8. SAFETY ANALYSIS

In this section an analysis of abnormal operating conditions will be made with conclusions concerning the effects on safety to the reactor, the public, and the operations personnel, as a consequence of any abnormal operations.

The abnormal conditions that will be analyzed are:

1. Clad rupture
2. Loss or reactor coolant
3. Reactivity accident

8.1. FISSION PRODUCT RELEASE

In the analysis of fission product releases under accident conditions, it is assumed that a fuel element in the region of highest power density fails.

8.1.1. Fission Product Inventory

Table 8-1 gives the inventory of radioactive noble gases and halogens in the TRIGA Mark I after continuous operation at 250 kW for four years (i.e., 1 MW-yr).

8.1.2. Fission Product Release Fractions

The release of fission products from U-ZrH fuel has been studied at some length. A summary report of these studies (Ref. 1) indicates that the release from the U-$ZrH_{1.6}$ fuel meat at the steady-state operating temperatures is principally through recoil into the fuel-clad gap. At high temperatures (above 400°C or 500°C), the release mechanism is through a diffusion process and is temperature-dependent, unlike recoil.

TABLE 8-1
NOBLE GAS AND HALOGENS IN THE REACTOR

Isotope	Quantity (Ci)
Br-83	1,020
Kr-83m	1,[illegible]20
Br-84	2,060
Br-85	2,150
Kr-85m	2,150
Kr-85	113
Kr-87	5,400
Kr-88	7,700
Kr-89	9,750
Kr-90	10,850
Kr-91	7,350
I-131	5,950
Xe-131m	48
I-132	8,850
I-133	14,350
Xe-133m	350
Xe-133	14,350
I-134	16,100
I-135	13,400
Xe-135m	4,050
Xe-135	13,850
I-136	12,950
Xe-137	12,550
Xe-138	11,700
Xe-139	11,800
Xe-140	8,100

For the accident considered here, it is assumed a fuel element in the region of highest power density fails in water and that the peak fuel temperature in the element is less than 300°C. At this temperature, the long-term release fraction would be less than 1.5×10^{-5}. For the purpose of this analysis it is also assumed that 100% of the noble gases and 50% of the halogens are released from the highest power density fuel element in which 2.6% of the total power is generated.

It is important to note that the release fraction in accident conditions is characteristic of the normal operating temperature and not the temperature during the accident conditions. This is because the fission products released as a result of a fuel clad failure are those that have collected in the fuel-clad gap during normal operation.

Other assumptions concerning the transport from the fuel to the exit of the stack are:

1. 100% of the noble gases released from the fuel are transported to the building exhaust stack.

2. 10% of the halogens released from the fuel are in the form of organic compounds and all of these halogens escape from the tank water.

3. Only 1% of the balance of the halogens escapes from the tank water.

4. There is no plate-out of any of the fission products.

5. The stack radiation monitor fails to place the ventilation system in the emergency mode (and that the reactor operators also fail to do so).

6. The effective building ventilation rate is 15 air changes/hr. (This is greater than the actual release rate but results in larger dose rates.

The net effect of these assumptions is that for the accident condition, the fraction of the noble gases released from the building is:

$$f_{NG} = 1.5 \times 10^{-5} \times 1.0 \times 2.6 \times 10^{-2} = 3.9 \times 10^{-7} \quad ,$$

and of the halogens:

$$f_H = 1.5 \times 10^{-5} \times 0.5 \times (0.1 \times 1.0 + 0.9 \times 0.01) \times 2.6 \times 10^{-2}$$
$$= 2.1 \times 10^{-8} \quad .$$

8.1.3. Downwind Dose Calculations

The minimum roof level dilution factor was calculated, in Section 5.4, to be 4.2×10^{-2} sec/m^3. This is based on mixing in the lee of the building when the wind velocity is 1 m/sec.

The calculation of whole body gamma doses and thyroid doses downwind from the point of release was accomplished through the use of the computer code GADOSE (Ref. 2). In this code the set of differential equations describing the rate of production of an isotope through the decay of its precursors and the rate of removal through radioactive decay and removal by the ventilation system is integrated for each member of the chain. The release rate q_i to the environment for the ith isotope at time t_i, in hours is:

$$q_i(t) = g_i\ Q_i(t)\ (\ell/V)/3600 \quad ,$$

where $Q_i(t)$ = the concentration of the ith isotope in Ci/m^3,

ℓ/V = the building leakage rate in (m^3/hr)/m^3,

$g_i = 1 - \varepsilon_i$,

ε_i = the filter efficiency for the ith isotope.

The quantity $Q_i(t)$ is the concentration of the ith isotope in the discharged air at the time, t. This concentration is given by

$$Q_i(t) = f_i Q_i(0) e^{-(\lambda_i + \ell/V)t}$$

where $Q_i(0)$ = the concentration of the ith isotope as found in Table 8-1,

λ_i = the decay constant for the ith isotope, and

f_i = the release fraction to the reactor hall.

The concentration downwind at a distance x for the ith isotope is calculated from

$$Q_i'(t,x) = q_i(t-\tau) \cdot \psi(x) e^{-\lambda_i \tau} \quad ,$$

where τ = the transit time from the release point to the dose point, hr,

ψ = the dilution factor at the distance x, sec/m^3.

