MND-P-2309
PHYSICS AND MATHEMATICS

SNAP I MERCURY BOILER DEVELOPMENT, JANUARY 1957 TO JUNE 1959

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June 1960

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Prepared by:

John Jicha
James J. Keenan

Published,
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A handwritten signature in cursive script, reading "Hugh L. St. Harvey". The signature is written in dark ink and is positioned above a horizontal line.

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FOREWORD

This report was prepared by the Nuclear Division of The Martin Company under Contract AT(30-3)-217 for the United States Atomic Energy Commission. It describes the mercury boiler development program conducted under the SNAP-I Project from January 1957 until April 1959. The chronological design concepts are reviewed from inception through transition to the SNAP-IA Program.

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SUMMARY

The mercury boiler development program was undertaken to develop a system that would utilize the heat of radioisotope decay to boil and superheat mercury vapor for use with a small turbine-generator package. Through the use of a Rankine cycle, the mercury vapor could be provided continuously to power a turbine-driven alternator and produce electricity for extended periods of time. This mercury boiler and the related power conversion system was planned for a satellite that would orbit the earth. This system design and development program was designated as SNAP-I.

Historical development of the mercury boiler is presented in Chapter I.

A chronological description of the various mercury boiler concepts is presented in Chapter II. It includes the individual mercury boiler concepts from the original conceptual design up to the final isotope boiler design. The period covered was from January 1957 until April 1959. This program was subsequently phased out in favor of the SNAP-IA Program (thermoelectric direct conversion).

The applicable results of an extensive mercury literature survey are given in Chapter III.

The mercury boiler experimental test program, described in Chapter IV, provides complete coverage of each experimental boiler and its relation to the system design of that period. All experimental mercury boilers were electrically heated, simulating the isotope fuel assembly. Isotopic heat, however, was never mated to the mercury boiler system since the program was phased out before this became a reality. By use of electrical heaters, superheated mercury vapor was produced at 1300° F and 200 psia for the design flow conditions of the full-power boiler (1.87 lb/min and the half-power boiler (0.935 lb/min).

A summary of all mercury boilers and their final disposition is given in Chapter V.

I. INTRODUCTION

An isotope survey was conducted by The Martin Company in 1956 to determine the feasibility of deriving useful energy from fission products and artificially produced isotopes. From this survey, it became apparent that certain isotopes had sufficient thermal output to power long life energy conversion devices. A conceptual design of a radioisotope-fueled auxiliary power unit (APU) was proposed for operation with a mercury vapor Rankine cycle. By proper control of the isotopic heat, mercury could be evaporated and superheated to drive a small turbine-generator set. The mercury vapor from the turbine exhaust would be condensed and pumped back into the boiler for additional cycles. With this continual recycling, the APU could operate as long as heat was delivered in the boiler.

This topical report will be limited to the design and development of the mercury boiler. Additional topical reports will be prepared on the other components of the SNAP-I system.

The chronological progress from January 1957 to April 1959 will be reviewed to point out the stepwise progression from the conceptual design to the final isotope-fueled mercury boiler. Experimental mercury boilers and test facilities will be also described to indicate the procedures used and the results obtained.

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II. HISTORICAL DEVELOPMENT

During the mercury boiler development period of January 1957 through April 1959, many changes and revisions were made to the overall conceptual designs and functional prototypes. To describe the development steps taken during this period, a chronological review of the mercury boiler development is presented in this chapter. Table 1 summarizes the mercury boilers investigated and the period over which the investigations took place.

TABLE 1
Mercury Boilers and the Period of Active Use

APU Power (watts)	Boiler Power	Isotope Fuel	Fuel Shapes	Heat Transfer Mediums		Shielding	Time Period
				Inner	Outer		
133	1 full power	Po-210	Steel capsules	Na	Na	None	Jan 1957
133	1 full power	Ce-144	Metal capsules	Na	Na	None	Jan 1957
500	1 full power	Ce-144	Metal capsules	Na	Na	None	Feb to Mar 1957
500	1 full power	Ce-144	Metal capsules	Na	Pb	None	Ap to Aug 1957
500	1 full power	Ce-144	Fuel block	Mo	Pb	None	Sep 1957
500	1 full power	Ce-144	Fuel block	Mo	Pb	Hg	Sep 1957
500	2 half power	Ce-144	Fuel block	Mo	Pb	Hg	Oct 1957 to Apr 1959

The internal construction and final disposition of each of the mercury boilers listed in Table 1 will be described briefly.

A. CONCEPTUAL DESIGN

In January 1957, the initial concept of a radioisotope-fueled auxiliary power unit in an outer space environment consisted of an isotope-powered mercury boiler producing superheated mercury vapor to power a turbo-electric generator. Electrical power would be delivered from the generator to storage batteries to replenish the power needs of an earth-orbiting satellite. The power demand was established at an average of 3.2 kilowatt-hours per day. On this basis, the initial APU size was fixed at 133 electrical watts of continuous power.

An extensive survey of the isotopes was made to select one or more fuels that would be applicable for powering the high temperature mercury boiler. The criteria used for selecting the isotope were:

- (1) High specific power.
- (2) Availability.
- (3) Cost.
- (4) Status of existing technology.
- (5) Facility requirements.

Two isotopes, Po-210 (reactor-produced) and Ce-144 (fission product), were selected as the most suitable for an isotope mercury boiler. Polonium-210 could be produced in quantities to meet immediate needs without requiring additional facilities. Since Po-210 had previously been supplied in a capsule form that could be used, it was selected as the fuel for the initial mercury boiler design. Cerium-144 could be supplied in greater quantities and more cheaply than Po-210, but it would have been necessary to build a separation plant and establish its technology.

The Polonium-210 was to be produced by irradiating bismuth in a high neutron flux reactor at the Hanford production facilities. A six-month lead time was necessary for the irradiation process, and an additional month was required to separate and encapsulate the isotope at the Mound National Laboratory. The isotope when encapsulated in steel would be 0.7 inch in length and have a 0.3-inch diameter. Each capsule would contain approximately 300 curies of polonium and give off 10 watts of heat at the beginning of the operating life of the APU. For 133 electrical watts, 230 capsules would be required to provide the thermal energy for the 60 days of continuous APU operation, considering isotope decay and boiler thermal losses.

Figure 1 shows the initial concept utilizing Polonium-210 capsules immersed in liquid sodium and contained by stainless steel canisters. Liquid sodium was also to be used as the medium to transfer heat from the inner container to the mercury boiler coil wrapped within the annular space between the inner and outer cylindrical containers. The liquid mercury entered the boiler coil at the bottom and was preheated to the boiling point shortly after entry. Boiling of the mercury would take place thereafter in the next three or four coils. The last several turns would superheat the mercury vapor to a temperature of 1300° F, at a pressure of 210 psia.

This mercury boiler design was studied during January 1957.

B. CERIUM-144 FUELED BOILER NO. 1

Looking into the long range economics of isotope boilers, it was obvious that Polonium-210 would be too expensive if many high capacity mercury boilers were required. As a result, a second conceptual design was initiated with Cerium-144 as the fuel. The mercury boiler was to be capable of powering an APU requiring 133 electrical watts. This design was current during January 1957, along with the Polonium-210 conceptual design.

C. CERIUM-144 FUELED BOILER NO. 2

During the month of February 1957, the power requirements for the APU were doubled to 260 watts, and the number of required units increased. To provide a factor of safety, it was requested that the APU be designed to produce 500 continuous electrical watts for 60 days. To meet the new requirement, Cerium-144 was recommended as the fuel. The decision to follow this course was based on the following factors:

- (1) Availability--Initial requirements could be met utilizing existing facilities at Oak Ridge National Laboratory (ORNL). The construction of a separation plant at Hanford would enable operational fuel requirements to be met.
- (2) Cost--Cerium-144 is much more economical than Polonium-210. For an APU of 500 electrical watts, the comparative isotope fuel costs would be:

<u>Po-210</u>	<u>Ce-144</u>
\$1,395,000	Minimum--\$4,500--not encapsulated
	Maximum--\$58,100--not encapsulated

Fig. 1. SNAP Model

- (3) Hazards--In the oxide form (CeO_2), the cerium fuel has a melting point of 2600°C . Through the use of metallic bonding or sintering, the fuel could be made highly shock resistant. This would make it easier to confine hazards to a more local area. Polonium, on the other hand, would be in the liquid state with a high percentage existing as vapor.
- (4) Radiation--Cerium would present a more difficult operational problem than Po-210. The other advantages, however, outweigh this disadvantage.

To assist in making the decisions as to fuel, power level and schedule, a preliminary estimate of fuel availability, requirements and comparative costs were made at power levels of 133, 300, 500 and 1000 net electrical watts. Tables 2 and 3 contrast the results of Polonium-210 versus Cerium-144 fuel.

TABLE 2
Availability and Cost of Po-210

APU power--watts (e)	133	300	500	1,000
Output of boiler at end of life--watts (t) (based on 10% electrical conversion)	1,330	3,000	5,000	10,000
At shipping time--watts (t)	1,823	4,113	6,855	13,710
At startup--watts (t) (based on 60-day APU life)	1,796	4,052	6,753	13,506
Capsules per unit--number	230	488	913	2,505
Fuel capsule cost per APU	\$350,736	\$745,176	\$1,395,151	\$3,825,545

TABLE 3
Availability and Cost of Ce-144

APU power--watts (e)	133	300	500	1,000
Output of boiler at end of life--watts (t) (based on 10% electrical conversion)	1,330	3,000	5,000	10,000
At separation time-- watts (t)	1,729	3,900	6,500	13,000
At startup--watts (t) (based on 60-day APU life)	1,534	3,461	5,769	11,538
Cost--not encapsulated	\$ 1,200	\$ 2,700	\$ 4,500	\$ 8,960
Cost--exclusive of encapsulation				
Minimum	\$ 1,191	\$ 2,687	\$ 4,478	\$ 8,957
Maximum	\$15,400	\$34,800	\$58,100	\$116,000

From February 1957 until April 1959, the isotope fuel considered for the SNAP-I mercury boiler was Cerium-144. The APU electrical power output also remained constant at 500-electrical watts over this period.

