface, the axial stress σ_x becomes equal to the hoop stress σ_θ at the inner surface of the heat pipe and reaches its maximum tensile value there. The formula is

$$\sigma_\theta = \sigma_x = \frac{\alpha E Q b}{2(1-\nu)k} \left(1 - \frac{2b^2}{b^2-a^2} \ln \frac{b}{a} \right),$$

where α is the linear expansion coefficient, E is Young's modulus, ν is Poisson's ratio, k is the thermal conductivity, and a and b are the inner and outer radii, respectively. The maximum tensile stress condition is shown in Fig. 19 as a series of straight lines giving the dimensionless stress variable $2(1-\nu)\sigma_\theta/E$ as a function of the thermal load variable $\alpha Q b/k$ for various ratios of a/b . For the reference design above, the maximum tensile hoop stress is calculated to be 24 MPa, which is about one-tenth of the ultimate tensile strength of Al_2O_3 at this temperature.

B. Solar Collector Heat Pipe (W. A. Ranken, J. R. Phillips, and F. C. Prenger, Q-13)

Solar collectors for heating or cooling of buildings are subject to long periods of operation during which no useful heat is removed from them. For heating-only systems this is the usual situation

during summertime when the only demand is for heating hot water and the storage system is at maximum temperature. However, it is also the case during other times of the year, particularly during spring and fall. In the case of flat-plate collectors, unless they are covered or otherwise protected, shutdown of the heat removal system will cause the collector temperature to rise on the order of 70°C above normal operating temperature. This temperature increase has resulted in failure of some liquid-cooled systems because of the increased pressure of the coolant. The temperature increase and its potential consequences are even more severe in the case of low-concentration focusing systems that offer high efficiency for cooling applications. This temperature rise can be avoided by pointing the receiver reflector away from the sun or accommodated by using a low-vapor-pressure fluid. One could also drain the fluid from the collectors. For reasons of cost, efficiency, and maintenance, these solutions are not fully desirable.

An alternative method of eliminating these temperature excursions is to use variable conductance heat pipes to provide passive temperature regulation. The way in which such a heat pipe can be used for this purpose is illustrated in Fig. 20 for the case of a flat plate collector. This figure shows a row of gas-filled heat pipes embedded in the collector surface. In normal operation heat generated in the collector is conducted to these heat pipes and transferred by them to a heat collection system which may be either a circulating liquid system or another heat pipe. Temperature control is achieved by using the heat pipe evaporation/condensation cycle to pump the inert gas charge into the upper end of the pipe where a vapor/gas interface is established. This interface will normally be located within the heat pipe region encompassed by the heat collection system. The heat pipe operating temperature for this operating mode will be determined by the pressure of the inert gas when the interface is in the location a on Fig. 20. When the heat collection system is turned off, the vapor pressure of the heat pipe working fluid increases sufficiently to compress the inert gas into a finned section of the heat pipe until sufficient fins are exposed to the condensing working fluid to reject the solar heat by natural convection, for example, location b

Fig. 19. Hoop stress as a function of heat pipe flux for various a/b ratios.

Fig. 20. Flat plate solar collector configuration with gas-filled heat pipes providing automatic temperature control. "a" represents the vapor/gas interface location during normal operation at maximum solar power input. "b" is its location under the same condition, but with the heat collection system shut off.

on Fig. 20. Depending on the volume of the gas reservoir, the change of gas pressure required to achieve this fin exposure can be made as small as desired. Because the vapor pressure of the heat pipe working fluid, which will be approximately equal to the inert gas pressure, varies very rapidly with temperature, a design of a unit for which complete exposure of the finned section entailed a pressure increase of 20% would give a temperature increase on the order of only 2%. Thus a unit operating normally at 77°C would, when the heat collection system was turned off, automatically shift the heat load to the finned heat dissipation section of the heat pipe with a consequent temperature rise of no more than 7°C.

In order to test the effectiveness of heat pipe control of solar collector temperature, a demonstration unit was constructed for laboratory simulation of solar operation. This heat pipe was fabricated from stainless steel and had a length of 1.5 m and an outside diameter of 22.2 mm. The working fluid was water and the wick was a simple homogeneous

structure consisting of two layers of 100-mesh copper screen. This resulted in a vapor passage diameter of 16.5 mm. The evaporator length was 0.65 m (roughly one-third the probable length for an actual flat-plate solar unit) and heat was introduced along this entire length by clam shell heaters, which were overwrapped with insulation. The heat collection system was simulated by flowing water through a jacket surrounding the heat pipe just past the evaporator section. The flow rate and temperature change of the recirculating water system were used to measure the heat flow from the evaporator.

Adjacent to the water jacket was a finned section 0.52 m in length. This section consisted of 70 copper fins, 100 mm in diameter and 0.5 mm thick, brazed to the heat pipe. The final 0.2 m of the heat pipe served as inert gas reservoir volume. This was augmented by a separate reservoir with a volume of 227 cm³. The general configuration of this test heat pipe is shown in Fig. 21.

The temperature profile along the heat pipe was measured by a set of 16 thermocouples for several heating rates and initial gas-fill pressures. Three such profiles are shown in Fig. 21 for a heat flow

Fig. 21. Demonstration of solar collector temperature control with a gas-filled heat pipe for a transmitted power of 250 W and an initial gas-fill pressure of 46.7 kPa (350 torr). When the water cooling is turned off the vapor/gas interface moves from position 1 (within the water-cooling jacket) to either 2 or 3, depending on whether fin cooling is aided by a small fan or is accomplished solely by natural convection.

rate of 250 W and an initial gas-fill pressure of 46.7 kPa (350 torr.). Curve 1 was obtained with the heat collection system operating and shows the uniform temperature in the evaporator section of the heat pipe dropping precipitously within the water-cooling jacket so that the finned section of the pipe is operating at ambient air temperature. Curve 2 shows the temperature profile when the water flow was shut off and a small fan was used for forced convection cooling of the fin array. With this condition only one-third of the fins are needed to dissipate the 250-W heat flow. With just natural convection for cooling the fins (curve 3) the region of sharp temperature drop (the vapor/gas interface location) moves close to the end of the finned section. The evaporator temperature rises of 4°C from curves 1 to 2 and 9°C from curves 1 to 3 agree with calculations based on the existing gas reservoir volume if the vapor/gas interface locations are as shown by the arrows at the top of Fig. 21. As indicated above, these temperature increases can be made arbitrarily small by increasing the reservoir volume. Curve 1 in Fig. 21 indicates that the amount of heat lost to the finned part of the heat pipe during normal operation is small. This can be further reduced by interposing a short insulated section between the finned region and the heat collection water jacket.