The whole body gamma ray dose rate for the ith isotope, D_{w_i}, at the distance x and time t is calculated, assuming a semi-infinite cloud, through the expression:

$$D_{w_i}(t,X_i) = 900\,\overline{E}_i Q_i'(t,x) \quad ,$$

where $\overline{E}_i$ = the average gamma ray energy per disintegration, MeV, and the constant includes the attenuation coefficient for air as well as the conversion factors required.

Internal dose rates, in this case the dose rate to the thyroid, are calculated by:

$$D_{th_i}(t,x) = 3600\ B \cdot Q_i'(t,x) K_i \quad ,$$

where B = the breathing rate, m^3/sec, and

K_i = the internal dose effectivity of the ith isotope, rem/Ci.

The values for the breathing rate are given in Table 8-2 and are taken from USAEC Regulatory Guide 4.

The average gamma ray energy per disintegration and the internal dose effectivity for each isotope considered are given in Table 8-3.

The decay products of these isotopes are also included in the calculation; however, their contribution to the dose rates are small and therefore the data for these isotopes were not included in the table.

8.1.4. Downwind Doses

The whole body gamma dose and thyroid dose in the lee of the building are shown in Table 8-4. These doses are trivial in nature.

TABLE 8-2
ASSUMED BREATHING RATES

Time (hr)	Breathing Rate (m^3/sec)
0 to 8	3.47×10^{-4}
8 to 24	1.75×10^{-4}
Over 24	2.32×10^{-4}

TABLE 8-3
AVERAGE GAMMA RAY ENERGY AND INTERNAL DOSE EFFECTIVITY FOR EACH FISSION PRODUCT ISOTOPE

Isotope	E_i (MeV)	K_i (rem/Ci)
Br-83	0.92×10^{-2}	
Br-84	1.87	
I-131	0.40	1.486×10^{6}
I-132	1.96	5.288×10^{4}
I-133	0.56	3.951×10^{5}
I-134	3.02	2.538×10^{4}
I-135	1.77	1.231×10^{5}
I-136	2.91	
Kr-83m	0.8×10^{-3}	
Kr-85m	0.16	
Kr-85	0.4×10^{-2}	
Kr-87	1.07	
Kr-88	2.05	
Kr-89	2.40	
Xe-131m	0.82×10^{-2}	
Xe-133m	0.37×10^{-1}	
Xe-133	0.29×10^{-1}	
Xe-135m	0.46	
Xe-135	0.25	
Xe-137	1.22	
Xe-138	1.57	

TABLE 8-4
DOWNWIND DOSES FROM FISSION PRODUCT RELEASE

	Distance (m)	Whole Body Gamma (mrad)	Thyroid (mRem)
Accident condition (Release of fission products from one fuel element)	0	.24	2.66

8.2 LOSS OF REACTOR COOLANT

8.2.1 Summary

The reactor will operate at a calculated maximum power density of 6 kW/element when the reactor power is 250 kW and there are 63 elements in the core, all of which are standard TRIGA fuel. If the coolant is lost immediately after reactor shutdown, the fuel temperature (see Fig. 8-1) will rise to a maximum value of $\sim$275°C. The stress imposed on the fuel element clad by the internal gas pressure (see Fig. 8-2) is about 1200 psi when the fuel and clad temperature is 275°C and the yield stress for the clad is about 37,000 psi. Therefore, it can be concluded that the postulated loss-of-coolant accident will not result in any damage to the fuel, will not result in release of fission products to the environment, and will not requi.e emergency cooling.