A re-evaluation of the conceptual design of the mercury boiler was undertaken and completed during the second quarter of 1957. Figure 2 is the mercury boiler assembly for a thermal power design of six kilowatts. Liquid mercury enters at the bottom and superheated mercury vapor leaves at the top. The inner housing assembly (Fig. 3) contains the ceric oxide fuel pellets in liquid sodium. Initially, sodium was also considered for use between the fuel housing and the mercury boiler coil as a heat transfer material, but objections to the use of liquid sodium at the launch pad required a new thermal bond material which would not be hazardous to handle. Liquid lead was selected as the replacement material and is called out in Fig. 2.

Ceric oxide fuel would be fabricated in capsules 1/2 inch in diameter by 3/4 inch in length. The CeO_2 fuel was assumed to be 86% of theoretical density and the clad material would be 1/16-inch thick. Figure 3 illustrates the general arrangement of fuel pellets. Subassembly of

the sodium-filled fuel housing was planned as a hot cell operation at Oak Ridge, Tennessee. Transfer of the fuel assembly to the boiler housing was scheduled for on-site loading at the launch pad. No shielding was planned for this operation and all manipulations were to be performed remotely. Figure 4 illustrates the on-site installation of the mercury boiler and turbine-alternator package into the aft section of the satellite vehicle. Figures 5, 6 and 7 illustrate the technique for transferring the isotope fuel assembly to the mercury boiler housing.

This mercury boiler concept was under consideration from April to August 1957.

D. CERIUM-144 FUELED BOILER NO. 3

After presentation of the remote loading concept at the second SNAP coordinating meeting, it became evident that having a highly radioactive substance in the missile during the last portion of the countdown would impose a severe handicap on the ground support personnel. It was indicated that the gantry crane in all likelihood could not be remotely removed from the missile pad and several minor adjustments might be required during a missile hold. In view of this problem, a new mercury boiler concept was devised to circumvent the problems at the launch site. The two changes made were:

- (1) The liquid sodium in the fuel assembly was replaced by solid molybdenum metal which could be drilled, loaded with cerium pellets and capped adequately to contain the fuel under all hazardous conditions.
- (2) The thermal insulation surrounding the mercury boiler was replaced by a multiple layer of highly reflective metal which could be loaded with liquid mercury and used as a biological shield until the last few minutes of countdown. At that point, the liquid mercury could be drained and the mercury boiler could be launched without shielding weight.

Figure 8 illustrates the general design of the mercury boiler and the multilayers of reflective insulation. One section of the boiler could be moved aside to expose the boiler casing for excess heat release purposes. Nitrogen gas was used to cool the mercury boiler prior to launch. If the mission had to be postponed, the biological mercury was pumped back into the multibaffled chambers and the boiler cooled down by recirculating the mercury. By this technique, minor maintenance could be performed without overexposure to radiation. The entire assembly, as shown in Fig. 8 would be loaded and shipped from ORNL as a complete

Fig. 2. Boiler Assembly

Assembly

Fig. 5. General Arrangement Remote Handling Device

Fig. 6. Detail A--Remote Handling Device

Fig. 7. Detail B--Remote Handling Device

Fig. 8. Boiler and Shield Assembly

unit. During shipment and handling, the shielding mercury would be circulated through the baffles and then through a separate biological mercury line to a radiator to maintain a reasonable internal temperature. The boiler supply and return lines would be connected to the power conversion system after the boiler assembly was loaded into the missile.

This concept was active during the month of September 1957.

E. CERIUM-144 FUELED BOILER NO. 4

In October 1957, the concept of a large single mercury boiler was revised by using two half-power mercury boilers. This was done for two reasons. First, the single unit would not fit into the satellite vehicle without significant modifications because of the size increase to accommodate the mercury biological shield. This also caused interference with the gimbaled satellite rocket engine. Second, the weight of a single boiler, unless located on the centerline of the vehicle, would upset the weight and balance of the satellite. While the off-center boiler weight could be offset by ballast, extra weight was considered undesirable. Consequently, two identical half-power mercury boilers were oriented diametrically opposite in the aft section of the satellite. Figure 9 illustrates the relative position of the APU components. The concept of two half-power boilers remained unchanged from October 1957 until April 1959, when the mercury boiler program was phased out. Figure 10 illustrates the design of one half-power mercury boiler. This design concept was used from October 1957 until the fall of 1958.

F. CERIUM-144 FUELED BOILER NO. 5

Three separate design changes occurred during the summer and fall of 1958 which made the half-power mercury boiler larger and heavier. The first change was to increase the volume of the fuel pellets since Oak Ridge National Laboratory could not produce the CeO_2 fuel in

the desired purity. Figure 11 illustrates the previous concept of a 3-1/2-inch OD molybdenum fuel assembly that was 6 inches long with 1/2-inch diameter CeO_2 fuel pellets. The revision required a 4-1/8-inch OD molybdenum fuel housing 6 inches long with 18 fuel holes similar to that shown in Fig. 11. To ensure adequate volume for the cerium fuel, the pellet diameter was increased from 0.5 to 0.64 inch.

The second change required an increase in the length of the mercury boiler to accept a 3-inch thick tungsten shield that was 4-1/8 inches in diameter. This shadow shield was positioned on top of the molybdenum

Fig. 9. SNAP-1, Space Mockup

Fig. 10. Schematic of Half-Power Mercury Boiler Assembly

Fig. 11. Fuel Element

fuel housing in both boilers to reduce the total radiation dosage to instruments located in the nose of the satellite. The exterior dimensions of the biological shield were unaffected since the increased boiler size was compensated by the excellent shielding characteristics of the tungsten block.

The third change occurred in the design of the thermal insulation and biological shield compartments. To meet the weight limits prescribed by the vehicle contractor for the booster during liquid oxygen fueling operations, the last isotope boiler design contained two separate compartments for biological mercury shielding. Maximum shielding was attained when both compartments were filled, but a partial shield could be maintained within the inner compartment to permit turbine-alternator startup as well as minor missile maintenance. Figure 12 illustrates the mercury boiler, the thermal insulation and the double compartment for biological mercury shielding. When the mercury boiler program was being phased out in March 1959, the isotope boiler fabrication had advanced to 50% of completion.

Fig. 12. Isotope Boiler No. 5 with Double Biological Shield and Thermal Shutter

III. MERCURY LITERATURE SURVEY

The inherent characteristics of liquid metals which allow operation with good heat transfer coefficients at high temperatures and low pressures have stimulated considerable experimental investigation. These obviously favorable characteristics, however, are combined with less obvious, unfavorable, operating characteristics which have limited investigations in range and scope to a few special conditions. Theoretical analysis has produced equations such as the Martinelli relationship and the more simplified relationships of Lyon, Seban and Shimazaki. Experimental results are not in agreement with theory and, in general, experimental results of different investigators do not agree. The difficulty in comparing the results of various investigators can be traced to the following factors:

- (1) Combined heat transfer coefficients for tubes and concentric annuli were obtained in some cases while other investigators obtained fully developed local or overall average coefficients.
- (2) Experiments were conducted on different bases, i.e., uniform heat input versus constant wall temperature systems.
- (3) Methods of measuring fluid temperatures differed. Some measured fluid centerline temperatures while others measured true bulk temperatures (mixing cup temperatures).
- (4) Other variations were caused by differences in velocity profiles, length-to-diameter ratios of test sections, and different liquid metal physical and thermal properties.

Thus, it can be stated that existing empirical correlations enable only a rough guess at the thermal design of liquid metal heat transfer systems while theoretical relationships are no better, if not worse. Therefore, it became apparent that mockup tests of the actual design were imperative. The need for these tests is further demonstrated by a cursory examination of the literature for the range of Peclet and Reynolds numbers investigated. In almost every case, the Peclet number is in the range of 10^2 to 10^4 or higher while the Reynolds numbers indicate turbulent flow ($Re > 2100$). The transition and laminar flow regions with Peclet numbers below 40 were avoided due to the inconsistencies experienced at these conditions.

The conditions of interest for the mercury boiler program were in the following range:

Flow rate	0.9 to 2.5 lb/min
Re	935 to 2600
Pe	10 to 35

It is evident that the range of interest in this program was entirely in the laminar and transition regions.

A second area of interest is that of forced convection boiling of mercury in the range of 200 psia with the flow rates quoted. Boiling heat transfer, in general, has received a great amount of attention over the last decade. A major portion of the work reported has been on pool boiling systems, while an equal volume of work has been carried out for nucleate boiling of fluids with high Prandtl numbers. Thus, a forced convection boiling program on liquid mercury would aid in closing the gap between the data previously obtained and what would be needed in practical design.

The third area of interest was that of heat transfer to superheated mercury vapor.

A. LIQUID METAL HEAT TRANSFER

The background material presented here will be limited to what has appeared in the literature for test sections with configurations with some semblance to those being tested in the SNAP-I Program.

1. Tubes

Lubarsky and Kaufman (Ref. 1) present the following correlation for all available (prior to 1955) fully developed heat transfer data for round tubes with constant heat input:

$$Nu = 0.625 Pe^{0.4} \quad (1)$$

This correlation represents data in the range of Peclet numbers from 10^2 to 10^4 and includes data for mercury, lead-bismuth and sodium, with and without wetting agents (magnesium).

Johnson, Hartnett and Clabaugh (Ref. 2) present data for round tubes with uniform heat input and fully developed flow in the laminar and transition flow regions which were close to the range of interest. Peclet numbers range from 20 to 300 for the lead-bismuth eutectic and mercury, but the data are not well correlated. Lubarsky uses the Lyon (Ref. 3) theoretical equation (Eq 3) to represent these data and both are plotted in Fig. 13. A cursory examination points out that the correlation is

Fig. 13. Mercury Forced Convection Heat Transfer Nusselt Number Versus Peclet Number

applicable in the low range of Peclet numbers. Thus, the data will be employed as a comparison, but little attention will be given the correlation for this range of operation.

Russian data is correlated by Mikheyev, et al., (Ref. 4) for the range of Peclet numbers from 10^2 to 2×10^4 .

$$\text{Nu} = 4.5 + 0.014 (\text{Pe})^{0.8} \quad (2)$$

This equation is based on data for tubes of seven different metals and of circular and other crosssections for constant heat input and constant wall temperature.