The tests of the simulated solar collector heat pipe have shown that collector temperature excursions can indeed be eliminated by passive means through the use of the gas-filled heat pipe. By adjusting the initial fill pressure the temperature can be controlled to whatever value the solar system designer may select. The primary design consideration is that the finned region be sized to dissipate the maximum amount of heat that the collector-associated heat pipe can receive, and do so under adverse ambient air conditions, such as still air at 30°C.

Although this test was set up to demonstrate the solar application, a very similar configuration can be used to control the temperature of terrestrial radioisotope heat supplies for remote-site power generation. In this case the heat source is a layer of radioactive material surrounding the evaporator section of the heat pipe and the heat collection system is replaced by a thermoelectric,

Brayton or Stirling power generation unit. Should the converter unit fail or need to be removed for maintenance, the temperature can be held relatively constant as the heat removal function is transferred automatically to the finned section of the heat pipe.

C. HEDL Gas-Controlled Heat Pipes (J. Deverall, E. S. Keddy, Q-13)

The HGK series of heat pipes were developed for the establishment and control of specimen temperatures of HEDL irradiation experiments in the EBR-II (Ref. 6). These pipes are 19-mm o.d. by 610 mm long with 0.51-mm walls and contain a helical trough wick structure for gravity-assist liquid return. There are four helical troughs with six turns per helix over the length of the pipe. The troughs, 1.59 mm wide by 3.18 mm high, are formed with 160-mesh screen and diffusion-bonded to a wall wick consisting of 5 layers of 250-mesh screen. The working fluid is potassium and neon is used for the control gas. Specific operating conditions for these pipes were 2.55 kW throughput at 640°C (1180°F) for HGK-R1 and 2.18 kW throughput at 718°C (1325°F) for HGK-R2.

1. Heat Pipe Fabrication. After the wick structure was installed, the tubes were sent to HEDL to have the end caps and fill tube (supplied by HEDL) welded in. When the complete heat pipes were returned they were outgassed at 900°C and then helium-leak-checked.

Two different potassium loadings were used depending on the intended operating temperature. HGK-R1 was loaded with 11.0 g for operation at 640°C (1185°F) and HGK-R2 was loaded with 10.0 g for operation at 718°C (1325°F). The proper amount of high-purity potassium was vacuum-distilled into each pipe and sealed under vacuum with a bellows valve. After loading, the pipes were wet-in at 700°C for 48 h using an induction heating coil.

2. Performance Testing. Before loading the control gas, the heat pipes were operated with a gas-gap calorimeter to test the heat transfer capability and wicking performance. Each pipe was tested at operating temperature and up to 155% of its required heat transfer capacity. A sonic limit point and a pressure-gradient limit point at 600°C were obtained to establish the sonic and pressure-gradient limit curves for this wick geometry.

Each pipe was then loaded with research-grade neon gas to establish the required operating temperature at the nominal heat transfer rate. HKG-R1 was loaded to 4.41 kPa (33 torr) to control at 640°C (1185°F) with a heat transfer rate of 2.55 kW. HKG-R2 received 10 kPa (75 torr) to control at 718°C (1325°F) with a heat transfer rate of 2.18 kW.

The pipes were then fitted with an Inconel sleeve and a nickel-copper block, to simulate heat rejection in the reactor, and with a gas-gap calorimeter to measure heat transfer rate (see Fig. 22). The irradiation capsule arrangement being simulated is shown at the bottom right side of the heat pipe opposite the test mock-up. An Inconel sleeve was clamped to the heat pipe to mock-up the corresponding Inconel section in the capsule and six thermocouples were used to determine the temperature gradient along the sleeve. The sleeve was surrounded

by a nickel block to mock-up the thermal conductivity of the static-sodium section of the capsule. A copper block was brazed to the top of the nickel to simulate the heat transfer at the flowing reactor-sodium area.

Data points were obtained at various power levels with the average control-gas and block temperature held at the expected reactor-sodium coolant temperature of 420°C (see Fig. 23).

When the control capability tests were completed, the heat pipe fill-tubes were pinch-seal-closed. The heat pipes were then cleaned and heated in a vacuum furnace at 700°C for a minimum of 8 h to ensure that the pinch-seal-closure was tight.

3. Final Performance Test. The heat pipes were again tested with the block system and calorimeter to determine if the pinch-off operation had caused any change in the operating temperature or control capability. Three data points were obtained for each pipe to establish an average control curve with the control gas temperature at 420°C. One point was also obtained at a nominal level with the control gas temperature at 400°C. The pinch-seal-closure resulted in a change of 7°C in the control temperature of the 640°C heat pipe and a 2°C change in the 718°C pipe.

Over the operating range, the average thermal control was approximately 6°C/kW change for the 640°C heat pipe and 10°C/kW change for the 718°C heat pipe (see Fig. 24).

4. Reporting and Shipment. A report entitled "Construction and Testing of HEDL Thermal Control Heat Pipes," along with a copy of all quality assurance documentation, was submitted to HEDL. HKG-R1 and R-2 heat pipes were shipped to HEDL in February 1978.

D. Presentations

1. W. A. Ranken, "Energy Conservation Applications of the Heat Pipe." Talk given to the Energy Conservation Round Table, Department of Energy, Jan. 11, 1978.
2. H. Casey (CMB-6), E. W. Keddy (Q-13), and V. Vigil (CMB-6), "Fabrication of Welded Molybdenum/Lithium Heat Pipes for Use in Nuclear Thermionic Power Systems for Spacecraft," AIHE Annual Meeting, Denver, Colorado, March 1, 1978.

Fig. 22. Reactor mockup test.

Fig. 23. Temperature control performance of HGK series heat pipes.

Fig. 24. Effect of pinch seal on control temperature.

IV. NUCLEAR PROCESS HEAT

The overall technical objectives of the LASL Nuclear Process Heat program have been to provide a technological evaluation and analysis capability in support of the international development of the very high temperature gas-cooled reactor (VHTR), and to participate in the orderly development and transfer of technology between countries for evaluation and program planning leading to an ultimate demonstration of the concept. The development, refinement, and evaluation of appropriate computer codes-including the German pebble bed reactor (PBR) design code (VSOP)-for analysis of reactor core neutronic behavior, heat transport, and process system behavior continue to be major efforts in the LASL program.