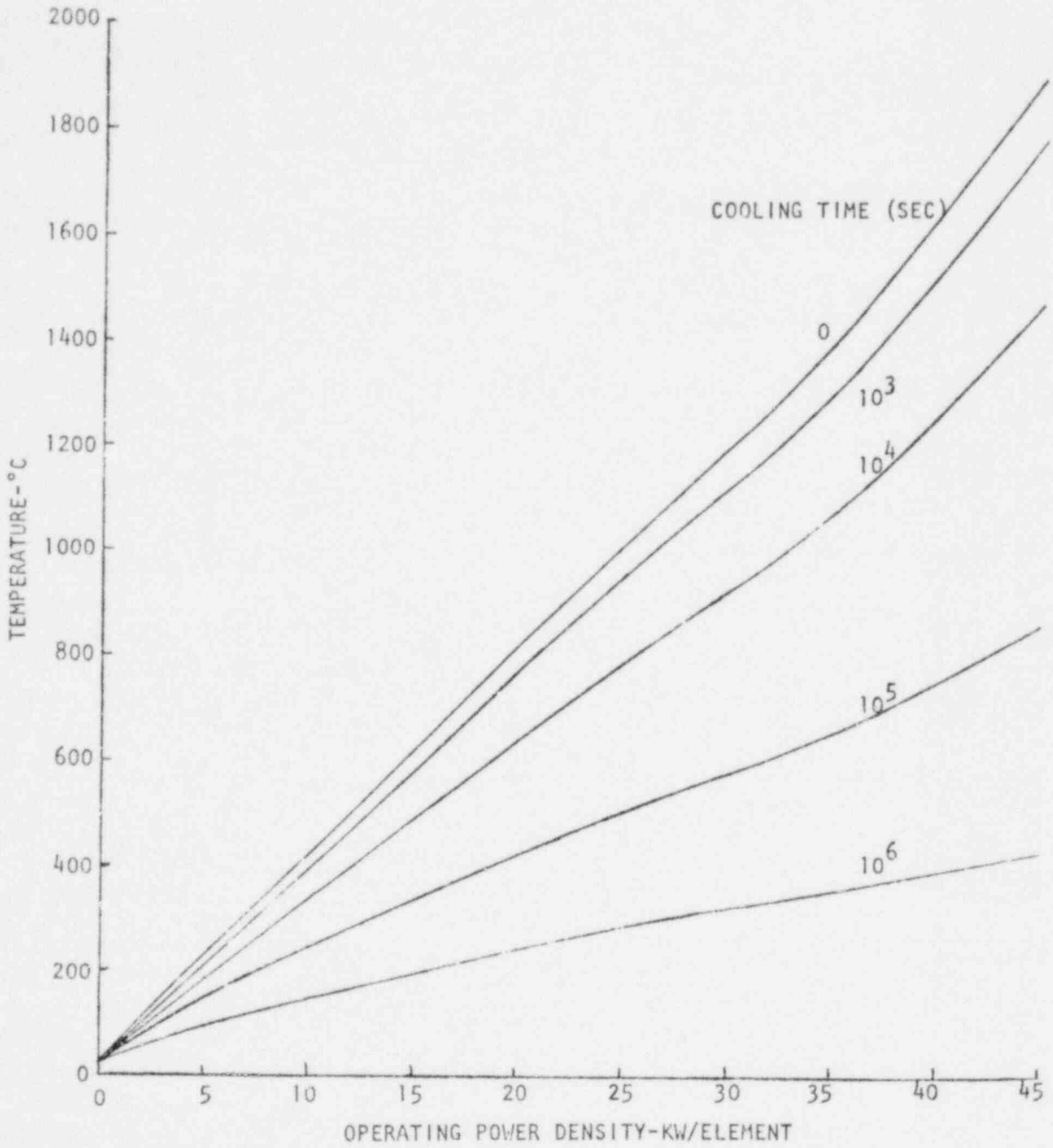

Fig. 8-1. Maximum fuel temperature versus power density after loss of coolant for various cooling times between reactor shutdown and coolant loss

