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ANALYSIS OF SAFETY CONSIDERATIONS FOR TRANSIENT
TESTING OF PBF PROTOTYPE FUEL RODS IN TREAT

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ANALYSIS OF SAFETY CONSIDERATIONS FOR TRANSIENT
TESTING OF PBF PROTOTYPE FUEL RODS IN TREAT

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ABSTRACT

A description of the experiment equipment and an analysis of the safety considerations pertinent to the transient testing of PBF prototype fuel rods in TREAT are presented in this report. The analysis includes an investigation of the nuclear effects of inserting the experiment in the TREAT facility and a summary of the consequences of postulated accident situations.

ANALYSIS OF SAFETY CONSIDERATIONS FOR TRANSIENT
TESTING OF PBF PROTOTYPE FUEL RODS IN TREAT

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1.0 INTRODUCTION

A series of transient tests in TREAT has been proposed⁽¹⁾ to proof-test three different fuel rod prototypes for the Power Burst Facility (PBF). These tests involve subjecting these prototypes to multiple transients at energy densities comparable to those specified in the PBF design, and single transient tests at energy densities 150% greater. Additional single transient tests are proposed on fuel rods having intentional defects. The test program will culminate with the selection of the most promising prototype for an endurance test consisting of one hundred transients, and a single transient test of the selected prototype having intentional defects.

2.0 DESCRIPTION OF EXPERIMENT EQUIPMENT

2.1 Fuel Rods

2.1.1 Rod Design

The basic design of the fuel rods to be tested is shown in Fig. 1. Each rod will contain 18 sintered fuel pellets 0.56 inches in diameter and 1 inch long. There are three basic variations in fuel pellet design that will be tested in TREAT. The first pellet type will be pure UO_2 having a layer of calcia-stabilized zirconia, 57 to 60 mils thick, coated on the pellet circumference. The second pellet type will be pure UO_2 having a sleeve of calcia-stabilized zirconia fitted around its circumference with a 6 mil minimum radial clearance. The sleeve will be 50 to 52 mils thick. The minimum density of the pure UO_2 pellets will be 10.31 grams per cm^3 and the uranium will be enriched to 6.0 ± 0.2 atom % in U^{235} . Therefore, each fuel rod will contain approximately 40 grams of U^{235} . The third type of fuel to be fabricated for the final test series will incorporate a more advanced fuel composition consisting of a ternary mixture of UO_2 , ZrO_2 , and CaO . The inclusion of the zirconia and calcia in the fuel mixture is intended to reduce the U^{238} content of the fuel while retaining the same geometry and an equivalent heat capacity as obtained with the pure UO_2 fuel. The Doppler coefficient of the fuel may thus be varied without seriously affecting its heat capacity. The fuel rods made with ternary fuel pellets will incorporate either the coated or sleeved type zirconia insulator, whichever is determined superior from preliminary testing.

The fuel pellets and thermal insulators will be encased in a type 304 stainless steel tube, 0.75 inches OD by 28 mils thick. A nominal radial clearance of 5 mils will be provided between the thermal insulators and clad tubing to allow for radial expansion. A stabilized zirconia insulator pad 62 mils thick will be provided at each end of the fuel pellet stack to restrict axial heat transfer from the

Fig. 1 PBF Prototype Fuel Rod

pellets. End caps, made of type 304 stainless steel, will be inserted into the tube ends and seal welded. A compressible spring will be provided at the upper end of each fuel rod to hold the fuel pellets in position during handling and shipping, as well as permitting up to 0.63 inches axial expansion of the fuel during transient testing. All fuel rods will be filled with helium at 1 psig prior to final seal welding. The welded fuel rods will be leak tested in a vacuum chamber, and a leak rate greater than 10^{-9} cm³/sec will be sufficient cause for weld rejection. Following fabrication, each fuel rod will be gamma-scanned to check the homogeneity of its U²³⁵ loading. Each fuel rod will also be X-rayed to determine the amount of axial clearance above the fuel pellets and also the relative positions of the fuel rod components.

2.1.2 Heat Transfer Considerations

The majority of the transient tests will produce a peak energy density of 210 calories per gram of UO₂ in the fuel pellets, which corresponds to a peak fuel temperature of 2500°C. However, two tests are proposed which will produce a peak energy density of 315 calories per gram, resulting in fuel melting and an estimated peak fuel temperature of 2830°C, calculated by extrapolating the empirical relationship for enthalpy change developed by Conway, et al⁽²⁾, where T is °C:

$$H_{T^{\circ}\text{C}} - H_{25^{\circ}\text{C}} = -4.593 + 7.919 \times 10^{-2}T - 9.734 \times 10^{-6}T^2 + 5.030 \times 10^{-9}T^3$$

Since the heat of fusion of UO₂ is approximately 59.3 cal/g, the energy available to raise the temperature of the UO₂ is 315 - 59.3 or 255.7 cal/g, which from the above equation corresponds to a peak temperature of 2830°C. The radial temperature distribution within a fuel rod has been calculated using the HEAT-1 thermal conduction program on an IBM 650 computer. This program calculates the temperature-time history at various radial locations within the fuel rod. The model used for calculating the temperature distribution consists of four radial regions, viz., fuel, thermal insulator, fuel rod cladding, and moderator-coolant water. The calculations ignore radiation losses, and also the thermal resistances of the clad-coolant interface and the helium gas gaps within the fuel rod. The initial temperature of the water annulus surrounding the fuel rod is assumed to be 20°C and the water is assumed to have an infinite heat capacity after it reaches 100°C. While the model does not provide an exact simulation of the true behavior, it does represent a conservative approximation since the computed heat fluxes are greater than those that would be observed in an actual transient.

