SAXTON NUCLEAR EXPERIMENTAL CORPORATION

DOCKET NO. 50-146
LICENSE DPR

Amendment No. 1 to Change Request No. 25

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1. Applicant hereby submits Amendment No. 1 to Change Request No. 25 supplying additional information requested in Division of Reactor Licensing letter of April 12, 1967.

SAXTON NUCLEAR EXPERIMENTAL CORPORATION

By /s/ R. E. Neidig
President

I. Your calculations indicate that the upper limit accident results in a two hour whole body dose in excess of 25 rem at the minimum exclusion radius of 800 ft. We request that you tell us how you intend to meet the exclusion area critieria of 10 CFR Part 100.

ANSWER

The calculation described in Question VII of this submission with results presented in the Saxton 35 MWt Safety Report results in a two hour whole body dose of less than 25 rem for distances beyond 270 meters (886 ft.). On the south side of the containment there is a small corner of property amounting to about .01 acre which is within 270 meters of the containment surface and is not part of the Pennsylvania Electric Company property. The closest point of this property is 865 feet from the containment center or 840 ft. from the containment surface. The dose calculation presented in the 35 MWt Safety Report results in 29.7 rem in 2 hours at this closest point of the property which is 18.8% above the 25 rem limit of 10 CFR 100. The following paragraphs discuss conservatism in that calculation and additional shielding pertinent to the sector in question which show that the dose to a person standing on this property in the worst 2 hours following the hypothetical accident would indeed be less than the 25 rem limit.

1. The calculation described in Question VII with results presented in the Safety Report were based on 66.3% of the containment free volume being above the operating floor and shielded only by the steel containment structure. A precise evaluation of the free volumes above and below the operating floor results in 58.5% of the free volume above the operating floor. The doses presented are therefore conservative by $\frac{66.3 - 58.5}{66.3} = 11.7\%$ in all directions from this consideration.

2. In the property region in question the Control and Auxiliary Building,

the scale house, the coal handling sheds, and a 10 ft. high earth dike give additional shielding not included in the analysis. The Control and Auxiliary Building is a two story building constructed of concrete block and is 24 feet above grade. The lower floor of this building has no windows and shadow shields the lower 12 feet of the containment from the nearest point of this property. The second story is also 12 feet and shields approximately 50% of the subtended angle from the nearest point. The reduction in dose from the shadow shielding provided by this building is calculated to be about 35%.

With the above considerations the dose at the closest point of this property, 840 ft. from the containment surface, would be about

$$(29.7)\ (1-.117)\ (1-.35) = 17 \text{ rem in two hours.}$$

On the north side of the containment just across the Juniata River the nearest property is 800 ft. from the containment center. On this side of the containment there is an additional 2-3 ft. of external concrete shielding around the lower 9 3/4 ft. of the containment above the operating floor. Considering this shielding and the 11.7% conservatism in the fraction of activity above the operating floor used in the initial analysis, the 2 hour dose is less than 25 rem for distances beyond 830 ft. from the containment center. The property in this 30 ft. region is not owned by Penelec, but has been leased and will be a part of the controlled access area during the 35 MWt program.

II. A. 1 Discuss the relative reliability of safety valves with and without anti-simmer devices and their required auxiliaries. Is the quality and reliability of the solenoid valves equal to that of the safety valves?

ANSWER

The anti-simmer device does not effect the relative reliability of the safety valve. (See Question II. A. 2). As the name implies, this device merely prevents simmer or weeping, by a fixed assist, when the system pressure is close to the set pressure of the safety valve. In the remote possibility that this anti-simmer assist fails to unload, the safety valve will still operate at the set pressure plus 4% which is well within the 110% code requirements. The quality of the solenoid valves, the pressure regulator and the servo pressure relief valves are of equal quality and reliability to that of the safety valves. In addition, redundancy is provided even though such redundancy is not required by code. The redundancy consists of two three way solenoids, either of which can operate the device, and a servo pressure relief valve set about 2% above the pressure regulator pressure thus assuring that the assist of about 4% cannot be exceeded.

II. A. 2 What is the status of anti-simmer devices with respect to the ASME Pressure Vessel Code?

ANSWER

The anti-simmer safety valve is a conventionally designed, spring loaded, self-actuated safety provided with an air loading device mounted to the bonnet cap that transmits a constant force to the valve spindle. The anti-simmer device is not directly attached to the safety valve spindle.

The air loading device is a diaphragm operator. The operator is energized through two three-way solenoid valves by a pressure regulator adjusted to provide the necessary spindle force. A soft seated safety valve is provided on the diaphragm operator as a safety protection against loss of functional performance of the pressure regulator and is set to relieve at approximately 2% above the regulator pressure. The electrical signal that energizes the solenoids (permitting air to flow to the diaphragm operator) is obtained from a pressure sensing element attached to the pressurizer.

During normal conditions with the system operating below design pressure, air is supplied to the diaphragm operator, exerting a force equal to 4% of the safety valve set pressure to the safety valve spindle. As the reactor coolant system pressure approaches the set pressure of the safety valve, the pressure sensing switches de-energize the solenoids and the diaphragm operator is vented, thus freeing the valve to function solely on spring load.

The solenoids will vent upon loss of the electrical signal providing a "fail safe" feature in event of electrical power failure. The soft-seated safety relief valves are provided to prevent accidental over-pressurization of the diaphragm operator. Under all possible failure modes in the control circuits, valve operation at set pressure plus 4% is assured which is well within the code accumulated pressure limit of 110% x design pressure.

Proof testing of the anti-simmer device has demonstrated a pop-pressure and blowdown repeatability that cannot be achieved with the spring loaded valve.

The anti-simmer safety valve is not a power operated safety valve, i.e., the valve is not dependent on the external power source to develop lift at set pressure. It does utilize an auxiliary power source to prevent weeping in that operating range where weeping normally occurs but the auxiliary power plays no part in the actual popping of the valve. Therefore, no code interpretation is necessary since the valve is not power operated.

The use of the anti-simmer device on the pressurizer safety valves has been reviewed by Mr. F. N. Moschini, Advisory Engineer, Westinghouse Atomic Power Divisions, with Mr. John Riddiough of the Department of Labor and Industry of the Commonwealth of Pennsylvania. Mr. Riddiough, by letter of August 16, 1966, accepted the anti-simmer device for operation at Saxton, acknowledging that the normal function of the safety valve is not affected by the addition of the anti-simmer device.

The traditional interpretation of a spring loaded safety valve under the rules of Sections I and VIII of the ASME Boiler & Pressure Vessel Code has precluded the use of any auxiliary device on a safety valve. Under the definition of Section III, par. N-911 of the ASME B&PV Code, the use of auxiliary devices, even including power-operated devices for lifting the valve (which the anti-simmer device definitely is not) are permitted so long as they do not interfere with the normal operation of the safety valve.

Mr. Moschini, a member of the Subcommitte on Nuclear Power of the ASME B&PV Code, has reviewed the use of the anti-simmer device with other members of the Subcommittee who concur that the safety valve with the anti-simmer device meets the intent of the par. N-911 of Section III of the ASME B&PV Code.

II. A. 3 Please supply an electrical schematic diagram of the anti-simmer circuits.

ANSWER

The electrical schematic diagram of the anti-simmer device circuit is shown in Figure II. A - 3 which supersedes Figure III - 1 of the Saxton 35 MWt Safety Report.