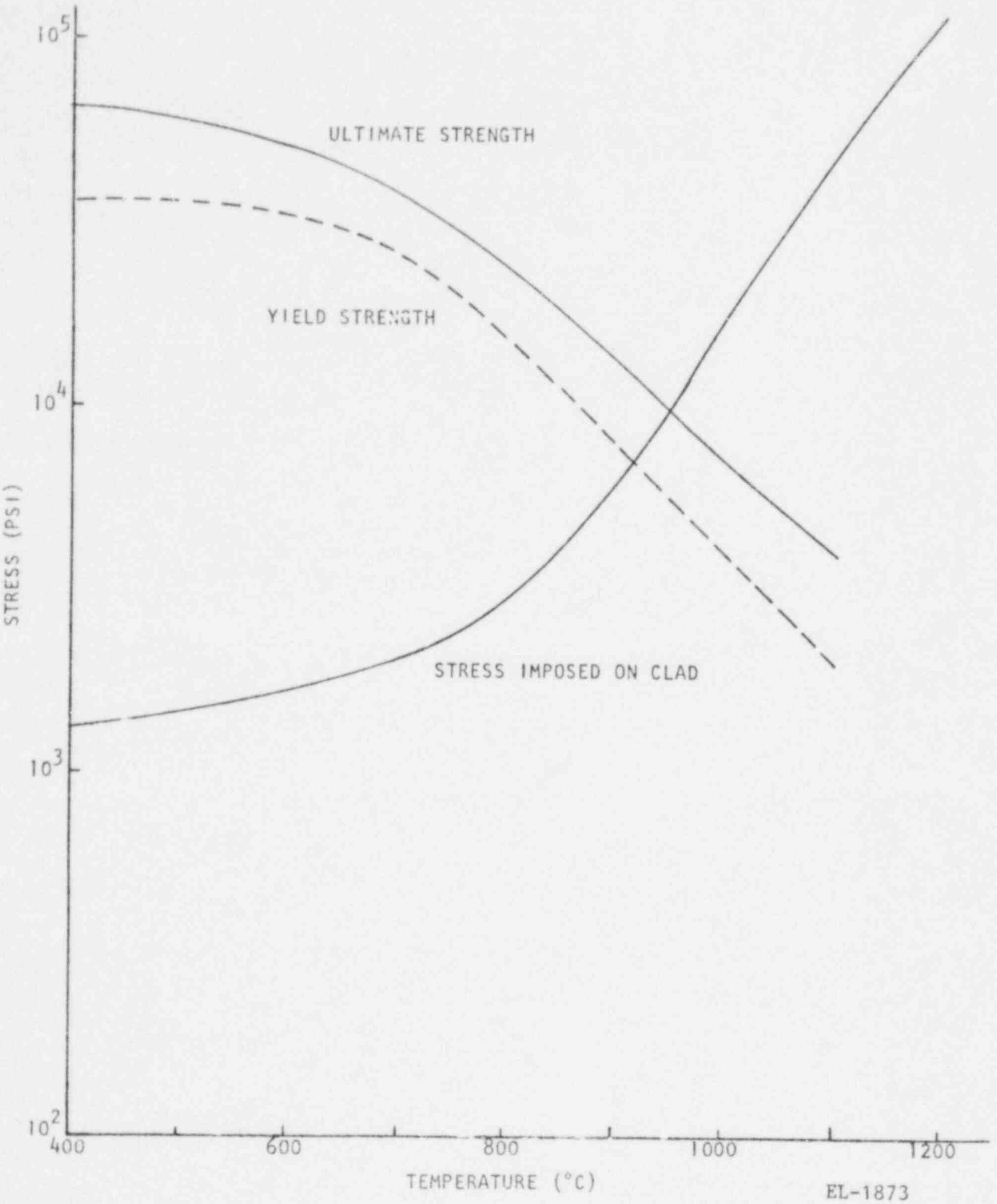

Fig. 8-2. Strength and applied stress as a function of temperature, U-$ZrH_{1.65}$ fuel, fuel and clad at same temperature

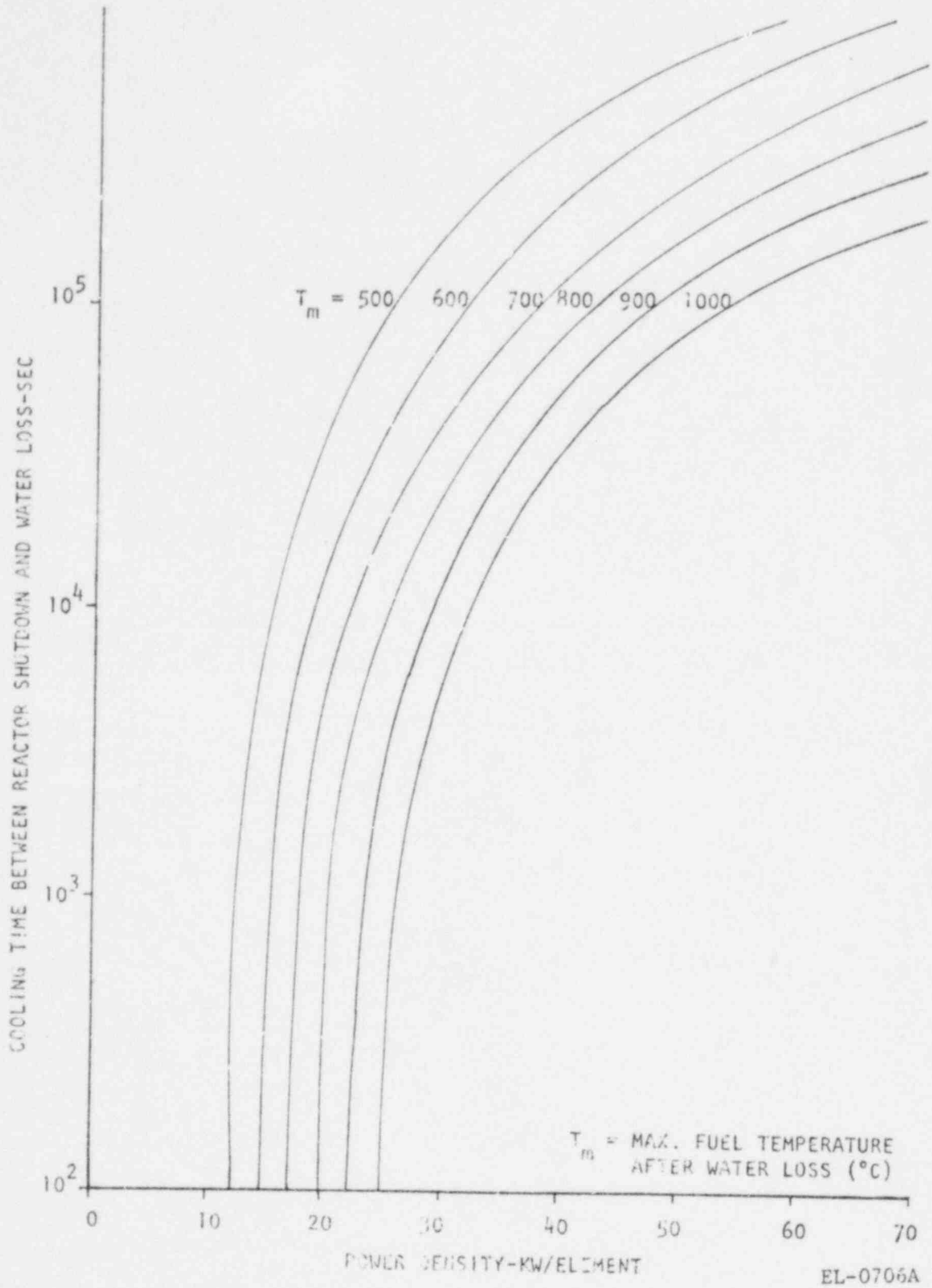

Fig. 8-3. Cooling times after reactor shutdown necessary to limit maximum fuel temperature versus power density

If the reactor tank is drained of water, the fission product decay heat will be removed through the natural convective flow of air up through the reactor core. If the decay-heat production is sufficiently low because of a low fission product inventory or a long interval between reactor shutdown and coolant loss, the flow of air will be enough to maintain the fuel at a temperature at which the fuel elements are undamaged. The following analysis shows that:

1. The maximum temperature to which the fuel can increase is 900°C without substantial yielding of the clad or subsequent release of fission products.