For this analysis, the radial distribution in power density has been assumed to follow a hyperbolic cosine distribution with a power-density depression factor

Fig. 2 Temperature Distribution in Fuel Rod after 80 msec TREAT Transient

of 0.75 at the fuel pellet centerline. For a peak energy density of 210 cal/g, the average energy input is approximately 194 cal/g which corresponds to an adiabatic fuel pellet temperature of 2350°C. The rate of energy input was assumed to follow the empirical expression, where t is time in seconds:

$$P/P_0 = e^{12.5 t}$$

The thermal properties of UO_2 , having 95% theoretical density, were taken from the data reported by Conway, et.al.⁽²⁾ and Lyons, et.al.⁽³⁾. The empirical expressions for thermal conductivity, k , and specific heat, c_p , were adapted to a form accommodated by the HEAT-1 program, and resulted in the following expressions where T is °C:

$$k = 0.894 T^{-1} + 0.01037 - 5.658 \times 10^{-6} T + 1.278 \times 10^{-9} T^2 \text{ cal/cm} - \text{sec} - ^\circ\text{C}$$

$$c_p = 0.664 + 2.328 \times 10^{-4} T - 1.88 \times 10^{-7} T^2 + 7.8 \times 10^{-11} T^3 \text{ cal/cm}^3 - ^\circ\text{C}$$

For the ZrO_2 thermal insulators, having 70% theoretical density, the thermal conductivity and specific heat were adapted from data reported by Goldsmith, et.al.⁽⁴⁾, resulting in the following empirical expressions, where T is °C:

$$k = 4.0 \times 10^{-3} + 1.0 \times 10^{-6} T \text{ cal/cm} - \text{sec} - ^\circ\text{C}$$

$$c_p = 0.403 + 4.432 \times 10^{-4} T - 2.32 \times 10^{-7} T^2 \text{ cal/cm}^3 - ^\circ\text{C}$$

Fig. 2 shows the radial temperature distribution across a fuel rod immediately following the completion of an 80 msec transient, ie, at 0.29 seconds after initiation of the transient. Since there is some heat transfer from the outer portion of the fuel pellet during the transient, the peak fuel temperature of 2500°C occurs approximately 53 mils from the outer surface. The calculated temperature difference between the temperature peak and the surface of the fuel pellet is approximately 975°C. This large temperature gradient will cause a severe tensile thermal stress to exist at the cooler outside surface of the fuel pellet. Cracking of UO_2 fuel pellets has been theoretically predicted to occur for center-to-surface temperature differences of 73°C or greater, and has been observed in tests having central temperatures of 600°C for center-to-surface temperature differences of 130 to 160°C⁽⁵⁾. In other tests⁽⁶⁾ similar cracking has been observed in uninsulated UO_2 pellets subjected to power transients in TREAT. Thus, cracking of the pellets in the PBF fuel rod prototypes is expected to occur on a gross, macroscopic scale, but is not expected to be progressive with thermal cycling. The performance of the fuel pellets is one of the primary items of interest in the proof-tests of these prototypes. A second curve shows the radial distribution at a time when the temperature difference

Fig. 3 Temperature vs. Time in Fuel Rod for 80 msec TREAT Transient

between the fuel centerline and the fuel surface is a maximum. The calculated maximum temperature difference of 1455°C occurs at about 10.5 seconds after initiation of the transient.

A maximum thermal gradient of 1485°C will exist momentarily across the thermal insulator during the time interval immediately following the transient. This thermal gradient will create a stress of the order of 80,000 psi in the insulator which exceeds the tensile strength of 20,000 psi given for cubic phase zirconia at room temperature. Cracking of the thermal insulator is expected, but as in the case of the fuel pellets the cracking is not expected to materially affect the performance of the insulator. However, the ability of the insulator to function effectively under such transient thermal conditions is a primary purpose for the conducting of this test series and the data obtained will provide valuable information regarding the required clearances between the pellets, insulators, and clad, as well as an indication of the effectiveness of the design in limiting heat transfer to the clad.

Temperature-time histories calculated for an 80 msec TREAT transient are shown in Fig. 3. It should be noted that the maximum temperature differences within each fuel rod component occur at different times. These temperature differences are of importance and are shown in the following table.

<u>Fuel Rod Component</u>	<u>Maximum Δt, °C</u>
Fuel Pellet	1455
Thermal Insulator	1485
Clad	70

Fuel rod temperatures were not calculated for those transient tests which produce fuel melting, since the values of specific heat and thermal conductivity have not been established for molten UO_2 .

2.1.3 Stresses in Fuel Clad

The stainless steel clad of the fuel rod will be subjected to stresses due to internal pressure in the fuel rod and thermal gradients in the cladding. The internal pressure may come from four sources; (1) heating of helium gas introduced into the rod during fabrication, (2) fission gas released from the fuel during operation, (3) UO_2 vapor pressure from fuel in the molten or vapor phase, and (4) mechanical forces due to fuel expansion and movement. The helium gas will be present at 1 psig at ambient temperature. If the small amount of gas in the radial clearances between the fuel insulator and clad is neglected, the gas is assumed to be heated to 2800°C, and the free volume is reduced due to the

* shortest period anticipated for tests

axial expansion of the fuel pellets, the resulting helium pressure would be about 500 psig. Fuel burnup will be very low for all transients with a maximum of 1.5 Mw-sec per gram of U^{235} for the 100 transient endurance test, and the fission gas release from the fuel is negligible. Data for the vapor pressure of UO_2 vary widely. Using the empirical expression⁽⁷⁾ given for UO_2 at temperatures above 2000° K, the vapor pressure corresponding to 2830°C is about 0.1 atmosphere. Therefore, the total internal pressure for the most severe transient proposed will be about 500 psig. The stress caused by this pressure loading may be computed from the expression⁽⁸⁾:

$$\sigma_p = \frac{p d}{2 h} = \frac{500 (0.75)}{2(0.028)} = 6700 \text{ psi}$$

where the symbols are as defined in reference 8. This stress value is about a factor of 4.5 below the minimum yield stress of 30,000 psi for the clad material.

The maximum temperature gradient across the cladding has been calculated to be 70°C which causes a maximum tensile thermal stress at the outer surface of the clad. Since the clad tubing is a thin walled cylinder, these stresses may be computed from the expression⁽⁹⁾:

$$\sigma_t = \frac{E \alpha (t_1 - t_2)}{2(1-\mu)}$$

where the symbols are as defined in reference 9.

$$\sigma_t = \frac{27 \times 10^6 (9.6 \times 10^{-6})(70)(9/5)}{2(1-0.3)} = 23,300 \text{ psi}$$

The maximum combined pressure and thermal stress is thus 30,000 psi which is equivalent to the minimum yield stress of the clad. Based upon Miller's treatment⁽¹⁰⁾ of combined thermal and pressure stresses under cycling conditions, it is unlikely that permanent growth (plastic flow) of the clad will occur due to this loading. In this regard, experience in prior tests in TREAT reported by Field⁽⁶⁾ indicates that clad failure does not occur at heat fluxes and energy transfer rates considerably in excess of the values predicted for PBF fuel tests.

In addition, it is possible that the fourth source of loading, thermal expansion and motion of the fuel pellets and thermal insulators, may exert mechanical forces on the clad. If radial or axial expansion of the fuel pellets and thermal insulators exceeds the limitation provided in the design, an additional pressure load may be applied to the cladding. In order for cladding growth to recur on subsequent cycles, the stress due to internal pressure and mechanical forces would have to increase from 6700 psi to about 25,000 psi.