II. A. 4 Can a single solenoid valve vent the full capacity of the pressure regulator?

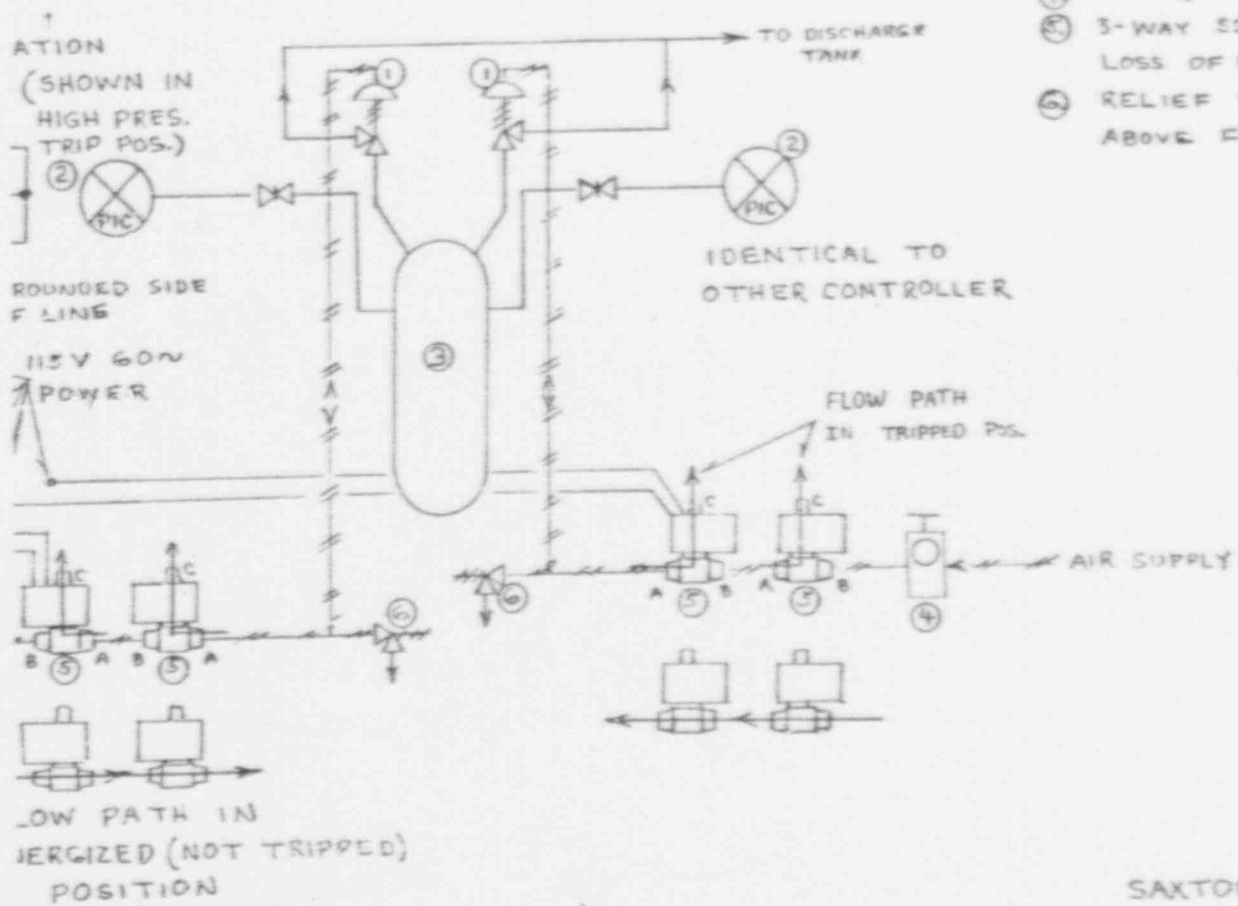
ANSWER

The solenoids are not intended to vent the pressure regulator, however the relief valve can vent the full capacity of the pressure regulator thus insuring that the assist does not exceed 4% of the safety valve set pressure. Either solenoid valve can vent the anti-simmer operator and thus unload the assist completely. See Figure No. II-A-3.

II. A. 5 How will the system be tested? How often will the system be retested?

ANSWER

The anti-simmer device will be tested whenever the safety valve is tested.



1. AIR LOADED, SPRING OPERATED SAFETY VALVE
2. PRESSURE INDICATING CONTROLLER
3. PRESSURIZER
4. FILTER REGULATOR WITH GAUGE
5. 3-WAY SOLENOID VALVE. UPON LOSS OF POWER, VALVE VENTS A TO C
6. RELIEF VALVE. SET APPROX 2% ABOVE FILTER REGULATOR (4).

SAXTON SAFETY VALVE
WITH ANTI-SIMMER DEVICE

II. B. 1 Please supply a schematic diagram similar to Figure III.2 showing the existing reactor trip circuits as well as the added circuits.

ANSWER

See Revised Fig. III-2

II. B. 2 Why is the permissive switch which bypasses the new reactor trip signals provided? If it is necessary to bypass the new trip signals at lower power, can this be accomplished by redundant, automatic equipment?

ANSWER

The permissive switch is provided in order that the reactor coolant pump can be run from the normal 440 V power supply without getting a low frequency reactor trip. The bypass would be used only if power operation is less than the present 23.5 MWt licensed power level. For power levels less than 23.5 MWt the low flow trip prevents fuel damage without the coastdown inertia supplied by the MG set. The loss of load analysis has shown that the pressure reducing valve trip and the auto stop oil pressure trip are not needed for plant protection even with operation at 35 MWt. Adequate protection is provided without these trips when the power is 23.5 MWt or less. Even at 35 MWt the absence of the low frequency trip would not endanger public health and safety. The analysis (see Question II.C.2) for loss of flow without benefit of the MG set provided coastdown inertia indicates limited clad damage and the consequence would be release of some gap activity into the coolant.