The Russian correlation is of the same form as the theoretical correlation presented by Martinelli (Ref. 5) and Lyon (see Eq 3), but it does not represent the data as well as the Lubarsky and Kaufman equation. It should be noted, however, that Lyon's theoretical correlation overestimated the observed coefficients by 20 to 40%. The Lyon correlation for round tubes and constant heat input is:

$$\text{Nu} = 7.0 + 0.025 (\text{Pe})^{0.8} \quad (3)$$

The discrepancy is attributed to nonwetting effects by Lyon, but this has not been verified experimentally. A possible explanation is that the error is in the assumptions made in deriving the equation. The equation is based on a motion similarity; i.e., each finite turbulent mass of coolant transfers heat across the stream in the same way as the quantity of motion. This assumption is followed by the argument that during the mixing process, the unit mass preserves its heat, i.e., its temperature remains constant. This heat and mass transfer analogy seems to fail at this point for fluids of high thermal conductivity ($\text{Pr} \ll 1$).

2. Annuli

Lyon (Ref. 3), Werner, King and Tidball (Ref. 6) present data for sodium and potassium, and in both cases the scatter of the data is very large. Some significance, however, can be placed on the fact that in almost every case the data fall below that predicted by the theoretical equation (Eqs 7 and 8). These data are shown in Figs. 14 and 15 with Eqs 7 and 8 shown for comparison.

Lubarsky presents data for a figure eight-type test loop using lead-bismuth in an annulus with an inner diameter of 0.402 inch and an annulus diameter of 0.50 inch.

Fig. 14. Mercury Forced Convection Heat Transfer Nusselt Number Versus Peclet Number

Fig. 15. Mercury Forced Convection Heat Transfer Nusselt Number Versus Peclet Number

Trefethen (Ref. 7) also used a figure eight-type of test section to measure heat transfer coefficients with mercury. Six different test sections ranging in tube diameters from 0.308 to 0.711 inch, inner annulus diameter of 0.378 to 0.748 inch, and outer annulus diameter of 0.874 inch were employed in the investigation. These data for the overall average heat transfer coefficient for the tube and annulus are shown in Fig. 16 along with Eq 7.

Hall and Jenkins (Ref. 8) present somewhat later data obtained in a double annulus heat exchanger for sodium and sodium-potassium in the range Peclet numbers for 70 to 1000. These data are presented in the form of a plot of Nusselt number versus Peclet number and are in fair agreement with Lyon's equation. Hyman (Ref. 9) represents these data and that of other investigators as well as his own data for sodium with the following correlation.

$$Nu = 1.32 (Pe)^{0.31} \quad (4)$$

This equation gives values 20 to 30% higher than those of Seban and Casey (Ref. 10) and Lubarsky for lead-bismuth alloy.

Harrison and Manke (Ref. 11) propose a correlation which preserves the form of the theoretical equation.

$$Nu = 4.9 + 0.0175 (Pe)^{0.8} \quad (5)$$

Hartnett and Irvine (Ref. 12) present a correlation for noncircular ducts which is suggested for use for annuli heated on one side.

$$Nu = 4.2 + 0.015 (Pe)^{0.8} \quad (6)$$

Little theoretical work has been accomplished for fully developed heat transfer coefficients in annuli. The Liquid Metals Handbook (Ref. 13), however, recommends the use of the theoretical relation of Seban (Ref. 14) which was originally derived for a parallel plate with heat transfer through one side.

$$Nu_f^1 = 5.8 + 0.020 (Pe_f^1)^{0.8} \text{ for } Du/De \leq 1.4 \quad (7)$$

A second correlation is recommended for annuli of diameter ratios greater than 1.4.

$$Nu_f^1 = 0.75 (De/Di)^{0.3} 7.0 + 0.025 (Pe_f^1)^{0.8} \text{ for } De/Di > 1.4 \quad (8)$$

This correlation approximates the theory presented by Bailey (Ref. 15).

Fig. 16. Mercury Forced Convection Heat Transfer Nusselt Number Versus Peclet Number

B. BOILING LIQUID METAL HEAT TRANSFER

The material presented here will not, in general, cover the great volume of literature on the boiling of ordinary fluids. Coverage, however, will be extended to those correlations originally obtained on ordinary fluids that have been proposed for boiling liquid metals.

Farmer (Ref. 16) has obtained some preliminary mercury boiling data for a horizontal circular plate which was heated by an induction coil. This investigation was carried out at reduced pressures (0.25 inch of Hg, $t_{\text{(sat)}} = 340^\circ \text{F}$) for two surfaces, copper and chromium. Results of this investigation are shown in Fig. 17 where the copper-mercury boiling data fall more than 100% above that of the chromium-mercury system. This difference is attributed to the good wetting action of mercury on copper. The plot of heat transfer coefficient versus ΔT_w (t surface-fluid temperature) is typical for that of boiling fluids. The data presented, however, are insufficient to determine reliably the point at which boiling is initiated.

Lyon, Foust and Katz (Ref. 17) conducted a comprehensive test program where boiling heat transfer coefficients were obtained for mercury, mercury plus 0.1% sodium, mercury plus 0.02% magnesium and a trace of titanium, sodium, sodium-potassium alloy and cadmium. These pool boiling data were collected at atmospheric pressure on a single, horizontal stainless steel tube. The results for pure mercury and mercury with the indicated wetting agents are shown in Table 4.

TABLE 4
Pool Boiling Data

<u>Fluid</u>	<u>Boiling Point (°F)</u>	<u>Temperature Difference Range (°F)</u>	<u>Heat Flux Range² (Btu/hr-ft²)</u>
Hg	670	11 to 959	4140 to 30,000
Hg plus 0.10% Na	670	1.3 to 33.7	2100 to 57,400
Hg plus 0.02% Mg and trace Ti	670	0.6 to 13.2	2100 to 100,300

Fig. 17. Mercury Free Convection Boiling

Figures 18 and 19 represent the data obtained and clearly point out the tremendous effect of the wetting agents. The heat transfer rates for pure mercury are much lower than those shown for mercury with additives. The ΔT_w for the mercury plus 0.1% sodium were typical for nucleate boiling systems, i.e., a ΔT_w of 35°F a maximum heat flux of $60,000\text{ Btu/hr ft}^2$ (inflection point) was realized. Further efforts to increase this ΔT resulted in rapid wall temperature increases which would indicate that at a ΔT of 35°F , nucleate boiling ceased and partial film boiling was initiated.

Mercury plus 0.02% magnesium and a trace of titanium displays the same characteristics at low ΔT as mercury plus sodium. When boiling was initiated, however, the two curves differed radically, with mercury plus magnesium and titanium giving much higher heat transfer rates for a given ΔT . The range of data for mercury plus magnesium and titanium is limited and the transition from nucleate boiling to partial film boiling is not realized. This phenomenon seems to lead to the conclusion that the initiation of partial film boiling is a function of the wetting properties of the fluid rather than the coalescence of the bubbles to form a continuous vapor blanket. Thus, the authors (Ref. 17) state that "film boiling may be more a manifestation of nonwetting phenomena than the coalescence of many bubbles." It is interpreted to mean that the cause of the bubble coalescence is in itself a manifestation of greater adhesion of the vapor for the heat transfer surface and, therefore, the nonwettability of the liquid.

A second point made by Lyon, Foust, Katz is that the boiling curve for pure mercury does not display the sharp inflections of the typical boiling curve. This can be interpreted as a direct change from simple convection heat transfer to film boiling heat transfer as a result of high heat transfer surface temperatures. This effect would also be anticipated for a fluid of high surface tension such as mercury, since high surface tension is detrimental to the initiation of nucleate boiling.

Heat transfer coefficients decreased with increasing temperature difference throughout the entire range of heat fluxes for which Lyon, Foust and Katz obtained data (ΔT_w as high as 1000°F) for pure mercury.

Mercury plus 0.1% sodium gave heat transfer coefficients 10 times greater than those of pure mercury while this increase was even greater for mercury plus magnesium and titanium. The maximum heat transfer coefficient obtained was $3000\text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$ at a ΔT of 12°F .

Fig. 18. Film Boiling of Mercury

Fig. 19. Boiling of Mercury

Bonilla, et al., (Ref. 18) conducted pool boiling heat transfer tests with mercury in the pressure range from 4 mm of Hg to 45 psi. Like Lyon, et al., Bonilla obtained data for pure mercury and mercury with 0.02% Mg and 0.0001% Ti. The range of variables investigated in pool boiling is given in Table 5.

TABLE 5

Ranges Investigated in the Pool Boiling of Mercury by Bonilla (Ref. 18)

Substance	Absolute Range Pressure (mm Hg)	Temperature Difference Range (°F)	Heat Flux Range (Btu/hr-ft ²)	Pool Depth (cm)
Hg	10 to 1550	12 to 600	25 x 10 ³ to 125 x 10 ³	2.0
Hg	11.4 to 805	4 to 500	4 x 10 ³ to 100 x 10 ³	3.2
Hg	4 to 1550	15 to 120	30 x 10 ³ to 125 x 10 ³	10.0
Hg plus 0.02% Mg and 0.0001% Ti	83 to 800	4 to 35	7 x 10 ³ to 200 x 10 ³	3.2

Bonilla, et al., substantiated Lyon's findings with respect to the effect of wetting on nucleate boiling and the transition from nucleate to film boiling. Lyon's and Bonilla's data for the same pressure (1 atm) are shown in Fig. 19.

Film boiling data obtained by Bonilla, et al., pointed out the extreme temperature fluctuations and the same high temperatures experienced by Lyon for film boiling operation.

Camack and Forster have proposed the use of Forster and Zuber's correlation for maximum heat flux in a boiling liquid at rest, for nucleate boiling of liquid mercury. This correlation is shown in Fig. 12 along with Bonilla, et al. (Ref. 18) data for mercury. It appears that this correlation which was originally obtained for methanol and water represents the data quite well but final decision must be withheld until more boiling liquid metal data are obtained.