2.2 Experiment Capsule

2.2.1 Capsule Design

The experiment capsule consists of a cylindrical pressure vessel equipped with a TREAT gripping fixture at its upper end, and a support tube and bottom fuel element fitting at its lower end. The exterior of the experiment capsule has the same shape and dimensions as a TREAT standard fuel assembly, except that the central portion is cylindrical rather than square. The capsule is shown in Fig. 4.

The pressure vessel is made of Type 347 stainless steel, seamless tubing, ASTM-A269, and has a 3.5 in. OD, a 2.75 in. ID, and a length of 62 in., including its top head closure. The top head is 3 in. thick and is bolted to the pressure vessel with sixteen capscrews, 3/8-16 UNC. A stainless steel-asbestos gasket seals the top head in a tongue and groove joint. The bottom head has a hemispheric interior and a flat exterior, and is 0.93 in. thick at its center. The pressure vessel is designed for continuous service at 3200 psig and 705°F in accordance with the ASME Code for Unfired Pressure Vessels, Section VIII. All welds have been radiographed and inspected with dye penetrant after the root pass and after the final pass. The vessel has also been hydrotested at 6000 psig and 70°F.

The pressure vessel contains the fuel rod(s) to be tested, a tubular aluminum filler piece subassembly, and the water heat sink. The filler piece subassembly supports and positions the fuel rod(s) within the pressure vessel, and reduces the annular thickness of water surrounding the fuel rod(s). It is suspended from the top head, and has ample clearance at its lower end for differential expansion. The sections above and below the fuel rod(s) are slotted to permit natural circulation of the water coolant.

The experiment capsule is designed for transient testing of two different fuel rod arrangements: (1) a single fuel rod, and (2) a cluster of three fuel rods. In both arrangements, the fuel rods will be centered within the TREAT core. A cross-section of the single rod arrangement, taken at the midplane of TREAT, is shown in Fig. 5. The fuel rod is centered at its top and bottom by aluminum grids within a 2.25 in. OD by 1.25 in. ID aluminum filler piece. The grids also contain holes to permit natural circulation of water. The filler pieces above and below the grids are made of 2.25 in. OD by 2.0 in. ID aluminum tubing. Fig. 6 shows the cross-section for the three rod cluster. The rods are arranged on a 0.78 in. triangular pitch and are centered by aluminum grids within a 2.25 in. OD x 2.0 in. ID aluminum filler piece. In both configurations the capsule bottom head is protected

Fig. 4 Experiment Capsule - Schematic

Fig. 5 Cross-section of Single Fuel Rod Configuration

ALUMINUM
FILLER
PIECE
(2.0" I.D.-
2.25" O.D.)

347 SST
PRESSURE
VESSEL
(3.5" O.D.-
2.75" I.D.)

FUEL ROD
(0.75" O.D.-
0.78 PITCH)

WATER ANNULI

Fig. 6 Cross-section of Three Fuel Rod Configuration

from damage due to contact with molten fuel by a type 304 stainless steel screen, 0.5 in. thick, located about 3.1 in. below the bottom grid. The screen is 1.63 in. above the capsule bottom head and contains 140 holes, 1/16 in. diameter, which improves the transmission of pressure pulses to the pressure transducer attached to the bottom head.

The pressure vessel contains demineralized water which serves as a heat sink and moderator. With a water level 2.25 in. above the top of the fuel rods, the pressure vessel contains 3.34 lbs. of water for the single rod arrangement, and 4 lbs. of water for the three-rod arrangement. A 29 in. vapor space is provided between the water level and pressure vessel top head to accommodate the volumetric expansion of water at 705°F. The vapor space will be pressurized with helium at 20 psia by means of a high-pressure needle valve attached to the top head.

2.2.2 Pressure Containment Capability

The experiment capsule has been designed for continuous service at a static internal pressure of 3200 psig at 705°F, in accordance with the ASME Code for Unfired Pressure Vessels, Section VIII. At static pressures exceeding these design conditions, the capsule capabilities have been calculated from the Lamé equation for thick-walled cylinders⁽¹¹⁾:

$$P_i = \frac{\sigma (b^2 - a^2)}{a^2 + b^2}$$

where the symbols are as given in reference 11. Using minimum yield strength and ultimate strength values at 70°F and 705°F, the following pressure capabilities are calculated:

<u>Temp. °F</u>	<u>Yield Pressure, psig</u>	<u>Rupture Pressure, psig</u>
70	7,100	17,800
705	5,200	14,000

The previous calculations of yield and rupture limits for the experiment capsule are the values appropriate to steady state pressurization. However, the pressure generation internal to the capsule as a result of heat transfer from the fuel rod to the moderator-coolant water during a reactor transient is a transient pressure spike. Considerable research oriented toward establishing the relationship between static and dynamic rupture strength of materials is currently in progress. In an earlier work Tardiff and Erickson⁽¹²⁾ report that at strain rates of 1000 sec⁻¹ the yield strength of mild steel is about a factor of three greater than the static yield strength and that while the ultimate strength also increases with increasing strain rate, the rate of increase is lower than that for the yield strength.

Following the formulation of Newmark⁽¹³⁾, a relationship has been developed by Alvy⁽¹⁴⁾ for the static load equivalent of a given transient pressure pulse. The parameter:

$$P_{\max}/q = \frac{(2\mu - 1)^{0.5} T}{\pi t_1} + \frac{(1 - \frac{0.5}{\mu})}{1 + \frac{0.7T}{t_1}}$$

is used as a measure of the tolerable overpressure, where:

$P_{\max}$ is the maximum permissible transient pressure,

q is the equivalent static pressure,

μ is the ductility of the structural material used,
(here defined as the deformation at rupture divided by the deformation at yield),

T is the natural period of vibration of the vessel, and

t_1 is the time of loading resulting from the transient pressure pulse.

A reasonable value for the ductility of type 347 stainless steel is $0.5/.002 = 250$. Thus, for a type 347 stainless steel pressure vessel the ratio $P_{\max}/q$ is about a factor of eight for a pulse duration, t_1 , equal to the natural period, T . For shorter duration pulses the ratio increases, and when $t_1 = 0.1T$, the ratio is about seventy.

Alvy gives for the approximate natural period of vibration the following relationship⁽¹⁴⁾:

$$T = 2\pi r \left[\frac{M}{Eg} \right]^{1/2}$$

where:

T is the natural period of vibration, seconds

M is the density of material, lb/cu in.

r is the average radius, inches

E is the modulus of elasticity, lb/sq in.

g is the gravitational constant, 386 in/sec²

For the TREAT experiment capsule the natural period is thus:

$$T \sim 2\pi (1.56) \left[\frac{0.29}{(28 \times 10^6)(386)} \right]^{1/2}$$

$\sim 50 \text{ } \mu\text{sec}$

Hence, for this application if the time of loading, t_1 , is less than 50 μsec , the vessel will have a dynamic rating about a factor of eight above the static rating. An alternate formulation by Chiapetta and Hodge⁽¹⁵⁾ yields similar results.