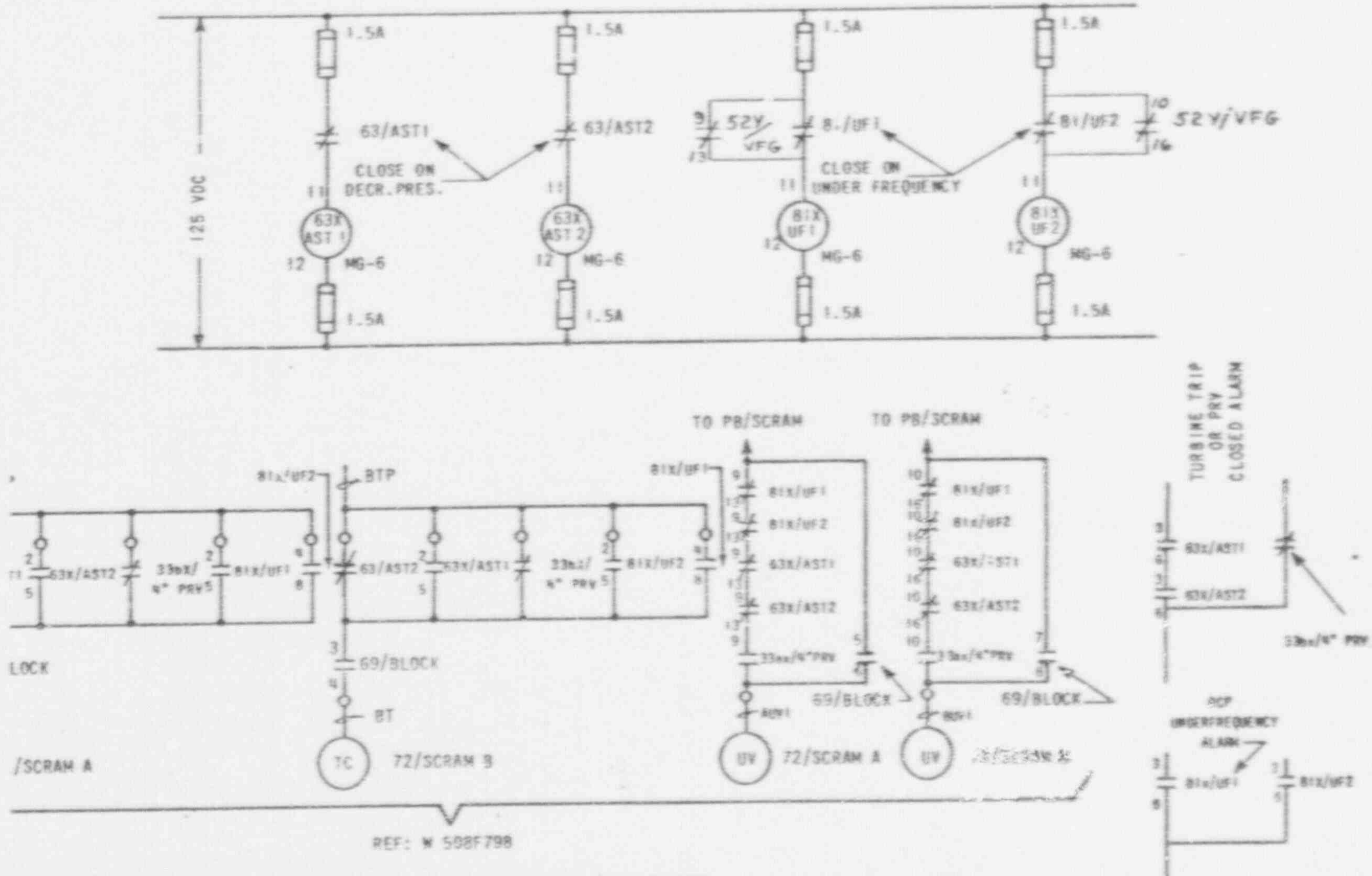
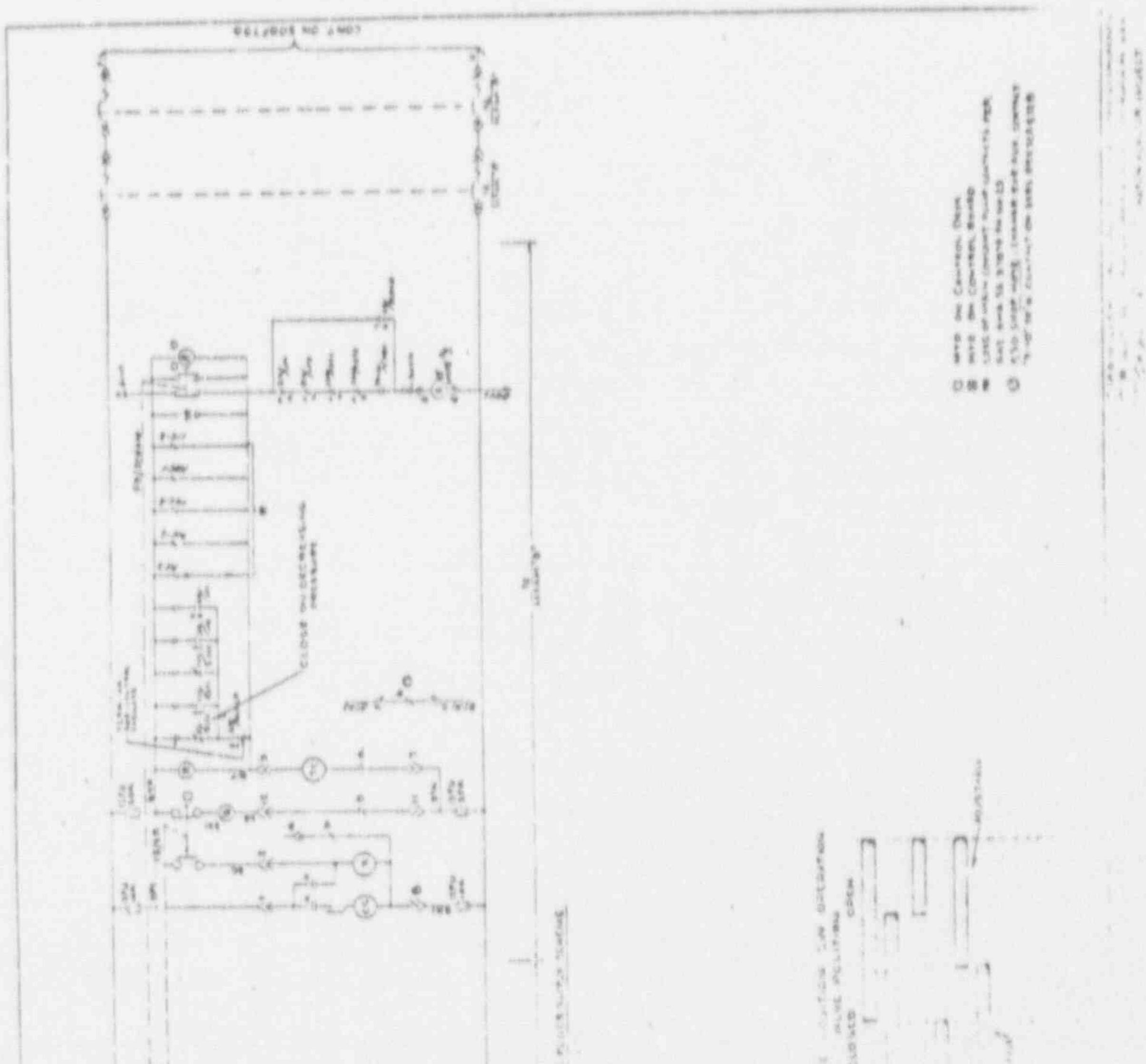


FIG. III-2 - MODIFICATION OF SAXTON REACTOR TRIP CIRCUITS



MODIFICATION OF SAXTON REACTOR TRIP CIRCUITS
Revision 1

II. B. 3 Can the low flow scram by itself protect against a loss-of-flow accident from full power?

ANSWER

The low flow trip will prevent core damage for a loss of flow occurring from full power provided the MG set inertia is available. Without variable frequency set inertia limited core damage may occur (See answer to Question II - C - 2). The loss of flow analysis in the safeguards report was based on the low flow trip.* The minimum DNB ratio was 1.31 based on the following conditions.

Power: 35 MWt x 1.03 = 36.05 MWt

Inlet temperature: 480° + 5° = 485°F

Primary pressure: 2200-50 = 2150 psia

Initial flow rate corresponding to MG set low frequency reactor trip setpoint: 60 cps (95% nominal)

The minimum DNB ratio would be greater than 1.31 had credit been taken for the low frequency trip.

Assuming that a complete loss of power to the variable frequency set occurs from a flow condition equivalent to the low flow trip and equivalent to 3 cycles per second below the low frequency trip, the minimum DNB ratio for the above adverse conditions of power, inlet temperature, and primary pressure is 1.26.

* Note: The low flow trip setpoint listed in the Technical Specifications Change Request No. 25 is a misprint. It should be 2.89×10^6 lbs/hr. instead of 2.75×10^6 lbs/hr.

II. C. 1. What prevents the reactor coolant pump from operating from the 440 volt bus at high power? Could the underfrequency reactor trip be made ineffective by operating the MG set at 63 cycles with the generator breaker (52 VFG) open?

ANSWER

The reactor coolant pump cannot operate from the 440 volt bus at high power because contacts have been installed in parallel with the low frequency contacts that will trip the reactor whenever breaker 52/VFG is open. See the revision to drawing III-3a, in the Safeguards Report for the Saxton Reactor Operating at 35 MWt.

II. C. 2 What are the consequences of a loss-of-flow accident from maximum reactor power caused by inadvertent opening of the MG set generator circuit breaker 52/VFG?

ANSWER

Opening circuit breaker 52/VFG would result in loss of flow without benefit of the MG set inertia. The reactor would be immediately tripped by the breaker trip which is backed up by the underfrequency trip and the low flow trip. Based on the following adverse initial conditions, the minimum DNB ratio is 0.992 and occurs in 1.6 seconds. Reactor trip was assumed to result from the delayed low flow trip.

Power: 35 MWt x 1.03 = 36.05

Inlet temperature: 480 + 5 = 485°F

Primary pressure: 2200 - 50 = 2150 psia.

Approximately 107 rods would have a DNB ratio less than 1.3. It is conservative to estimate that less than 107 fuel rods might have clad damage if no inertia is available. Release of gap activity from the 107 rods would increase the coolant activity by approximately 1500 μc/cc iodine isotopes and 1500 μc/cc noble gases. The plant would be taken to hot shutdown conditions and purification flow may be initiated. Assuming a demineralizer removal efficiency of 0.9 per pass for the non-gases and a flow rate of 10 gpm through the demineralizer the iodine activity would be reduced by a factor of 1000 in 40 hours. The noble gas activity would be vented to the waste gas tanks for decay and controlled release to atmosphere. After 45 days holdup the noble gas activity will have decayed to about 650 curies and may be released at a controlled rate within the 3750 curie per year release limit.

II. D. 1. Could a single failure in the flow totalizer keep the pumps from starting or stop them prematurely?

ANSWER

The flow of water from the safety injection pumps is split into two 3-inch streams. Each of these streams is monitored for flow by an orifice and differential pressure transmitter. The output of the transmitter, an air signal of 3-15 psi, goes to a square root converter whose output is 3-15 psi proportional to 0-500 gpm flow.

A computing relay receives the air signal from each flow path and averages them accordingly; output, $C = \frac{A + B}{2}$. This output signal is an increasing air pressure for an increasing flow, and goes to the integrator.

