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AEC
RESEARCH
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DEVELOPMENT
REPORT

ENGINEERING DEVELOPMENT DEPARTMENT

QUARTERLY REPORT

JANUARY, FEBRUARY, MARCH, 1966

H. HARTY, et al.

APRIL, 1966

BATTELLE-NORTHWEST

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ENGINEERING DEVELOPMENT DEPARTMENT QUARTERLY REPORT

JANUARY, FEBRUARY, MARCH, 1966

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ENGINEERING DEVELOPMENT DEPARTMENT QUARTERLY REPORT†
JANUARY, FEBRUARY, MARCH, 1966

DIVISION OF REACTOR DEVELOPMENT AND TECHNOLOGY

CIVILIAN POWER REACTORS

FAST FLUX TEST FACILITY

Heat Transfer Studies - J. M. Yatabe

Cermet Fuel

Sintering of PuO_2 and/or the build-up of fission gas in a low density matrix may lead to gaps being formed around the PuO_2 pellets in the cermet fuel during irradiation. The resulting thermal barrier between the fuel particle and the stainless steel matrix of a cermet fuel will introduce a time delay in the negative reactivity coefficient due to fuel expansion. This could lead to catastrophic results under fast transient power insertions.

To determine the magnitude of the temperature rise in the fuel, an analytic model corresponding to a single fuel pellet and an associated matrix cell has been developed and programmed for the digital computer. This model assumes that at low fuel particle-to-matrix temperature differentials (less than approximately 400°F (222°C)), a gap exists between particle and matrix. The modes of heat transfer across the gap are by radiant heat transfer and conduction through the fission gases. At larger pellet-to-matrix temperature differences, the differential thermal expansion coefficients cause the particle to contact the matrix. The contact or interface pressure is a linear function of the temperature differential up to the yield strength of the softer material (stainless steel). By combining this relationship for contact pressure with the experimental measurements of interface heat transfer coefficients, tables have been added to the computer program which provide interface heat transfer coefficients for any condition of contact between fuel particle and matrix. Linking the gap conductance and the contact conductance relationships provides a complete history of the fuel-to-matrix heat transfer coefficients under all possible conditions.

†The classified activities are reported in BNWL-300 SUP.

Preliminary results based on transient step power increases of 2:1, 3:1, and 10:1 indicate that for combined gap heat transfer coefficients below 1000 Btu/hr-ft²-°F, the average matrix temperature would increase slowly as opposed to a sharp increase in the particle temperature. Contact due to differential thermal expansion would occur for such brief periods (approximately 1 μsec) that it would not significantly increase matrix temperatures.

Fuel Cycle Core Parametric Analysis - D. T. Aase

Basic Parametric Survey Results

During the current report period, a major parametric study was made utilizing the design and economics analogues for both the cermet and mixed-oxide driver fuels being considered for use in the FFTF. A description of the analogue simulations was presented in the previous quarterly report.¹ The purpose of the study was to estimate the fuel cycle costs for both cermet and mixed-oxide driver fueled cores over a range of reactor powers from 200 MW to 500 MW.

During this study, several of the design and operating parameters were held constant. The parameters common to all studies are:

T = Allowable Peak Transient Temperature = 4850°F

ΔT = Bulk Coolant Temperature Rise = 300°F

T_2 = Bulk Coolant Mixed Mean Outlet Temperature = 800°F

ΔP = Bulk Coolant Core Pressure Drop = 60 psi

Φ = Peak Neutron Flux = 1×10^{16} nv

The oxide power survey was made on the analogue computer. The resulting analogue data are shown in Figures 1 through 6. Additional output channels may be employed if desired for other design parameters. Each figure represents a value of transient overpower factor and assumed attainable burnup. The values considered were:

¹H. Harty, et al. Engineering Section Quarterly Report, Reactor and Materials Technology Department, October, November, and December, 1965, BNWL-219, January, 1966. (Confidential)

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BNWL-300

<u>Figure No.</u>	<u>Overpower Factor</u>	<u>(B.U. (Avg.))</u> <u>(MWD/T (U+P))</u>
1	1.5	100,000
2	1.5	45,000
3	1.25	100,000
4	1.25	45,000
5	1.0	100,000
6	1.0	45,000

FIGURE 1

FFTF Oxide Core Power Survey

FIGURE 2

FFTF Oxide Core Power Survey

FIGURE 3

FFTF Oxide Core Power Survey

$F_T = 1.23$
 $T_C = 4850$
 $\Delta T = 300$
 $T_2 = 800$
 $\Delta P = 60$
 $Bu = 45,000$
 $\phi = 1016$

FFTF Oxide Core Power Survey

FIGURE 5

FFTF Oxide Core Power Survey

FIGURE 6

FFTF Oxide Core Power Survey

Each strip in each figure represents a reactor power and core volume which yields 1.0×10^{16} nv peak neutron flux and a core subdivision or total number of subassemblies. The total number of subassemblies was subdivided as follows:

	61 Subassembly Core	91 Subassembly Core
Number of Control Rods	6	9
Number of Package Loops	1	1
Number of Small Closed Loops	2	3
Number of Large Closed Loops	2	3
Number of Driver Fuel Subassemblies	44	73
Number of Open Loops	6	2

The subdivision of the core was the same for both the cermet and oxide studies; a calculational error caused the number of effective power producing subassemblies to be about 10% high in the cermet studies.

The cermet power survey was run on the MIDAS program of the design and economics analogues. MIDAS, which stands for Modified Integral Digital Analogue Simulator, is used primarily to check the analogue simulations and for very limited survey studies. It can be programmed directly from the analogue diagram, but does not require scaling. Since the calculations in the simulations are almost entirely algebraic, the digital program gives better accuracy.

Due to the low thermal conductivity and slump potential of the oxide fuel type, the parametric calculations are constrained by a safety consideration:

The centerline fuel temperature is not permitted to reach the melting point within a factor of safety that is referred to as an overpower factor.

In other words, the reactor power, if increased by the overpower factor, would just raise the hot spot, centerline temperature to melting. It is important that this factor be established and that it not be overly conservative so as to bias the fuel cycle costs on the high side. On the

other hand, the very nature of the FFTF may require more restrictive fuel criteria than a conventional oxide fast breeder.

The calculation of fuel temperatures, of course, depends strongly on the thermal conductivity data employed. The data employed in these calculations for oxide fuel are shown in Figure 7. It represents data which have been utilized in PRTR analyses and is characteristic of vibrationally compacted fuel material. The $\int k dt$ to the melting point is near 75/watts/cm. Account is taken of in-core sintering by reducing the radial conduction path and by the discontinuity in the thermal conductivity curve showing the transition from 85% to 100% theoretical density.

A further consideration, as in the cermet case, is the attainable exposure that can be achieved with oxide fuel. Overpower factor can be traded off against burnup since the lower fission gas pressure and clad damage at lower exposures may permit containment of the fuel under higher temperature conditions.

As previously indicated, the penalty for going to small pin diameters in order to decrease the centerline temperatures is an increase in fabrication costs. On the other hand, with increasing pin diameter, cycle costs tend toward a constant minimum, since the attainable exposure is considered to be independent. However, a decrease in core height comes about because an increase in the number of pins per subassembly accompanies the increased pin diameter. This results because the linear heat rate is also maintained for a given centerline temperature so the pin length decreases. Figure 8 shows these effects:

The fuel cycle cost that results as a function of fuel pin diameter for an oxide fueled core of 690 liters, operating at 400 MW, yielding a peak flux of 10^{16} n/cm²-sec.

Two cases are shown here - one for an overpower factor of 1.0 and the other for 1.5. Also, two values of exposure are shown:

100,000 MW days per ton and 45,000 MW days per ton.

The number of subassemblies has been held constant at 91, but the height varies from 1.3 ft at a pin diameter of 0.26, to nearly 3 ft or so, at a diameter of 0.12 in. Again, the rise in fuel cycle costs at the smaller

FIGURE 7

Mixed Oxide Thermal Conductivity Data

FIGURE 8

FFTF Total Fuel Cycle Costs, $\$10^6$ vs. Fuel Pin Diameter, Inches

pin diameter is due to increased fabrication costs. In fact, the difficulty in fabricating an oxide pin of the order of 0.1 in. diameter may well result in a cost penalty even higher than that shown in the figure.

The differences in costs due to changing the required exposure from 100,000 MWD/ T to 45,000 MWD/ T are perhaps most significant. Although it appears that the penalty for increasing the overpower factor is not large, it must be pointed out that the H/D ratio for the dotted curve and the solid curve at the same pin diameter is not the same. Therefore, a tradeoff in core height is incurred that affects core test requirements, and perhaps control requirements, although the fuel cycle costs remain nearly the same.

The cycle costs decrease in the direction of increasing fuel pin size. Unfortunately, the penalty in core height becomes rather severe at these larger pin diameters. It would appear that, for a conservative oxide design, cycle costs are in the range of 3 to 4×10^6 /year for a core of this size with an H/D near unity.

The effect of overpower factor on cycle costs is perhaps better illustrated by considering reactors of differing size. An increase in the overpower factor at fixed burnup and reactor size causes a decrease in pin size and increased pin throughput and hence, increased fabrication cost. In the smaller reactor sizes, the pin diameter is already comparatively small, so that the cycle costs climb much more rapidly with overpower factor. These variations are illustrated in Figure 9 showing cycle costs for a range in overpower factor of from 1.0 to 1.5 and reactors with power levels of 300, 400 and 500 MW.

For the small cores, the increase in cycle costs is of the order of a factor of two over this range. The cost penalty for the larger cores is more on the order of 30% or so. Thus, the importance of setting fuel safety criteria realistically is important to minimizing operating costs.

Cermet Results

In evaluating the cermet core, the irradiation stability of the fuel is taken into account as shown in Figure 10. The relationship between attainable exposure and surface temperature of the cermet is shown. The

FIGURE 9

FTR Oxide Fuel Cycle Cost vs. Overpower Factor

FIGURE 10
Irradiation Stability of UO_2 -SS Dispersions

Irradiation Stability of UO_2 -SS Dispersions

centerline temperature is of the order of 100°F higher than shown on the abscissa. The tradeoff between fuel temperature and fuel exposure is seen to be very significant above 800°F. The solid line is the correlation used in the calculations. The concept of overpower factor does not strictly apply in the cermet as it did in the oxide, because attainable exposure limits the operating temperature far under the melting point.

The effect of pin diameter in the case of cermet fuel is the same as in the case of oxide fuel for small pin diameters; that is, the fabrication costs begin to dominate. However, in this case, since the fuel temperature is tied directly to the attainable exposure, the penalty for increased throughput at higher temperatures dominates the larger pin diameters. The net result is a minimum in cycle costs as a function of pin diameter.

Figure 11 shows the total fuel cycle costs as a function of fuel pin diameter for a core height of 2.8 ft, 61-subassemblies, 600 liters, operating at 300 MW_t to yield a peak flux of 10^{16} n/cm²-sec. In this case, the minimum fuel cycle costs of about $\$2.8 \times 10^6$ /year occur for a pin diameter of 0.16 to 0.20 in. diameter and 271 to 397 pins per subassembly. As the pin diameter decreases below 0.16 in., the fabrication costs begin to rise. On the other hand, as the pin size increases above 0.18 in., the fuel centerline temperature rises, decreasing the attainable exposure as shown along the right-hand scale. The increased throughput results in a rising fuel cycle cost. It is interesting to note the relatively high dependence of burnup (and centerline temperature) on pin diameter.