Despite the difficulties inherent in precise prediction of the duration of pressure pulses generated during operation of a system of this type, an improvement in capsule containment capability with decreasing pulse duration times (ie, sharper rising pulses) is indicated. Further, for pressure pulses that have a duration

appreciably exceeding 50 μ sec, the effects of pressure relief at the gas-water interface within the capsule will affect the effective pressure at the pressure vessel wall. The magnitude of the relief afforded is a complex function of geometry, pulse shape, and pulse duration and must be balanced against the reflected pressure contributions from metal-water interfaces which, in general, will act to increase the pressure at the vessel wall. For long duration pulses the effects of pressure relief at the gas-water interface will predominate. In particular, the acoustic transit times for pressure relief to be sensed at the pressure vessel wall at the core midplane is less than 1 msec. For pressure pulses having half widths as long as 10 msec, the effects of liquid displacement at the interface will be a dominant factor in determining the pressure level at the pressure vessel wall. Hence, for long duration pressure pulses the effects of liquid displacement at the free surface act to limit the pressure level at the vessel wall, and generation of pressure levels of sufficient magnitude to damage the vessel is improbable. For short duration pressure pulses the dynamic strength of the vessel is considerably better than the static strength, and for pulse duration times of 50 μ sec, the dynamic rating of the vessel is about a factor of eight higher than the static rating.

During the course of conducting metal-water tests in TREAT, stainless-steel-clad UO_2 fuel samples have been tested at energy densities up to 655 cal/g. The attendant pressure spikes had peak levels up to 670 psi⁽¹⁶⁾. In other tests⁽¹⁷⁾ stainless steel- UO_2 cermet fuel pins were tested at energy densities up to 796 cal/g with pressure levels of 960 psi, which was the highest level observed during these tests. Hence, on the basis of the operating experiences at TREAT, the pressure level anticipated (<1000 psi) in the proposed test series is approximately one order of magnitude below the yield strength of the experiment capsule.

An additional factor of interest is the energy content of the pressure pulse, since even when the pressure level exceeds the rupture limit, the vessel can fail only if sufficient energy is available to deform it to the point of rupture. The approximate static fracture energy per unit volume of the vessel wall is given by Lessels⁽¹⁸⁾ as:

$$E_{\text{fracture}} = (\sigma_y + \sigma_u) \frac{\epsilon}{2}$$

where σ_y , σ_u and ϵ are, respectively, the classical yield strength, ultimate strength and unit strain of the material. Using the minimum values appropriate to type 347

stainless steel, the energy required for fracture is:

$$E_{\text{fracture}} = \left[(30,000 + 75,000) \frac{\text{lbs}}{\text{in}^2} \right] \left[\frac{0.5 \text{ in.}}{2 \text{ in.}} \right] \left[1.64 \times 10^{-3} \frac{\text{cal/cm}^3}{\text{in. lb/in}^3} \right]$$

$$= 43.2 \text{ cal/cm}^3$$

Since there are 23.75 cm^3 of stainless steel in a 1 cm length of the pressure vessel wall, the appropriate fracture energy per cm of vessel length is 1026 cal/cm. By way of comparison, the corresponding fracture energy for the autoclave used in the TREAT metal-water tests is approximately 720 cal/cm.

The peak energy densities specified for the proposed tests are 210 and 315 cal/g for the single fuel rod configuration and 210 cal/g for the three fuel rod configuration. The total heat energy generated per cm of rod length is shown in the following table. For purposes of comparison, the heat energy value for a 778 cal/g transient in TREAT metal-water studies⁽¹⁹⁾ is included.

Configuration	Nuclear Energy cal/cm	Chemical Energy cal/cm	Total Heat Energy cal/cm	Maximum Energy In Pressure Pulse cal/cm
TREAT UO_2 pins, 778 cal/g	4,170	10	4,180	251
Single Rod 210 cal/g	3,470	3	3,473	208
Single Rod 315 cal/g	5,200	13	5,213	313
Three Rods 210 cal/g	10,400	10	10,410	625

All energy values given are for a 1 cm length of fuel rod. The chemical energy source used is that reported for tests conducted with stainless steel clad- UO_2 samples in TREAT⁽²⁰⁾ in which the reaction proceeded to 1.2% completion for an energy density of 200 cal/g in the UO_2 core and 5.4% completion for 300 cal/g. The maximum energy associated with the pressure pulse was calculated from the SL-1 data as follows. The SL-1 pressure vessel was a cylindrical vessel with a nominal ID of 52 in. and was filled with water to a nominal depth of 7 ft above the core. The pressure vessel and core are shown schematically in Figure 7. The SL-1 accident report⁽²¹⁾ concluded that the water above the core was accelerated upward by the pressure generated within the core. The total energy associated with the

Fig. 7 SL-1 Vessel and Core-Schematic

accelerated water slug is given as 460,000 ft lbs. Assuming that the water slug had the same diameter as the vessel (as stated in the SL-1 report⁽²¹⁾) the energy density per unit surface area would be:

$$E_a = [460,000 \text{ ft lbs}] \cdot \left[\pi \left(\frac{26}{12} \right)^2 \text{ ft}^2 \right]^{-1}$$

$$= 31,300 \text{ ft lbs/ft}^2$$

The force which gives rise to this motion of the water slug is applied at some point within the water and consists of a combination of the direct pressure pulse and a reflected component arising from reflection at the pressure vessel walls. An upper estimate of the energy in the pressure pulse may be obtained by computing the energy passing through a spherical surface having a radius as depicted in Figure 7, and an energy density equal to that calculated above for the water slug. The area of the sphere is:

$$A = \pi D^2 = \pi \left[\frac{100}{12} \right]^2 = 218 \text{ ft}^2$$

and the total energy in the pressure pulse is calculated as:

$$E = [31,300 \text{ ft lbs/ft}^2] [218 \text{ ft}^2]$$

$$= 6.8 \times 10^6 \text{ ft lbs.}$$

which is equivalent to 9.3 MW-sec. The total energy release in SL-1 is given as 157 MW-sec⁽²²⁾, of which 133 MW-sec was nuclear energy and 24 MW-sec was chemical energy. Thus, the conversion efficiency from total heat energy to energy associated with the pressure pulse may conservatively be estimated as:

$$\eta = \frac{9.3}{157} = 5.9\%$$

This value should represent an upper limit for SL-1 because of the conservative approach used. An efficiency of 6% is used in calculating the energy associated with the pressure pulse in the above table and elsewhere in this report.