The integrator consists of an electric motor driven cam and an air positioned cam switch. The motor driven cam rotates at a constant 5 rpm and operates the cam switch once each cycle. The cam switch contacts are so positioned by the measuring element that the duration of contact is proportional to the measured flow. The duration of contact determines the time current is available to the timer and hence the total time involved to trip the control relay and the safety injection pumps.

The control relay is an energize-to-trip relay and hence it is fail-safe. The motor in the integrator, the motor in the timer and the control relay have a common power supply.

Since all the air signals are an increasing pressure to indicate an increasing flow, these circuits are extremely reliable.

II. D. 2. Could the injection block switch disable all safety injection? Can a single operator error or a single switch failure disable safety injection?

ANSWER

The "Safety Injection Block" switch on the control console prevents initiation of safety injection due to low pressure in the main coolant system or operation of the safety injection relay. Both pumps can be started by control switches located on the control console. Breaker position indicating lights are located adjacent to the control switches and also at the switchgear.

The block switch is alarmed in the control room when in the "block" position. Prior to reactor startup all alarms are recorded and the reason for the alarm established.

The mechanical integrity of the block switch is enhanced by using separate switch sections for the two pumps. The safety injection system is operationally checked once each month.

III. A. Prepare a distribution curve showing the fraction of the core (and number of rods) operating at the various power levels for design and overpower conditions.

ANSWER

The design and best estimate radial power distributions are plotted in Figure III-A. The design curve is used for both the design power and overpower cases, unless otherwise specified.

III. B. Using the appropriate DNB correlation and the above distribution, determine the corresponding DNB ratios and the statistical number of fuel rods that could experience DNB.

ANSWER

A preliminary evaluation was made to predict the number of fuel rods in the core that might reach DNB, both under normal operating conditions and under assumed overpower conditions. For this calculation, a convolution procedure was utilized in which the product of the number of fuel rods experiencing a given DNB ratio and the probability of DNB was summed over the entire core.

Two cases were investigated using this method: one in which the nominal conditions (P = 100% of 35 MWT, T_{inlet} = 485.0°F and pressure = 2200 psia) were used and a second in which the worst overpower conditions (P = 114% of 35 MWt, T_{inlet} = 485.5°F and Pressure = 2200 psia) were used. The design power distribution corresponding to a peak factor F_Q = 2.81 was used. The results obtained for these cases were that for the nominal 100% power case, 0.05 rods might experience DNB and for the worst overpower case, 0.74 rods might experience DNB.

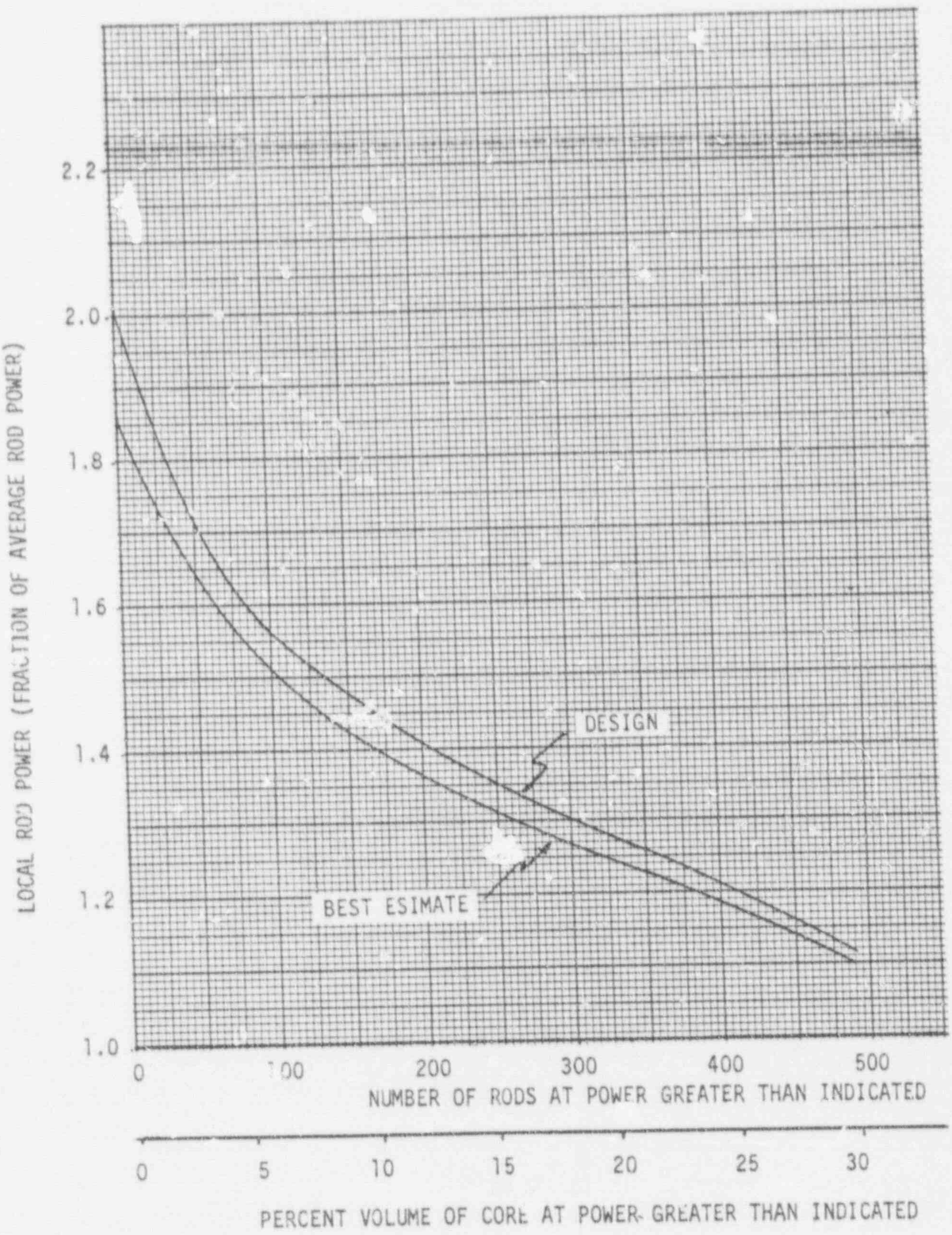

LOCAL ROD POWER (FRACTION OF AVERAGE ROD POWER)
2.2
2.0
1.8
1.6
1.4
1.2
1.0
DESIGN
BEST ESIMATE
0
100
200
300
400
500
NUMBER OF RODS AT POWER GREATER THAN INDICATED
0
5
10
15
20
25
30
PERCENT VOLUME OF CORE AT POWER GREATER THAN INDICATED

FIGURE III-A

III. C. Perform an uncertainty analysis by arbitrarily assuming certain errors in major parameters used in calculating the number of rods experiencing DNB. For example, calculate the number of rods with DNB, as a function of possible percentage errors in the DNB correlation, power distributions, flow rates, and power levels.

ANSWER

The following summary is presented to show the effect of major parameters on the statistical number of fuel rods which may experience DNB, using the design axial power distribution and the design (2.02) and best estimate (1.88) peak radial factors.

(a) Effect of Varying Power Distribution

Power Distribution	Power (% of Nominal)	T_{in} (°F)	Pressure (psia)	Statistical Number of Fuel Rods Which may experience DNB*
Design	114	485.5	2000	0.74
Best Estimate	114	485.5	2000	0.29

(b) Effect of Varying Power Level

Power Distribution	Power (% of Nominal)	T_{in} (°F)	Pressure (psia)	Statistical Number of Fuel Rods Which may experience DNB*
Best Estimate	100	485.5	2000	0.04
Best Estimate	114	485.5	2000	0.29

(c) Effect of Varying Flow Rate at 114% of Nominal Power

Power Distribution	Flow (% of Nominal)	T_{in} (°F)	Pressure (psia)	Statistical Number of Fuel Rods Which may experience DNB*
Best Estimate	100	485.5	2000	0.29
Best Estimate	90	485.5	2000	0.64

* The maximum number of rods which could experience DNB taking into account the distribution of the experimental data from which the W-3 DNB correlation was developed, and the distribution of the power in the core.

IV. A In view of anticipated high burnups and power levels, wouldn't it be reasonable to assume that fuel swelling will occur at a higher rate than .0016 $\left(\frac{\Delta v}{v}\right)$ per 10^{20} fissions/cc as shown in Figure 7 of Reference 1.