Figure 12 shows the same relationships with an increase in height to 3.1 ft and an increase in power to 370 MW at 900 liters. The minimum cycle costs are relatively unchanged, but the minimum occurs at a larger pin diameter. Thus, the dependence of cycle cost on reactor power does not appear to be a strong function of power level. This is demonstrated in Figure 13 where the fuel cycle costs as a function of pin diameter have been summarized for a range of reactor power levels from 150 to 600 MW_t. A plot of the locus of minima is shown to result in a rather broad minimum from about 250 to 350 MW_t. The H/D is constant at 0.9 and the peak neutron flux is held at 10^{16} n/cm²-sec. The minimum fuel cycle costs are seen to be near $\$2.7 \times 10^6$ /year. It may be further noted that the core subdivision varies discretely with reactor power.

FIGURE 11

FFTF Fuel Cycle Cost, \$10⁶/Year and Centerline Temperature, °F vs. Pin Diameter - Inches

FIGURE 12

FFTF Fuel Cycle Cost, $\$10^6/\text{Year}$ and Centerline Temperature, $^{\circ}\text{F}$ vs. Pin Diameter - Inches

FIGURE 13

FFTF Fuel Cycle Cost \$10⁶/Year vs, Reactor Power, MWT, H/D = 0.897

Comparative Results

Similar data have been developed for the oxide reactor designs, and it is of interest to compare cycle costs for the two fuel types. The results of Figure 13 may be compared with the locus of minimum fuel cycle costs for the oxide in Figure 14. The oxide design shown is one of conservative rating. That is, an attainable exposure of 45,000 MWD/ T and an overpower factor of 1.5 have been assumed rather arbitrarily. In all cases, the peak neutron flux is held constant at 10^{16} n/cm²-sec. The minimum fuel cycle costs are roughly a million dollars per year higher for the oxide, being near $\$3.6 \times 10^6$ /year as opposed to $\$2.6 \times 10^6$ for the cermet. These minima appear to fall in the 300 to 400 MWD/ T power range. However, latest results to be presented in the next quarter's report show somewhat higher cermet fuel cycle costs. These new costs are similar to the oxide costs shown.

The stretch capabilities of the two fuels is of interest. The fuels from Figure 9 have been reevaluated with proposed performance improvements to allow higher exposures and operating temperatures. These are shown in Figure 15. The improvement in the oxide case is assumed to permit an attainable exposure of 100,000 MWD/ T with the peak center point fuel temperature just approaching melting. The improvement in stainless steel cermet capability, on the other hand, is assumed to increase the burnup by 20% for a given fuel temperature. The smaller improvement for the cermet is due to the selection of less conservative initial conditions.

The data discussed previously have been represented at only one neutron flux, namely 10^{16} n/cm²-sec; it is worthwhile to examine the sensitivity of the cycle cost to neutron flux. The comparison is shown in Figure 16 where conservative and advanced fuel behavior are again illustrated for a 600-liter core. Since the increase in flux is brought about by increasing specific power, the penalties associated with increased fuel temperature are responsible for the rising cost. It appears that on the order of a million dollars per year is required to increase the flux by 20%. It may well be, however, that the value of an increase of this magnitude is greater than the increased cost. Not shown is the fact that the oxide core has some 30% less test volume. If a weighting of test volume were applied, the results would appear to favor the cermet.

FIGURE 14

FFTF Fuel Cycle Cost \$10⁶/Year vs Reactor Power, MWT; H/D = 0.7

FIGURE 15

FFTF Fuel Cycle Cost vs. Reactor Power

FIGURE 16

FFTF Fuel Cycle Cost vs. Centerpoint Flux

The evaluation of the results to date can only be semiquantitative. However, the several trends are quite well defined and are summarized here.

Fuel Pin Diameter

Diameter of the fuel pin is a factor in fuel fabrication cost since the difficulty of fabrication increases with decreasing diameter. If the core fuel density is maintained, the smaller pin diameter increases the number of pins and tends to increase the cladding fraction. On the other hand, the smaller diameter permits a lower operating temperature. Thus, the tendency for fuel with low conductivity is towards smaller pin diameters where high flux is a requirement.

Fuel Temperature

The fuel temperature is a limitation from the standpoint of attainable exposure as well as reactor safety. Fuel temperatures above given limits, depending on the fuel, tend to drastically reduce fuel life and hence, increase in specific power may be traded off against fuel life - an important consideration in attaining high neutron flux.

Fuel Exposure

Fuel and clad damage in the driver fuel can be a more serious limitation than reactivity lifetime, a situation common also to fast breeder reactors. Fission gas pressures and other damage mechanisms become significant at high burnups. Fuel throughput again is affected adversely where exposure is limiting.

Fuel Subassembly

The size of the fuel subassembly and the pins per subassembly are factors in the fabrication costs, increasing with finer core subdivision. The number of subassemblies and the pins per subassembly are constrained to discrete quantities in the practical case to fit geometrically regular patterns. This has the effect of quantizing fuel cycle costs.

Fuel Length

Fuel length, of importance in a test reactor to accommodate a wide range of test fuel length within the active zone, affects the coolant pressure drop across the core. Aside from pumping costs, if the pressure drop is fixed by

an upper limit on pump head, or mechanical constraint, then the fuel pin diameter must decrease to allow a larger flow area to counteract an increase in height.

Reprocessing

Driver fuel characteristics that influence reprocessing, handling during fabrication, and conversion costs must be accounted for in any fuel cycle comparisons. Materials that do not dissolve readily or require special head end treatment and remote handling are examples.

A number of conclusions are indicated from the results to date:

1. On a conservative basis, cermet fuel cycle costs appear to be less than those of oxide for a neutron flux close to 10^{16} n/cm²-sec.
2. The ultimate cycle costs of both fuels appear reasonably close. The stretch capability of each fuel type and the fuel safety criteria become extremely important in this regard.
3. From a standpoint of fuel cycle costs alone, the optimum cores for both oxide and cermet driver fuels are in a range of 300 to 400 MW_t.
4. Increasing flux by 20% near 10^{16} n/cm²-sec represents approximately a factor of 2 increase in fuel cycle costs.

It should be emphasized that constraints due to achievable test volume, reactor safety, and capital costs for each fuel system must be given weight in addition to the cycle costs to properly evaluate the driver fuel in total.

CONCEPTUAL REACTOR DESIGN STUDIES

Nitride Fuel Cycle Studies - J. F. Fletcher and J. Greenborg

Calculations have been completed in the study of the characteristics and the economic potential of uranium and uranium-plutonium mononitrides as fuel for a typical fast breeder power reactor concept. Earlier work in this study was reported in a previous quarterly report.² Significant results during this reporting period include a fuel cycle cost analysis based on private-fuel ownership and a final revision of physics data.

²H. Harty, et al. Engineering Section Quarterly Report, Reactor and Materials Technology Department, October, November and December, 1965, BNWL-219, January, 1966. (Confidential)

Fuel Cycle Costs

Fuel cycle cost comparisons for nitride and carbide fuels, reported last quarter, have been extended to a privately-owned fuel case. In accordance with a suggestion of the USAEC (Washington) Reactor Evaluation Staff, the fuel investment charges were taken as a flat 10% carrying charge on the time-average total fuel inventory. Capitalization charges, as in previous calculations, were taken as a 30-year amortization of the fabrication charges, plutonium loss charges, and shipping charges for the first core, as well as amortization of the 10% inventory carrying charge for the period prior to initial reactor startup. The amortization factor was taken as 13.265%, consisting of 6% interest, 6% insurance and taxes, and 1.265% "sinking fund" allowance (standard 30-year factor). Fuel cycle costs were computed on these bases for a nitride-fueled reactor, assuming cases where metallic and nonmetallic intermediates are used in nitride conversion, and for a "BNW carbide" reactor described by Combustion Engineering³ recalculated at Battelle-Northwest by the same computer techniques used for the nitride-fueled models, to provide more direct comparison between nitride and carbide.

The resulting fuel cycle costs are summarized in Table I. As was previously calculated for the leased-fuel case, the nitride fuels appear competitive with carbides if economical processes are developed for conversion of the fuel materials to the nitrides.

Additional comment appears warranted on the distinction between the "nonmetallic" and "metallic" nitride cases. Pure uranium and plutonium nitrides are currently produced (on a laboratory scale) by reduction of the source materials to metallic uranium and plutonium, superficially hydriding the metals, and reacting in a nitrogen atmosphere to form the nitrides. The "metallic" nitride case assumes this process to be applied on a "commercial" (full-reactor) scale. A significant cost factor in this process appears to be the reduction of plutonium to the metal, estimated at the Handbook value of \$1.50 per gram of plutonium.

³Liquid Metal Fast Breeder Reactor Design Study, CEND-200, Combustion Engineering, Inc., January, 1964.

TABLE I.

FUEL CYCLE SUMMARY - PRIVATELY-OWNED FUEL (carrying charge 10% on inventory)			
	Mills/kwh		
	(Nonmetallic) ^(a) NITRIDE	(Metallic) ^(b) NITRIDE	BNW ^(c) CARBIDE
Inventory Charge (10%)	0.4112	0.4112	0.4036
Reprocessing	0.2273	0.2273	0.2274
Pu Loss & U Makeup	0.0259	0.0259 ^(d)	0.0253
Shipping	0.0468	0.0468	0.0467
Fabrication	0.2648	0.4162 ^(d)	0.2768
Capitalization ^(e)	0.1239	0.1611	0.1271
Subtotal	1.0999	1.2885	1.1069
Pu Credit	(0.2628)	(0.2628)	(0.3400)
Net Cost	0.8371	1.0257	0.7669

(a) Nitride conversion through nonmetallic intermediates.

(b) Nitride conversion through metallic intermediates.

(c) "BNW Carbide" reactor is the 1000 MWe reactor described by Combustion Engineering (CEND-200), recalculated using the same computer programs used for the nitride cases.

(d) Assumes nitride process conversion losses equal for metallic and nonmetallic conversion process.

(e) Capitalization consists of fabrication charges, Pu loss charges, and fuel carrying charges (10%) for first core prior to reactor startup, amortized over 30-year period at 13.265% (6% interest, 6% taxes and insurance, and 1.265% "sinking fund" for 30-year amortization).

Several other processes have been proposed for nitride production but are not yet developed. Typical of such processes is the "carbothermic reduction" process assumed for the "nonmetallic" nitride case. In this process, the oxides (UO_2 and PuO_2) are mixed with carbon and heated in a nitrogen atmosphere to form the nitrides. This process appears to be potentially competitive with carbide conversion processes and was assumed for this study. As was indicated in previous reports, the difference in fuel cycle

costs for the two nitride cases is caused virtually entirely by the assumed \$1.50 per gram cost of plutonium reduction in the "metallic" nitride case.