2. This temperature will never be exceeded under any conditions of coolant loss if the maximum operating power density is 22 kW/element or less.

3. For maximum operating power densities greater than 22 kW/element, emergency cooling can be provided to ensure that the fuel-element temperature does not exceed 900°C. The required emergency cooling time as a function of maximum operating power density is shown in Fig. 8-3.

8.2.2. Fuel Temperature and Clad Integrity

The strength of the fuel element clad is a function of its temperature. The stress imposed on the clad is a function of the fuel temperature as well as the hydrogen-to-zirconium ratio, the fuel burnup, and the free gas volume within the element. In the analysis of the stresses imposed on the clad and strength of the clad the following assumptions will be made:

1. The fuel and clad are at the same temperature.

2. The hydrogen-to-zirconium ratio is 1.65.

3. The free volume within the element is represented by a space 1/8-in. high within the clad.

4. The reactor contains fuel that has experienced burnup equivalent to only about 4.5 MW-days.

The fuel element internal pressure p is given by

$$p = p_h + p_{fp} + p_{air} \quad , \qquad (1)$$

where p_h = hydrogen pressure,

p_{fp} = pressure exerted by volatile fission products, and

p_{air} = pressure exerted by trapped air.

For hydrogen-to-zirconium ratios greater than about 1.58 the equilibrium hydrogen pressure can be approximated by

$$p_h = \exp\left[1.767 + 10.3014x - 19740.37/(T_K)\right] \text{ (atmospheres)} \quad , \qquad (2)$$

where x = ratio of hydrogen atoms to zirconium atoms, and

T_K = fuel temperature (°K).

This expression was derived from least-square fits to the data of Dee and Simnad (Ref. 3). For $ZrH_{1.65}$ the hydrogen pressure becomes

$$p_h = 1.410 \times 10^8 \exp\left[-19740.37/(T_K)\right] \text{ (atmospheres)} \quad .$$

The pressure exerted by the fission product gases is given by

$$p_{fp} = f \left(\frac{n}{E}\right) \frac{RT_K}{V} E \quad , \qquad (3)$$

where f = fission product release fraction,

n/E = number of moles of gas evolved per unit of energy produced, moles/MW-day,

R = gas constant, 8.206×10^{-2} liters-atmospheres/mole °K,

V = free volume occupied by the gases, liters, and

E = total energy produced in the element, MW-day.

The fission product release fraction (Ref. 4) is given by

$$f = \int_{\nu} \left\{1.5 \times 10^{-5} + 3.6 \times 10^{3} \exp\left[-1.34 \times 10^{4}/(T_{\nu})\right]\right\} d\nu \quad , (4)$$

where T_{ν} = fuel temperature in the differential volume of the element during normal operation, °K, and

ν = fuel volume normalized to 1.

The fission product gas production rate n/E is not independent of power density (neutron flux) but varies slightly with the power density. The value $n/E = 1.19 \times 10^{-3}$ moles/MW-day is accurate to within a few percent over the range from a few kilowatts per element to well over 40 kW/element. The free volume occupied by the gases is assumed to be a space 1/8-in. (0.3175-cm) high at the top of the fuel so that

$$V = 0.3175 \pi r_i^2 \quad , \qquad (5)$$

where r_i = inside radius of the clad (1.822 cm).

For standard TRIGA fuel the maximum burnup is about 4.5 MW-days/element.

Finally, the air trapped within the fuel element clad would exert a pressure

$$P_{air} = RT_K/22.4 \quad , \qquad (6)$$

where it is assumed that the initial specific volume of the air (22.4 liters/moles) is present at the time of the loss of coolant. Actually, the air forms oxides and nitrides with the zirconium so that after relatively short operation the air is no longer present in the free volume inside the fuel element clad.

For $ZrH_{1.65}$ fuel burned up to 4.5 MW-days/element, with a maximum operating temperature of 600°C, the internal pressure as a function of maximum fuel temperature T_K is

$$p = 1.410 \times 10^{8} \exp(-19740.37/T_K) + 3.66 \times 10^{-3}\, T_K \text{ (atmospheres)}$$

or

$$p = 2.073 \times 10^{9} \exp(-19740.37/T_K) + 5.38 \times 10^{-2}\, T_K \text{ (psi)} \quad . \quad (7)$$

The stress imposed on the clad by the gases within the free volume inside the clad is

$$S = \frac{r_c}{t}(p) \quad , \qquad (8)$$

where r_c = clad outside radius (= 1.873 cm),

t = clad thickness (= 0.051 cm).