ANSWER

Total fuel pellet swelling has been evaluated by a simplified model to calculate expansion for peak temperature conditions. Total swelling from accumulation of fission products may be expressed as

$$\frac{\Delta v}{v} = \left(\frac{\Delta v}{v}\right)_s + \left(\frac{\Delta v}{v}\right)_g$$

Where $\frac{\Delta v}{v}$ is fractional volume expansion, and the subscripts "s" and "g" refer to swelling from solid and gaseous fission products, respectively. The model assumes the following:

1. $\left(\frac{\Delta v}{v}\right)_s = 0.0016 \frac{\Delta v}{v}$ per 10^{20} fission/cc, effect of solid fission product.

2. Volume swelling from fission gas accumulation in the fuel matrix results from isotopic expansion of gas bubbles within a plastic matrix, assuming yielding of the matrix according to the Von Mises criterion.

3. Fractional fission gas release according to the Booth diffusion model.*

Fission gas release from the fuel by the recoil mechanism was conservatively assumed to be negligible. A yield strength of 14,000 psi for the fuel matrix was used, with the internal pressure from fission gas governed by the natural gas law. The fuel pellet was assumed to be isothermal, with an average temperature of 1880°K (at 19.1 kw/ft). With these assumptions, calculated swelling of fission gas bubbles is $\left(\frac{\Delta v}{v}\right)_g = 0.058$.

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* A.H. Booth, "A Method of Calculating Fission Gas Diffusion from UO_2 Fuel and its Application to the x-2-f Test," CRDC-721, September 1957.

The model predicts no volume increase in fuel matrix from fission gas accumulation until the swelling of the gas bubbles exceeds the initial porosity (in this case, 6%) of the fuel. Prior to this, the low swelling rate of 0.0016 $\frac{\Delta v}{v}$ per 10^{20} fissions/cc may be assumed.

This analysis indicates that a burnup of 50,000 MWD/MTU at 19 kw/ft is not sufficiently large to produce the higher swelling rate of (0.007 $\frac{\Delta v}{v}$ per 10^{20} fissions/cc), given an initial fuel density of 94% of theoretical.

IV. B. What will be the temperature distribution in a fuel element in a high flux position? What will be the temperature gradient across the cladding, across the gap and in the fuel itself if the density of the fuel is varied between some specified limits?

ANSWER

The temperature distribution in the fuel element in the high flux position (19.1 kw/ft), including the temperature gradient across the Zr cladding and the gap, is presented in the attached Table IV-B. This result includes the temperature distributions for a maximum density variation of ± 2% of the theoretical density value.

FUEL ROD TEMPERATURE DISTRIBTUION (°F)

Table IV - B
35 MWt
Zr Cladding

Fuel Density	Fuel			Cladding		
(%T.D.)	Center Line	Average	Surface	Inner Surface	Average	Outer Surface
96	4210	2560	1040	782	718	654
94*	4250	2580	1030	782	718	654
92	4290	2590	1010	782	718	654

*Design Value

IV. C. What are the data which support the statement, "tube reduced Zircaloy will not fail for 7.4 years at the maximum expected design stress of 16,000 psi?"

ANSWER

Information from the literature (1-5) has been used to construct a stress rupture vs. Larson-Miller parameter-type plot. A criterion of a 1% creep strain limit for irradiated Zircaloy-2 was assumed, conforming to the PRTR design limit. The referenced data were obtained by uniaxial tests of Zircaloy strip is significantly lower than the ultimate hoop strength of Zircaloy tubing).

Based upon a typical PWR stress level of 16,000 psi (tensile or compressive) the temperature and time-to-rupture functional relationship has been approximated as $T(20 + \log t) = 28.85 \times 10^3$ for tube-reduced Zircaloy-4. T = degrees Rankine, t = hours to failure.

For a mean clad temperature of 700°F, t $\sim$ 7.4 year.

For a mean clad temperature of 721°F, t $\sim$ 3.2 year.

Recent Bettis data (6) on 15% cold-worked Zircaloy-4 tubing indicate that these figures are conservative.

NOTE:

The maximum stress is expected to be less than 16,000 psi for a 19.1 kw/ft rod with nominal gap and the maximum burnup of 30,000 MWD/MTU. However, with the minimum cold gap expected the clad stress will probably exceed 16,000 psi, but will remain well below the yield strength of the material and the strain will remain well below 1% (See Question IV-D).

References:

1. KAPL-2110, R. L. Mehan and F. W. Wiesinger, Feb. 1961
2. KAPL-2000-9, Reactor Technology Report No. 12, Metallurgy, March 1960.
3. AECL-1048, E.C.W. Perryman, June 1960.
4. HW-75267, P. J. Pankaskie, October, 1962.
5. HW-7[illegible]8, R. L. Knecht and P. J. Pankaskie, March, 1962.
6. WAPD-TM-585, C. R. Woods, ed., December, 1966, p . 126.

IV. D What are the applicable composition, dimensions, and inspection specifications for zirconium alloy tubing and the closure that make sure that the expected design stresses cannot be exceeded?

ANSWER

The Zircaloy clad PuO_2-UO_2 fuel rods were designed using the same basis that are now being used for the design of the IPP-II, RCE and other W designed PWR reactors. The design bases are:

a. The clad is free standing under normal reactor operating conditions.
b. The clad stresses are below the yield strength of the material (approximately 40,000 psi for unirradiated Zircaloy 4 at 725°F) at all times under normal operating conditions.
c. The clad strains, considering the combined effects of internal pressure, external pressure, fuel pellet swelling and clad creep are limited to 1% throughout core life.
d. The pellet to clad cold gap is sized so that at the hot spot under normal operating conditions, the gap is essentially zero at the beginning of life.
e. A gas plenum is provided at the end of the fuel rod so that the internal gas pressure is always less than the external coolant pressure under normal operating conditions.

An analysis of the clad stresses and strains under the proposed operating mode shows that the stresses and strains are within the design limits defined above at all times. The analysis was based on a peak power density in the fuel of 19.1 kw/ft at a peak burnup of 23,500 MWD/MTF occurring immediately upon achieving 35 MWt operation. A linear decrease in peak power from 19.1 to 16.7 kw/ft at 50,000 MWD/MTF was used in the analysis. (Even though the peak burnup of the fuel assemblies will be approximately 30,000 MWD/MTF.) The clad peak O.D. stress and strain for various burnups are shown below:

	PEAK BURNUP MWD/MTU					
	23,500 MWD/MTU		44,000 MWD/MTU		50,000 MWD/MTU	
Initial Cold Gap	Total O.D. Stress	Total Strain	Total O.D. Stress	Total Strain	Total O.D. Stress	Total Strain
.0071 in.	3,300 psi	.007%	11,600 psi	.08%	19,400 psi	.26%
.0050 in.	30,400 psi	.298%	15,300 psi	.64%	19,300 psi	.9%

The initial cold gap of .0071 in. is the nominal gap while the .0050 in. is the minimum cold gap expected. Below 44,000 MWD/MTF the fuel swelling rate was taken as 0.16% $\frac{\Delta v}{v}$ per 10^{20} fissions/cc. Above 44,000 MWD/MTF the swelling rate is expected to be greater and a value of .7% $\frac{\Delta v}{v}$ per 10^{20} fissions/cc was used. This increased swelling rate is primarily responsible for the peak clad strain of .9% at 50,000 MWD/MTF, if this burnup could be achieved.

The maximum stress of 30,400 psi occurs at the initiation of 35 MWe operation primarily because of fuel clad differential thermal expansion. The stress reduces rapidly after reaching this peak because of clad creep and resulting stress relaxation.

The cladding dimensions, pellet sizes, and radial and end gaps for the Core II fuel rods are given in Table IV-3 of the Safeguards Report for The Saxton Reactor Partial Plutonium Core II. The moisture content and allowable gas volumes specified for the Core II fuel are as listed in Table IV-4 of the same report.