The activities in the past quarter were mainly concerned with verification of the pebble bed hydraulic model, integration of the heat transport module into the pebble bed hydraulic code, and continuation of the VSOP code evaluation effort. We can now predict experimentally measured hydraulic results on a full-scale pebble bed experiment with the LASL pebble bed thermal-hydraulic code. In the VSOP code evaluation and adaptation task, the thermal-hydraulic and life system display code TIK-LSD has been successfully run on the SLAC (IBM) computer, and the VSOP code is now running on the AMDAHL computer at Texas A & M. A resonance integral study was completed.

A. Pebble Bed Thermal-Hydraulic Code (K. R. Stroh, Q-13)

The hydraulic portion of the previously reported model⁷ has been verified by comparing computer code predictions with measured values from the full-scale mockup of the Oak Ridge Pebble Bed Reactor Experiment (PBRE). The Oak Ridge PBRE was a small pebble bed, with a bed diameter to ball diameter ratio of 20.⁸ Coolant flowed upward through the core support plate, and the bed was topped by a fill-cone at the angle of repose. The core support was an inverted 30° cone, with a conical ball discharge dome located at its center. The flow area of the support plate varies considerably with radius, and the bed void fraction varies radially because of the wall effect. Figure 25 illustrates the bed geometry and the flow conditions of this experiment.

Fig. 25. Oak Ridge Pebble Bed Reactor configurations.

Samples of code output and comparison of predicted with measured hydraulic values can be seen in Figs. 26-29. Dimensionless mass flux (density times velocity) streamlines can be seen in Fig. 25 for the fill-cone geometry; Fig. 26 illustrates the normalized pressure distribution in this geometry. Figures 27 and 28 compare code predictions for the coolant velocities measured with the fill-cone removed and above the fill-cone.

Successful modeling of this complex geometry has demonstrated the flexibility of the computer code. The code has converged to the solution, in an orderly manner, for all physically realizable situations modeled to date.^{9,10} Coupled thermal-hydraulic test problems are now being run using Kernforschungsanlage (KFA) designs and computed fission power profiles from VSOP. The code is being prepared for coupling to the VSOP code package.

MASS FLUX STREAMLINES, PBRE BED 13FCA

IDENTIFICATION	CONTOUR VALUE
A-A-A-A	0.
B-B-B-B	5.000E-03
C-C-C-C	2.000E-02
D-D-D-D	4.500E-02
E-E-E-E	8.000E-02
F-F-F-F	1.250E-01
G-G-G-G	1.800E-01
H-H-H-H	2.450E-01
I-I-I-I	3.200E-01
J-J-J-J	4.050E-01
K-K-K-K	5.000E-01

Fig. 26. Mass flux streamlines in PBRE Bed 13.

ISOBARS (NORMALIZED), PBRE BED 13FCA

IDENTIFICATION	CONTOUR VALUE
A-A-A-A	1.000
B-B-B-B	1.003
C-C-C-C	1.006
D-D-D-D	1.009
E-E-E-E	1.012
F-F-F-F	1.015
G-G-G-G	1.018
H-H-H-H	1.021
I-I-I-I	1.024
J-J-J-J	1.027
K-K-K-K	1.030

Fig. 27. Pressure distribution in the PBRE Bed 13.

EXIT VELOCITY, PBRE BED 13FTa

Fig. 28. Comparison of code predictions with the velocity profile measure in PBRE Bed 13 (fill-cone removed).

EXIT VELOCITY, PBRE BED 13FCa

Fig. 29. Comparison of code predictions with the velocity profile measure in PBRE Bed 13 (above fill-cone).

B. VSOP Code Status (Lucy Carruthers, Q-13)

1. TIK-LSD Program. The TIK-LSD code is a thermal analyses program (TIK) together with the life display program for interpolating PBR ball temperatures and hydraulic parameters between points in a two-dimensional mesh. The TIK-LSD code was taken from a tape belonging to K. Petersen (KFA) and run on the Texas A & M AMDAHL computer. The object program and several test problems (an axial flow stand-alone problem, a modified THTR stand-alone problem and a VSOP problem) were copied on tape at Texas A & M and taken to the SLAC IBM computers where they have run successfully. In order to study the operation of the temperature calculation program, a short program was written to read and print the communication file written by VSOP which is input to the TIK-LSD code.

2. DATA-2, ZUT-DGL, and VSOP Programs. The three VSOP programs were copied on tape at SLAC and run successfully at Texas A & M. This involved accommodating to a somewhat different system and constructing time and date routines using the programs supplied with the Texas A & M system. The transfer was quite straightforward because of availability of the SLAC WYLBUR system on the Texas A & M computer--local modifications to WYLBUR necessitated some changes, however.

Running times at Texas A & M are somewhat longer (about 30%), but a different charging algorithm approximately compensates for this, so that the night-time charges at Texas A & M are approximately the same as the SLAC charges for an equivalent amount of computing.

3. Resonance Integral Calculations. As an outgrowth of difficulties in reproducing a VSOP test problem that had been run at KFA, Germany, a number of resonance integrals characteristic of PPBs were calculated. The variation of the resonance integrals (RI) with carbon-to-heavy-metal ratio (C/HM), uranium enrichment, and temperature was studied.

The resonance integral calculated by VSOP is an effective resonance integral, defined as the integral of the effective microscopic absorption cross section divided by energy, over the energy range from fission energy to thermal energy. The effective microscopic absorption cross section is the microscopic absorption cross section times the macroscopic cross section for scattering, divided by the sum of the macroscopic cross sections for scattering and absorption. The effective resonance integral is also sometimes defined as the integral of the product of the microscopic absorption cross section times the flux, where the flux is normalized to $1/E$.

at large E. Thus the resonance integral is a measure of the "bite" taken out of the neutron flux as it slows down.

The main contributions to the integral come from sharp peaks or "resonances." As temperature increases these resonances broaden, so that cross sections which have been averaged over the distribution of nuclear velocities are referred to as Doppler-broadened cross sections. It is this Doppler effect which causes the resonance integral to depend on temperature.

A typical set of resonance integrals calculated by VSOP for C/HM = 110 and 10 percent enrichment is given in Table IV.