Forster and Zuber correlation:

$$q/A = K_1 \Delta T^{1.25} \Delta P^{0.75}$$

$$K_1 = \frac{0.172 K_L^{0.8} \rho_L^{0.5} C_L^{0.45}}{L^{0.25} \sigma^{0.8} \mu_L^{0.3} \rho_V^{0.25}}$$

$$\Delta T = (T_w - T) \quad (9)$$

ΔP = pressure above saturation corresponding to the superheater.

σ = surface tension

ρ = density

K = thermal conductivity

μ = viscosity

C_L = specific heat

Subscripts L, v, w = liquid, vapor, heating surface, respectively.

C. SUPERHEATED MERCURY HEAT TRANSFER

The investigation of superheated mercury vapor should follow the conventional heat transfer correlation for turbulent flow in concentric annuli. McAdams (Ref. 19) recommends the following correlation for annulus ratios from 1 to 10:

$$j = \frac{h_L}{(C_p)_b G} \frac{C_p}{K_b}^{2/3} \frac{\mu_w^{0.14}}{\mu_b} = \frac{0.023}{(D_e G / \mu_b)^{0.2}} \quad (10)$$

Results obtained with this correlation for long straight tubes should be multiplied by the following ratio to take into account the helical coil:

$$1 + 3.5 D/D_{He}$$

Thus, the data obtained in this investigation for superheated mercury vapor could be compared to the results given by Eq. (10) with the appropriate modification.

D. THERMODYNAMIC PROPERTIES OF MERCURY

The major thermodynamic properties of saturated mercury, both in the liquid and vapor phases, were obtained from a report prepared by M. Handel (Ref. 20). The data were computed from the semiempirical equations of T. B. Douglas, et al., (Ref. 21) and cover that portion of the saturated vapor line between 200 and 1400° F. The values were presented in two tables, the first, a function of temperature, and the second, a function of pressure. The following properties were given at 1° F intervals.

- (1) Saturated vapor enthalpy--Btu/lb
- (2) Enthalpy of vaporization--Btu/lb
- (3) Specific heat of the vapor--Btu/lb-°F
- (4) Enthalpy of the saturated liquid--Btu/lb
- (5) Specific heat of the liquid--Btu/lb-°F
- (6) Saturated vapor entropy--Btu/lb-°F
- (7) Entropy of vaporization--Btu/lb-°F
- (8) Saturated liquid entropy--Btu/lb-°F
- (9) Isothermal enthalpy--pressure coefficient-- $(dH/dP)_T$
- (10) Saturation pressure--psi
- (11) Specific volume of vapor--ft³/lb
- (12) Specific volume of liquid--ft³/lb.

Other properties required for the thermal and hydraulic design of the APU were obtained from the Liquid Metals Handbook (Ref. 15) and are shown in Figs. 20, 21 and 22. Figure 20 presents the thermal conductivity of liquid mercury as a function of temperature while Fig. 21 is a plot of the Prandtl number versus temperature. The viscosity of liquid mercury is presented as a function of temperature in Fig. 22.

International Critical Table (Ref. 22) data for the viscosity of mercury vapor is plotted versus temperature in Fig. 23. The lack of experimental values of the thermal conductivity of mercury vapor required the use of an approximate analytical method. Sutherland's equation was employed to calculate the thermal conductivities presented in Fig. 24 along with the data of McAdams (Ref. 19) for other gases.

Fig. 20. Thermal Conductivity Liquid Mercury (Ref. 21)

Fig. 21. Prandtl Number Liquid Mercury (Ref. 21)

Fig. 22. Viscosity of Liquid Mercury (Ref. 21)

Fig. 23. Viscosity Versus Temperature Mercury Vapor (Ref. 21)

Fig. 24. Thermal Conductivities Gases (Ref. 22)

The mercury Mollier diagram employed to trace the theoretical Rankine cycle with superheat that was employed in the APU design is presented in Fig. 25. This chart is based on National Bureau of Standards data. The quantities required for the numerical calculations of cycle work and efficiency can, in many instances, be read from the diagram with sufficient precision.

Fig. 25. Mercury Mollier Diagram

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IV. EXPERIMENTAL BOILER PROGRAMS

The chronological design concepts were discussed in Chapter II, but little was mentioned of the fabrication and testing programs required to substantiate the preliminary designs. In this chapter, all experimental boilers, facilities and testing programs will be discussed to provide a better understanding of the complete mercury boiler development program.

A. MERCURY HEAT TRANSFER EXPERIMENT

The need for a well instrumentated loop to obtain basic data for the three modes of heat transfer encountered in the SNAP-I Program was dictated by the findings of the continuing literature search. In general, the present state of liquid metal heat transfer technology still falls short of design requirements. The unique nature of the SNAP-I boiler coil (see Fig. 26) compounded the problems encountered. The state of the art of liquid metal heat transfer was discussed previously in Chapter III.

A test program was designed which had as its objectives the following:

- (1) Forced convection film boiling heat transfer correlation.
- (2) Forced convection heat transfer correlation for a helical coil system.
- (3) Reliable pressure drop data for a boiling mercury system.
- (4) General dynamics corrosion information on the use of Type 347 stainless steel in a boiling and condensing mercury system.

The test loop was composed of a test section and its housing, condenser-subcooler, pump, pressurizer, flowmeter and preheater, and was housed in a closed container that was equipped with an exhaust system. A schematic and photograph of the loop are shown in Figs. 27 and 28. A detailed discussion of the test section and loop apparatus follows.

Fig. 26. Mercury Heat Transfer Test Coil

Fig. 27. Schematic for Basic Heat Transfer Test Loop

Fig. 28. Heat Transfer Loop

1. Apparatus

The mercury heat transfer loop was designed to obtain heat transfer data in the liquid, boiling and superheated regions of operation. A primary consideration in this design was to obtain the points at which boiling and superheating were initiated. Thus, the design of the test section included the placement of thermocouples for both bulk and wall temperature readings every 1.6 turns on a 24-turn coil (32 points). The coil was made up of a 3/8-inch OD tube with an ID of 0.245 inch which housed a turbulence promoter consisting of a tube (0.109-inch OD) wrapped with a 0.051-inch wire (1/4-inch pitch). Figure 26 shows an X-ray photograph of the Type 347 stainless steel test section and Fig. 29 shows the actual coil, heaters, thermocouples and housing prior to final assembly. Bulk temperature measurements were made at the same point in the coil as the wall temperature was recorded, thus local heat transfer coefficients could be obtained. A true bulk temperature (mixing cup) is obtained by cutting 1.5 inch of the turbulence promoter out at the point of measurement. Three pressure taps had been incorporated in the test section at the 3rd, 12th and 18th coils, respectively. These pressure taps were manifolded into a Heise pressure gauge and would give both the pressure and differential pressure throughout the coil.

The test section housing was composed of a 14-inch length of 12-inch, Schedule 120, stainless steel pipe and contained the test section, tubular heaters and the thermal bond material. Power was supplied by three tubular electrical heaters, with a total capacity of 19.3 kilowatts. This power was provided through a Barbara Wheelco saturable reactor from 400-volt, 3-phase supply. There were two, 7-inch diameter heaters of 7300 watts each located externally to the test coil. A third heater provided 4700 watts and was located within the test section helix. The thermal bond between the three heaters and the test section was provided by molten lead and copper pellets. Figure 27 shows a flow diagram of the loop, composed of the previously described test section and a Lapp Pulsafeeder pump, accumulator, Potter flowmeters, secondary flowmetering assembly and condenser. A Lapp Pulsafeeder pump, capable of delivering 2.9 gph (5.6 lb/min) at 445 psig, was employed to circulate the fluid. This pump was powered by a 1/3-horsepower motor and operated with a double diaphragm remote head system. The 58 beat per minute pulse of the pump was dampened out by an accumulator through which the system was loaded, drained and pressurized.

Fig. 29. Heat Transfer Test Coil, Heaters and Housing

Flow rate was measured by two Potter (turbine-type) flowmeters. One flowmeter was placed in the main leg of the loop while a second was placed in the bypass leg. The Potter meters were tied into the same recorder such that the differential of the meters would be recorded, i.e., total flow minus the bypass flow giving the flow through the test section. A secondary flowmetering assembly consisted of two 6-inch sections of hypodermic tubing and two ΔP cells. Thus, with the proper calibration of this assembly, a constant positive check on the flow rate was assured. Figure 30 illustrates the instrumentation panel for operating the heat transfer test loop.

A preheater employing Lewis resistance wire was operated in conjunction with a Minneapolis-Honeywell pyro-vane temperature controller to ensure that the desired inlet temperature was maintained.

In addition to the three pressure taps on the test section proper, two other pressure taps were provided. The outlet pressure was recorded on a Fischer-Porter proportional reset pressure recorder that actuated the diaphragm over-pressure control valve, thus maintaining the desired system pressure. Inlet pressure was also recorded on a pressure recorder.

The outlet leg from the test section to the condenser was wrapped with Lewis wire and served as a guard heater. This prevented refluxing in the leg between the test section and the condenser. Mercury vapor would be condensed and subcooled in a radiation-type condenser consisting of two water-cooled copper plates, one mounted internally and the other externally to the mercury carrying stainless steel coil. A 1/4-inch OD (0.032-inch wall) copper coil 24 feet long was brazed to the internal copper plates while 31 feet of the same coil was brazed to the outer copper plate. The stainless steel tube was 40 feet long and was wrapped in a coil 12 inches in diameter and 7.5 inches deep. Mercury temperatures were measured at the inlet and outlet of the condenser.

2. Results

After the loop was cleaned and leak tested, a heat loss test was conducted prior to loading the mercury charge. The heat loss varied from 1.75 to 3.2 kilowatts for various test section temperatures up to 1500° F. A zero power test was initiated to check out the instrumentation and to obtain isothermal pressure drop data for the analysis of boiling pressure drop data. During this phase of testing, a leak was experienced at a pressure tap and the test section and housing were removed for reworking. While the test section was undergoing repairs, the SNAP-I program was phased out and the basic heat transfer program discontinued.