Physics Data

Significant results which add to or modify the data presented in previous reports include:

Sodium Void Coefficient, $\% \Delta K$,
for Sodium Loss from:

	<u>Carbide Fuel</u>			<u>Nitride Fuel</u>
	<u>C.E.</u> ^(a)	<u>AEC</u> ^(b)	<u>BNW</u>	<u>BNW</u>
Total Reactor	0.0		-0.8	-1.4
Core and Axial Blanket				-1.0
Core Only	+2.4	+1.0	+1.5	+1.0
Inner Core Only				+2.9
Small Volume at Core Center				+0.031
$\% \Delta K/\text{liter}$				

(a) Liquid Metal Fast Breeder Reactor Design Study, CEND-200, Combustion Engineering, Inc., January, 1964.

(b) An Evaluation of Four Design Studies of a 1000 Mw(e) Ceramic Fueled Fast Breeder Reactor, COO-279, Chicago Operations Office, U. S. Atomic Energy Commission, December, 1964.

Median Fission Energies, kev

<u>Region</u>	<u>C.E.</u>	<u>BNW Carbide</u>	<u>BNW Nitride</u>
Inner Core	302	410	440
Outer Core	311	420	450
Radial Blanket	354	510	580
Axial Blanket	448	680	690

Median Absorption Energies, kev

Inner Core	92	115	150
Outer Core	110	132	185
Radial Blanket	52	65	80
Axial Blanket	34	38	43

Effort during this reporting period has been directed toward preparing the Nitride Reactor (and comparison reactors) physics data for publication. This included plotting in final form neutron spectra, absorption and fission spectra, power distributions, etc. A core neutron spectra plot is included (Figure 17) for general interest. Shown are the core (Region Number 1)

FIGURE 17

Comparison of CE Carbide, BNW Carbide and BNW Nitride Reactors - Core I Spectra

spectra: 1) reported for the Combustion Engineering carbide reactor⁴, 2) calculated for the BNW carbide reactor, and 3) calculated for the BNW nitride reactor. The C.E. reactor was calculated with a modified YOM⁵ cross section set and a diffusion theory code. The BNW reactors were calculated with the 26-group Russian set⁶ and a S_n transport code. The difference in spectral shape for the identical carbide reactors has been shown to be due primarily to differences in the cross section set (rather than the physics codes used). However, results using the Russian set should be better because this cross section set includes basic cross section data which became available subsequent to compilation of the YOM set. It is seen in comparison of Core 1 spectra of the BNW reactors that the nitride reactor has a harder spectrum. Although this has some advantage in enhancing the fast fission effect and in breeding, this benefit is nullified by high energy nitrogen capture.

NUCLEAR ELECTRIC PLANT CAPITAL COSTS VS. SITE LOCATION - F. J. Kempf

A study was initiated to determine the variation in capital cost of a nuclear electric plant as affected by geographic location. A 1000 MWe Boiling Water Reactor Plant⁷ was selected as the reference design. The sites under consideration include: Malibu, San Onofre, Connecticut Yankee, Dresden, Oyster Creek, Nine-Mile Point, Hanford, and a hypothetical AEC site. Turkey Point, south of Miami, Florida, and a site in Maine, as yet undefined, will also be considered to the extent allowed by availability of detailed site information.

An initial separation of the potentially significant differential costs from those which do not appear significant has been completed. This preliminary evaluation indicates the following:

⁴Liquid Metal Fast Breeder Reactor Design Study, CEND-200, Combustion Engineering, Inc., January, 1964.

⁵S. Yiftah, E. Okrent, and P. A. Moldover. Fast Reactor Cross Sections, Pergammon Press, New York, 1960, p 48.

⁶I. I. Bondarenko, et al. Group Constants for Nuclear Reactor Calculations, Consultants Bureau, New York, 1964.

⁷1000 Mw(e) Boiling Water Reactor Plant Feasibility Study, GEAP-4476, General Electric Company, APED, San Jose, California, February 14, 1964

Incremental plant construction costs associated with earthquake protection should not be a large cost factor although the sites under consideration are located in seismic-probability zones ranging from 0 to 3. Seismic zone 0 indicates no earthquake damage and zone 3 is associated with major earthquake damage. Local geological considerations can reduce the probability of major earthquake damage at a particular site. The reference reactor plant considers a seismic zone 1 design basis and only minimal structural modifications would probably be required for construction at any of the sites under consideration.

Local siting considerations will significantly influence differential costs between sites but dollar differences on some local factors will be impossible to establish. Variations in local labor rates at the sites under consideration are estimated to affect total direct construction costs by 3% at most. This estimate was obtained by selecting the hypothetical AEC site as a reference site with labor rates typical of western Massachusetts. Comparison of estimated direct construction costs at each site, based on local wage rates from the Bureau of Labor Statistics, showed direct costs at California sites to be largest and about 3% greater than at the reference site.

Legal and technical work associated with licensing activities and public relations costs, which can easily vary with population density and potential land use, are examples of the type of cost which is not readily established. In this study, it has been assumed that acceptance of an existing relatively low-power nuclear facility at each site implies acceptance of a 1000 MWe plant with its associated larger fission product inventories and decay heat energy.

Local land costs will be a significant differential cost factor if land requirements are based on AEC siting criteria, which suggests exclusion areas and isolation distances from population centers in relation to reactor size. For example, coastal property values can be in the \$20,000 to \$100,000 per acre range at southern California sites. Also, coastal sites located in relatively low population density areas may have transient population densities on weekends which significantly alter the site

compliance with specified criteria on distance from a population center. In high population density regions, increased capital costs for engineered safeguards may serve as a sound substitute for distance and land requirements.

Transportation facilities to each site should prove adequate and based on current shipping rate structures, incremental costs of transporting major equipment items to the various sites will probably not be a significant differential cost factor.

USAEC-AECL COOPERATIVE PROGRAM

Cross-Flow Mixing Between Parallel Flow Channels During Boiling - D. S. Rowe

Accurate prediction of boiling burnout in multirod bundle fuel elements is not possible because of difficulties in calculating the local flow and enthalpy throughout the bundle. This difficulty arises because of differences in coolant subchannel sizes in some fuel rod arrangements such as the 19-rod bundle. The shapes of these subchannels are such that the ratio of heat transfer surface area to flow area is not equal for all subchannels. This causes the coolant enthalpy in some subchannels to be much greater than in others. This imbalance in coolant enthalpy affects the subchannel flow distribution, especially during boiling conditions. Since the pressure drop is nearly the same in each subchannel in the rod bundle, there is an interchange of flow between any of the subchannels in which the gain in coolant enthalpy is not equal.

The purpose of this study is to obtain a method of predicting local flow and enthalpy conditions in each subchannel of rod bundle fuel elements, and to determine the effect of the interchange of flow on boiling burnout. The study consists of 1) the development of analytical techniques and 2) an experimental program to verify the analytical work and supply key information which can only be obtained from experiments.

Analytical Study

At the present time, there is very limited information concerning mixing in multirod bundles when two-phase flow is present. In two-phase flow, mixing occurs because of both turbulent mixing (as in single-phase flow) and the

diversion flow from one channel to another. In the current study, the equations of continuity, momentum, and energy describing cross-flow mixing in a multirod bundle were derived assuming the following:

- . Steady, one-dimensional, two-phase separated slip flow exists in each subchannel of the bundle.
- . Superimposed turbulent and diversion cross flow exist between the subchannel under study and immediately adjacent subchannels.

The resulting equations were nonlinear and only for very simple cases, such as two parallel channels and single-phase flow, have closed form analytical solutions been obtained. For a more general case, a numerical solution is required.

A digital computer program is being developed to solve these equations. This program will allow analysis of most any type of rod bundle fuel element since the layout of the fuel rods and flow channels is an input variable.

To facilitate development of the computer program, assumed forms of the turbulent and diversion cross flow are being used. It is assumed that a single value of turbulent intensity exists throughout the fuel bundle. This assumption permits calculation of a turbulent cross flow from the average cross-flow velocity between adjacent subchannels. The diversion cross flow is assumed to be a function of the square root of the adjacent subchannel pressure difference. Accurate values of the cross-flow resistance are not known; therefore, values computed from a simple friction model are being used. Both of these assumptions will be improved upon when the results of the experimental program are available.

Some numerical solutions to the cross-flow mixing equations have been obtained for two-parallel and five-parallel channels. These solutions were performed using the fourth order Runge-Kutta integration formulas; however, the solutions are not entirely satisfactory because of unstable behavior in some calculations. This unstable behavior was found to be dependent upon the value of diversion cross-flow resistance and step size used in the numerical integration. Several calculations have shown that this unstable behavior can be eliminated by choosing a sufficiently large value for the diversion cross-flow resistance and a small step size. The

calculations have also shown that the cross-flow resistance is a weak parameter and variations in it cause only small variations in the computed values. Since it is desirable to allow actual values of diversion cross-flow resistance and a reasonably large step size, this stability problem must be investigated further.

Experimental Program

Experiments are planned as part of this study to verify the analytical description of mixing and also to obtain values of the interchannel mixing. The electrically heated test sections to be used for these experiments will be constructed to simulate two adjacent subchannels in a 19-rod bundle. At the inlet of the test sections, flow will be introduced into the two subchannels through separate inlets. The total flow will be extracted from the outlet of each subchannel and condensed. By measuring the inlet and outlet flow and enthalpy, the amount of mixing will be determined. Pressure measurements at the inlet, outlet, and several intermediate points of each subchannel will be used to set the proper end conditions. Also, from the pressure drop measurements between the subchannels at the intermediate pressure tap locations, lateral flow resistance can be determined.

During boiling, both flow diversion and turbulent cross flow occur simultaneously. This makes measurement of the diversion cross flow difficult if the turbulent cross flow is large. To accurately measure the diversion cross flow, two different shapes of test sections will be used in the experiments (Figure 18). The first is one which simulates two adjacent subchannels formed by rods on a "square" arrangement as shown in the left portion of the figure. In this test section, flow diversion should be negligible because of the equal heat input and flow area of the subchannels. To measure the amount of fluctuating cross flow, a small amount of heavy water will be continuously injected into the inlet of one channel. Since heavy water has thermodynamic properties similar to light water, heavy water vapor and liquid would be transported between subchannels during boiling. By measuring the heavy water concentrations at the outlet of the two streams, the amount of turbulent cross flow during boiling will be determined.

The second test section shape will simulate two adjacent subchannels formed by rods on a "square" and "triangular" arrangement as shown in the right portion of Figure 18. In this test section, both types of cross flow will occur with diversion being significant during boiling. The values of turbulent cross flow determined from tests using the first test section shape will then be subtracted from these results to isolate the effects of mixing due to diversion.

Sizes Shown are Approximately 2-1/2 Times Actual Size

Figure 18, Test Section Shapes for Parallel Channel Experiment

Parallel Channel Reactor BoilingStability Study - D. S. Rowe and J. R. WordenDevelopment of Equations

In the previous Quarterly Report⁸, a description of the basic mathematical model being used for this study was presented. This included the equations for the system hydrodynamics and neutron-kinetics. Also included was a description of the method being used to compute fuel temperature and steam drum pressures. During this quarter, the hydrodynamics equations used for the model have been recast for actual use on the analogue computer.