On the basis of the foregoing analysis, the experiment capsule will rupture only if the pressure pulse has both a pressure level of at least 17,800 psig and an energy content of at least 1026 cal/cm of capsule length. The total heat energy generated per cm of capsule length for the accident situations is discussed in Section 6.3.

2.3 Experiment Instrumentation

The experiment assembly will be instrumented primarily for temperature and pressure measurements. Each fuel rod will have two thermocouples attached to its clad outer surface at the core midplane and thermocouple leadwire will be provided for up to six such thermocouples. Thermocouples also will be provided to measure the bulk water temperature at points above and below the fuel rods. On selected tests a pair of strain gages will be attached to a fuel rod. A pressure transducer will be mounted in the bottom of the pressure vessel primarily to measure transient pressure pulses. Instrumentation leads will be routed through a Conax fitting in the top closure and through the gripping fixture using a multiple pin connector. The male half of the pin connector will be mounted in a plug holder which fits inside the top element fitting. Shielded cable leads, 80 feet long, will be provided for connecting the male pin connector to the TREAT reference junction box outside the reactor.

3.0 NUCLEAR EFFECTS ON TREAT

The net reactivity effect associated with removing the central TREAT fuel assembly and replacing it with the experiment capsule has been computed using a two dimensional diffusion code on the IBM 7040. Results of the calculations indicate the effect of loading the capsule containing a single fuel rod to correspond to a negative 6% $\Delta k/k$ insertion. The effect of loading the capsule with three fuel rods was not calculated. However, the relatively large water moderating annulus provides for fairly effective isolation of the parent core from neutrons generated in the capsule, and the differences to be expected in reactivity effects for the single rod and three rod configurations are probably within the accuracy of the calculational model. Subsequent measurements in TREAT using a mockup of the test assembly containing a single fuel rod indicate that its insertion is worth a negative 4.5% $\Delta k/k$ as compared to a void, and a negative 5% $\Delta k/k$ as compared to a standard TREAT fuel element.

The effect of rearrangement of the fuel material within the capsule has also been computed. Calculations for a uniform dispersion of the fuel within the moderator-coolant water when compared with an intact fuel rod indicate a reactivity decrease of approximately 0.3% $\Delta k/k$ associated with such a redistribution of the fuel. If the fuel were to collect in a lump on the screen near the bottom of the capsule, calculations indicate a resultant decrease in reactivity of about 0.5% over the base case of an integral fuel rod located in the capsule center.

The changes in control rod worth and temperature coefficient of reactivity resulting from insertion of the experiment capsule into the TREAT facility were not computed. These effects are expected to be similar to those calculated for the GE-APED capsule experiments conducted in TREAT as reported by Leitz⁽²³⁾, for which the change in temperature coefficient was negligible and the change in control rod worth was less than +5% of the initial worth.

4.0 TEST CONDITIONS

The proposed tests will produce peak energy densities in the PBF fuel rods ranging from 105 to 315 cal/g UO_2 . The complete test program is summarized in reference 1. Most of the tests will be conducted at a peak energy density of 210 cal/g which will require TREAT energy releases of about 130 MW-sec for the single rod configuration and about 150 MW-sec for the three rod array. Two transients producing 315 cal/g peak energy density in a single rod are also planned, which will require TREAT energy releases of about 200 MW-sec. These energies are based upon fission rate measurements made with mockups of the test assembly inserted in TREAT. Average radial fission rates of 3.72×10^{12} and 3.25×10^{12} fissions/g U^{235} -MW-sec were measured for the single and three rod configurations, respectively. In order to achieve the desired energy releases within an accuracy of $\pm 5\%$, initial reactor periods of 140 msec with 1.3% $\Delta k/k$ will be used for the 130 and 150 MW-sec clipped transients. A maximum TREAT energy release of 350 MW-sec is possible if these transients are not clipped. For the 200 MW-sec transients, 1.5% $\Delta k/k$ and an initial period of 115 msec will produce the desired power burst within $\pm 10\%$, with an unclipped energy potential of 450 MW-sec.

The experiment capsule and its contents will be at an ambient temperature of about 20°C prior to a transient test. The fuel rod(s) in the pressure vessel will be immersed in demineralized water and the vapor space above the water will contain helium at 20 psia. The fission energy generated in the fuel during a power transient will be transferred from the fuel rod to the water heat sink. The water, in turn, will circulate within the pressure vessel by natural circulation, and will give up some of its heat to the aluminum filler piece and the pressure vessel wall. Heat from the pressure vessel wall will be transferred to the TREAT core cooling air stream which ultimately will cool the experiment capsule to ambient temperature.

The heat transfer within the experiment capsule is a transient process rather than a steady state process, and is difficult to analyze. However, the adiabatic equilibrium conditions within the experiment capsule can be predicted with reasonable accuracy. Since it is difficult to calculate what portion of the heat energy will

be absorbed by the capsule components, two cases have been assumed: Case I, in which all the fission energy is assumed to be adiabatically distributed within the fuel rod(s), the entire water heat sink, and wetted aluminum filler piece components; and Case II, in which, in addition to the assumptions of Case I, the heat capacity of the initially wetted portions of the pressure vessel is included as a part of the heat sink

The heat capacities for the two fuel-rod configurations for the above two cases are as follows:

Single Fuel Rod Configuration

<u>Component</u>	<u>Heat Capacity - cal/°C</u>	
	<u>Case I</u>	<u>Case II</u>
Fuel Rod (1)	90	90
Filler Piece and Grids	640	640
Water	1520	1520
Pressure Vessel		<u>1930</u>
Total	2250	4180

Three Fuel Rod Configuration

<u>Component</u>	<u>Heat Capacity - cal/°C</u>	
	<u>Case I</u>	<u>Case II</u>
Fuel Rods (3)	270	270
Filler Piece and Grids	260	260
Water	1850	1850
Pressure Vessel		<u>1930</u>
Total	2380	4310

The equilibrium temperatures for both Cases I and II have been calculated for the most severe power transients for each experimental configuration. Assuming no longitudinal variation in the fuel rod fission rate, and a radial power-depression factor of 0.75, the total fission energy deposited in a single fuel rod has been calculated to be 2.2×10^5 calories for power transients producing peak energy densities of about 315 cal/g and an adiabatic peak fuel temperature of about 2830°C. For a three rod array tested at 210 cal/g peak energy density corresponding to peak fuel temperatures of about 2500°C, the total energy generated has been calculated to be 4.4×10^5 calories. The following equilibrium conditions result for the two assumed cases:

Test Condition	Water Temp. °C	Water Partial Pressure, psia	Helium Partial Pressure, psia	Capsule Internal Pressure, psia
Single Fuel Rod 315 cal/g peak, Case I	118	27	28	55
Single Fuel Rod, 315 cal/g peak, Case II	73	5	24	29
Three Fuel Rods, 210 cal/g peak, Case I	205	250	38	288
Three Fuel Rods 210 cal/g peak, Case II	122	30	29	59

5.0 GAMMA DOSE RATES FROM EXPERIMENT CAPSULE AND FUEL RODS

As a consequence of exposure to the high integrated neutron flux existing in TREAT during transient operation, the stainless steel experiment assembly will become quite highly activated. For a given transient the activation of all isotopes may be calculated from the following relationship adapted from the formulation of Bopp and Sisman⁽²⁴⁾:

$$A = (\phi t_1) W \sum_{i=1}^n \Sigma_{x_i} E_i \lambda_i e^{-\lambda_i t_2}$$

where the symbols are as defined in reference 24, and the subscript "i" indicates the particular radioactive isotope. The dose rate encountered in the proximity of the irradiated assembly is given approximately by the relationship⁽²⁵⁾:

$$D = KB \frac{S_L}{2\pi a} F(\theta, b) = KB \left[\frac{A}{L} \right] \frac{F(\theta, b)}{2\pi a} \text{ R/hr}$$

where K is an energy to dose conversion factor and all other symbols are as given in reference 25. For a single 150 MW-sec transient in TREAT the exposure at one foot from the experiment due to activation of the stainless steel may be calculated from the above relationship using appropriate values for the constants as follows:

$$D = 6.4 \times 10^9 \sum_{i=1}^n \Sigma_{x_i} E_i \lambda_i e^{-\lambda_i t_2}$$

The calculated dose rate at one foot from the capsule two hours after shutdown is about 620 R/hr. This dose rate results primarily from the 2.5 hr Mn⁵⁶ activity and at 24 hours after exposure it has decreased to about 1 R/hr. For the 100 transient endurance test the exposure level increases as a consequence of a buildup of the activity due to the longer-lived activation products, primarily that of 26.5 day Cr⁵¹ and 46 day Fe⁵⁹. The calculated dose rates at 2 and 24 hours after shutdown from the hundredth transient are 840 R/hr and 6 R/hr, respectively.

In addition to the activation of the stainless steel pressure vessel, the fission product decay gammas from the fuel rod(s) must also be considered. None of the fuel rods to be tested will have been pre-irradiated. Following Way and

Wigner⁽²⁶⁾, the gamma energy emission is given approximately by:

$$\Gamma = 1.26 \times 10^{-6} t^{-1.2} \text{ Mev/sec-fission}$$

where t is the time after fissioning in days. For a single 150 MW-sec TREAT transient, the dose rate at one foot from a three rod array may be computed as in the equation above (ref. 25) to give:

$$D = 17.8 t^{-1.2} F(\theta, b) \text{ R/hr}$$

If the shielding effect of the experiment capsule is included in the $F(\theta, b)$ term, the calculated dose rates at 2 and 24 hours after a single transient are 90 R/hr and 5 R/hr, respectively. For the 100 transient endurance test, these dose rates must be computed from a summation over the previous exposure histories. When this summation is performed, the computed dose rates are about 170 R/hr and 40 R/hr, respectively. The calculated dose rates are summarized below.

Dose Rate at 1 Foot (R/hr)

Type of Exposure	2 hrs. after exposure			24 hrs. after exposure		
	Stainless Activation	Three Fuel Rods	Total	Stainless Activation	Three Fuel Rods	Total
Single 150 Mw-sec TREAT Transient	620	90	710	1	5	6
100 Transient Endurance Test	840	170	1010	6	40	46

The experiment assembly can be removed from the TREAT core using the TREAT transfer coffin, and transferred directly to a vertical cask immediately after the last test, if desired. A shielding cask mounted on a lo-boy trailer will be provided for transporting the assembly to the TAN hot shop or other hot cell location on NRTS. The cask will provide sufficient shielding to meet standard requirements for transfer immediately after completion of a transient.

6.0 ANALYSIS OF POTENTIAL ACCIDENTS

6.1 Maximum Credible Accident

The maximum credible accident is based upon the following conditions:

1. The three rod array has been exposed to 99 power transients and fails on the 100th transient.
2. The energy release in TREAT exceeds the specified energy release by a factor of 2.33.
3. The capsule and all three fuel rods fail so as to release the fission products into the coolant air stream in TREAT.

The fission product inventory has been computed on the basis of 99 transients conducted at TREAT energy releases of 150 MW-sec and a final transient at 350 MW-sec. The TAN Radiological Safety Analysis Computer program (RSAC), which is based on the CURIE code, was used in the calculations. If 50% of the halogens, 100% of the noble gases, and 1% of the solids instantaneously escape from the fuel rods into the cooling air⁽²⁷⁾, the activities are as shown in the following table.

Time	Activity (Curies)		
	Halogens	Noble Gases	Solids
Instantaneous	8.5×10^4	7.8×10^4	10^4
3 Hrs After Failure	18	23	2

The majority of the materials released have very short half lives and the concentrations decrease rapidly with time. The residual activities 3 hours after release are very low as illustrated in the table.

The total fission product inventory is about one thousandth that indicated for the TREAT maximum credible accident⁽²⁸⁾. If the conditions of the TREAT Hazards Summary Report are used and a release to the atmosphere of 100% of the fission product inventory in the experiment is assumed, the beta and gamma doses downwind, as obtained by scaling the data in Table V of Reference 28, are as shown below.

Distance (Miles)	Dosage (Rep)	
	Gamma	Beta
0.5	0.04	0.004
11.0	10^{-5}	3.7×10^{-7}

From these data it is evident that there is no danger to personnel either at 0.5 miles from the reactor, ie, the TREAT control building, or at 11 miles from the reactor, which corresponds to the NRTS boundary in the prevailing wind direction. Since the dose rates at 0.5 miles are well within the acceptable limits of 0.5 R/hr at the site boundary specified in AECM 0524, further calculation of the dose rate at the NRTS boundary in other directions is not necessary.

6.2 Rupture of Fuel Rod Clad

Rupture of the fuel rod clad is not expected to occur during any of the tests except those in which the clad contains intentional defects or the fuel rod has been purposely water-logged. Three such tests are planned⁽¹⁾ and for these clad rupture will likely occur.