Fig. 30. Instrument Panel for Heat Transfer Loop

B. MERCURY BOILER TEST FACILITY

A boiler test facility was designed to demonstrate the feasibility of the mercury boiler design under prelaunch, transient in-flight and orbital conditions. The facility designed to accomplish this objective is shown in Fig. 31. The full-power boiler (5000 watts thermal) of the conceptual design is shown mounted horizontally in the environmental chamber. This chamber is a double-walled cylinder, 50 inches in diameter and 60 inches in height (see Fig. 32). Also shown are the expansion and filling lines for the lead and sodium thermal bond material.

The original mercury flow system, as shown in Fig. 32, was composed of the following:

- (1) Pressurizing system.
- (2) Lapp Pulsafeeder controlled volume pump.
- (3) Full-power boiler.
- (4) Air-cooled condenser.
- (5) Air-cooled subcooler.

A vacuum pump (see Fig. 33) is tied into the environment chamber that is capable of simulating the pressure decrease the APU would experience while attaining orbit. This system, therefore, provides the means of testing the mercury boiler at temperature and pressure design conditions in each of the three environments the system is expected to encounter during its normal operational life.

In Phase I, the preflight phase, the mercury system would be brought up to the operating temperature of 1350° F and pressure of 210 psia in a normal atmospheric environment. The ground operation of the boiler could be tested in the chamber for both rated and excess power. The latter condition would be as a means of determining the ability of the thermal shutter to dissipate excess heat and thereby automatically control the operating parameters of the system. Since a test of this nature simulates conditions in the missile before launch but after the liquid oxygen has been loaded, liquid nitrogen would have to be used in the annular space of the double-walled environmental chamber to obtain this condition. This test represents the most severe condition of the preflight phase.

Fig. 31. Flow Sheet of Boiler Test Facility

Fig. 32. Boiler Mockup Test Loop

Fig. 33. Vacuum Pump for Environmental Chamber

In Phase II, the in-flight phase, the transient conditions to which the APU would be subjected while attaining orbit would be investigated to determine their effect on design parameters and the operational stability of the system. The missile skin heating during flight would be simulated by strip heaters appropriately positioned in the environmental chamber. The rapidly changing atmospheric pressure that would be experienced during the flight would be closely simulated by the rapid application of a vacuum to the chamber.

Phase III, the orbital environment test, would entail operation at full power in a 0.1-micron vacuum and at -195°C . The ability of the thermal shutter to regulate the systems operating parameters at design conditions would be tested for rated and excess power. Liquid nitrogen could again be employed to attain the low environmental temperatures.

During the course of the developmental program, extensive modification to both loop components and instrumentation was performed. These modifications will be discussed in connection with various boiler designs.

C. FULL-POWER BOILER

The first boiler concept proposed and subsequently tested was the full-power boiler; i.e., a single boiler producing the full 5000 watts thermal requirement. During the procurement and fabrication stage, the design philosophy of the APU changed. Two half-power boilers of 2500 thermal watts were adopted. As outlined previously, space allocations and mass symmetry considerations within the satellite vehicle fostered this change. As a result of the change, the full-power boiler was built without the thermal shutter mechanism and the test program was reduced to a loop checkout and a design feasibility investigation. The design feasibility would be carried out while the new boiler concept was being fabricated.

The boiler heat transfer design was based on the heat transfer information gained from a literature search in the field of forced convection, boiling and superheated mercury heat transfer. Results of the original literature search and the continuing search were presented in detail previously. The information available in the forced convection field was sketchy and almost nonexistent in the boiling mercury field for the parameters of interest. The results of the full-power boiler test were needed to give some indication of the solution to the heat transfer problems present.

Figure 34 illustrates the internal construction of the full-power boiler, showing in detail the 3/8-inch stainless steel tubing wrapped around the electrical heater housing. Since two thermal bonds were employed in this boiler concept, two additional lines were required as shown in Fig. 35. Lead was employed as the thermal bond between the heater housing and the 25 feet of heat transfer coil. Sodium was the thermal bond between the electric heaters and the heater housing. Figure 36 shows the insulated full-power mercury boiler mounted horizontally in the boiler support stand. Figures 37 and 32 show respectively the boiler mounted in the environmental tank and the principal components of the boiler test loop.

The initial phase of the full-power boiler test was terminated in mid-May 1958 after considerable running time. During this test, the maximum mercury outlet temperature realized was 1120° F at 210 psi. The following conclusions could be drawn from the operating experience and data gained:

- (1) Additional heat transfer area was required to attain design conditions which, for this system, were 1.87 lb/min of mercury vapor at 1300° F and 210 psia.
- (2) The combined condenser and subcooler heat transfer area was too large, resulting in subcooled mercury inlet temperatures.
- (3) Type 316 stainless steel tubing appeared to be undergoing a considerable corrosion as indicated by an accumulation of corrosion products on the heat transfer surface.

In respect to Conclusion (1) above, it should be noted that this boiler design and all succeeding units were limited heat transferwise by the maximum operating temperature of the electric heaters. While the normal design operating temperature of conventional electric heaters is 1500° F, they can be operated with a reduced life at 1600° F. Thus, the heat transfer driving force, i.e., the temperature gradient, is limited during testing while the actual isotope-powered unit does not suffer from this limitation. Because the isotope power source produces heat without a similar temperature limitation, it will achieve the temperature necessary to transfer the quantity of heat required. A second and potentially more serious detriment to efficient heat transfer was also encountered in the accumulation of corrosion scale on the boiler coil wall. The solution to this problem was investigated in a separate program and will be discussed in another report.

Fig. 54. Full-Power Boiler

Fig. 35. Full-Power Mercury Boiler

Fig. 36. Insulated Full-Power Mercury Boiler

Fig. Fig. 37. Insulated Boiler in Environmental Tank

A second phase of the full-power boiler program was initiated after the boiler and necessary test loop modifications were accomplished. Phase II of the full-power boiler test was initiated the week of June 23, 1958. During the interim period between Phase I and II, the following modifications were accomplished:

- (1) Additional heat transfer area was obtained by installing a new boiler coil containing 50 feet of heat transfer length.
- (2) The condenser was bypassed and the subcooler functioned as both a condenser and subcooler.
- (3) A wire spiral with a pitch of 1/2 inch was wrapped around a 0.109-inch OD tube and installed in the 3/8-inch boiler coil to act as a turbulence promoter.
- (4) A thermocouple for measuring mercury bulk temperatures was placed within the 0.109-inch OD tube of the turbulence promoter.

Additional heat transfer area enabled considerable flexibility in the investigation of various parameters. It was possible to investigate the superheat attainable from flow rates in the range from 50% below to 50% above the design condition of 1.87 lb/min. Superheats in excess of the design condition (1350° F) were attained at all flow rates with an appropriate adjustment of the power input.

A spiral turbulence promoter such as that shown in Fig. 42 was employed to increase the boiling heat transfer coefficient. This insert improved the wetting condition at the tube wall by directing liquid mercury against the wall and by producing greater turbulence in the boiling area. The effectiveness of the turbulence promoter was borne out by Phase II data which, unlike that of Phase I, showed the tube wall temperature only slightly higher than the mercury bulk temperature.

Mercury bulk temperatures in the superheat region were measured by inserting a thermocouple in the 0.109-inch tube of the turbulence promoter to a point 12 feet from the boiler outlet. When the bulk coolant thermocouple was employed in this manner, the conduction and radiation corrections were reduced to negligible amounts.

During this test, both full- and half-power boiler design conditions were achieved with 38 feet of boiler coil. It was apparent from the results that the turbulence promoter had increased the thermal efficiency (i.e., the overall driving force was reduced).

As anticipated, the increased thermal efficiency was paid for by an increased pressure drop, resulting in increased pumping power. The average pressure drop at full-power design conditions with a 50-foot coil containing a 1/2-inch pitch spiral turbulence promoter was 125 psi. Since reduction in the pressure drop could be accomplished by changing the pitch of the turbulence promoter, other pitches were investigated in succeeding programs.

Results of the full-power boiler tests with the turbulence promoter were so promising that this concept was incorporated into the design of two half-power boilers designated Preliminary Boiler Prototype PBP 1 and PBP 2.

D. HALF-POWER BOILER PBP 1

During the period that the full-power boiler was being experimentally tested, design considerations and changes were being made to the SNAP-I APU. Space and symmetrical loading considerations required that two half-power boilers be employed rather than one full-power unit. Ground handling and missile countdown procedures required that the biological shield be retained in the APU until shortly before launch. This resulted in the use of a reflective thermal shield which provided compartments for the required liquid mercury biological shield. Another feature was also provided that contributed to the overall workability of the APU system and its marriage to the satellite vehicle. This feature was an auxiliary gas-cooling system which enabled the APU to meet the cooling demands imposed by a "hold order" given after the biological mercury had been drained and the APU had been placed into operation. During a hold, the biological mercury would have to be replaced to allow the necessary access to the working area. Prior to a hold order, the boiler and shield internals would be 1300° F which is above the boiling point of mercury (674.2° F). This would make it necessary to cool the unit before replacing the biological shield if vaporization of the shield mercury is to be eliminated. The gas cooling system served this function, and also provided a means to cool the APU boiler in the event of a primary coolant loss.

The PBP 1 was designed to incorporate all of these design concepts and to demonstrate the feasibility of the half-power boiler concept that used 12.6 feet of boiler coil with a 1/4-inch turbulence promoter pitch in a 4-inch coil diameter. This unit was fabricated in the summer and fall of 1958.

Utilizing the full capability of the boiler test facility, a complete design checkout through the preflight phase, transient in-flight phase and the orbital environment phase was planned for PBP 1. Checkout and calibration of the thermal shutter control system was completed and the preflight test phase was initiated when a leak was encountered in the mercury line. The leak in the coil resulted in an early failure of the electrical heaters. Thus, PBP 1 was removed from the test facility and PBP 2 installed to further the evaluation of the design concept. The additional design changes that will be enumerated made PBP 1 obsolete and no additional development effort was expended in this area.

E. HALF-POWER BOILER PBP 2

The design and fabrication of PBP 2 was initiated during the fabrication of PBP 1 as a backup to the unit and for operation in a dual boiler test loop. The dual boiler test loop, which will be discussed later, was designed to study the stability of two half-power boilers in a parallel flow system. The PBP 2 did not have a biological shield nor a thermal shutter, but the heater housing, i.e., molybdenum block and mercury heat transfer coil, were identical to that of PBP 1. Thermal insulation was provided by potassium titanate which is a conventional fiber-type insulation capable of high thermal resistances under continuous high temperature operation.