The basic partial differential equations of continuity, momentum, and energy have been presented previously; however, they are presented again to show the treatment given to them for application to the analogue computer.

$$\frac{\partial G}{\partial x} + \frac{\partial p}{\partial t} = 0 \quad (1)$$

$$\frac{\partial G}{\partial t} + \frac{\partial}{\partial x} \left[\frac{\phi 2G^2}{\rho} \right] = -g \left[\frac{\partial p}{\partial x} + \left(\frac{f}{2Dg\rho} \right)_{SP} KG^2 + \rho \cos \theta \right] \quad (2)$$

$$\frac{\partial}{\partial t} [\rho h \phi_1] + \frac{\partial (Gh)}{\partial x} = \frac{q'}{A} + \frac{1}{J} \frac{\partial p}{\partial t} \quad (3)$$

Where the terms are defined as

A	Cross-sectional area
D	Hydraulic diameter
f	Friction factor based on all-liquid flow
G	Mass velocity, $[\alpha \rho_g + (1-\alpha) \rho_f] u_f$
g	Gravitational constant
h	Enthalpy, $Xh_g + (1-X) h_f$
J	Conversion factor
K	Two-phase friction multiplier
p	Pressure
q'	Heat addition per unit length
t	Time
X	Quality, $m_g/(m_f + m_g)$
x	Space variable

⁸H. Harty, et al. Engineering Section Quarterly Report, Reactor and Materials Technology Dept., October, November and December, 1965. BNWL-219 (Confidential)

- α Void fraction, $A_g / (A_g + A_f)$
 γ Slip ratio, u_g / u_f
 ρ Density, $\rho_g \alpha + \rho_f (1-\alpha)$

The subscripts f, g, and SP refer to saturated liquid and vapor, and single-phase fluid, respectively. The correcting functions ϕ_1 and ϕ_2 are defined as

$$\phi_1 = \frac{\rho_g \alpha h_g + \rho_f (1-\alpha) h_f}{\rho h} \quad (4)$$

$$\phi_2 = \frac{\rho u_f^2}{G^2} [\rho_g \alpha \gamma + \rho_f (1-\alpha)] \quad (5)$$

Both of these functions can be evaluated as functions of enthalpy and pressure using a void fraction correlation. The Armand correlation, as modified by Massena,⁹ is presently being used.

$$\alpha = \frac{(0.833 + 0.167 x) X_v}{X_v + (1 - X) v_f} \quad (6)$$

For the range of pressure being considered in this study, the functions ϕ_1 and ϕ_2 are primarily functions of the enthalpy above saturation enthalpy and only a weak function of pressure for the range of pressures expected. This is also true for the density (ρ). These functions may then be written as

$$\phi_1 = \phi_1 (h-h_f, p) \quad (7)$$

$$\phi_2 = \phi_2 (h-h_f, p) \quad (8)$$

$$\rho = \rho (h-h_f, p) \quad (9)$$

If the second term in equation (3) is expanded and combined with equation (1), another form of equation (3) results:

$$\frac{\partial}{\partial t} [\rho h \phi_1] - h \frac{\partial h}{\partial x} + G \frac{\partial h}{\partial x} = \frac{q'}{A} + \frac{1}{J} \frac{\partial p}{\partial t} \quad (10)$$

⁹W. A. Massena. Steam Water Pressure Drop and Critical Discharge - Flow A Digital Computer Program, HW-65706, June 17, 1960.

By making a difference approximation to the space derivative between points 1 and 2, we may write equations (1), (2), and (10) as

$$\frac{d\bar{\rho}}{dt} = - \frac{(G_2 - G_1)}{(\Delta_x)} \quad (11)$$

$$\begin{aligned} \frac{d\bar{G}}{dt} = -g \left[\left(\frac{p_2 - p_1}{\Delta_x} \right) + \left(\frac{f}{2Dg\rho} \right)_{SP} \bar{K} \bar{G}^2 + \rho \cos \phi \right] \\ - \frac{(\phi_2 G^2 / \rho)_2 - (\phi_2 G^2 / \rho)_1}{\Delta_x} \end{aligned} \quad (12)$$

$$\bar{\rho} h \frac{d\bar{\phi}_1}{dt} + \bar{\rho} \phi \frac{d\bar{h}}{dt} + \bar{h} (\phi - 1) \frac{d\bar{\rho}}{dt} + \frac{\bar{G}(h_2 - h_1)}{\Delta_x} = \frac{q'}{A} + \frac{1}{J} \frac{d\bar{p}}{dt} \quad (13)$$

where the bar ($\bar{}$) indicates the average values and Δ_x is the distance between points 1 and 2. Equations (11) and (12) or various arrangements of them are being used to set up the actual analogue computations; however, equation (13) is very awkward in this form. We can simplify it by evaluating the terms $d\bar{\phi}_1/dt$ and $d\bar{\rho}/dt$ and making appropriate assumptions. If we evaluate these total derivatives from equations (7) and (9), we find

$$\frac{d\phi_1}{dt} = \frac{\partial \phi_1}{\partial (h - h_f)} \left[\frac{dh}{dt} + \frac{\partial h_f}{\partial p} \frac{dp}{dt} \right] + \frac{\partial \phi_1}{\partial p} \frac{dp}{dt} \quad (14)$$

$$\frac{d\rho}{dt} = \frac{\partial \rho}{\partial (h - h_f)} \left[\frac{dh}{dt} + \frac{\partial h_f}{\partial p} \frac{dp}{dt} \right] + \frac{\partial \rho}{\partial p} \frac{dp}{dt} \quad (15)$$

In equations (13), (14), and (15), the terms involving the rate of pressure change are important only for very fast pressure transients. These are not expected to occur in this stability analysis; therefore, we assume that the terms involving dp/dt are negligible and that a reference pressure (p^*) may be used. We do, however, let the saturation enthalpy vary with local pressure when evaluating ϕ_1 , ϕ_2 , and ρ . Once initial computer calculations have been made, the validity of this assumption can be checked.

Applying the above assumptions to equations (13), (14), and (15), and combining the equations gives

$$\frac{d\bar{h}}{dt} = \frac{\frac{q'}{A} - \bar{G} \left(\frac{h_2 - h_1}{\Delta x} \right)}{f(\bar{h} - h_f, p^*)} \quad (16)$$

where

$$f(\bar{h} - h_f, p^*) = \bar{\rho} \bar{h} \frac{\partial \bar{\phi}_1}{\partial (\bar{h} - h_f)} + \bar{\rho} \bar{\phi}_1 + \bar{h}(\bar{\phi}_1 - 1) \frac{\partial \bar{\rho}}{\partial (\bar{h} - h_f)} \quad (17)$$

The function given by equation (17) was defined to eliminate many of the multiplications and function generators required for the analogue computer. Figure 19 shows that the behavior of this function is consistent with the functions used to compute it - primarily a function of the enthalpy above saturation enthalpy.

Analogue Computations

Complete block diagrams for the analogue computer have been completed and debugging has started. First runs to assess the validity of the model being used for these computations should be completed during the next quarter.

Evaluation of Zr-2.5 wt% Nb Pressure Tubing - P. J. Pankaskie

The general objective of this program is to evaluate Zr-2.5 Nb as a pressure tube material with reference to the tests and reactor experience that has been obtained on Zircaloy-2 pressure tubes. Continuation of the crack propagation tests on tubes in the cold worked condition and as hydrided to 250-300 ppm has produced the results tabulated below:

FIGURE 19

Function for Evaluating Simplified Enthalpy Transport Equation

CRACK PROPAGATION DATA FOR Zr-2.5 Nb ALLOY

<u>Specimen Identification</u>	<u>Specimen Condition</u>	<u>Test Temp. °C</u>	<u>Slot Length (in.)</u>	<u>Hoop Stress at Failure (psi)</u>	<u>Crack Length at Failure</u>
30-21	30% CW Nonhydrided	-30	zero	145,000	Brittle full length
30-20	30% CW Nonhydrided	-30	2-1/2	35,000	Brittle full length
30-19	30% CW Nonhydrided	-30	4	39,000	Brittle from one end of crack only
30-22	30% CW Nonhydrided	150	zero	105,000	Ductile
60-23	60% CW Nonhydrided	-30	zero	155,100	Ductile
60-22	60% CW Nonhydrided	-30	1-1/2	49,300	Ductile
60-25	60% CW Nonhydrided	150	zero	117,000	Ductile
30-15	30% Hydrided	R.T.	1-1/2	44,800	Brittle

These data show that the brittle crack propagation temperature is lower for the 60% CW material than for the 30% CW material. Metallographic examinations show the 60% CW tubing to have a hydride impurity level of less than 10 ppm while the 30% CW tubing has a hydride impurity level of about 30-40 ppm. It appears from these data that decreasing the hydride impurity levels also decreases the brittle fracture temperature.

A creep-rupture test has been started in the pressure tube creep apparatus installed in the 350°C corrosion loop. This test has run for 400 hours at a temperature of 280°C and a hoop stress of 44,000 psi. After 400 hours, the 350°C corrosion loop was shut down to terminate a corrosion test. During this cycle, the creep apparatus functioned satisfactorily. Some minor piping modifications, however, will be done during the loop outage to facilitate control and provide added precautions against specimen failure from external pressures during loop startup and shutdown. The use of a water column manometer between the output pressure taps of the differential pressure cells appears to be working satisfactorily. Based on preliminary

analysis, creep strains of about 0.3% to 0.4% were measured in this test. These strain values appear to be somewhat high and are based on pressure vs. strain calibrations obtained at room temperature. Calibration data at test temperature must yet be obtained.

Elevated Temperature Crack Propagation - P. M. Jackson

Evaluation of the critical length for propagation of a crack through the wall of a pressure tube has been limited to 150°C, the maximum temperature a blowout patch over the crack could be made to hold pressure. A crack propagation test consists of milling an axial slot of certain length partially through the tube wall. This reduced wall area is then split by pressurizing the tube until the web breaks. The crack so formed is further propagated by hydraulic action; however, this requires some type of patch or expanding "mouse" over the previously faulted crack.

A new method of inserting a snug fitting copper tube liner within the previously detected tube has been developed and has proved satisfactory at test temperatures to 300°C. The test specimen is prepared as shown in Figure 20. First, a thin-walled fully annealed copper jacket is fitted with a supporting patch of heavier stainless sheet metal. This copper liner with patch installed is then positioned within the specimen with the patch directly over the crack in the pressure tube. After the end closures are secured, the tube specimen is ready for elevated temperature crack propagation testing. The pressure required to deform the annealed copper is a small part of the total pressure and may be subtracted or neglected. The fracture strain of the annealed liner is greater than the fracture strain at the pressure tube; therefore, the tube fails first. The development of this liner has permitted the extension of the crack propagation tests to 300°C.

FIGURE 20

Crack Propagation Specimen Showing High Temperature Sealing Liner

APPLIED AND REACTOR PHYSICSPHOENIX FUEL PROGRAM - D. S. Trent

The MTR core configuration under consideration for the Phoenix Fuel experiment at present is a symmetric 3 x 9 partial plutonium load with eight control rod positions. This configuration consists of a central 3 x 3 plutonium load with 3 x 3 loads of standard MTR fuel boxes at either side.