The nondestructive and destructive tests used to ensure the tubing integrity were as follows:

TEST	No. Samples/Tube Lot
1. Room temperature mechanical properties	2
2. Elevated temperature tensile test	2

3. Corrosion test, 750°F and 1500 psi for 3 days	2
4. Elevated temperatures burst test	1
5. Ultrasonic testing - standard established	each tube
6. Fluid penetrant test on outside diameter	each tube
7. Hydride orientation	6

For welding of the tubing, the weld parameters and inspection procedures were established to ensure a minimum weld thickness equal to 90% of the minimum tube wall thickness. In the case of the Vipac fuel rods, the weld parameters and procedure were established based on a lot of 14 sample welds delivered to WAPD for evaluation. The actual fuel rod welds were inspected for acceptance by visual examination and sectioning of welds on one sample rod per lot of 19 rods or less. The sample welds were checked by photomicrograph for minimum wall and minimum weld thicknesses.

For the pelletized rods, the weld parameters and procedures were established on the basis of 10 preproduction sample rods supplied to WAPD for evaluation. Acceptance of the production rod welds was based on visual examination of all welds and sectioning of welds on one sample rod per lot of 33 rods or less. As with the vipac rods, the welds on the production sample rods were checked by photomicrograph for minimum weld thickness. The welds on each rod were also dye penetrant inspected for surface defects.

In all cases the rods were helium leak tested after welding and as a final check against weld and tube surface metallic inclusion or contamination, each rod was corrosion tested.

IV. E. Under normal conditions stresses in the cladding will be compressive and strains will be limited by the fuel pellet; but what will be the stress and strain pattern in a sudden decompression followed by a scram when fuel temperatures are high and fission gas pressure is effective.

ANSWER

Under normal conditions in the hot rods there will be contact between the clad and pellet and the clad will be in tension. Clad strain is controlled primarily by fuel pellet thermal expansion and swelling because fission gas pressure is always less than the system pressure. On sudden depressurization the fission gas pressure may exceed system pressure but will not be greater than approximately 2000 psi. Figure IV-E shows the burst pressure as a function of clad temperature. For a mean clad temperature of 721°F the burst pressure is seen to be in excess of 6000 psi; therefore, no clad failure will occur.

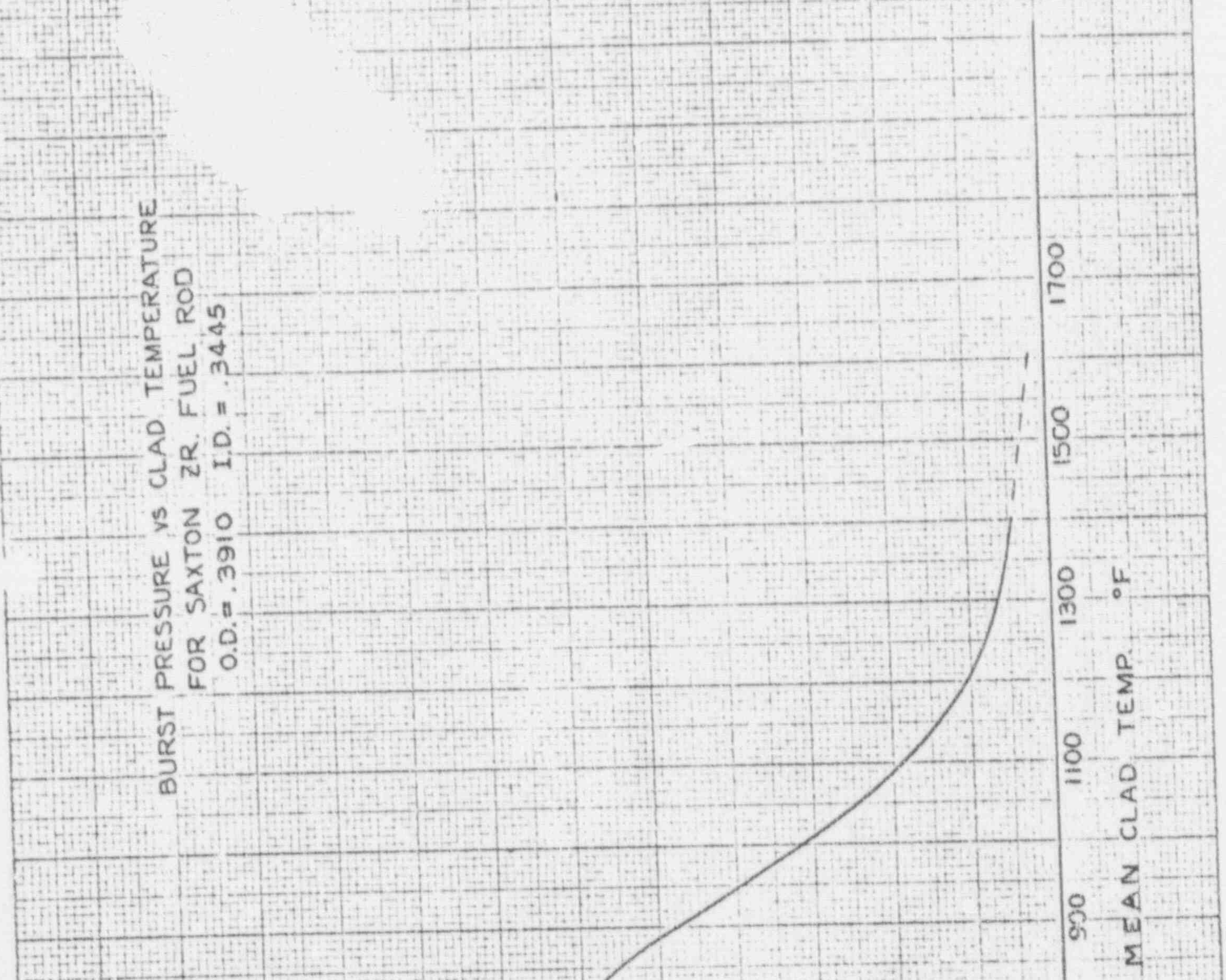
IV. F. Is there experimental data available on hydrogen absorption by cladding that goes beyond the burnups and power level of Reference 3 page V-3? What is the effect of hydrogen absorption by cladding. on its fatigue properties and what is the combined effect of hydrogen absorption and radiation embrittlement?