In the postulated accident situations in which the transients proceed unclipped, UO_2 fuel temperatures above the melting point will be produced. For a 450 MW-sec unclipped transient on a single fuel rod, a peak energy density of 710 cal/g would occur, causing an estimated⁽²⁾ temperature of 3300°C and partial vaporization of UO_2 . In the case of the three rod array at 350 MW-sec, a peak energy density of 490 cal/g and an associated temperature of 3300°C would be expected. The UO_2 vapor pressure calculated empirically⁽⁷⁾ for 3300°C is about 2.5 atmospheres, or 37 psi. In addition, the helium within the fuel rod would be heated to 3300°C, causing its pressure to increase to about 570 psig. Thus, the total internal pressure would be approximately 610 psig, well within the yield strength capabilities of the clad at temperatures up to 800°C.

In the event of clad rupture, the exposed portions of the fuel will transfer their energy rapidly to the water, creating a pressure pulse. The magnitude of this pressure pulse has been discussed in Section 2.2.2. The gas volume within three fuel rods is only 0.2% of the gas volume within the experiment capsule. Therefore, if the helium and UO_2 vapor at 610 psig are released from the fuel rods, the increase in static pressure level within the capsule would be only 1.4 psi. In addition to the energy released from the fuel to the water, a metal-water reaction between the stainless steel clad and water is possible. Previous ANL tests indicate that for a UO_2 fuel energy density of 500 cal/g, this reaction is 19% complete⁽²⁰⁾, releasing 27 cal/g of clad; at 700 cal/g the reaction would be about 32% complete⁽¹⁶⁾, releasing 46 cal/g of clad. However, the presence of the thermal insulator between the fuel and the clad will limit the clad temperature to 170°C in the PBF prototype fuel rods. Some local hot spots could occur in the clad due to local failure of the insulators, but there is no conceivable mode of clad failure which would allow heating

of the entire clad mass to a level sufficient to cause the degree of metal-water reactions referenced above. The failure of 10% of the insulator surface has been assumed which then exposes 10% of the stainless steel clad in the PBF fuel rods to high enough temperatures to cause an appreciable metal-water reaction. On this basis, only 16 g of the 160 g of clad (excluding end caps) would be available for chemical reaction with the water, and the percent completions of the reaction are assumed to be 19% and 32% for 490 and 710 cal/g in the fuel, respectively.

The chemical reaction between type 304 stainless steel and water produces 0.401 standard liters of hydrogen (H_2) per gram of metal⁽²⁰⁾. While it is possible for the hydrogen involved to react with the zirconia thermal insulators, the reaction is endothermic and requires 60 kcal/gram-atom of oxygen. Therefore, a zirconia-hydrogen reaction would absorb energy from the system rather than releasing additional energy. Assuming that 10% of the metal clad is involved in a metal-water reaction, approximately 3.7 standard liters of hydrogen would be produced from three rods at 19% reaction completion, and 2.1 standard liters from a single rod at 32% reaction completion.

The total nuclear and chemical energy available for transfer to the water heat sink and capsule is 5×10^5 cal for the single rod accident case, and 1.03×10^6 cal for the three rod accident case. The adiabatic equilibrium temperatures and pressures within the capsule, calculated using the Case II heat sink as discussed in Section 4.0, are shown below.

	<u>Accident Condition</u>	
	<u>Single Fuel Rod, 710 cal/g peak</u>	<u>Three Fuel Rods, 490 cal/g peak</u>
Water Temp., °C	140	262
Water Partial Pressure, psia	52	681
Helium Partial Pressure, psia	42	46
Hydrogen Partial Pressure, psia	26	51
Capsule Total Internal Pressure, psia	120	778

6.3 Rupture of Experiment Capsule

Since the peak pressure level predicted for this test series from previous metal-water tests in TREAT is less than 1000 psi (see Section 2.2.2), rupture of the experiment capsule is not expected. The efficiency of conversion from nuclear (heat) energy to energy associated with the pressure pulse has been taken to be 6% following the treatment in Section 2.2.2. The appropriate energy releases per cm of rod length for the postulated accident conditions in which the transients proceed unclipped are shown in the following table:

Configuration	Nuclear Energy cal/cm	Chemical Energy* cal/cm	Total Heat Energy cal/cm	Maximum Energy In Pressure Pulse cal/cm
Single Rod, 490 cal/g	8,100	50	8,150	490
Single Rod, 710 cal/g	11,700	80	11,780	710
Three Rods, 490 cal/g	24,300	150	24,450	1,470

* Conservatively assumes clad is uninsulated.

The energy associated with the pressure pulse is below the rupture energy of the capsule (1026 cal/cm) for all accident conditions except the three-rod configuration, in which case the energy calculated on this rather conservative basis is slightly greater than the rupture energy. As stated in Section 2.2.2, the vessel will rupture only when a pressure level of 17,800 psi and an available energy of 1026 cal/cm exist simultaneously. Examination of data from the TREAT metal-water studies leads to estimates of the pressure level during the proposed PBF tests in TREAT that are at least one order of magnitude below the rupture pressure. From the above considerations it is concluded that rupture of the experiment capsule is very unlikely, since the pressure levels predicted are far below the rupture pressure of the capsule and the empirical energy releases in the form of pressure are below the capsule fracture energy for all accident conditions except the three rod configuration.

For a postulated capsule rupture, the following events would be expected to occur:

- (a) The water heat sink would drain from the capsule into the air plenum below the TREAT core. It would then gradually evaporate and be discharged from the exhaust stack.
- (b) Partial melting of the fuel rod cladding might occur due to loss of cooling water.
- (c) Any fission products which may have been released from the fuel rod(s) to the capsule interior would be swept into the cooling air stream, and would pass through the exhaust filters to the stack. The residual fission product inventories are very low (see Section 6.1) and should be carried from the reactor to the exhaust stack with no appreciable accumulation within the core.

- (d) Any excess energy beyond that required for rupture of the experiment capsule would be available to do work on the environment surrounding the capsule. In any event, the excess above the fracture energy would be small and no physical damage to the TREAT core is expected to occur as a result of conducting these tests.