The plutonium fuel box design which has thus far received the majority of attention is an arrangement having sixteen 80-mil fuel plates with 116-mil thick coolant channels. The fuel plate has a 20-mil clad thickness and approximately 40 mils of PuAl fuel. The standard MTR fuel box has the same size coolant channels but has nineteen 50-mil plates.

The different fuel box configuration causes hydraulic differences between the plutonium fuel boxes and the standard fuel boxes. Since the MTR operates at a near constant head (the available plenum-to-plenum pressure drop is essentially independent of core flow rate) and since the plutonium fuel boxes have a smaller overall flow area, they will accommodate less coolant flow than the standard MTR boxes. However, the net effect is an increase of the available plate pressure drop and, hence, an increase of coolant velocity through each coolant channel between fuel plates. This results from a decreased pressure drop in the portion of the fuel box external to the fuel plates. Calculations show that the coolant velocity in the plutonium boxes will be about 5% greater than in the standard boxes.

From the heat transfer point of view, the velocity increase is desirable in that it improves the film coefficient and yields lower bulk temperatures for a given heat flux. There is an undesirable effect in that an increase of pressure drop and coolant velocity across the fuel plates tends to decrease the saturation temperature of the coolant at the hot channel hot spot permitting local surface boiling to occur at a lower heat flux. Preliminary calculations reveal that the saturation temperature at the hot spot of a plutonium box is about 245°F where the corresponding temperature for the standard box is about 248°F.

Standard MTR thermal hydraulics calculations have been performed, with the aid of a computer program written especially for this purpose, to establish reactor power capability over a wide range of axial and horizontal power peaking factors. In all cases, it was assumed that the fuel element surface temperature is limiting at a nominal temperature of 245°F and that the maximum hot spot occurs at the point where 70% of the plate power has been dissipated to the coolant. The latter assumption was made in the absence of calculated axial power distributions for the plutonium-fueled core, and will be reexamined when these data are available. These results are illustrated in Figure 21. For an example case, the Engineering Physics Unit calculated a horizontal power peaking factor of 1.93 for a preliminary core design having no control rods and at startup. This corresponds to a reactor power of about 34 MW if the vertical power peaking factor is 1.5 and about 18 MW if the vertical peaking factor is 3.0.

The computer program has been altered to perform hydraulics calculations external to the fuel plates, along with plate hydraulics and heat transfer, so that different fuel box configurations may be investigated in a minimum time should the need arise.

REACTOR FUELS AND MATERIALS

IRRADIATION DAMAGE TO REACTOR METALS

Gas Loop Development - R. J. Evans

The model gas loop at PNL is a 2000°F, 500 lb/hr flow, helium recirculating loop that is approximately a 1/10 model of the ATR Helium Loop designed for installation in the Advanced Test Reactor (ATR) at Idaho Falls. One important function of the model loop is to support the design of the ATR loop. This function is nearly complete except for the gas heater. The heater design represents a significant departure from the commercial practice, therefore, a similar type of heater was built for the model loop.

A prototype gas loop heater was completed and installed in the model gas loop this quarter. This heater was designed and built to incorporate and

FIGURE 21

Reactor Power vs Horizontal Peak Factor

investigate essential details of a compact, two-stage, concentric heater being designed for the ATR gas loop. Major design features of this heater are described in previous Quarterly Reports.^{10,11,12,13,14}

When instrumentation work had progressed to the point where it was possible to check the system, the heater was turned on and operated at power levels up to approximately 1/2 power (40 kW). This operation was done at low flow conditions (65 lb/hr). Outlet temperatures of the heater were brought up to 2000°F (1093°C).

Since the water cooling system for the model loop is not complete, temporary water cooling was used in the initial high temperature run. After the cooling water system is finished, high temperature operation will continue, and gas flow, pressure and heater input power will be varied to determine the operating characteristics of the model heater.

ATR GAS LOOP OPERATION AND MAINTENANCE

ATR Gas Loop Heater - G. L. Fox

The ATR gas loop heater is being designed to heat helium flowing at a rate of 4140 lb/hr to temperature of 2100°F (1150°C). The heating is accomplished in two stages with electric resistance heating elements. The gas first passes through an annular ring of nickel-chromium tubes, makes a 180 degree turn, and then passes through a center core of molybdenum tubes. Both tube banks are supported with aluminum oxide ceramic. The heater internals are insulated from the outside conditions with metallic foil insulation.

¹⁰ Quarterly Progress Report, Metallurgy Research Section, October, November, December, 1964, edited by J. J. Cadwell, HW-84573.

¹¹ Quarterly Progress Report, Metallurgy Research Section, January, February, March, 1965, edited by J. J. Cadwell, BNWL-79, April 15, 1965.

¹² Quarterly Progress Report, Metallurgy Research Section, April, May, June, 1965, edited by J. J. Cadwell, BNWL-120, July 15, 1965.

¹³ Quarterly Progress Report, Metallurgy Research Section, July, August, September, 1965, edited by J. J. Cadwell, BNWL-166.

¹⁴ Quarterly Progress Report, Metallurgy Research Section, October, November, December, 1965, edited by J. J. Cadwell, BNWL-207.

There are metal liners within the heater which support the aluminum oxide and act as flow barriers. Since the expansion of the aluminum oxide is less than the metal liners, some gaps within the core will increase while other gaps will decrease. These gaps form paths so that the gas flow may bypass the heating elements. These bypass gas flows have been calculated and found small enough so that heater performance is not adversely affected.

A computer program has been written to study the heater performance over a range of flow conditions. This program was used to determine heater tube temperatures and pressure losses over the range specified by EBASCO specification M-3003 which defines the heater operating limits. The results state that the heater tube temperatures will stay below the design temperature of 2000°F (1093°C) for the nickel-chromium tubes and 3000°F (1649°C) for the molybdenum tubes provided the power split between the tube sections is adjusted for the inlet gas temperature. At the maximum flow rate of 4140 lb/hr and an inlet temperature of 1500°F (816°C), 44.4% of the power should be provided by the nickel-chromium tubes while 55.6% should be provided by the molybdenum tubes. At the low flow condition of 1800 lb/hr with an inlet temperature of 1300°F (704°C), 50% of the power should be provided by each tube section.

Further work is needed to define the problems during heater transient operation. This will provide information on startup and shutdown procedure, control requirements, and short term electrical power demands.

ENGINEERING DEVELOPMENT

CRITICAL FLOW AT HIGH TEMPERATURES AND PRESSURES - F. R. Zaloudek

The two-phase steam-water critical flow phenomenon is of particular importance in nuclear reactor technology because it imposes a condition which effectively restricts the quantity of coolant which can be transmitted through various piping geometries in boiling water and pressurized water reactors. Furthermore, it is the mechanism that limits the quantity of coolant which is lost to the environs through a leak or following a piping rupture. Therefore, it is important to have exact knowledge of the mechanism of this phenomenon in

order to economically design reactor cooling system piping, vapor suppression systems, and reactor confinement vessels, and to adequately analyze the hazards associated with reactor operation.

In the past 20 years, there has been considerable effort by a number of investigators to develop analytical methods to predict steam-water flow rates from various flow conduit configurations during a choked-flow condition. Most of the early attempts resulted in unsatisfactory results when compared to anything more than selective data. Recently, improved critical flow theories have been proposed, and many of these have been verified over limited ranges of variables.

One of the principal difficulties which arises in the application of these improved theories to nuclear reactors is the lack of their experimental verification in the range of pressures and temperatures common to high efficiency reactor systems. The purpose of this present program is to obtain experimental information in the range of temperature and pressures applicable to modern water-cooled reactor system practice. This information could be used to verify existing critical flow theories or to assist in the establishment of more improved theories.

In the current report period, critical flow data were gathered on three different pipe-type test sections:

- . 0.5 in. ID and 7 in. long
- . 0.5 in. ID and 14 in. long
- . 1.0 in. ID and 7 in. long.

These test sections were provided with wall pressure taps 0.0910 in. in diameter at locations approximately 0.025, 0.060, 0.090, 0.125, 0.250, 0.500, 1.00, 2.00, and 8.00 in. from the discharge end. A steam-water mixture of known enthalpy was caused to flow through a constant area flow passage to the atmosphere while measurements were made of flow rate and pressures along the flow passage. These pressures were extrapolated by graphical techniques to determine the pressure at the exit of the passage. This pressure is called the "critical pressure" and has been successfully correlated in earlier investigations with the mass flow rate and enthalpy of the steam-water mixture at the exit.

Water or steam-water mixtures of desired enthalpy and pressure were supplied to the test section by the auxiliary apparatus shown in Figure 22. One of the principal pieces of equipment in this auxiliary apparatus was an insulated 1000 gal, 3000 psi, 650°F pressure vessel in which water was pressurized, heated, and stored before discharge through the test section to the atmosphere. An air system was provided to maintain constant pressure in the vessel during the entire expulsion of the water through the test section. Maintenance of steady vessel pressures enabled steady-state experimentation and circumvented many of the measuring problems associated with "saturated blowdowns."

The water was carried from the bottom of the vessel to the testing area by a 4 in. Schedule 160 pipe. Before the water entered the test section, its flow rate was measured by a differential pressure flowmeter. The flowmeter was followed by a flash-flow valve. This valve regulated the quantity of flow so several test conditions could be investigated under constant vessel conditions. The transition section between the piping and the test section consisted of a 2 in. by 1 in. pipe reducer, 4 in. of 1 in. Schedule 40 pipe, and finally, a conical transition passage of 20 degrees total angle connecting the 1 in. pipe to the internal diameter of the test section.

The pressures along the test section and across the flow tube were measured by dc-excited strain-gage type transducers. The signals from the pressure transducers and from thermocouples installed upstream of the flowmeter were routed to a "medium speed" automatic data-acquisition system. Here, the signals were automatically analyzed, scaled, and then digitally recorded on an automatic typewriter and on punched paper tape.

Using the test section described above, the critical flow phenomenon was investigated for initial water enthalpies of 470, 505, and 570 Btu/lb. The pressure in the 1000 gal pressure vessel was maintained at least 100 psi over the saturation value by the introduction of high pressure air into the vessel. When equilibrium conditions were obtained during the "blowdown" of the vessel, the pressures at the test section wall taps were measured and the flow rate was determined. The pressures along the test section were subsequently extrapolated graphically to the end of the test section to find the "critical pressure."

FIGURE 22

Schematic Drawing of Test and Auxiliary Apparatus

The data gathered for the series of critical flow experiments on the 7 in. long 0.5 in. ID test section are given in Figure 23. It was assumed that the principal location of flow choking was at the end of the test section, and accordingly, this graph reflects an attempt to correlate the data in the conventional manner. The solid points in Figure 23 indicate the observed existence of a secondary upstream choking phenomenon which was reported earlier (HW-80535). In every case where downstream choking was observed to exist alone (i.e., critical flow in the conventional sense), the data agreed with the Fauske critical flow theory within 10% or better. When comparing the data obtained with the 0.5 in. ID, 7 in. long test section with the data obtained with the one 0.5 in. ID and 14 in. long, excellent agreement was observed. This indicated that the length of the 7 in. long test section was sufficient to eliminate metastable expansion behavior reported by other investigators.