TABLE IV
TOTAL RESONANCE INTEGRAL

<u>T °C</u>	<u>(barns)</u>
20	28.503
400	33.121
700	35.521
1000	37.397

A semi-empirical relation giving the dependence of resonance integrals on temperature was obtained by E. Hellstrand, P. Blomberg, and S. Horner.¹¹

$$RI/RI(300\text{ K}) = 1 + A [\sqrt{T} - \sqrt{300\text{ K}}].$$

This equation is consistent with experimental data and also with equations for the resonance integral obtained by substituting approximate expressions for the Doppler-broadened cross sections above.

Our resonance integrals calculated with VSOP do not fall exactly on a straight line when plotted vs $[\sqrt{T} - \sqrt{300\text{ K}}]$. Since the equation above is an approximate expression obtained for the temperature range 20-600°C, we do not consider the deviation any indication of error. The data are best represented by a quadratic where the departure from linearity is rather small. The data are graphed in Fig. 30.

The variation of RI with uranium enrichment was looked at next. For a given C/HM and temperature, the resonance integrals increase with enrichment in an approximately linear manner as shown in Fig. 31. Data are tabulated in Table V for T = 697°C, C/HM = 110.

TABLE V
RESONANCE INTEGRALS WITH ENRICHMENT

<u>Enrichment</u>	<u>(barns)</u>
.03	34.411
.10	35.499
.19	37.054
.20	37.239

One can look at Fig. 31 and find the enrichment corresponding to a given total resonance integral. In the problem under investigation the desired resonance integral is 37.02 barns at 697 °C, which gives an enrichment of 18.9%. At a temperature of 404 °C the analysis is repeated and the desired resonance integral (34.64 barns) is found to correspond to an 18.9% enrichment also, so the analysis does not vary significantly with the temperature chosen for comparison.

Finally the variation of RI with carbon-to-heavy-metal ratio was studied. For a given enrichment and temperature, the resonance integral varies smoothly with C/HM as shown in Fig. 32. Specifically for T = 697 °C and an enrichment of 3% we have the data in Table VI.

TABLE VI
RESONANCE INTEGRALS WITH C/HM

<u>C/HM</u>	<u>(barns)</u>
80	30.605
110	34.411
135	36.891
160	38.935

Here the C/HM corresponding to a resonance integral of 37.02 barns is seen to be about 136. Thus we find another combination of C/HM and uranium enrichment which gives the same resonance integral for this problem. The full situation, showing the combinations of enrichments and carbon-to-heavy-metal combinations that give the same resonance integral, 37.02 barns, is depicted in Fig. 33.

Fig. 30. Variation of RI with temperature (C/HM = 110, enrichment = 10%).

Fig. 32. Variation of RI with C/HM, (T = 697 °C, enrichment = 3%).

Fig. 31. Variation of RI with enrichment (T = 697 K, C/HM = 110).

Fig. 33. Enrichments and carbon-to-heavy-metal ratios giving the desired resonance integral.

V. GASEOUS CORE REACTOR STUDY EVALUATION

A. LASL Support to DoE on NASAP Evaluation of the Gaseous Uranium-Fueled Reactor (GUFR) (L. L. Lowry, T. McLaughlin, Q-14)

SSAI, study contractors on this NASAP evaluation, began their efforts by acquiring descriptions of all known gaseous core nuclear reactor designs. They asked us to review these designs and give them our comments. Some were very sketchy and most addressed nuclear reactors for space applications rather than electrical power generation and therefore did not consider a number of essential subsystems. Our review concluded that the Heterogeneous Gas Core Reactor designed by Nils Diaz at the University of Florida was the only possible alternative to the LASL-UTRC Mixed Flow reactor. Only LASL-UTRC has designed a Plasma Flow reactor as a terrestrial power plant.

In late January, Ira Helms, DOE study monitor, called for a formal review of the SSAI study. It was attended by representatives from the University of Florida, Georgia Tech, ORNL, LASL, UTRC, NASA, SSAI, and Black & Veatch. The latter is part of the SSAI study team.

SSAI's presentation made two major recommendations: (1) use a steam cycle for electrical generation, and (2) focus design study on a reactor that would use features of both the LASL-UTRC Mixed Flow Reactor and the University of Florida's Heterogeneous Gas Core Reactor. The recommendation to use a steam cycle was accepted by DoE, probably for two reasons. First, the entire tone of NASAP is directed toward minimum modification or development and, second, the choice of the heat transfer system is not too important as long as its function is reasonably accomplished. We believe that the closed-cycle helium gas turbine would also be a good choice. In the time scale needed for the development of the GUFR, there is ample time to complete the development of the gas turbine generator which has already started under the high-temperature gas-cooled reactor (HTGR) program. It makes sense to use a gas turbine because steam cycles cannot handle the high temperature GUFR is capable of delivering.

The second SSAI recommendation was not accepted. According to DoE, the NASAP schedule has been shortened and they needed certain information by March 1.

There simply was not enough time to design a different reactor. Therefore, DoE directed SSAI to assemble the information needed by NASAP using the LASL-UTRC Mixed Flow Reactor design.

At the insistence of the DoD/ISA representative, an additional requirement was made part of subsequent studies. This was to include denatured cores in all future calculations. The denatured core was to consist of 13% ^{233}U and 87% ^{238}U , which would subvert diversion by rendering the fuel essentially useless as weapons material. This requirement holds even though the fissile material is transported to the GUFR only once for initial start-up. Denaturing also renders the fuel useless as weapons material for the user country in the event it chooses to abrogate its agreements and disassemble the power plant to get the material.

To meet the NASAP March 1 due date, SSAI asked us to generate the information that depended on reactor performance with denatured fuel. We made a series of ONETRAN calculations with the denaturing ratio as a parameter. This was for a simple cavity one meter in diameter. The critical mass increases rather slowly from 13 kg of pure ^{233}U to 25 kg ^{233}U at a 19:1 denaturing atom ratio. At this point the curve rises rapidly so that beyond an atom ratio of 23:1 the reactor could not be made critical. These masses and ratios were then used to scale up the single critical mass TWOTRAN calculation made on a 7-cavity reactor having 1.4-m-diam by 6.0-m-long cavities.

From this basic information, we could generate numbers to fill in the NASAP forms. Since the forms were designed to describe solid fuel reactors, many of the questions didn't apply to the GUFR. Some of the data we provided were average discharge exposure, internal and external fissile inventory, specific power, power density, initial core mass for ^{235}U , and lifetime requirements to ThO_2 and U_3O_8 .

B. Proposal to DoE for Gaseous Uranium Fueled Reactor Technology Studies (L. L. Lowry, Q-14)

The proposal defines a 15-year program for the development of both a Mixed Flow Reactor and a Plasma Reactor. The program is divided into four major objectives which are listed with the tasks involved in Table VII. This proposal was submitted to DOE on March 21.