6.4 Capsule Displacement as a Result of Water Hammer

Since the experiment capsule is not completely filled with water, an upward acceleration of water at its free surface could conceivably result in a "water hammer" effect similar to that observed in the SL-1 accident⁽²¹⁾. If such a water hammer were to occur, a net vertical displacement of the capsule could result. The net energy that might be imparted to the capsule as a result of a water hammer reaction has been calculated as follows. The three rod configuration is assumed to encounter an accident situation in which a 1000 psi peak pressure pulse (see Section 2.2.2) is generated at the midplane of the fuel rods. This pressure forces the mass of water above the midplane through the seven 9/16 in. diameter holes in the upper grid and up through the filler piece until it strikes the top head. The water below the midplane is forced through the partially covered holes in the lower grid and up the annulus between the capsule wall and filler piece until it strikes the top head also. The amount of energy associated with the acceleration of the water has been determined by calculating the water flow rate through the orifices, the velocity at which this water flows after it is discharged from the grid orifices, and the kinetic energy of each of the two masses of water traveling at its respective flow velocity. In the three rod configuration there is 0.91 lb of water above the midplane, and 2.75 lb of water below the midplane (excluding 0.34 lb trapped in the bottom head). The water flow rate may be calculated from the expression⁽²⁹⁾:

$$Q = CA \left[2g (h_1 - h_2) \right]^{1/2}$$

where the symbols are as defined in reference (29). In general the pressure pulse may be approximated closely by a sine function so that for a peak pressure of 1000 psi, the root mean square value would be about 700 psi. The orifice coefficient, C , for a short tube orifice is given in reference (29) as 0.8. Thus, the water is discharged through the grids at the following rates:

$$Q_{\text{upper grid}} = 0.8 \frac{(1.74)}{144} \left[\frac{2 (32.2)(700)(144)}{62.4} \right]^{1/2} = 3.11 \text{ cfs}$$

$$Q_{\text{lower grid}} = 0.8 \frac{(1.04)}{144} \left[\frac{2 (32.2)(700)(144)}{62.4} \right]^{1/2} = 1.86 \text{ cfs}$$

The flow through the upper grid moves up the 2 in. ID filler piece at a velocity of 143 fps, and the flow through the bottom grid moves up the annulus between the capsule wall and filler piece at 137 fps. It should be noted that these velocities are quite comparable to the velocity of 159 fps estimated for the water slug in the SL-1 accident⁽²⁹⁾. The kinetic energies associated with the upper and lower masses of water are 290 and 845 foot pounds, respectively, making a total of 1135 foot pounds against the top head.

While an accident of this type and magnitude is considered highly unlikely, a hold-down device will be provided to limit vertical motion of the experiment capsule. Figure 8 is a schematic of this device which is designed to dissipate 1200 ft lb of energy within 2 inches of vertical movement of the capsule. The hold-down device consists of a pre-loaded piston brake which slides within a cylindrical tube mounted on the TREAT 25 ton rotating shield plug. A connecting tube extends downward from the piston assembly and fits against the tapered surfaces of the experiment capsule gripping fixture. The piston brake has two split sections which are pre-loaded to the desired amount of frictional resistance by tightening bolts against a 10° tapered center plug, thereby wedging the piston halves against the cylindrical tube. Asbestos friction lining, having a coefficient of friction of 0.45, is thermally bonded with epoxy to the cylindrical surfaces of the piston halves. The device will be adjusted to provide a resisting vertical force of 8000 lb. A positive stop is provided above the piston brake to limit the capsule vertical displacement to a maximum of 3 inches.

6.5 Failure of Experiment Instrumentation

Failure of the instrumentation for measuring temperatures, clad strain and pressure within the experiment assembly does not pose a hazard to the operation of the experiment, since its purpose is to obtain experimental data and not to control the operation of the experiment.

6.6 Loss of TREAT Cooling Air

For a postulated loss of cooling air flow through the TREAT core immediately following a 450 MW-sec power transient, the core would reach a peak temperature of approximately 500°F⁽³⁰⁾. The experiment capsule is designed to accommodate this situation by providing a gas volume above its water level which will accommodate the volumetric expansion of the water heat sink. Should this accident occur, the equilibrium pressure inside the capsule would reach about 780 psig, which is well below its design rating.

Fig. 8 Hold-down Device

7.0 CONCLUSIONS

The proposed transient tests on PBF prototype fuel rods will produce peak energy densities in the fuel up to 315 cal/g with an associated peak temperature of 2830°C. The maximum internal pressure within the fuel rod will be 500 psi, creating a pressure stress of 6700 psi. The temperature difference across the fuel rod clad will be about 70°C, creating a thermal stress of 23,300 psi. For an accident situation in which the transient energy is 233% of the energy desired, a peak fuel energy density of 710 cal/g and a temperature of 3300°C have been predicted. The fuel rod internal pressure for this case would be about 610 psi. Gross rupture of the clad is not expected to occur during any of the tests except those in which the clad contains intentional defects or the fuel rod has been purposely water-logged.

The transient pressure level in the experiment capsule expected during an accident situation when testing PBF fuel rods is the same magnitude (670 psi) as was observed in TREAT metal-water tests of stainless-steel clad, UO_2 pellets at energy inputs up to 655 cal/g of fuel. Equilibrium pressures resulting from heating of the helium cover gas and water within the experiment capsule have been estimated to be about 730 psi. Hydrogen gas evolution from a stainless steel-water reaction has been conservatively calculated to be 3.7 liters which contributes a partial pressure of about 50 psi. The maximum equilibrium pressure level in the experiment capsule therefore would be 780 psi. Hence, the combination of the maximum transient pressure level of less than 1000 psi and the equilibrium pressure level of 780 psi for the postulated accident conditions (~ 710 cal/g) are well below the 17,800 psi rupture pressure of the experiment capsule. Further, from the analysis of the SL-1 data a conservative calculation indicated an upper limit of 6% for the efficiency of conversion of nuclear (heat) energy into mechanical (pressure) energy. On this basis the available mechanical energy would be less than the capsule fracture energy for all tests except the three rod configuration under accident conditions. Since both the rupture pressure level (17,800 psi) and the fracture energy (1026 cal/cm) must be exceeded simultaneously before capsule rupture can occur, rupture of the capsule is not expected for any test during this proposed series.

In the event the integrity of the capsule were violated and 100% of the fission product inventory were released, the residual source strengths 3 hrs following the maximum accident, involving fuel exposed to 100 transients, would be:

Solids - - - - -	20 curies
Halogens - - - - -	36 curies
Noble Gases- - - - -	23 curies

These activities decrease rapidly with time and pose no serious hazard, especially since the majority of the activity is gaseous and would be swept from the TREAT core to the exhaust stack by the reactor coolant air stream. The integrated dose immediately after capsule rupture at distances of 0.5 mile downwind would be 0.04 Rep gamma and 0.004 Rep beta which are far below the limits set in AECM 0524 for maximum levels at an uncontrolled boundary.

From the above analysis of the pertinent maximum accident conditions, it is concluded that conducting the proposed test series in the TREAT facility will not result in an undue hazard to the facility or the surrounding area.

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