Superheated mercury vapor (0.935 lb/min) at the design condition of 1350° F and 210 psia was produced with PBP 2, but the pressure drop across the boiler was approximately 50 psi. This high pressure drop was unacceptable and design changes were initiated to reduce the overall boiler pressure drop.

F. HALF-POWER BOILER PBP 3

The 50-psi internal pressure drop for PBP 2 was 3.5 times that allowed for in the design of the turbine-driven pump being developed by Thompson Ramo Wooldridge. Pressure drop could be lowered by either increasing the turbulence promoter pitch or reducing the length of the boiler coil. Both means would entail a reduction in heat transfer, a condition that had to be avoided to maintain the design performance of the unit. Since mercury boiler PBP 3 was completed except for final assembly, a decision was made to increase the turbulence promoter pitch from 1/4 to 1 inch and to compensate for the reduced heat transfer coefficient by increasing the length of the boiler coil from

12.5 to 19 feet. This was accomplished by extending the boiler by 3 inches and using a 3-1/2-inch diameter block of molybdenum to simulate a tungsten shield. Since the diameter of the heater housing and molybdenum shield were already 3-1/2 inches, only 19 feet of tubing could be coiled in the annular spacing. Figure 38 illustrates the construction of half-power boiler PBP 3. The heater housings in PBP 4, 5 and 6 were increased to 4-1/8-inch diameters without an increase in the boiler length and the coil was increased to 24 feet. Only 19 feet of this contained the turbulence promoter.

The increased boiler coil length was obtained by incorporating a tungsten radiation shield within the isotope boiler to protect the radiation sensitive equipment contained in the satellite vehicle. The shield consisted of a 3-inch thick tungsten block positioned at the top of the molybdenum core as shown in Fig. 39. Since tungsten has excellent thermal properties, it was decided to take advantage of its fin effect and incorporate the extended boiler coil in this area. Thus, 19 feet of coil containing a 1-inch pitch turbulence promoter was fabricated in PBP 3 and subsequently tested.

The APU design conditions of 1300° F superheated mercury vapor at 210 psia were realized with an average pressure drop of only 12 psi. These results established the PBP 3 heat transfer and hydrodynamic design as that which would be employed to check out the secondary power conversion system being developed concurrently at Thompson Ramo Wooldridge Corporation.

Prior to the mating of two half-power boilers with the power conversion system, a dual boiler loop was designed to investigate the flow dynamics of the combined boiling systems.

G. DUAL BOILER TEST FACILITY

The dual boiler test facility was designed to determine the stability of two half-power boilers in a parallel flow loop when each is operating at design conditions. The problems anticipated here are directly analogous to two boiling channels of a nuclear reactor. In this situation, if one of the channels, i.e., the hot channel, is producing more power than the other, boiling will be initiated first in the hot channel. When boiling is initiated, the pressure drop at a given flow rate is increased, but since the pressure drop across parallel channels must remain the same, the flow rate to the hot channel decreases while the flow in the nonboiling channel increases. As the flow in the hot channel decreases, the amount of boiling increases and, therefore, the flow decreases even more. This condition eventually results in vapor blockage or choking in one channel and eventual burnout, i.e., physical destruction of the hot channel.

Fig. 38. Electric Boiler PBP3--Sectionalized After Test Program

Fig. 39. Engineering Drawing of Complete Assembly

This argument could be applied to the system composed of two-half-power boilers in parallel. Thus, the philosophy of the dual boiler check-out tests was to obtain the design conditions in both boilers with a simulated APU flow loop and then subject the flow to programmed transients. In this manner, the response of the system could be studied and the flow stability determined.

The first boilers to be tested in the dual loop were PBP 2 and 3 which were used to check out the loop instrumentation during fabrication of PBP 4 and 5. The boilers differed radically in length and fluid flow path configuration, thereby producing the maximum potential flow instability. A critical test for the flow control instrumentation checkout was therefore provided.

Figure 40 illustrates schematically the dual boiler test loop. Flow was provided by a Chempump with a discharge head of 50 feet and a capacity of 5 gpm. It was necessary to bypass a major portion of the flow. An air-cooled condenser, capable of dissipating the power developed in two half-power boilers, was provided. In addition to the conventional components, the loop contained:

- (1) Two air-to-close Research Control flow control valves.
- (2) Two ΔP flowmetering assemblies.
- (3) A simulated APU flow regulator consisting of one Hoke valve in parallel with a Hoke valve and a solenoid valve.
- (4) Venturi flowmeter through which flow was measured with a calibrated differential pressure transducer.
- (5) A choke nozzle that simulated the APU turbine.

The flow control valves enabled stable operation at design conditions with PBP 2 and 3 and enabled the preliminary checkout of the loop instrumentation to be completed.

H. HALF-POWER BOILERS PBP 4 and 5

Mercury boilers PBP 4 and 5, for which the dual boiler test facility was specifically designed, were identical boilers containing 23.7 feet of coil wrapped in a 4.625-inch diameter helix (see Fig. 41). The increased coil length and helix diameter resulted from enlarging the molybdenum core to compensate for additional fuel material. This additional fuel volume was necessitated by the production of ceric oxide of a lower

Fig. 40. Dual Test Loop

Fig. 41. Boiler Housing Components

purity than originally anticipated, as shown in Fig. 42. A turbulence promoter 19 feet long with a pitch of 1 inch was housed in the 3/8-inch OD by 0.245-inch ID stainless steel tubing. The remaining components of the electrically heated boilers (PBP 4 and 5) are shown in Fig. 43. The uninsulated assembly is shown in Fig. 44.

The principal function of the dual boiler test facility was to study flow dynamics of PBP 4 and 5 in parallel. In addition, specific tests were conducted to determine the response of turbine inlet pressure to step changes in the mercury flow rate. Step changes in the flow rate were accomplished by activating a solenoid valve which was positioned in the loop in such a manner as to reasonably simulate the prototype flow regulator. Satisfactory operation of the dynamic system instrumentation was not achieved, and the step change flow test was not completed at the termination of the SNAP-I Program. After checkout, both boilers were cleaned and delivered to Thompson Ramo Wooldridge on April 25, 1959 for further APU system testing.

One spare mercury boiler, PBP 5A, was fabricated, installed in the experimental facility and operated for 28 hours. Results of the abbreviated checkout test gave a heat balance within 5%; the average exit mercury temperature was 1305° F with a molybdenum core temperature at 1602° F, lead bath at 1515° F, inlet mercury at 330° F and a flow rate of 0.935 lb/min. This unit was also forwarded to Thompson Ramo Wooldridge after the shakedown test to be employed as a spare boiler for the dual boiler test facility.

I. BOILER COMPONENT DEVELOPMENT

Various analytical investigations and corresponding experimental studies were conducted on the boiler core to ascertain a complete thermal history of the unit. The principal areas of study were:

- (1) Steady-state thermal analysis of the molybdenum block.
- (2) Transient analysis of the molybdenum block.
- (3) Combined axial and radial core temperature distribution and mercury heat transfer study.
- (4) Boiler and boiler loop heat loss.

In each instance enumerated, the complexity of the problem dictated that special techniques should be employed in the investigation. The initial steady-state thermal analysis of the molybdenum core for both

Fig. 42. X-Ray Photograph of Mercury Coil

Fig. 43. Single Heater and Boiler Components

Fig. 44. Uninsulated Electrical Mercury Boiler (PBP4)

the full- and half-power units was carried out on a thermal conductivity analog. The transient analysis, the combined core and mercury heat transfer study, and heat loss studies were accomplished with IBM-704 FORTRAN codes.

Each investigation and the techniques employed are discussed below:

1. Thermal Conductivity Analog--Molybdenum Block Steady-State Temperature Distribution

A rigorous analytical solution of the heat flow problem for the molybdenum core was too complex to be practical; therefore, an electrical analog was employed to yield a rapid solution. The application of a passive electrical network is well known for the solution of many problems in such fields as acoustics and heat transfer. The electrical network used in heat transfer problems relies on the analogies which can be drawn between electrical and heat transfer quantities. These analogies are:

<u>Electrical</u>	<u>Thermal</u>
Potential	Temperature
Current	Heat flow rate
Conductivity	Conductivity
Capacity	Heat capacity
Charge	Stored thermal energy

The actual problem is three dimensional in scope but this could not be achieved with an electrical simulator. The boiler cross section is circular and it was assumed that the axial heat flow was negligible. Thus, a circular sheet of conducting paper, representing a radial slice, can be employed to simulate the thermal resistance of the entire boiler. It is then necessary to simulate the thermal capacity by means of lumped electrical capacitance, achieved by connecting capacitor leads to the conducting paper. The sources of heat in the boiler are simulated by sources of constant current placed in accordance with the orientation and geometry of the heat sources in the boiler. Heat is removed from the boiler by two separate but interdependent mechanisms: transfer from the heat source by conduction through the molybdenum block and by natural convection in the thermal bond material. The thermal bond dumps heat by two mechanisms: forced convection to mercury and subsequent transport out of the boiler, and radiation of excess heat to space from the thermal shutter. These mechanisms of heat transfer were simulated by the electrical network shown in Fig. 45.

Fig. 45. Design of the Simulator

The maximum thermal gradient from the center of the core to its circumference was found to be approximately 70° F. The resultant temperature distribution was determined to within ±10%. The boiler and molybdenum core analyzed in this study are shown in Figs. 11 and 46.

2. Transient Analysis--Molybdenum Fuel Element Cooling and Self-Brazing Techniques

Loading of the molybdenum fuel element was scheduled to be accomplished in a hot cell at Oak Ridge National Laboratory. The isotope pellets would be loaded into the molybdenum fuel element by remote handling techniques. This operation would be further complicated by the high fuel block temperatures that would be encountered. This would make it necessary to insulate the "hands" of the mechanical slave or to use a saddle with ceramic handles to manipulate the cylindrical fuel element.