If Eqs. 1 and 8 are combined, the stress can be rewritten as

$$S = 36.7\, p$$
$$= 7.61 \times 10^{10} \exp(-19740.37/T_K) + 1.97\, T_K \text{ (psi)} \quad . \qquad (9)$$

In Fig. 8-2 this imposed stress is plotted as a function of maximum fuel temperatures. Also plotted are the yield and ultimate strength of the type 304 stainless steel clad. The ultimate strength of the clad is not exceeded if the maximum fuel temperature is maintained below about 950°C and the yield strength is not exceeded for any fuel temperatures below about 920°C. The limit is set at 900°C, slightly below the yield point and well below the rupture point.

8.2.3. After-Heat Removal Following Coolant Loss

It is assumed that the reactor operates continuously at a constant power density level P_o so that the maximum inventory of fission products is available to produce heat after the reactor is shut down. The power density after reactor shutdown P is given by

$$P = 0.1\, P_o \left[(t + 10)^{-0.2} - 0.87\,(t + 2 \times 10^7)^{-0.2}\right] \times \{1.3 \cos [2.45\,(0.026\ell - 0.5)]\} \quad , \tag{10}$$

where P_o = operating power density, W/cm^3,

t = time after reactor shutdown, sec,

ℓ = distance from the bottom of the fuel region, cm.

At the time that the coolant is lost from the core the fuel and its surroundings are assumed to be at a temperature of 27°C. This is not necessarily true, for an accident can be postulated in which the coolant loss is the mechanism by which the reactor is shut down. (For the standard non-gapped fuel element, under normal conditions, the time to cool down from operating temperatures is a matter of one to two minutes.) Although such an accident does not appear to be conceivable, calculations indicate that: if it is assumed that the average fuel temperature at the time of coolant loss is equivalent to the operating average fuel temperature, the maximum temperature after the coolant loss is not appreciably different (2% - 4% higher) from that calculated assuming 27°C fuel initially.

The after-heat removal will be accomplished by the flow of air through the core. To determine the flow through the core the buoyant forces were equated to the friction, end, and acceleration losses in the channel as shown in the expression

$$\Delta p_b = \Delta p_f + \Delta p_e + \Delta p_i + \Delta p_a \quad . \tag{11}$$

The buoyant forces are given by

$$\Delta p_b = \rho_o L - \int \rho d\ell = \rho_o L - \rho_o L_o - \bar{\rho} L_f - \rho_1 L' \quad , \tag{12}$$

where ρ_o, $\bar{\rho}$, ρ_1 = the entrance, mean, and exit fluid densities, respectively,

L = the effective length of the channel (= $L_o + L_f + L'$),

L_o, L_f, L' = the length of the channel adjacent to the bottom end reflector, fuel, and top end reflector plus ten channel hydraulic diameters, respectively.

The friction losses in the flow channel are given by

$$\Delta p_f = \sum_i f_{F_i} \left(\frac{4L_i}{D_e}\right) \frac{w^2}{2g\rho_i A_c^2} \quad , \tag{13}$$

where the summation is over the lower unheated length, the heated length, and the upper unheated length,

f_{F_i} = the friction factor (= $23.46/R_e$)(Ref. 5),

D_e = the hydraulic diameter (= 0.0601 ft),

A_c = the flow area through the core per element (= 0.0058 ft^2),

$g = 4.17 \times 10^8$ ft/hr^2.

The sum of the exit and inlet losses, using appropriate expansion and contraction coefficients, is given by

$$\Delta p_e + \Delta p_i = (\Sigma K) \frac{w^2}{2g\rho_o A_c^2} ,$$

with

$$\Sigma K = \sum_j \left[k_j \left(\frac{A_c}{A_j} \right)^2 \right] = 1.57 .$$

where k_j is appropriate expansion or contraction coefficient from regions of area A_j.

The acceleration losses are given by

$$\Delta p_a = \left(\frac{1}{\rho_1} - \frac{1}{\rho_0} \right) \frac{w^2}{gA_c^2} . \tag{14}$$

By substituting the appropriate expression in Eq. 11, using the definition of the Reynolds number, and L = 2.40 ft, L_o = 0.29 ft, L_f = 1.25 ft, and L′ = 0.87 ft, one obtains

$$\left(\frac{0.700}{\rho_1} - \frac{0.149}{\rho_0} \right) \times 10^{-4} w^2 + \left(0.153 \frac{\mu_i}{\rho_0} + 0.665 \frac{\mu}{\bar{\rho}} + 0.153 \frac{\mu_2}{\rho_1} \right) \times 10^{-2} w + \left(1.25 \bar{\rho} + 0.889 \rho_1 - 2.139 \rho_0 \right) = 0 , \tag{15}$$

with the flow w in units of lb/hr and μ the viscosity in units of lb/hr-ft.

The properties of air for use in Eq. 15 are expressed as

$$\left.\begin{aligned} &\rho_i = 40/T_i \ (\mathrm{lb/ft^3}) \\ \text{and} \qquad &\mu_i = 5.739 \times 10^{-3} + 7.601 \times 10^{-5}\, T_i - 1.278 \times 10^{-8}\, T_i^{\,2} \\ &\qquad (\mathrm{lb/hr\text{-}ft}) \quad , \end{aligned}\right\} \quad (16)$$

where T_i is the appropriate temperature in °R.