This investigation will be continued during the next quarter by the study of the critical flow from a test section 1.939 in. ID and approximately 3 ft long.

PLUTONIUM RECYCLE PROGRAM

REACTOR ENGINEERING DEVELOPMENT

Heat Transfer Calculations for the PRTR Batch Core - G. M. Hesson

Calculations were made to determine the specific powers which would cause incipient melting in PRTR fuel rods and to determine the molten radius as a function of specific power. This information is needed to insure that the PRTR batch core meets the criterion of no significant melting.

If the best estimates of thermal conductivity of vibrational compacted UO_2 and of the contact coefficient between the UO_2 and the jacket are used, the calculations show that a 2% PuO_2 - UO_2 fuel will reach incipient melting at specific powers of 23 kW/ft. Calculations were also made using the lowest reasonable estimate of the values of the thermal conductivity and contact coefficient. These calculations give a specific power of about 20.5 kW/ft at incipient melting.

FIGURE 23

Recent Critical Flow Results. Solid Points Indicate a Possible Metastable Expansion Upstream of the Location of Critical Flow

Molten radii from these calculations were compared with molten radii measured from three fuel rods irradiated in the FERTF. The agreement is not as good as desired. The lack of agreement probably arises from uncertainties in the knowledge of the thermal conductivity of the vibrationally compacted powder and the fuel-to-jacket contact coefficient, and how these change as the fuel expands and contracts, melts and freezes, and sinters as the fuel rod undergoes thermal cycling. The lack of agreement also probably reflects uncertainties in the knowledge of the exact specific power at which the rods operated. There are, however, four data points from fuel rods operated at about 19 kW/ft which showed no melting. These may be compared with incipient melting specific power of 20.5 kW/ft calculated using the most conservative values of UO_2 conductivity and thermal contact coefficients. This indicates that while the calculations are not particularly successful in predicting molten radii, they do adequately predict incipient melting. Operation of the batch core at a specific power of 20 kW/ft or less should satisfy the criterion of no significant melting.

The administration of the requirement of no significant melting requires that the peak specific power in a fuel element be related to the tube power which can be measured. An equation was developed which accounted for uncertainties in the power calculation and accounted for the spatial power generation peaking in the fuel element to calculate a permissible tube power. For a specific power of 20 kW/ft, a maximum tube power of 1080 kW was calculated. The spatial power peaking factors are subject to change as new experimental data from the Plutonium Recycle Critical Facility and batch core critical tests provide better knowledge of the neutron fluxes in the reactor.

Heat Transfer Calculations for the FERTF - G. M. Hesson

Heat transfer calculations were made to determine the temperatures which might be expected in a pressure tube or a basket following the rupture of a molten core fuel element in the Fuel Element Rupture Test Facility (FERTF). Transient heat transfer calculations were made to determine the temperatures of a Zircaloy basket, a stainless steel basket, a Zircaloy pressure tube, and a Hastelloy "C" pressure tube might reach if they were contacted with molten UO_2 .

The model chosen for the calculations was a cylinder of molten UO_2 at 5500°F (3040°C), 1/2 in. in diameter and 0.3 in. thick protruding from a ruptured rod of the fuel element. It was assumed that the end of the cylinder would bridge the annular gap containing the coolant and would come to rest against the basket or pressure tube. It was assumed that no heat would flow into the UO_2 cylinder from the ruptured rod. Heat was assumed to flow from the circumference of the molten UO_2 to the coolant at a rate of $250 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$. The heat transfer contact between the UO_2 and the metal plate was assumed to be perfect. For the basket cases, heat was assumed to flow from the UO_2 into the basket and then to the coolant from both surfaces of the basket. A heat transfer coefficient of $5000 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$ was assumed for the heat transfer from the basket to the coolant. For the pressure tube cases, heat was transferred to the coolant only from the same metal face as was in contact with the molten UO_2 ; the opposite face was assumed to be adiabatic. The UO_2 was assumed to generate heat at a rate 50% greater than that of a rod operating at 30 kW/ft . The transient calculations were made using the TIGER V heat transfer code on the 1107 computer.

The calculations gave the temperatures in the pressure tube or basket walls within the UO_2 cylinder as a function of time. The calculations showed that there was a very steep temperature gradient in the metal of the pressure tube or basket and that 3/16 in. away from the molten UO_2 , the metal temperatures were within a few tens of degrees Fahrenheit of the coolant temperature. The metal temperatures reported in the succeeding portions of the report will be the average temperatures through the metal directly adjacent to the UO_2 .

The calculations show that a Zircaloy-2 basket would reach a peak temperature of approximately 1480°F (804°C) about a sec after the contact with the molten UO_2 , and then would decrease within the next 2 or 3 sec to a near steady-state value of 1100°F (593°C). At these temperatures, the basket is believed to have enough strength to retain the molten UO_2 . A stainless steel basket would reach a peak temperature of about 1200°F (650°C) which would decrease to nearly a steady-state value of 1000°F (540°C) within 6 to 8 sec.

A Zircaloy pressure tube would reach a temperature of about 1850°F (1010°C) about 3 sec after contact and would then level off to a temperature between 1800 and 1850°F (980 and 1010°C). At these temperatures, a 3.25 in. ID Zircaloy-2 pressure tube at 1100 psia pressure would be expected to fail rapidly.

A Hastelloy "C" pressure tube would reach a temperature of about 1600°F (870°C), and would level off at that value. At this temperature, a Hastelloy pressure tube would be expected to remain intact for several hours, if not indefinitely.

These calculations contain many assumptions which will affect the conclusions. One assumption that has a large uncertainty factor is the calculational model which describes the failure of a fuel rod such that molten UO_2 contacts a basket or pressure tube in the manner described. These assumptions prevent placing a high level of confidence in the accuracy of the temperatures. However, it is believed that the assumptions are reasonable, that a Zircaloy pressure tube would fail, and a Hastelloy pressure tube probably would not fail if it were contacted with molten UO_2 under the conditions used for the calculations. A Zircaloy-2 basket and a stainless steel basket would probably retain the molten UO_2 .

MATERIALS DEVELOPMENT

PRTR Pressure Tubes - M. C. Fraser

The PRTR pressure tubes were fabricated from Zircaloy-2 ingots by a forging-hot extrusion-tube reduction process. These tubes have a unique shape, one end is flanged and the other tapers to a reduced diameter. They also have a unique microstructure distribution; there is an annealed region between the taper and about tube mid-length and the remainder is cold worked. The ability of these tubes to sustain PRTR system loads with ever increasing neutron exposure has been a subject of study and testing since reactor startup. The following paragraphs describe the study and testing done during this reporting period.

Properties of Unirradiated PRTR Pressure Tubes

Crack propagation experiments were performed wherein unirradiated specimens were tested at temperatures ranging from -46°C to $+150^{\circ}\text{C}$. The slot induced fracture procedure¹⁵ was used. This involves milling of a slot 80% of the tube wall deep and then pressurizing until the remaining 20% of the wall fails and produces a crack equal in length to the slot. This crack is then caused to propagate by imposing pressure on the specimen. Propagation stress is defined as the maximum Lamé hoop stress that would be expected in the region of the slot had the slot not been there. Slot lengths of 0.465 in. and 2 in. were used for these experiments.

The data obtained are shown in Figure 24. They show that the 0.465 in. long crack propagated at hoop stress of 103,000 psi at -46°C , and with increasing temperature, the propagating hoop stress decreased to 89,000 psi at 95°C . At 150°C , the propagating hoop stress was 81,000 psi. The line joining these data points is straight between -46°C and $+95^{\circ}\text{C}$ and begins to bend downward between 95°C and 150°C . The fractures were partly at an angle of 45 degrees to the tube surface, (shear) and partly at 90 degrees to the tube surface (cleavage). An estimate of the amount of cleavage surface in each case was made and these values indicated a linear inverse temperature dependence between -46°C and $+95^{\circ}\text{C}$. At -46°C , the amount of cleavage surface was about 65%. This decreased to about 56% at $+95^{\circ}\text{C}$ and to 36% at 150°C . These data are shown on Figure 24.

The specimen which had a 2 in. long simulated crack failed by crack propagation at a hoop stress of about 16,000 psi and a temperature of 147°C . At room temperature, this length slot would have propagated at a hoop stress of about 30,000 psi.¹⁶ On Figure 24, it can be seen that the slope of the line joining these two points has the same slope as the line for the 0.465 in. crack data.

¹⁵R. C. Aungst and L. J. Defferding. Crack Propagation Tests on Normal and Hydrided Zircaloy-2 Reactor Pressure Tubing, HW-80567, August, 1964.

¹⁶M. C. Fraser. Postirradiation Evaluation of Zircaloy-2 PRTR Pressure Tubes - Part III, BNWL-5, January, 1965.

FIGURE 24

Effect of Temperature on Crack Propagation of Unirradiated PRTR Tube Specimens

These data indicate that the ability of the PRTR pressure tubes to resist crack propagation is lower above room temperature. For example, the data of BNWL-5 indicate that the crack length necessary for room temperature propagation at operating pressure lies between 6 and 10 in. Combining data from BNWL-5 and Figure 24 indicates that a 2.8 in. long crack would propagate at operating pressure and 150°C. In this example, the minimum conditions are a 2.8 in. long crack and 150°C. Additional testing of unirradiated and irradiated specimens to determine minimum conditions will be pursued.

TEST REACTOR OPERATION

PRTR Process Tube Inspection Cage - M. R. Kreiter

A movable viewing cage, which could be used while inspecting the installed PRTR pressure tubes, was designed, fabricated, installed, and tested. The installed cage is shown in Figure 25. It consists of guide poles on each side of the chair which are attached to outriggers mounted on the borescope housing. Vertical motion is attained through use of the building crane. Forward, backward, and sidewise motions are accomplished by movement of the fueling vehicle to which the borescope and cage are attached. "Safety dogs," mounted on the chair, prevent personnel injury due to accidental dropping of the cage.

NUCLEAR SAFETY

CONTAINMENT SYSTEMS EXPERIMENT

Facilities and Equipment - G. J. Rogers and C. E. Linderoth

At the end of the quarter, construction of facilities was estimated to be 69% complete and engineering design 91% complete. The ventilation systems and drain lines were completed sufficiently to permit aerosol transport studies. Other systems required for these tests, including the aerosol and liquid sampling systems, and the aerosol injection line, are still under construction.

Figure 25. Process Tube Inspection Cage

A scope study was completed and design efforts were started on a system for heating the contained atmosphere during leak tests. Scoping efforts on a dummy hydraulic reactor core and an external reactor simulator loop were started.