TABLE VII
PROGRAM SCHEDULE FOR GASEOUS-FUEL REACTOR DEVELOPMENT

<u>TASK</u>	<u>Fiscal Years</u>
<u>Basic Data--Mixed Flow Reactor</u>	
1. Nuclear and system design.	79 -- 93
2. UF ₆ containment chemistry.	79 -- 81
3. Fluid dynamics of convective transport.	79 -- 81
4. Radiation transport.	79 -- 81
5. Fission product fluorides.	79 -- 81
6. Mixed flow assembly.	79 -- 81
<u>Basic Data--Plasma Reactor</u>	
1. Nuclear and system design.	79 -- 93
4. Radiation Transport	79 -- 81
7. Uranium containment chemistry.	79 -- 81
8. Fluid dynamics of confinement	79 -- 81
9. Buffer confined assembly.	79 -- 81
<u>Laboratory Process Loops</u>	
10. Mixed flow fission product extraction loop.	81 -- 83
11. Mixed flow fission product plateout loop.	81 -- 83
12. ²³³ U extraction & fluorination loop.	81 -- 83
13. Mixed flow reactor loop with heat sink.	81 -- 83
14. Plasma extraction & uranium injection loop.	82 -- 84
15. Plasma core reactor loop with heat sink.	81 -- 83
16. Liquid uranium fission product extraction loop.	81 -- 83
<u>Engineering Prototype</u>	
17. Development of safety analysis reports.	83 -- 85
18. Mixed flow reactor system with all process loops operating. Power dumped to heat sink.	84 -- 88
19. Same kind of system for plasma reactor.	84 -- 88
<u>Demonstration Plant</u>	
20. Characteristics of this plant will be defined later.	89 -- 93

VI. PLASMA CORE REACTOR PROGRAM

A. Restacking of PCA Beryllium Reflector (D. M. Barton, G. A. Jarvis (consultant), members of Groups Q-14 and Q-8)

Most of January and the first week in February were devoted to obtaining a detailed inventory of our beryllium (Be) supply and in preparing Kiva I for the restacking operation prior to the next series of experiments in which we will attempt to demonstrate vortex confinement of UF_6 gaseous fuel in the core. Each piece was weighed, measured, and marked with its induced gamma radiation reading. If we noticed BeO on any of the surfaces, the affected pieces were further cleaned in the large Pajarito Site ultrasonic bath.

There are several basic Be shapes on hand left over from the Nevada tests and Kiva mockups of Rover reactors. Some pieces recorded high radiation dose rates, while others, that had obviously never left Los Alamos, showed no induced radiation. We tried to use as much of the Be that showed little or no radiation to build up the 50-cm-thick reflector with as high an effective density as possible. We also had to provide access for the new 132-cm-long ^{10}B control vanes. The diagram of Fig. 34 illustrates how these pieces were used in the outside reflector layer to replace all but two of the rotary drum segments. We kept the same inner reflector graphite and Be parts except that we interchanged the 43.2-cm-long Phoebus boron segments with the 132-cm-long NRX segments; the latter are now on top to provide channels for the new control system. Fig. 35 shows a vertical cross section of the new stacking.

The dramatic reduction in radiation levels around the PCA is indicated in Fig. 36, which compares the previous gamma survey with the new readings at the same locations in parentheses. A factor of $>10^3$ improvement is seen at the mid-plane surface of the reflector. In the vicinity of the gas-handling system, the dose level is now about 1/20th of its previous reading.

During the restacking operation, the personnel radiation exposure was distributed by using 17 volunteers from Groups Q-14 and Q-8. Sixteen 3-to 5-man crews, each working 4-hour shifts were required to take down the old stacking and replace it with the new arrangement. The total integrated personnel

Fig. 34. Schematic of Be pieces used in outer layer of new PCA reflector.

Fig. 35. New reflector stacking in PCA.

Fig. 38. Schematic of Be and fuel rod positions on PCA central hydraulic ram.

Fig. 39. Fuel rod layout in PCA central section.

Fig. 40. Calibration curve for the two ganged PCA rotating control drums.

In order to evaluate options for placement of the proposed linear control vanes, we found the worth of ^{10}B wires and plates in various locations in the reflector. The effect on reactivity of a 0.51-mm-diam (20 mil) ^{10}B wire is shown in the radial transverse of Fig. 41 through the two layers of NRX Be. It appears that 1.5 \$ to 2.0 \$ per control vane can easily be achieved in this region.

These measurements on the ^{10}B vane worth and the control drum calibration will have to be redone when we place the vortex canister in the cavity and rearrange the fuel rods to compensate for the reactivity effect of its 56 kg of 6061 Al. Also there will be a new opening in the bottom plug for routing pipes to the gas system.

Fig. 41. Reactivity poisoning of ^{10}B in PCA reflector.

C. PCA Control System for the Vortex Experiments (D. M. Barton, W. Bernard, Q-14)

We met with the Group Q-14 Safety Committee to discuss problems associated with the vortex experiments and the design of an appropriate control system. We decided to use four ^{10}B safety vanes, each worth from 1.5 \$ to 2.0 \$ operated in a vertical "in-out" mode with a 122-cm stroke, in combination with four similar control vanes which will have continuous position indication. The drive mechanisms for these vanes have been designed and the required hardware ordered. The preliminary choice for the vane locations, shown in Fig. 42, now appears to be satisfactory. The system will be set up with conventional critical assembly reset interlocks. These circuits and the control room operating panels have yet to be designed and fabricated.

D. Status of the Vortex Flow System (LASL-UTRC)

A schematic of the gas handling system furnished by UTRC is shown in Fig. 43. The equipment operates in the blowdown mode. It will be shipped without the liquid UF_6 pump, relief valve, and heater. In the blowdown mode, a weighed mass of liquid UF_6 will be placed in the supply cylinder of the oven, heated to gas phase, mixed at a controlled rate into the helium stream, and injected into the canister through the argon buffer gas. The mixture of He, Ar, and UF_6 is condensed on cold plates in the UF_6 separator, allowing the He and Ar to flow to the Ar condenser through a NaF UF_6 trap. The Ar is condensed and returned to the buffer flow while the He is vented.

UTRC has now operated the Ar flow loop at 20 g/s continuously for up to one hour with the Ar vortex running. Most of the difficult problems associated with this part of the loop appear to be solved.