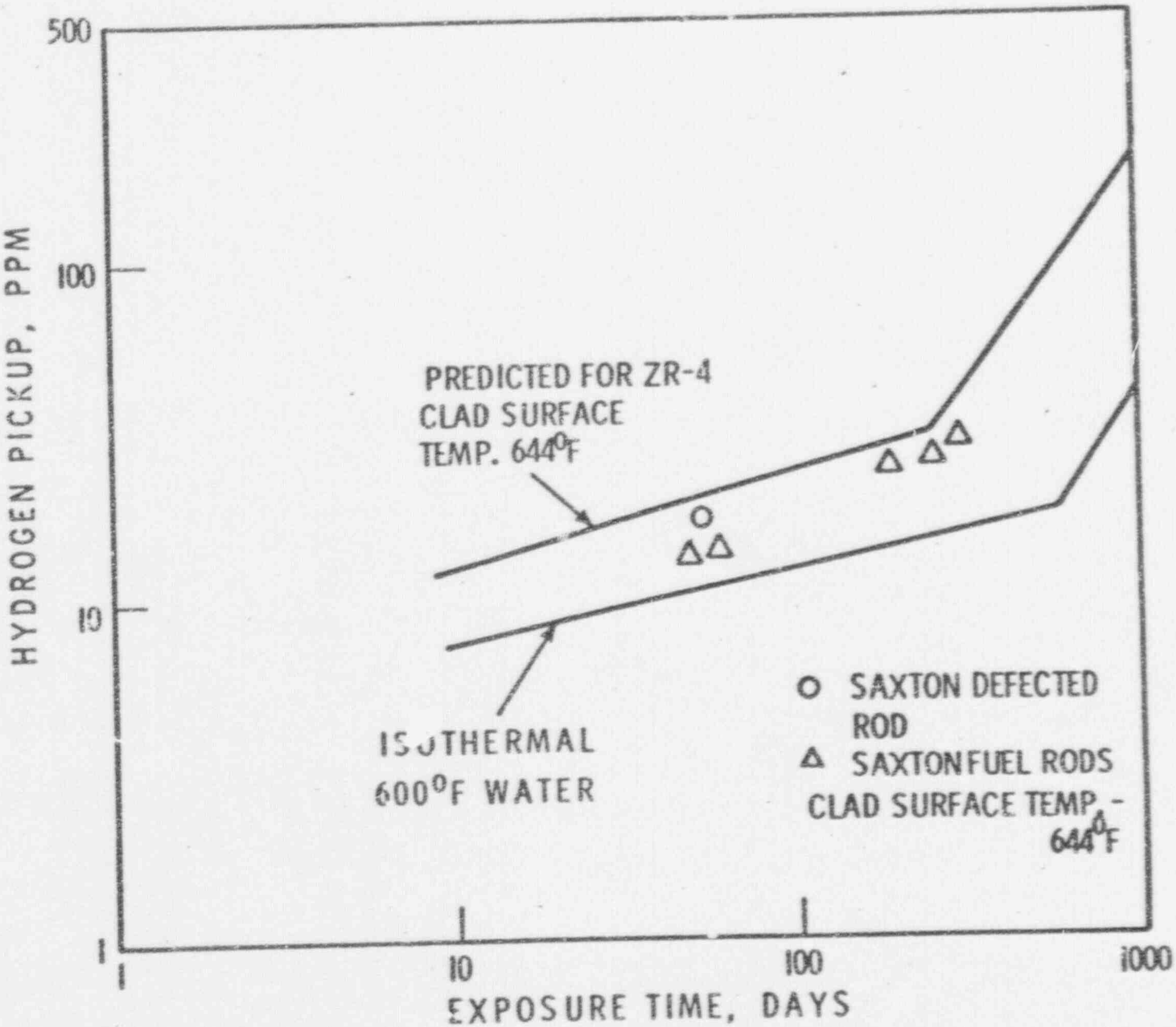
ANSWER

1. Study of the in-reactor performance of Zircaloy clad fuel rods exposed to a borated pressurized water environment has been performed in the Saxton reactor.[1] Irradiated fuel rod cladding specimens were obtained from give experimental sub-assemblies which operate as part of the Saxton reactor core at surface heat flux levels of 400,000 to 500,000 BTU/hr-ft^2. Nucleate boiling was experienced over most of the length of the rods, resulting in a clad surface temperature of 642°F. In-reactor exposure ranged from 40 to 300 days (3000-17,000 MWD/MTU) at temperature.

   Hydrogen content of the irradiated cladding was determined by hot extraction techniques and metallography of the irradiated fuel ro cladding. The hydrogen pickup of the cladding while operating in Saxton is illustrated in the attached Figure IV-F. The results

FIGURE IV-E


BURST PRESSURE vs CLAD TEMPERATURE
FOR SAXTON ZR. FUEL ROD
O.D. = .3910 I.D. = .3445
BURST PRESSURE - KSI
0
1
2
3
4
5
6
7
8
MEAN CLAD TEMP. °F
700
900
1100
1300
1500
1700



HYDROGEN PICKUP FOR ZIRCALOY IN SAXTON

FIGURE IV-F

are compared with out-of-pile tests in 600°F water and the computed hydrogen pickup for Zircaloy-4, when operating at 644°F surface temperature. The hydrogen absorption by the cladding does not differ from out-of-pile results. WAPD observed that 20 to 35% of the hydrogen produced by the corrosion reaction was absorbed by the clad.

The hydrogen pickup of the defected rod is also in agreement with the results for other Zircaloy clad rods in Saxton and the predicted hydrogen pickup for Zircaloy-4. The value (18 ppm) is equivalent to absorption of 21% of the amount of hydrogen produced by the corrosion reaction.

2. The effect of hydrogen absorption on mechanical properties of zircaloy is discussed by Coleman and Hardie[(2)]. As illustrated by Figure #9 of that paper, the fracture transition temperature is well below the operating temperature of the Saxton Zircaloy-4 cladding, at anticipated hydrogen pickups. No significant loss of ductility from hydriding is expected in the Saxton cladding, and this effect may be neglected with respect to strain fatigue behavior.

A model[(3)] for strain fatigue under multiaxial stress conditions has been developed, applicable to the design of Zircaloy-4 cladding at conditions of approximately 5-20% cold work and between 650 and 800°F. The model was applied to a design problem involving fuel rod cladding for the San Onofre Reactor, taking into account plant power cycling and the effect of irradiation on Fracture ductility. The predicted fraction of clad fatigue life consumed over a core lifetime of 45 months was only 30%. Within the limitations of this study it appears that there will not be a strain fatigue problem with the Saxton Zircaloy-4 clad.

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(1) D.R. McClintock, "In-Pile Experience with Zircaloy Clad Fuel Rods in a Borated PWR Environment. TRANS. ANS 9, p.32.

(2) C.E. Coleman and D. Hardie, "The Hydrogen Embrittlement of α - Zirconium A Review", J. Less Common Metals, 11 (1966), p. 168-185.

(3) R.E. Schreiber, "Biaxial Strain Fatigue of Zircaloy-4" WCAP-3269-42A&B October, 1965.

V. Provide (1) comparison of the operating conditions for the test assemblies versus previously approved conditions at 23.5 Mw and (2) safety evaluations for any new steady state or transient conditions.

ANSWER

Although Saxton contains many test rods and assemblies, the limiting thermal and hydraulic conditions occur in the hot center assembly. The basic ground rules are:

(1) No test rod shall exceed 19.1 kw/ft at 35 MWt.

(2) No test rod shall operate with a DNBR less than 1.3.

The design power distribution for the hot assembly can be used to compare the test rods to the regular rods.

Hot Typical PuO_2 rod	19.1 kw/ft
Hot PuO_2 test rod	17.5 kw/ft

A COMPARISON OF THE OPERATING CONDITIONS OF THE TEST FUEL ASSEMBLIES IN THE REACTOR AT 35MWt IS GIVEN IN THE FOLLOWING TABLE

					Operating Limitations		
Test Fuel Assembly	Change Request	No. of Rods	Clad	Fuel Enrichment	23.5MWt 2000psi	35 MWt 2200psi	Description
IX	14	51	SS	5.69% $U\text{-}O_2$ pellets	None	None	Std. U Rods
X	17	4	Zr-4	6.6% PuO_2 pellets	None	None	Std. Pu Rods
	17	4	Zr-4	6.6% PuO_2 vipack	None	None	Std. Pu Rods
	17	1	--	Flux Thimble	None	None	Std. Flux Thimble
XV	24	5	SS	5.7% $U\text{-}O_2$ pellets	None	None	Std. U Rods
	24	4	--	see Note 1			
XVI	24	5	SS	5.7% $U\text{-}O_2$ pellets	None	None	Std. U Rods Except for end closure welds that are sub-standard for test purposes
		4		see Note 1			

NOTE 1 - In these two test fuel assemblies any of the following rods may be inserted:

Rods	Clad	Fuel Enrichment	Operating Limitations 23.5 MWt	35 MWt
2	Zr-4	17.4% UO_2	Peripheral Core Location	Peripheral Core Location (Note 2)
2	St.Stl.	5.7% UO_2	None	None
2	St.Stl.	5.7% UO_2	None	None
4	Zr-4	Zircaloy-4 Test Capsules	None	None

NOTE 2 - The 17.4% enriched rods may be expected to operate in a peripheral core location at 9.5 Kw/ft with the reactor at 23.5 MWt and at 14.3 Kw/ft with the reactor at 35 MWt. This maximum expected power is well within the 16 Kw/ft allowable design power for this rod. Even at the higher linear power for these rods the operating tensile stress due to the internal gas pressure does not increase. The slight increase in internal pressure due to the higher temperature is offset by the increase in external pressure of the clad which results from increasing the reactor operating pressure from 2000 psi to 2200 psi. The net result in operating clad stress is a reduction of approximately 100 psi from 21,700 psi to 21,600 psi.

VI. Provide a description and drawing of the mechanical stops on the control rods which are designed to prevent a rod ejection accident. Include an analysis to demonstrate that these stops would not fail in the event of a complete shearing of a control rod nozzle.

ANSWER

The mechanical stops (See Fig. VI) were not provided to prevent rod ejection in the event of complete shearing of a pressure vessel nozzle, but were originally designed to prevent a control rod absorber section from traveling below the active fuel region in the event of failure of the dashpot in the control rod drive mechanism during scram. Although the stops are adequate for this purpose, at the energy level of the control rod in the case of nozzle shearing it would take extensive testing and anlysis to predict the results of the impact between the stop and the fuel assembly. However, even if the stop sheared completely off and did not succeed in stopping the rod, the rod would overtravel only a distance of approximately 7 inches before contacting the bottom of the pressure vessel.