Isotope pellets would be loaded into the molybdenum fuel block using 18 drilled fuel cavities, 9 on each end of the block. A screw-in plug would be inserted in each fuel cavity and solid microbrazed placed over each. At this point, the block would be insulated to allow the loaded end to attain the 1950° F brazing temperature. Thermal insulation was required since the equilibrium temperature of the fuel elements with nine holes filled (i.e., half of the 3835 thermal watts) was analytically determined to be 1160° F. An IBM-704 machine code was written to solve the following relationship:

Heat production - heat loss = heat accumulation

$$Q - 2\pi RL \sigma \epsilon T^4 - \theta^4 + h T - \theta - \zeta \pi R^2 \sigma \epsilon T^4 - \theta^4 + h T - \theta = \pi R^2 L \rho C \frac{dT^*}{dt}$$

* See nomenclature in the appendix.

This equation takes into account heat lost by both radiation and natural convection from the cylindrical surface and circular ends. An experimentally determined emissivity of 0.7 was employed for the block surface (Colmonoy 5 protective coating was used to prevent oxidation of the block at high temperatures) while an average heat capacity of 0.0742 Btu/lb-°F was used. The total isotopic heat production rate was taken at 13,100 Btu/hr (3835 watts).

Fig. 46. Boiler Assembly

Also investigated was the time required to heat the unit and the resultant equilibrium temperature with the full 3835-watt isotope loading. The results of this case with one end of the block insulated, to simulate the physical model with the block setting on an insulated pad, indicated that a 1500° F equilibrium temperature would be realized in approximately one hour. When the brazing process was completed on one end, i.e., the block was held at the brazing temperature for 20 to 30 minutes, the thermal insulation would be removed and the block cooled in air by natural convection and radiation. This slow cooling process was required due to the brittle nature of the braze material. The block would then be turned upside down and the isotope loaded into the remaining nine holes. Loaded with the full isotope charge, the molybdenum block would reach 1500° F at equilibrium.

Self-brazing of the second end of the block requires that the temperature of the lower end (end that was self-brazed with half the isotope charge) be below brazing temperature while the top is in excess of the brazing temperature. This would be accomplished by employing a water-cooled copper plate to conduct heat from the base of the block.

A test which proved the feasibility of the copper cooling plate was designed and conducted. The need for the test was dictated by the unknown contact area and contact heat transfer coefficient between the copper plate and the base of the block. These unknown characteristics arose from the rough surface deposits of the microbraze material and the recesses of the plugs. Thus, an analysis of this step of the process would have been approximate at best.

After both ends of the block are brazed, the insulation cap would be removed and the loaded block cooled by radiation and natural convection to the environment and by conduction to the water-cooled copper plate.

Prior to installing the molybdenum fuel block into the boiler housing, it was planned to cool the block directly by water so the APU fabrication process could be initiated with a relatively cool block. A second test was desirable to check out this cooling procedure. This test would also determine the thermal shock capabilities of the APU boiler design.

In the first of the tests, a comprehensive series of fuel block simulated brazing tests were performed. The results indicated that the self-brazing technique of fuel element closure is feasible with close control, employing the cooling plate and insulation cap. Also, the equilibrium centerline temperatures of half charged and fully charged fuel blocks placed on an insulated pad were determined to be 1165 and 1460° F, respectively. These temperatures correspond quite well with the analytical results of 1160 and 1500° F stated earlier.

When the bare simulated fuel block was placed on the water-cooled copper plate, the equilibrium temperatures of the center of the block were 890° F for a half fuel charge and 1250° F for the full charge. Tests conducted with an insulation cap over the top portion of the simulated fuel block and with the bottom cooled by the cooling plate showed an approximate 400° F temperature gradient between the two ends of the block. This temperature gradient was reduced from 400 to 100° F by employing a stainless steel shim (0.002 inch thick) and a stainless steel screen (0.020 inch thick) between the block and the cooling plate. These results showed that by varying the amount of insulation at the top and bottom of the fuel block, the required brazing temperature at the top of the block could be attained with the bottom end of the block maintained below the remelt temperature of the initial closure braze.

In this test, the maximum temperature that could be attained with the simulated block was limited by the maximum operating temperature of the electric heater. Therefore, a second phase of the program was conducted which employed induction heating of the fuel block. This test was successful as the top of the fuel block was maintained at 1950° F while the bottom was held at approximately 1500° F.

The fabrication cooling and self-brazing feasibility study was carried to completion despite the phasing out of the SNAP-I Program because the study was felt to be directly applicable to the SNAP-1A unit that was subsequently developed by Martin Nuclear.

Thermal shock tests were conducted in which PBP 3 was stripped of its external can and thermal insulation, the stainless steel boiler housing exposed, and the boiler thermally shocked a total of 18 cycles by heating to equilibrium housing temperatures ranging from 500 to 1100° F and immersing in room temperature water. No physical damage to the boiler resulted. Figure 47 shows a typical set of thermal shock test results in which the various boiler component temperatures are shown. During the initial 15 seconds of immersion, the housing side (top) temperature decreased from 1100 to 220° F, an average rate of 59° F/sec with a 2.5-kilowatt power input. Equilibrium temperatures were reached within 8 minutes with the boiler housing temperature at 150° F, lead thermal bond at 160° F and the molybdenum fuel block at 340° F.

At the conclusion of this test, visual and dye penetrant inspection revealed no effect on the boiler integrity. These results proved the feasibility of using this method as an emergency cooling technique during hot cell fabrication and launch site hold orders.

Fig. 47. Temperature Transient Curves--Boiler Mockup Immersion Tests

3. Combined Core Temperature Gradient and Mercury Heat Transfer Study

The objective of this study was to obtain a complete radial and axial temperature distribution in the molybdenum fuel block for a somewhat simplified model of both the full and half-power boilers. This study, in addition to the initial thermal conductivity analog investigation, was necessitated by the continuing design changes to the APU boiler and the experimental data realized in the interim period. An IBM-704 program investigated six radial regions with varying heat conduction properties and 79 axial sources. The engineering quantities calculated and printed out are fluid velocity, temperature and quality, molybdenum block and tube wall temperatures, and forced convection, boiling and superheated heat transfer coefficients. Three problems employing a one-dimensional temperature gradient were run satisfactorily, giving data superior to that of the analog.

The two-dimensional code was being refined when the SNAP-I Program was terminated. Refinement was required due to the slow convergence of the iteration process. This code modification was underway when the program was phased out.

The IBM-704 machine code solved the following differential equation:

$$\frac{\partial^2 T(x,r)}{\partial x^2} + \frac{\partial^2 T(x,r)}{\partial r^2} + \frac{1}{r} \frac{\partial T(x,r)}{\partial r} + \frac{S(x)}{k} = 0 \quad n$$

where

$$1 \leq n \leq N$$

$$2 \leq N \leq 6$$

$$S(x)_{n > 1} = 0.$$

The boundary conditions are:

$$\frac{\partial T(x,r=0)}{\partial r} = 0$$

Internal interfaces

$$T(x,r=R)_{n-1} = T(x,r=R)_n$$

where R = interface radius

$$k \frac{\partial T(x, r = R)}{\partial r} \Big|_{n-1} = k \frac{\partial T(x, r = R)}{\partial r} \Big|_n$$

Surface ($n = N$)

$$k \frac{\partial T(x, r = R)}{\partial r} \Big|_n = -k(x) T(x, r = R) - \theta(x)$$

$$\frac{\partial T(x = 0, r)}{\partial x} = \frac{\partial T(x = L, r)}{\partial x} = 0$$

The coolant temperature or quality is defined as follows:

$$(1) \text{ For } \theta_{in} \leq \theta(x) \leq \theta_{sat}, \psi(x) = 0$$

where

θ_{in} = inlet temperature

θ_{sat} = saturation temperature

ψ = coolant quality

$$\frac{d\theta(x)}{dx} = \frac{2\pi R_N h(x) f}{GC} T(x, r = R_N) - \theta(x)$$

The ratio of tube effective heat transfer area to the surface area of the core (not including the ends) is given by "f".

$$\text{For } \theta(x) = \theta_{sat}, 0 < \psi(x) < 1$$

$$\frac{d\psi(x)}{dx} = \frac{2\pi R_N h(x) f}{GH} T(x, r = R_N - \theta_{sat})$$

For $\theta(x) > \theta_{sat}$, $\psi(x) = 1$

$$\frac{d\theta(x)}{dx} = \frac{2 R_N h(x) f}{GC(x)} T(x, r = R_N) - \theta(x)$$

The heat transfer coefficient is defined by:

(1) For $\theta_{in} \leq \theta(x) \leq \theta_{sat} - 300$, $\psi(x) = 0$

$$h(x) = 2.3 \frac{k}{D} \left[1 + 0.1 \frac{G^1 DC}{K} \right]^{1/2}$$

(2) For $\theta_{sat} - 300 < \theta(x) < \theta_{sat}$, $\psi(x) = 0$

$$h(x) = \frac{\theta_{sat} - \theta(x)}{300} \left[1 + 0.1 \frac{G^1 DC}{K} \right]^{1/2} 2.3 \frac{k}{D} \\ + 1 - \frac{(\theta_{sat} - \theta(x))}{300} A + B \xi(x) + C \xi(x)^2 + D \xi(x)^3$$

where A, B, C and D are constants determined by data read into the machine.

$$\xi(x) = T(x, r = R_N) - \theta(x)$$

(3) For $0 \leq \psi(x) \leq 0.6$, $\theta(x) = \theta_{sat}$

$$h(x) = A + B \xi(x) + C \xi(x)^2 + D \xi(x)^3$$

(4) For $0.6 < \psi(x) < 1$, $\theta(x) = \theta_{sat}$

$$h(x) = \frac{1 - \psi(x)}{0.4} A + B \xi(x) + C \xi(x)^2 + D \xi(x)^3 \\ + \psi \frac{(x) - 0.6}{0.4} 0.023 \frac{k(x)}{D} \frac{DG^1}{\mu(x)}^{0.8} \frac{(x) C(x)}{k(x)}^{0.3}$$

u, c, k are vapor properties evaluated at the film temperature.

$$(5) \text{ For } \theta(x) \geq \theta_{\text{sat}}, \quad \psi(x) = 1$$

$$h(x) = 0.023 \frac{k(x)}{D} \frac{DG}{\mu(x)}^{0.8} \frac{u(x) C(x)}{k(x)}^{0.3}$$

where the properties are evaluated at the film temperature.