Emphasis on the UF_6 separator is now possible and parts are being assembled for preliminary tests. More time must also be spent on instrumentation and remote operation of flow valves.

Fig. 42. Proposed ^{10}B vertical control vanes in PCA.

Fig. 43. Schematic of UF_6 vortex flow system.

VII. CRITICALITY EXPERIMENTS

A. Critical Assembly Engineering -- SKUA (T. F. Wimett, W. Bernard, E. O. Ferdinand, L. C. Osborn, R. G. Wagner, Q-14)

With the completion of mechanical construction and the wiring for control circuits, we have moved the SKUA pulsed critical assembly into the south end of Kiva III. All of the control room panels with their required electrical connections to the Kiva circuits are installed and our initial assembly sequencing tests were performed without difficulty. Of course, there is no active fuel in the assembly during these preliminary tests.

The initial approach to critical will proceed as soon as the Experimental Plan is approved by the Albuquerque Area Office (ALO). We expect to receive this approval early in April. Since the central flux trap acts as such a large reactive poison, it is a significant criticality component and must be in position before the uranium-alloy fuel rings can be added. Although there have been delays in fabricating parts for this flux trap, all the essential pieces are complete and we will be able to proceed with the critical experiment by temporarily substituting normal graphite for the pyrolytic graphite liner. We will also use polyethylene in place of the zirconium hydride pieces which have not yet been delivered.

A top view of the SKUA assembly is seen in the photograph of Fig. 44 and in the schematic diagram of Fig. 45. One of the three copper safety blocks is withdrawn in the foreground. Spaced between these safety blocks are the rotary control sectors, shown in the photograph almost fully turned to their least reactive position. When the control and safety sectors are inserted they complete the copper reflector which surrounds the core fuel.

The flux trap assembly is shown in Fig. 46. The components are partially removed from the outer aluminum can to reveal, from the center outward, the inner liner, polyethylene, cadmium, and boral sleeves. Not shown is the outer uranium carbide sleeve. When assembled, the 2.54-cm-thick copper cover is positioned with screws and the complete unit can be moved remotely at the center of the core.

Fig. 44 Top view of SKUA critical assembly without fuel rings or flux trap.

Fig. 45 Schematic of SKUA Safety and Control System.

B. Critical Assembly Operations (T. F. Wimett, R. G. Wagner)

1. Godiva-IV Irradiations. A total of 41 burst irradiations were conducted since the last report. Of these, 23 were for experiments with Group Q-8 for the nuclear pumping of lasers. One irradiation was for D. E. Hankins of the Hazards Control Group at LLL in which thermoluminescent gamma dosimeters were exposed on a phantom to

Fig. 46. Flux trap components for initial critical experiments.

correlate with sulfur and hair activations that he had made previously at LASL. The other 17 were made as part of a feasibility study to demonstrate our group's proposal of a new technique for measuring prompt neutron multiplication.

Because all of these bursts were relatively high in yield, of the order of 4 to 5×10^{16} fissions each, it will be necessary to shut down for about two weeks to allow for the decay of the fuel radiation level. Then we intend to disassemble the core and inspect the fuel rings for any possible damage.

2. Central Reactivity Coefficient Measurement in Big Ten [G. E. Hansen, J. Koelling, (WX-8)]
We have gathered together all of our central reactivity coefficients measured since 1975. Big Ten is an all-uranium cylindrical critical assembly composed of a 10%-enriched uranium core and a 15-cm-thick depleted-uranium reflector. Reactivity changes due to material additions to a central cavity in Big Ten were measured by means of a calibrated reflector control rod, and the results, corrected to zero cavity size and expressed in units of $\$$ per gram atom are listed in Table VIII. To convert this listing to "change in k_{eff} per g-at.," one may multiply by the void-coefficient-based effective delayed neutron fraction, $\beta_{\text{eff}}(\text{Big Ten}) = 0.00738 \pm 0.00009$.

TABLE VIII
CENTRAL REACTIVITY COEFFICIENTS IN BIG TEN

Material	Coefficient ($\$/\text{g-at.}$)
^1H	$-1.3 \pm 0.3 \times 10^{-2}$
^6Li	-2.4 ± 0.1
^7Li	-0.21 ± 0.03
Be	-0.24 ± 0.02
^{10}B	-10.4 ± 0.1
^{11}B	-0.28 ± 0.05
C	-0.21 ± 0.02
O	-0.14 ± 0.03
Al	-0.19 ± 0.02
In	-2.74 ± 0.04
Au	-2.42 ± 0.03
^{232}Th	-2.1 ± 0.1
^{235}U	20.0 ± 0.2
^{238}U	-1.01 ± 0.01
^{237}Np	2.2 ± 0.2
^{238}Pu	22.6 ± 0.6
^{239}Pu	34.2 ± 0.3

3. Reactivity Coefficients of Heavy Isotopes in Big Ten and Flatton (G. E. Hansen, C. C. Byers, E. A. Plassmann, D. R. Smith, Q-14, J. Koelling, WX-8)
We are attempting a systematic investigation of heavy isotope behavior in the Pajarito fast-neutron critical assemblies. Included will be central reactivity coefficient measurements and fission rate measurements in the natural-uranium-reflected plutonium and 93%-enriched uranium, U (93), configurations of Flatton and in Big Ten, a depleted-uranium-reflected, 10%-enriched uranium metal assembly.

A few reactivity coefficients have recently been measured for ^{235}U , ^{237}Np , ^{238}Pu , ^{239}Pu , ^{242}Pu , and ^{241}Am . The experimental technique has been described by Stubbins, Barton, and Lonadier,¹² and preliminary results are listed in Table IX.

Similar data for some of these isotopes in unreflected plutonium (Jezebel) and U(93) (Godiva) have previously been reported.^{12,13,14}

Central reactivity coefficients are closely related to neutron net-production cross sections of the samples, which in turn are related to critical masses. By establishing the bias in the computed coefficients, one may obtain improved estimations

TABLE IX
CENTRAL REACTIVITY COEFFICIENT RATIOS IN
FLATTOP-PU, FLATTOP-0193, AND BIG TEN

Isotope	Flat-top-Pu	Flat-top-0193	Big Ten
^{235}U	0.524 ± 0.007	0.528 ± 0.010	0.585 ± 0.006
^{237}Np	-	0.42 ± 0.02	0.065 ± 0.006
^{238}Pu	0.899 ± 0.014	0.889 ± 0.014	0.661 ± 0.016
^{239}Pu	1.00	1.00	1.00
^{242}Pu	0.494 ± 0.010	0.479 ± 0.010	-
^{241}Am	0.695 ± 0.011	0.530 ± 0.010	-

of critical dimensions. Six-group cross-section sets, developed for the above isotopes, have been used in this fashion to give the following bare-sphere critical mass estimates:

$$\begin{aligned} m_c(^{238}\text{Pu}) &= 12 \text{ kg} \\ m_c(^{242}\text{Pu}) &= 90 \text{ kg} \\ m_c(^{241}\text{Am}) &= 58 \text{ kg} \end{aligned}$$

It is hoped that these data will be useful to the standards work group which is currently developing a criticality safety standard for operations with the actinide elements.