The heat transfer coefficient was calculated through the relationship

$$\begin{aligned} N_u &= 6.3 & \left(R_a \le 1000\right) \\ &= 0.806\, R_a^{\,0.2976} & \left(R_a > 1000\right) \quad , \end{aligned}$$

where N_u = the Nusselt number = hD_e/k,

R_a = the Rayleigh number = $D_e^{\,4}\, \rho^2 g\beta\Delta T c_p/\mu k L$,

h = the heat transfer coefficient, Btu/hr-ft^2-°F,

k = the thermal conductivity of the laminar film, Btu/hr-ft^2-°F,

β = the volumetric expansion coefficient, °F^{-1},

ΔT = the temperature rise over the channel length, L(°F),

c_p = the specific heat of air (Btu/lb-°F).

The expression for the Nusselt number was derived from the work of Sparrow, Loeffler, and Hubbard (Ref. 6) for laminar flow between triangular arrays of heated cylinders.

The thermal conductivity and specific heat are given by

$$\left.\begin{aligned} k &= 2.377 \times 10^{-4} + 2.995 \times 10^{-5}\, T - 4.738 \times 10^{-9}\, T^2 \\ &\quad (\text{Btu/hr-ft-°F}) \\ \text{and} \qquad c_p &= 2.413 \times 10^{-1} - 1.780 \times 10^{-6}\, T + 1.018 \times 10^{-8}\, T^2 \\ &\quad (\text{Btu/lb-°F}), \end{aligned}\right\} \quad (17)$$

where T is the appropriate temperature in °R.

These two expressions, as well as that given for the dynamic viscosity of air in Eq. 16, are least-square fits to the data presented by Etherington (Ref. 7).

TAC2D (Ref. 8), a two-dimensional transient-heat transport computer code developed by Gulf Energy & Environmental Systems, was used for calculating the system temperatures after the loss of tank water. The parameters derived above were programmed into the calculations.

The maximum temperatures reached by the fuel are plotted as a function of operating power density in Fig. 8-1 for several cooling or delay times between reactor shutdown and loss of coolant from the core. For reactor operation with maximum power density of less than 22 kW/element, loss of coolant water immediately upon reactor shutdown would not cause the maximum fuel temperature to exceed 900°C. Operation at maximum power densities greater than 22 kW/element will not result in fuel temperatures above 900°C, if the coolant loss occurs sometime after shutdown, or if emergency cooling is provided. (The time required between shutdown and the beginning of air cooling depends on power density.)

In Fig. 8-3, the data presented in Fig. 8-1 were replotted to show the time required for natural convective water cooling or emergency cooling, after reactor shutdown, to produce temperatures no greater than a given value. Thus, for example, for a reactor in which the maximum operating power density is 30 kW/element, there must be an interval of at least 1.3×10^4 sec (or 3.6 hr) between reactor shutdown and either the loss of tank water from the core or the cessation of emergency cooling.

8.2.4. Radiation Levels

Even though the possibility of the loss of shielding water is believed to be exceedingly remote, a calculation has been performed to evaluate the radiological hazard associated with this type of accident (see Table 8-6). Assuming that the reactor has been operating for a long period of time at 250 kW prior to losing all of the shielding water, the radiation dose rates at two different locations are listed below. The first location (direct radiation) is 18 ft above the unshielded reactor core, at the top of the reactor tank. The second is at the top of the reactor shield; this location is shielded from direct radiation but is subject to scattered radiation from a thick concrete ceiling 9 ft above the top of the reactor shield. The assumption that there is a thick concrete ceiling maximizes the reflected radiation dose. Normal roof structures would give considerably less backscattering. Time is measured from the conclusion of a 250 kW operation. Dose rates assume no water in the tank.

The above data show that if an individual does not expose himself directly to the core he could work for approximately 16 hours at the top of the shield tank 1 day after shutdown without receiving a dose in excess of that permitted by AEC regulations for a calendar quarter.

TABLE 8-5
CALCULATED RADIATION DOSE RATES
FOR LOSS OF REACTOR POOL WATER

Time	Direct Radiation (r/hr)	Scattered Radiation (r/hr)
10 sec	2.5×10^3	0.65
1 day	3.0×10^2	0.075
1 week	1.3×10^2	0.035
1 month	3.5×10^1	0.01

For persons outside the building, the radiation from the unshielded core would be collimated upward by the shield structure and, therefore, would not give rise to a public hazard.

8.3. REACTIVITY ACCIDENT

The rapid insertion of the total excess reactivity in the reactor system is postulated. The method of inserting this reactivity is through the rapid removal of a control rod or experiment. This reactivity insertion is the most serious that could occur. It is also the normal pulsing condition and the analysis is presented here as a point of information since it is not actually an accident condition.

The sequence of events leading to the postulated reactivity accident is:

1. The reactor is just critical at a zero power level.

2. Upward force is applied to a high worth control rod or experiment causing it to be ejected from the core and to introduce the total excess reactivity of the core; i.e., $3.00.