4. Boiler and Mercury Loop Heat Loss Analysis

The overall efficiency of the power conversion cycle and the useful life of the APU is directly dependent on the heat loss from the boiler itself, and the heat lost in the primary loop between the boiler and the turbine. Since space and weight limitations were quite rigid, it was necessary to optimize the thermal insulation design. This was done with the aid of two IBM-704 machine codes. The first of these codes was designed to investigate the boiler insulation problem while the second analyzed the heat loss from the loop piping.

The mathematical model chosen for the first analysis consisted of a cylindrical solid at some known temperature surrounded by cylindrical regions which were in turn surrounded by elliptical regions. Thus, the reflective thermal shield design of the APU, which also functioned as a compartment for the liquid mercury biological shield, could be studied thermally for a geometry determined on a minimum biological shield weight basis. The regions of the model employed could be a solid, a gas or a vacuum. It was possible, therefore, to investigate systems composed of all reflective thermal shields or one composed of a combination of reflective shields and conventional fibrous insulations. Both systems were studied and the results indicated an optimized APU boiler design with a combination of reflective shields and fibrous insulation. This new design feature, which was incorporated in PBP 6, enabled the biological mercury to be contained in the APU while the system was operating. The excess isotopic heat would be removed by the auxiliary gas cooling system during such checkout operations at design conditions.

Since the actual problem was complex if analyzed in detail, an approximate approach was used. Basically, this consisted of choosing average heat transfer areas perpendicular to the heat flow instead of using the diffusion equation in the required coordinate form. The heat flow rate as represented by Fourier's third law is:

$$q = -k A(x) \frac{dT(x)}{dx} . *$$

*See nomenclature in Appendix.

A cylindrical solid region has the following area at any point:

$$A(x) = 2\pi R + 2x^2 + 2\pi R + xL + 2y$$

where x and y are related by:

$$y = \frac{xY}{X}$$

In this equation, X and Y are respectively the total radial thickness and total axial thickness of the region. These relationships combine to give the following expression for outside temperature (T_o) and inside temperature (T_i):

$$T_i = T_o + \frac{q}{2\pi k} \left[L^2 + 4R \frac{Y}{X} + 2 \frac{2Y}{X} - L \right]^{-1/2} \ln \frac{8X + 4Y + 4R + L + \frac{2RY}{X} - L^2 + 4R \frac{Y}{X} + 2 \frac{2Y}{X} - L}{8X + 4Y + 4R + L + \frac{2RY}{X} - L^2 + 4R \frac{Y}{X} + 2 \frac{2Y}{X} - L}^{1/2}$$

$$\frac{4R + L + \frac{2RY}{X} - L^2 + 4R \frac{Y}{X} + 2 \frac{2Y}{X} - L}{4R + L + \frac{2RY}{X} - L^2 + 4R \frac{Y}{X} + 2 \frac{2Y}{X} - L}^{1/2}$$

It can be shown that the area swept by rotating an ellipse about the axis "a" is:

$$A = 2\pi b^2 + \frac{2\pi ab}{1 - \frac{b^2}{a^2}} \sin^{-1} \left(1 - \frac{b^2}{a^2} \right)^{1/2}$$

provided $a > b$ and where,

$2a$ = maximum height of the ellipse

$2b$ = maximum width of the ellipse.

Heat transferred in elliptical regions was considered on the basis of the average area for the region:

$$q = \frac{k\bar{A}}{X} \Delta T$$

Radiative heat transfer was treated on the following basis

$$q = \frac{\sigma A_1 T_1^4 - T_2^4}{\frac{1}{\epsilon_1} + \frac{A_1}{A_2} \frac{1}{\epsilon_2} - 1}$$

The second part of the investigation, that of the heat loss from a pipe containing flowing mercury at elevated temperatures, was also analyzed by IBM code. This code investigated the flow of heat by conduction and/or radiation in any reasonably chosen combination. Thus, reflective and conventional fibrous insulation or any combination of these could be employed in the analysis.

Equation A was employed for the radiation heat transfer while the following equation was used for conduction:

$$q = \frac{2\pi KL}{\ln \frac{R_2}{R_1}} \frac{T_1 - T_2}{R_1}$$

Heat transfer across the fluid film was given by

$$q = hA_1 \theta - T_1$$

where h = appropriate film coefficient.

V. SUMMARY OF MERCURY BOILERS AND DISPOSITION

This section will serve to identify the various mercury boilers fabricated under the SNAP-I Program and to account for the final disposition of the test units. Table 6 lists the important test boilers and their ultimate use.

All mercury boilers from PBP 2 through 6 in Table 6 could produce superheated mercury vapor at 1300° F and 210 psia pressure. This was clearly demonstrated by boilers PBP 2, 3, 4, 5 and 5A while they were being operated in the boiler test facility. The coil lengths for PBP 2, 3, and 4, respectively, were 12.5, 19.0 and 23.7 feet. All coils contained turbulence promoters and produced superheated mercury vapor at the desired temperature and pressure. The phasing out of the SNAP-I Program discontinued all further mercury boiler development work.

TABLE 6
Summary of Fabricated Test Boilers

<u>Unit</u>	<u>Thermal Shutter</u>	<u>Coil Pressure Drop (psi)</u>	<u>Shield Block</u>	<u>Thermal Insulation</u>	<u>Biological Shield</u>	<u>Intended Test Program</u>	<u>Final Use</u>
Full-power boiler (Fig. 35)	No	125	No	Super X	None	Boiler test facility	Operated to destruction
Preliminary boiler prototype 1 (similar to Fig. 10)	Yes	50	No	13 reflec- tive shields	Yes	Boiler test facility	Operated to destruction
PBP 2 (similar to PBP 1 without 13 reflective insula- tion shields)	No	50	No	Potassium Titanate	No	Boiler test facility	Operated to destruction
PBP 3 (Fig. 3)	No	12	Mo	Potassium Titanate	No	Boiler test facility	Operated in dual boiler and thermal shock tests
PBP 4 (Fig. 44)	No	15	Tung- sten	Potassium Titanate	No	Dual boiler test	Delivered to TRW
PBP 5	No	15	Tung- sten	Potassium Titanate	No	Dual boiler test	Delivered to TRW
PBP 5A	No	15	Tung- sten	Potassium Titanate	No	Backup boiler for dual boiler test loop	Delivered to TRW
PBP 6	Yes	15	Tung- sten	Potassium Titanate	No	Thermal shutter and per- formance	Cancelled and modified for SNAP-IA heat transfer test
PBP 6A	Yes	15	Tung- sten	Potassium Titanate	No	Backup for tests on No. 6	Cancelled during fabrica- tion
Isotope boiler BSP 1	Yes	15	Tung- sten	Potassium Titanate	Double compart- ment	Isotope test program	Cancelled at 50% construc- tion

VI. CONCLUSIONS

Throughout the development period of the SNAP-I APU system, the mercury boiler program has maintained a steady progressive course to develop a simulated isotope boiler. Electrical heaters substituted for the isotope fuel, and prototype mercury boilers, were fabricated, installed in a simulated mercury loop, and operated at the design conditions specified for the APU system.

Some difficulty was encountered in arriving at a low total pressure drop per individual boiler. Table 6 illustrates the pressure losses encountered which ranged from 125 psi for a full-power boiler to 15 psi for the final half-power mercury boiler. Additional difficulty was experienced in splitting the mercury flow to the half-power boilers, without increasing the overall pressure drop by more than 10 psi. At the close of the program, however, operation of dual boilers could be performed with air control valves.

Another difficult operation was to deliver the high temperature mercury vapor to the turbine nozzle without cooling and condensing. Figure 25 is a Mercury Mollier Diagram which indicates the enthalpy values for mercury vapor and liquid. Values obtained from this chart are as follows:

<u>Pressure (psia)</u>	<u>Temperature (°F)</u>	<u>Enthalpy (Btu/lb)</u>
210	1300	163.0
210	1200	160.5
210	1100	158.0
210	1050	156.7
210	1025	156.1

The total enthalpy change from 1300° F vapor to the saturation vapor line at 1025° F is only 6.9 Btu/lb. Each half-power boiler delivered at design conditions 0.935 lb/min and, therefore, would only require a loss of 6.45 Btu/min to reach the saturation line at 1025° F. Because of this, all exit vapor lines were wrapped with electric heaters to maintain high wall temperatures and prevent excessive heat losses. In spite of these precautions, mercury vapor temperatures dropped 200° F and lower at the simulated turbine nozzle package. No further work could be performed

since this portion of the program was phased out, but heat losses in the exit lines are a decided problem that also need resolution.

In summary, approximately five simulated mercury boilers were manufactured and tested for successful performance. In each case, design conditions of 1300° F and 210 psia were reached in each prototype boiler. The first isotope mercury boiler was 50% fabricated prior to the SNAP-I phase out program.

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IX. NOMENCLATURE OF SYMBOLS

A	=	area normal to heat flow
Q	=	heat production rate
C	=	coolant heat capacity
G	=	coolant mass flow rate
r	=	radial position
R	=	boiler radius
D	=	equivalent diameter of tube
L	=	boiler length
J	=	product of dimension less heat transfer groups
H	=	evaporation enthalpy
σ	=	Stefan-Boltzmann constant
ϵ	=	relative emissivity
T	=	boiler temperature (a function of time)
G	=	mass flow rate per unit area
θ	=	surrounding temperature, coolant temperature
h	=	film coefficient due to convection (a function of time)
k	=	thermal conductivity
ρ	=	boiler density
u	=	coolant viscosity
c	=	boiler heat capacity
N	=	number of radial regions
S	=	volumetric heat production rate

t	=	time
x	=	thickness in the radial direction
X	=	axial position ($0 \leq x \leq L$)
Y	=	thickness in axial direction
Pr	=	Prandtl number = $\mu c/k$
Pe	=	Peclet number = $Re \times Pr$
Re	=	Reynolds number = DG/u
Nu	=	Nusselt number = hD/k