4. Evaluation of a Proposal to Use the Godiva Burst Reactor in PhERMEX Operations (R. E. Malenfant, G. E. Hansen, W. L. Talbert, T. F. Wimet, R. G. Wagner, Q-14). The proposal is to determine subcritical multiplication of an imploding assembly of fissionable material by interrogating it with a burst of neutrons from a Godiva burst reactor and measuring the resulting fission neutrons. A typical experimental configuration is drawn schematically in Fig. 47. Complications include: attenuation and scattering of neutrons from structural material; neutron moderation, attenuation, and scattering from any low-Z material that may be present; and other sources of signal such as photoneutron production or photofission when the technique is employed at PHERMEX, a flash x-ray machine used to take pictures of imploding systems.

A series of exploratory measurements are being made in Kiva III to evaluate these complications. The geometry for the present series of measurements is indicated in Fig. 48. With Godiva IV placed 1.92 m from and lined up with a 7.5-cm-diam hole through the inner west concrete wall of Kiva III,

Fig. 47. Schematic experimental configuration for Godiva burst assembly at PHERMEX.

Fig. 48. Experimental setup for multiplication measurements in Kiva III.

burst neutrons can be collimated into the low background room behind the wall. Samples, including subcritical Pu spheres and inert scattering material, are positioned 0.7 m from the inner east concrete-block wall of the room in line with the collimating hole. Neutron detectors placed 1 m below the sample

measure neutrons from fissions in the sample while another detector positioned over the top of the collimator hole monitors the incident flux. We used Reuter-Stokes ^{235}U -lined-proportional counters, type RSN-34A-M1, for these measurements.

The following exposures have been made:

1. Background evaluation with no fissile material present;
2. Evaluation of background with cables to the neutron detectors disconnected from the chambers but routed as in the active experiment using a neutron monitor in collimator hole for the purpose of evaluating the signal (if any) from direct interaction of radiation with the cables;
3. Passive scatter from a 5.08-cm right circular cylinder of brass positioned in the sample location to evaluate the signal from a pure scatterer;
4. Background with a 5.08-cm-thick steel plate covering the collimator hole to evaluate detector response to scattered neutrons from a steel containment vessel which will be required in the experiments at PHERMEX;
5. 6-kg Pu sphere at sample location;
6. 9.7-kg Pu sphere at sample location;
7. 6-kg sphere and 5.08-cm steel plate;
8. 9.7-kg sphere and 5.08-cm steel plate; and
9. Brass cylinder and 5.08-cm steel plate.

Some typical scope traces are shown in Figures 49a-49c. The response of a ^{235}U fission chamber placed 1 m below the sample to a Godiva exposure is shown in Fig. 49a. Both traces show the same signal with the scope adjusted to provide the different sensitivities. Figure 49b shows the response of a similar chamber directly in the beam but shielded by the steel plate. This monitor was used to verify the transmissions of neutrons through the collimator. By comparing the Fig. 49a signals from exposures to different size spheres, the difference in scattering and multiplication for various samples can be readily determined. Figure 49c shows the shape and relative magnitude to the Godiva pulse as measured by a fast-response photodiode. As indicated, horizontal sweep speed in all cases was 50 $\mu\text{s}/\text{cm}$.

49a ^{235}U Chamber
0.05 & 0.005 V/cm
50 $\mu\text{s}/\text{cm}$

49b ^{235}U Chamber in Beam
1 & 0.1 V/cm
50 $\mu\text{s}/\text{cm}$

49c Godiva Pulse, Photodiode

Fig. 49. Detector response to neutrons from multiplication experiment with Godiva IV.

We made a series of calculations to determine the sensitivity of this technique. The geometry is indicated in Fig. 50. A monodirectional 7.6-cm-diam disc-shaped neutron source with an energy spectrum characteristic of the Godiva IV leakage spectrum ($\bar{E} \approx 1.7$ MeV) was employed in the MCNP Monte Carlo program. Point detector response and the surface multiplication of the sample were calculated. Results for several configurations are summarized in Table X. Radius and density of ^{238}U scatterers were varied to isolate the effects of the total number of scattering centers in the beam, and the relative sizes of the scattering sphere and the collimated beam. An understanding of the latter phenomenon is essential to the analysis of detector response expected at PHERMEX.

The equivalent leakage source of neutrons calculated for a typical Godiva IV burst is indicated in Fig. 51. It is important to note that the width of the pulse will be large compared to the time during which photoneutrons will be produced by PHERMEX and the lifetime of fission neutrons in the system. This will allow one to measure the multiplication as a function of time all during the implosion of the sample, provided of course that the complications mentioned above can be discounted or shown to be small. Our conclusion, from an evaluation of the experiments and calculations performed so far, is that the technique still looks promising and should be investigated further.

Fig. 50. Geometry for Monte Carlo calculations of Godiva/PERMEX experiments.

Fig. 51. Computed Godiva equivalent leakage source. (The notation 2.55/15 means 2.55×10^{15} , etc.)

TABLE X

CALCULATED RESULTS USING A CALCULATED GODIVA LEAKAGE SPECTRUM

Case	Radius (cm)	Density (g/cm ³)	Multiplication	Response n/cm^2-N_s
Pu Sphere	5.3028	15.53	5.97	1.68E-5
Pu Sphere	4.53319	15.53	3.64	8.53E-6
Fe Sphere	4.53319	8	1.00	1.72E-6
238 Sphere	4.53319	18.74	1.06	1.77E-6
238 Sphere	5.3028	18.74	1.08	2.74E-6
238 Sphere	3.0	18.74	1.06	1.25E-6
238 Sphere	3.0	38.39	1.08	1.57E-6
238 Sphere	2.0	18.74	1.05	6.83E-7
238 Sphere	2.0	129.56	1.05	1.07E-6
Pu Sphere In Steel Shell	5.3028	15.53	5.65	8.16E-6
Pu Sphere In Steel Shell	4.53319	15.53	3.94	6.20E-6
Fe Sphere In Steel Shell	4.53319	15.53	1.00	3.84E-6

VIII. NUCLEAR CRITICALITY SAFETY

(D. R. Smith, C. Byers, J. McLaughlin, Q-14)

A. General

We have completed the annual criticality safety reviews for both the Metal Production and the Research and Development Sections of Group CMB-11.