The consequences of the above sequence of events are:

1. An increase in reactor power to a maximum power of approximately 1200 MW.

2. A maximum energy release of approximately 16 MW-sec when the maximum fuel temperature of 539°C is reached.

3. Stresses in the stainless steel cladding of approximately 1650 psi. These pressures are caused by expansion of the air and fission

product gases and the hydrogen release from the fuel material. Neither of the preceding stress values will cause cladding rupture.

The analysis of this accident is conservative in a number of ways, some of which have been indicated in the reactor design bases (Section 3). For example, the equilibrium pressure of hydrogen over the fuel is not achieved during a pulse or step insertion of reactivity.

Analysis

It was assumed that the reactor is just critical at zero power level with a fuel and coolant temperature of 20°C. Additional input parameters are summarized in Table 8.5.

Calculations of reactor transient conditions were performed with the PULSE computer code using the preceding initial conditions and input parameters. PULSE is a reactor kinetics code based on the Fuchs-Nordheim-Scalletar model and developed by General Atomic.

TABLE 8-6
REACTIVITY TRANSIENT INPUT PARAMETERS

Reactivity insertion, \$	3.0
Prompt fuel temperature coefficient, $\frac{\delta k/k}{°C}$	-1.1×10^{-4}
β, %	0.70
ℓ, μsec	43
C_p(fuel)$\frac{\text{watt-sec}}{\text{element}}$	$817 + 1.6\ T_{fuel}$
C_p(water)$\frac{\text{watt-sec}}{\text{element}}$	879
Thermal resistances, °C, MW:	
Fuel to cooling channel	5.29×10^4
Coolant to pool	1.42×10^3

The PULSE calculations indicate that the average fuel temperatures in the U-$ZrH_{1.65}$ core would be 297°C. This temperature would occur at approximately 1.2 seconds after initiation of the transient. The peak-to-average power ratio used in these calculations was 2.21. Using this peak to average power ratio and considering the energy release during the transient coupled with the volumetric heat content of the fuel, the maximum fuel temperature was obtained on the average temperature computed by PULSE. This maximum temperature was 539°C. The reactor power level after the transient with no control rod insertion was calculated to be less than 1 MW. It has been shown in the reactor design bases that this power level poses no safety problems to the core. Of course, reactor shutdown would be initiated immediately by both power level and period trips or by manual scram and would be achieved even with the most reactive rod stuck out of the core.

During the time of peak fuel temperature the stress on the clad from the pressure produced by the expansion of air and fission product gases and the hydrogen released from the fuel is less than the strength of the clad material and therefore there is no loss of clad integrity.

Calculation of the fission product gases in a fuel element of the highest power density gives a total of 3.1×10^{21} atoms of stable and radioactive gases produced for continuous operation at 250 kW for four years. If the release fraction is taken as 1.5×10^{-5} as discussed in Section 8.1, then

$$N_{fp} = \frac{3.1 \times 10^{21}}{6.02 \times 10^{23}} (1.5 \times 10^{-5}) = 7.7 \times 10^{-8} \text{ moles.}$$

The partial pressure exerted by fission product gases is

$$P_{fp} = N_{fp} \frac{RT}{V} = 7.7 \times 10^{-8} \frac{RT}{V}$$

where initially the volume V is taken as a 1/8-in. space between the fuel and reflector end piece. This is conservative since the graphite reflector pieces have a porosity of 20%.

The volume then is

$$V = \pi r^2 h = \pi(1.80)^2\ 0.317\ \text{cm} = 3.23\ \text{cm}^3 \quad .$$

From this, one obtains

$$P_{fp} = 2.40 \times 10^{-8}\ RT \quad .$$

The partial pressure of the air in the element is

$$P_{air} = \frac{RT}{22.4} \times 10^3 = 4.46 \times 10^{-5}\ RT \quad .$$

The total pressure exerted by the air and fission products is

$$P_1 = (4.46 + 0.002) \times 10^{-5}\ RT = 4.46 \times 10^{-5}\ RT = P_{air} \quad .$$

Also we have

$$P_{air} = 14.7\ \frac{T}{273}\ \text{psi} \quad .$$

As an upper limit, assuming an air temperature of 539°C or 812°K (equal to the peak fuel temperature), one obtains

$$P_1 = (14.7)\ \frac{812}{273} = 44\ \text{psi} \quad .$$

The equilibrium hydrogen pressure over $ZrH_{1.65}$ at 539°C is negligible. The total internal pressure then is

$$P_t = P_h + P_1 = 44 \text{ psi} \quad .$$

Assuming no expansion of the clad, the stress produced in the clad by this pressure is

$$S = \left(\frac{r}{t}\right) P_t = \left(\frac{0.735}{0.020}\right) P_t = 36.75\ P_t = (36.75)\ (44) = 1620 \text{ psi} \quad .$$

For a reactivity insertion of \$3.00, the clad surface temperature would be approximately equal to the saturation temperature of the water which is 113°C at a pressure of 23.4 psia. At this temperature, the ultimate tensile strength for type 304 stainless steel is approximately 70,000 psi. Comparing this strength with the stress applied to the cladding during the reactivity insertion, it is seen that the strength of the material far exceeds the stress which would be produced. Therefore there would be no loss of clad integrity or damage to the fuel as a result of the reactivity accident.

Chapter 8 References

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