Aerosol Injection Methods - R. K. Hilliard, L. F. Coleman, and J. D. McCormack

Fission product aerosols to be used in mass transport studies in the CSE will be generated in a laboratory adjacent to the containment vessels and injected into the vessels at pressures up to 50 psig. This can be accomplished by either pressurizing the aerosol generation apparatus or by using a compressor with the generation apparatus at atmospheric pressure on the suction side. The latter method requires simpler equipment and is preferred because it affords greater personnel safety. One type of compressor which can be used is a steam jet. Jets were procured and operated against back pressures up to 56 psig. Four iodine aerosol containment tests were made in the ADF (Aerosol Development Facility) to determine whether injection method affected aerosol behavior. The results are summarized in Table II. The airborne behavior is shown graphically in Figure 26. Figure 27 compares the fractions removed by steam condensate as a function of time. Poor overall material balances were obtained on two of the runs, and the following conclusions are tentative pending further investigation:

- . No mechanical problems were encountered in any of the runs.
- . The airborne containment half lives were nearly identical for all three pressurized tests as shown in Figure 27.
- . Low overall recovery of iodine injected resulted in the two tests where jets were used. This may be related to the use of jets or it may be coincidental with the iodine preparation difficulties also experienced in these two runs.
- . As in all previous tests in the ADF, the initial airborne half life was inversely proportional to the steam condensation rate. Because of the relatively small volume of the ADF tank (0.9 m^3), the steam delivered by the jet itself caused excessive pressurization and high condensation rates. This effect would be minimal in the large CSE vessels.
- . A strange phenomenon occurred in Run 55. About 60% of the iodine in the condensate was in a water-insoluble form, as determined by filtering the condensate. In all other ADF

SUMMARY OF MIXED AEROSOL AND SINGLE-COMPONENT RUNS

Containment Atmosphere: 80 °C, 0 psig; Steam/Air Molar Ratio = 1.

COMPONENT	RUN 53 TOTAL IODINE	RUN 53 CESIUM	RUN 53 TELLURIUM	RUN 53 RUTHENIUM	RUN 53 BARIUM	RUN 50 TOTAL IODINE	RUN 46 CESIUM	RUN 47 TELLURIUM	RUN 52 RUTHENIUM	RUN 51 BARIUM
Mass Heated, mg	0.95	0.82	1.2	3.0	1.5	0.90	10.0	8.0	3.0	3.0
Mass Delivered to Tank, mg	0.91	0.53	0.73	1.22	0.74	0.47	9.3	2.6	1.0	2.2
Fraction Delivered to Tank	0.95	0.65	0.61	0.70	0.48	0.94	0.93	0.33	0.34	0.73
C_0 , Airborne Conc. @ t = 0, mg/m	1.0	0.60	0.81	1.36	0.82	0.53	10.2	2.9	1.1	2.4
Steam Condensation Rate, g/min	22	22	22	22	22	12	14.5	10.3	29	23
Initial Airborne Half Life, min	7.3	7	6	6	6	9	11	20	11	10
Material Balance, t = 30 min ^(a)										
Fraction Airborne	0.11	0.13	0.023	0.026	0.026	0.12	0.23	0.36	0.16	0.14
Fraction in Condensate	0.38	0.57	0.021	0.009	0.026	0.11	0.25	0.01	0.18	0.15
Fraction on Surfaces ^(b)	0.51	0.30	0.956	0.965	0.948	0.77	0.52	0.63	0.66	0.71
Material Balance, t = 60 min										
Fraction Airborne	0.007	0.005	0.002	0.002	0.003	0.012	0.013	0.187	0.025	0.019
Fraction in Condensate	0.555	0.825	0.034	0.018	0.053	0.034	0.440	0.043	0.205	0.263
Fraction on Surfaces	0.438	0.17	0.964	0.980	0.944	0.664	0.547	0.770	0.770	0.718
Material Balance, t = 300 min										
Fraction Airborne	3×10^{-4}	1×10^{-6}	3×10^{-7}	3×10^{-6}	3×10^{-6}	1×10^{-3}	2×10^{-7}	$< 1 \times 10^{-4}$	5×10^{-6}	1×10^{-6}
Fraction in Condensate	0.78	0.99	0.05	0.03	0.07	0.81	0.76	0.19	0.22	0.34
Fraction on Surfaces	0.22	0.01	0.95	0.97	0.93	0.19	0.24	0.81	0.78	0.66
Decontamination Efficiency ^(c)	0.12	~ 1	0.17	0.11	0.12	-----	0.28	1.2	1.3	-----
Fraction of Mass in Condensate ^(d) Removed by Filtering	0.01	0.02	0.5	0.99	0.5	0.005	0.02	0.15	0.25	0.01
Deposition Velocity at t = 15 min, cm/sec ^(e)										
Silver	0.027	0.022	0.040	0.012	0.026	0.039	-----	-----	-----	-----
S. S. 304	0.018	0.020	0.032	0.015	0.027	0.038	0.030	0.042	0.029	0.027
Carbon Steel	0.012	0.008	0.028	0.027	0.042	0.043	0.018	0.039	0.028	0.021
Teflon	0.012	0.015	0.014	0.018	0.045	0.012	0.017	0.028	0.024	0.015
Phenoline 302	0.033	0.027	0.054	0.028	0.031	0.055	0.030	0.031	0.052	0.037

(a) Material Balance Adjusted for Material Removed by Maypack Samplers.

(b) Mass Deposited on Surfaces was Determined by Difference.

(c) Fraction of Mass on Surfaces at t = 1200 Minutes Which was Removed by Various Decontaminating Sprays.

(d) Filtered Through Gelman Type A Glass Fiber Filter.

(e) 64 cm² Coupons Exposed for 5 Minutes at Mid-Time t = 25 Minutes.

TABLE II

BNWL-300

FIGURE 26

Effect of Injection Method on Airborne Behavior

FIGURE 27

Iodine Removal by Condensate - Effect of Injection Method

tests, including Run 58, also a jet injection, the iodine was essentially completely soluble. No satisfactory explanation for this unusual behavior has been found to date.

Aerosol Behavior - Single Component vs. Mixture - R. K. Hilliard, L. F. Coleman, and J. D. McCormack

An important consideration in developing a synthetic fission product aerosol for mass transport studies is the number of components required for an aerosol with transport properties similar to that released during an actual accident. Five containment tests were made in the ADF stainless steel tank using single-component aerosols of iodine, cesium oxide, tellurium oxide, barium oxide, and ruthenium oxide. Then, in Run 53, all five were generated, mixed, passed over molten stainless steel clad UO_2 , and injected into the containment vessel. Test conditions for the six tests were similar except for the type of aerosol. Table III lists the pertinent observations, and allows a comparison of behavior of each material alone with the same material when other aerosol materials were present. Figures 28 to 32 show the mass distribution in containment (airborne, condensate, and surface deposition) as a function of time. The comparison is good, dissimilarities being attributed to slight differences in test conditions, especially steam condensation rate. The primary conclusion is that the behavior of any of these five aerosol materials is not significantly affected by the presence or absence of other aerosol materials. A second conclusion is that tellurium, barium, and ruthenium behaved similarly, being largely particulate, depositing on solid surfaces and not washing off with continued steaming. Iodine and cesium, on the other hand, were essentially soluble in the steam condensate; after an initial rapid deposition on surfaces, they were removed by the condensate upon continued steaming.

Decontamination of Coatings - J. D. McCormack

Prior tests have shown that steaming for extended periods of time is an effective technique for removing iodine deposited on painted surfaces. Coupons coated with several paints are being tested by exposure to steam in an autoclave to determine the effects of time and temperature on the coatings in order to develop information needed for the experiments in the CSE vessels. At temperatures of 110°C and 127°C, blisters formed in the paint used in the

TABLE III.

COMPARISON OF AEROSOL INJECTION METHODS

Injection Method	<u>Run 55</u>	<u>Run 56</u>	<u>Run 57</u>	<u>Run 58</u>
	Jet	Pressurize after delivery	Generated at Con- tainment pressure	Jet
Containment Conditions				
Pressure, psig	25	25	25	0
Temperature, °C	116	116	116	80
Steam/Air molar ratio	1.75	1.8	2	0.9
Inlet Aerosol Form				
Filter %	98.7	19.2	4.4	93.3
Ag-Screens %	1.0	80.0	93.2	4.2
AC Char. Paper %	0.2	0.8	2.4	2.3
Char. Bed %	0.1	0.06	0.01	0.2
Initial Airborne Half Life, min.	~0.9	~1	~0.5	~3
Steam Condensation Rate, ^(a) g/min.	240	170	50	100
Final Mat'l. Bal., %	0.14	0.09	0.2	1.1
Removed by Maypacks	0.14	0.09	0.2	1.1
Removed by coupons	0.41	0.01	0.5	0.1
Removed by condensate	30.85	96.5	97.3	28.0
Recovered in tank wash	<u>11.85</u>	<u>2.97</u>	--	--
TOTAL Accounted For	43.25 ^(b)	99.56	98.0	29.3 ^(b)
Deposition Velocity at 15 min., cm/sec				
Silver	0.173	0.107	0.092	0.047
S.S. 304	0.390	0.026	0.018	0.006
Carbon Steel	0.048	0.022	0.030	0.014
Teflon	0.018	--	0.012	0.006
Phenoline 302	0.120	0.069	0.165	0.023

(a) During the first 5 minutes.

(b) I₂ preparation was abnormal.

CSE, but these blisters did not crack or peel at times up to 600 hours.

At 141°C, blisters formed after 40 hours and these, in turn, began to crack and peel after 120 hours.

FIGURE 28

Iodine Aerosol Behavior - Single-Component vs. Mixture

FIGURE 29

Cesium Aerosol Behavior - Single-Component vs. Mixture

FIGURE 30

Tellurium Aerosol Behavior Single-Component vs. Mixture

FIGURE 31

Barium Aerosol Behavior - Single-Component vs. Mixture

FIGURE 32

Ruthenium Aerosol Behavior - Single-Component vs. Mixture

Multichannel Analyzer Code - N. P. Wilburn

A Digital Equipment Corporation PDP-7 has been interfaced to four Nuclear Data 180-F 256-channel analyzers. This system is used to perform the large number of radioisotopic analyses which will result from the aerosol experiments performed in the CSE.

All functions of normal multichannel analyzers have been reproduced by suitable coding. In addition, a weighted least-squares method for estimating online the contribution of individual radionuclides in a multi-component pulse height spectrum has been incorporated. The isotopic analysis of the sample is obtained within 20 sec after the counting is complete, and the analysis report will appear as follows:

ANALYZER #1

RUN NO.	SAMPLE ID	SAMPLE LOC	SAMPLE TIME
A1-235	M4F	AXZ	2360
NUCLIDE	COUNTS	REL STD ERROR	DPM
Ce-137	53235	.013	432.75
I-131	45729	.047	527.62

The meaning of the data reported is:

ANALYZER - Which analyzer is reporting.

RUN NO. - A six-character run identification.

SAMPLE ID - A three-character sample mnemonic.

SAMPLE LOC - A three-character coordinate which locates position of sample in the vessel.

SAMPLE TIME - Time after some reference time at which sample was collected.

NUCLIDE - Isotopes for which analysis was made.

COUNTS - Counts in sample resulting from nuclide.

REL. STD. ERROR - Standard deviation of counts divided by counts.

DPM - Disintegration/minute for nuclide.

The report is indented and bracketed to make it easy to pick out from the rest of the typewritten information used to control the analyzers.