The several recommendations made by ALO following their inspection of Groups CMB-6, -8 and -11, were discussed with these groups and a memorandum of response was prepared which resolves the questions concerning the establishment of criticality limits and training documentation. To make sure that all the involved LASL groups understand the requirements of DOE Manual Chapter 0530, another memorandum was distributed emphasizing the guidelines for group responsibilities in maintaining auditable records for employee certification and retraining. During this quarter, we presented criticality safety lectures to Groups WX-3, Q-1, Q-2, Q-14, CMB-5, CMB-6, and the Recovery Section of CMB-11 to meet, in part, these retraining requirements.

Another session of our Nuclear Criticality Safety Training Course was held here at Pajarito Site on March 14 and 15. This time, sixteen members of the Laboratory staff and one employee of United Nuclear Corporation from Richland, WA, attended.

The nuclear safety of handling a hexagonal array of 434 enriched- UO_2 LMFBR fuel pins being prepared by Group P-1 for an experiment at the PHERMEX facility was investigated. We concluded that the array, with a center-to-center spacing of 0.75 cm would, if immersed in water, be significantly undermoderated and subcritical by a large margin, and that care should be taken to insure that spacing integrity is maintained.

At the request of Group CMB-6, we made TWO-TRAN criticality calculations for both expected and abnormal conditions during the hydrostatic pressing of a graphite annulus loaded with a nominal 2.0-kg charge of U(93). Our results indicate that even for a double U(93) charge, the system is far subcritical during all credible environmental circumstances.

The results of ONETRAN sensitivity calculations showing the influence of ^{238}Pu fission cross-section uncertainties on the computed critical

masses of the metal and oxide are presented in Table XI. As our base, we used the 16-group Hansen-Roach cross-section set derived from weapon test results, and in this study only the fission cross sections in the upper six groups (above 17 keV) were varied. Obviously, any measurements that can reduce these uncertainties should be considered.

TABLE XI
ONETRAN COMPUTED BARE, SPHERICAL CRITICAL MASS
SENSITIVITY TO FISSION CROSS SECTION

	^{238}Pu (kg)	$^{238}\text{PuO}_2$ (kg)
$(1.15)\sigma_f$	7.8	36
$(1.0)\sigma_f$	10.8	49
$(0.85)\sigma_f$	15.4	68

B. Criticality Considerations for the TA-55 Plutonium Facility

In connection with the move of Group CMB-11 from TA-21 to TA-55, we participated in the development of procedures for the temporary storage of material removed from the process lines while awaiting transfer to the new facility.

The limits and procedures for the use of the conveyor system at TA-55 have been established. We also recommended limits on the loadings of Pu metal in the Dymac random driver which will be used by Group Q-1 to assay the casting residues and metal rings from the electrorefining process. The LASL Nuclear Criticality Safety Committee asked us to also review the procedures for carbide fuel fabrication proposed by the Research and Development Section and the operations of the Metal Fabrication Section of Group CMB-11. Finally, the safety considerations applicable to TA-55 were summarized for a DOE review meeting.

Much work is still going on at TA-21 while this orderly move to the new facility continues. We gave guidance in modifying the arrangement for storage of oxides in Vault 81 and rooms 11 and 200 while maintaining the same level of criticality safety. Also, limits were established for the glove-box use of a SWECO separator used in the preparation of PuO_2 . Other criticality considerations involved the Pu charge limit for the "clamshell" ash furnaces, the use of Pu annealing furnaces, the volume

reduction of nitric acids containing small amounts of Pu, and volume limits for slurries used in the plutonium oxalate calcining and hydrocalcining processes.

C. Weapons Program Support

Criticality safety evaluations and memoranda were prepared for the Topmast and Transom devices. Also an evaluation was made for the W-76/MK4 JTU-12 configuration which is to be the subject of experiments conducted at Sandia Laboratories, Albuquerque.

A series of criticality calculations for LASL pits (currently in stockpile) in transportation containers is largely completed and will be documented during the coming quarter.

IX. COCKROFT-WALTON ACCELERATOR

(A. E. Evans, A. R. Brown, Q-14)

In February, Group Q-14 assumed responsibility for operation of the Pajarito Site Cockroft-Walton accelerator. This machine can deliver up to 700 μ A of 350-keV protons or deuterons onto an air- or water-cooled target. With targets of tritium adsorbed on titanium or zirconium, we can produce $\sim 10^{11}$ neutrons/s with an energy of 14.5 MeV from $T(d,n)^4\text{He}$ reaction. By using the $D(d,n)^3\text{He}$ reaction, about 10^9 3-MeV neutrons/s can be produced. One beam line is equipped with a particle monitor

which can measure the neutron source strength to within $\pm 3\%$ of its absolute value. The accelerator has a variety of beam-pulsing systems which are capable of delivering ion pulse-lengths from a few nanoseconds up to hundreds of milliseconds.

During this quarter, the accelerator was operated for a total of 44 hours for the following experiments:

Group CNC-11, $^{87}\text{Rb}(n,2n)^{86}\text{Rb}$	20 h
Group H-1, neutron detector calibration with $D(d,n)$ and $T(d,n)$ neutrons	7 h
Group CNC-11, Pu foil activation	2 h
Group CNC-11, $\text{Fe}(n,2n)$ and $\text{Fe}(n,p)$ reactions	15 h

We are attempting to improve the utility, reliability, and efficiency of the facility. As a first step, the cold traps on the vacuum lines are being connected through an automatic feed system to a 1000-gallon liquid-nitrogen (LN) trailer to simplify our maintenance of the LN system. Equipment has been installed to provide better information on the neutron source flux. We are also working on ways to improve the vacuum system, provide for absolute voltage calibration and voltage stabilization, monitor the high-voltage-column stack current and stray beam currents, improve beam-position stability, provide for better target cooling, and improve the accuracy of absolute neutron flux measurements.

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