REACTOR SAFETY ANALYSIS AND EVALUATION

Ultrasonic Monitoring of Bursts Tests - T. E. Michaels

At the present time, an ultrasonic listening technique appears to be very promising for the ultrasonic monitoring of crack growth. This technique employs a preamplified pickup transducer. Signals have been received and recorded from burst tests of A106B pressure vessel steel tubing. The signal band width varies from 50 KC to 10 mc. The recordings are being analyzed to establish a systematic failure pattern for A106B.

Testing Small Vessels - D. R. Ireland

An experimental investigation of failure in low alloy steel pressure vessels is in progress. The scope of this program is to provide information that is quantitative and readily incorporated into reactor safety analysis and design. The program will evaluate the effect of irradiation on the fracture toughness of multiaxial loaded pressure vessel steels and the failure of model steel pressure vessels. Although NDT temperature data are not the primary purpose, the materials evaluated in this program will be tested for this data so that the results of this program can be correlated with the published irradiation effects data.

The pressure vessel model being used in the first part of the program is a cylinder of approximately 2.5 in. OD, 0.25 in. wall thickness, and 14 in. long. The size of the model will be increased as the program progresses. This model is termed a subsize-vessel specimen. The subsize-vessel specimens are loaded by internally pressurizing with a liquid under closed end conditions. The materials currently being tested are ASTM A106B and ASTM A212B. Testing and failure analysis will be done on these materials until the validity of the approach has been proven, then other materials will be included in the program. The A106B subsize-vessel specimens are made from seamless pipe and the A212B subsize-vessel specimens are made from plate. The A212B specimens have two full-length longitudinal welds.

Two A212B subsize-vessel specimens were irradiated in the M3 position of the ETR and are ready for testing. Two more A212B subsize-vessel specimens will be inserted in the M3 position of the ETR during the reactor

cycle beginning in April, 1966. Testing of the irradiated specimens will begin during the next quarter.

During this report period, a modified drop-weight tear test was applied to samples made from unirradiated A106B and A212B subsize-vessel specimens.^{17,18} The NDT temperature of the A106B material is -40°C and of the A212B materials is -30°C . These results of the modified test agree with published results of standard tests of these materials. The drop-weight specimens of these materials indicated a $\pm 3^{\circ}\text{C}$ sensitivity to full specimen width crack propagation near the indicated NDT temperatures. This sensitivity is much greater than would be expected for a standard impact test. The reliability of this test and the apparent sensitivity will be checked by additional tests on other material and correlations with Charpy impact data.

The major effort to date in this program has been to optimize the amount of useful data obtained from a single subsize-vessel specimen. The elastic-strain energy release rate function (G) of longitudinal through-wall thickness cracks in subsize-vessel specimens is being determined by compliance testing procedures. This function will be used to determine the fracture toughness (G_c or G_{Ic}) of the irradiated specimens. This function will also describe the failure of subsize-vessel specimens in terms of standard laboratory test specimen data. The expansion of this function to include larger pressure vessel models and other crack orientations will provide a method of analytically describing failure of larger pressure vessels in terms of standard specimen material properties.

The compliance testing is the measuring of the elastic expansion of a subsize-vessel specimen as a function of internal pressure. The current compliance testing is done at ambient temperatures. The specimens are placed inside a chamber filled with water and the water, displaced by the

¹⁷R. C. Aungst. Quarterly Progress Report Metallurgy Research Section, July, August, September, 1965, BNWL-166, p. 6.9.

¹⁸R. C. Aungst. Additional Crack Propagation Tests on Zircaloy-2 Reactor Pressure Tubes, BNWL-SA-179, May 3, 1965.

elastic expansion of the specimen, is forced to rise in a burette. The volume displaced is measured to an accuracy of ± 0.001 ml. The relationship between internal pressure p and volume change of specimen or volume of water displaced ΔV is

$$p \cdot K = \Delta V ,$$

where K is the compliance. The elastic strain energy E of the expanded specimen can be found from the integral equation

$$E = \int_0^{\Delta V} p \, d(\Delta V) .$$

Evaluating the integral in terms of the compliance yields

$$E = \frac{\Delta V^2}{2K} .$$

The elastic strain energy release rate with respect to crack area A is

$$G = \frac{dE}{dA} ,$$

and can be represented as

$$G = \frac{p^2}{2t} \cdot \frac{dK}{dL} ,$$

where t is the specimen wall thickness and L is the crack length.

For a given material and specimen geometry and compliance K is a function of only the crack length. Values of K are being measured for several different crack lengths and the function f for

$$K = f(L)$$

will be found by a least squares fit to a polynomial equation. The order of the polynomial will be determined by the best fit to the measured data. The function f will be analytically and experimentally developed into a general form that will account for variations in specimen geometry and size. The first objective of the compliance testing is to develop the G function for the irradiated A212B subsize-vessel specimen. This function is being evaluated from tests on unirradiated A106B and A212B specimens. The

function will be sufficiently developed by the end of this fiscal year to be used for obtaining G_{Ic} or G_c values from the destructive tests of the irradiated A212B specimens.

During this quarter, several specimens were compliance tested. The important results of the compliance testing work this quarter are the improvements in equipment, testing procedure and data analysis. A major problem in the testing of subsize-vessel specimens, with through-wall thickness cracks, is the prevention of pressurizing fluid (water at ambient temperatures) leaking. A major leak will prohibit the pressurizing of the specimen. Several patching techniques used in the Hanford pressure tube testing programs were tried. Although these techniques did stop the leaking satisfactorily, the stress state in the crack area was significantly altered by the pressure of the patch. The effects of two of these techniques on the compliance data are shown in Figure 33. The reproducibility of compliance data with these patching techniques is not satisfactory. A new patching technique that appears to be satisfactory for the compliance testing is currently being used. The technique consists of using a thin-walled metal sleeve inside the test specimen. The pressure between the sleeve and the specimen is less than the pressurizing fluid pressure, but this reduction in pressure can be calculated.

Because of the sensitivity of compliance testing equipment, the volume change measurements are prone to errors caused by variations in atmospheric pressure and water temperature. These errors are being compensated by measuring the water temperature and atmospheric pressure during the compliance test and appropriately adjusting the data. The specimens are tested before and after a longitudinal crack is introduced in the specimen. A compliance test run consists of measuring the height of water in the burette, at 200 psi internal pressure increments, from zero pressure to a maximum pressure and back to zero pressure. Five such runs constitute a compliance test for a given specimen. The maximum pressure is less than 65% of the estimated specimen failure pressure. For most specimens, a minimum of 200 pressure-volume change measurements are used to determine the specimen's compliance K . Computer techniques are used to digest the

FIGURE 33

Example of Variation in Compliance, of Subsize-Vessel Specimens,
Caused by Method of Sealing the Crack

pressure height of water data, curve fit the data to a straight line, and compute the slope of the line. The slope of the line is the compliance K.

The specimen geometry must be accurately measured and accounted for when comparing the compliance of one specimen with the compliance of another specimen of slightly different wall thickness and specimen length. Ultrasonic testing techniques and equipment were adopted to measuring the subsize-vessel specimen wall-thickness, at any position on the specimen, to an accuracy of ± 0.0005 in. A specimen geometry factor Ψ is currently being evaluated so that

$$\frac{K_1}{\Psi_1} = \frac{K_2}{\Psi_2},$$

where the subscripts denote two different specimens geometries. It is expected that Ψ will have the form

$$\Psi = \frac{b^2 (b^2 + a^2)}{(b^2 - a^2)} \cdot H,$$

where b is the outside radius, a is the inside radius, and H is a specimen length factor. The value of H is a function of specimen length and end effects. The end effects are the results of restricting the outside radius at each end of the specimen from changing when the specimen is pressurized. This geometry factor Ψ will be evaluated and in use during the next quarter. A tentative geometry factor Ψ has been evaluated and some examples of the use of this factor on specimens of A106B without cracks are listed in Tables IV and V.

TABLE IV.

TYPICAL COMPLIANCE VALUES FOR SPECIMENS WITHOUT CRACKS

<u>Specimen Number</u>	<u>$K \times 10^{-4}$ in.³/psi</u>	<u>$\Psi \times 10^{-4(a)}$ in.³</u>	<u>$K/\Psi \times 10^{-4}$ psi⁻¹</u>
736	15.58	127.0	0.123
737	15.70	125.8	0.124
740	15.39	126.0	0.122
735	15.17	125.5	0.121
536	14.77	119.2	0.124
537	14.57	117.0	0.124
540	14.54	117.8	0.124
535	14.23	116.8	0.122

(a) Tentative geometry factor.

Some examples of the effect of a 2 in. long crack on the compliance of a specimen are shown in Table V.

TABLE V.

TYPICAL COMPLIANCE VALUES FOR SPECIMENS WITH AND WITHOUT A CRACK

Specimen Number	2a (in.)	S (in.)	$K \times 10^{-4}$ in. ³ /psi	$\Psi \times 10^{-4}$ in. ³	$K/\Psi \times 10^{-4}$ psi
540	0	11.5	14.54	117.8	0.124
540	2.0	11.53	15.39	118.2	0.130
740	0	14.06	15.39	127.8	0.121
740	2.0	13.97	16.37	127.0	0.129

Ψ = Tentative geometry factor

S = Specimen length

2a = Crack length

OTHER WORK

HIGH TEMPERATURE LATTICE TEST REACTOR DEVELOPMENT

Safety Rods - M. R. Kreiter

Changes in the HTLTR safety blade design were required when excessive boron transport from the proposed B₄C poison material was noted during Mock-up Run Number 4. Search for an alternate material resulted in the concept of co-precipitating rare earth oxides (e.g., Gadolinia and Europia) with nickel. Samples have been obtained from a vendor and are now undergoing laboratory testing.

Oscillator Development - D. H. White

Work was started to develop and fabricate the two mechanical oscillators for the HTLTR. Both performance and dimensional criteria for the oscillators were evaluated - first separately, as 1) the mechanical mechanism, 2) auxiliary equipment pieces, and 3) the drive system, and second - as a combined, integrated system.

It was decided that a number of modifications would improve the operational performance. Since estimates indicated that savings in fabrication

would pay for these improvements, modifications on all equipment, except the valves, were initiated.

As a parallel effort, a number of drive system concepts were evaluated. These were:

- . Hydraulic stepping amplifier.
- . Hydraulic hydrostatic drive.
- . Electrical dc, variable-speed motors, SCR controlled.
- . Mechanical power amplifier coupled to a stepping motor.
- . Variable-speed-drive utilizing an oil shear, constant slip, opposed clutch arrangement.
- . Hydraulic chain or cable type cylinders.

The ability of a drive system to meet the performance criteria was established; however, the ability of a particular drive system to meet the budget requirements was in doubt. The evaluation of these systems was continued.

A modified Key (a variable length, long handled, manually operated wrench used to attach the oscillator ram to the reactor sample piece) was designed and fabrication was started.

Design of the two carriages (platforms to hold the oscillator mechanism and incorporating three degrees of movement) was modified and fabrication was started.

Design details for the oscillator proper were completed and drafting of the assembly was started. A pretensioned cable was chosen for the rotary-to-linear power transmission system.

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