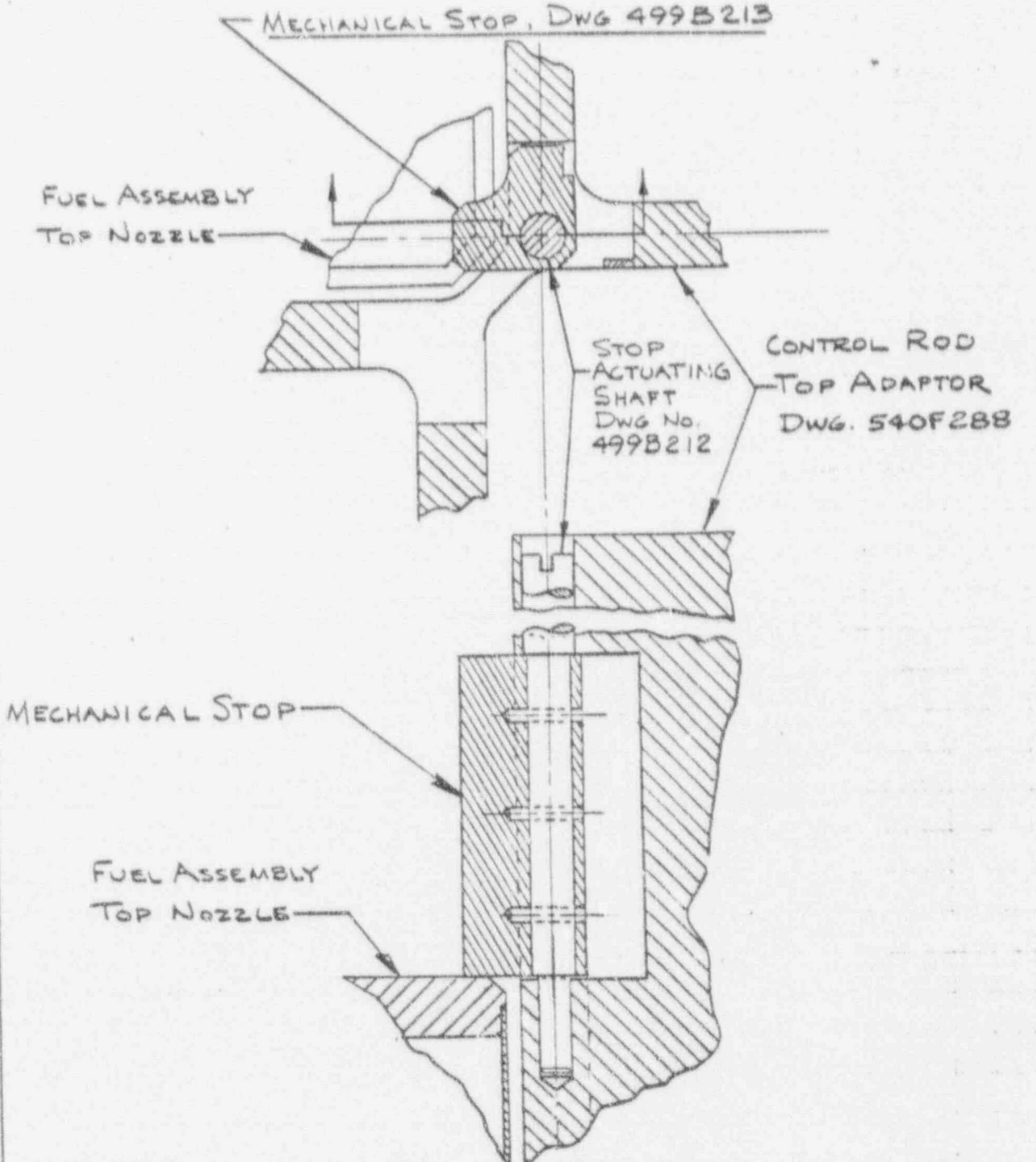
Since the control rod is significantly larger in cross-section than the nozzle penetration (5.62 inch compared to 3.75 inch) the rod cannot be ejected through the nozzle. In addition, the shroud which houses the control rod in the bottom plenum comes to within 4.17 inches of the bottom of the pressure vessel and, by virtue of the clearance between the rod and shroud, would prevent the rod from deflecting sideways more than 0.32 inch.

Thus if the stop failed and permitted a control rod to continue downward, the rod would overtravel its normal position by approximately 7 inches where it would come to rest. The maximum reactivity insertion would result if the rod was initially completely in the core and was ejected downward these 7 inches. The worth of this movement is less than 0.35% $\Delta k/k$. The only time rods are completely in the core is when the reactor is shutdown, tripped, or being started up. With the reactor shutdown or tripped the

reactor is subcritical by several percent and the 7 inch ejection would have insignificant reactivity effect. Even during startup when withdrawing the last control rod (Rod #2) criticality is not normally achieved until the rod is withdrawn at least 10 inches and it is doubtful that ejecting the rod down into the core and out 7 inches would return the core to critical. Assuming, conservatively that the reactor is critical with the rod completely in, upon ejection of the rod the reactor power would tend to increase to a steady state level of 6 MWt to compensate for the 0.35% $\Delta k/k$ reactivity addition. A reactor trip signal would be initiated immediately, however, because of high startup rate and subsequently by low pressure.

WESTINGHOUSE ELECTRIC CORPORATION

SAXTON REACTOR PLANT, CONTROL ROD MECHANICAL STOP



EDSK 329702
EB 4-21-67

FIGURE VI

VII. By use of the methods and parameters of TID-14844, we find that an exclusion radius of at least 300 meters is needed for 35 MW operation. Your calculations indicate that the exclusion radius should be at least 270 meters. Compare the assumptions used in your calculations to those used in TID-14844. Which factors contribute most to your lower dose estimates?

ANSWER

The direct gamma dose presented for the hypothetical loss of coolant accident in the Safeguards Report for the Saxton Reactor Operating at 35 MWt was calculated assuming 100% core meltdown resulting in release of the following fission product fractions:

Inert gases	1.0
Halogens	0.5
Remaining fission products	0.01

The core inventories are based on a power level of 35 MWt and an assumed full power operating time of 10,000 hours. The fission product yields, gamma transitions, and decay constants were obtained from ORNL-2127, NSE Volume 3 1958, and Review of Modern Physics Volume 3 No. 2 April 1958.

The initial intensity of the released sources for representative energy groups is:

Gamma Energy (MEV)	Initial Source Intensity (Mev/sec-MWt)
0.4	8.67×10^{14}
0.8	4.19×10^{15}
1.3	9.72×10^{14}
1.7	4.46×10^{15}
2.2	3.67×10^{15}
2.5	1.84×10^{15}
3.5	1.87×10^{15}
Total	1.79×10^{16}

These sources are larger than those of TID-14844. The activity released is assumed to mix homogeneously throughout the containment free volume of 1.43×10^5 ft^3. A large portion of the containment free volume is shielded by earth and concrete below grade on the side and by the thick concrete operating floor on the top. The "non shielded volume," i.e., the portion above grade, contains 9.48×10^4 ft^3 which is enclosed by the 11/16 inch thick steel cylindrical side wall and the 11/32 inch thick steel dome of the containment.

The dose from the containment source is calculated using a digital computer code which calculates the level of gamma ray radiation from a volume distributed source. The code accounts for the shielding effect (including dose buildup factors) of the steel containment and the atmosphere. The time dependence of the source intensity is based on the decay chains of the fission products, no credit is taken for clean-up or plate out of activity.

The above calculation results in a two hour whole body dose of 25 rem at a distance of 270 meters from the containment. In summary, the factors contributing to lower dose estimates than those in TID-14844 are:

1. A large portion of the containment is underground and concrete shielded.
2. A distributed volume source was considered where as the TID-14844 model is based on a point source.
3. The shielding of the containment structure is not included in TID-14844.

The first set of power increases will be toward a nominal thermal output of 16 kw/ft. The power will be increased in increments of 1 kw/ft thermal output. Each increase in power should be accomplished by allowing the core average temperature to decrease while maintaining constant rod position and boron concentration. After each power increase a partial flux map will be made and analyzed using standard on-site hand analyses techniques. Fission product monitor readings will be obtained following each power increase. The core inlet temperature should be returned to 480°F after each power increase by boron dilution.

A complete flux map and thermal-hydraulic measurements will be obtained when the nominal thermal output reaches 16 kw/ft. This data will be subjected to computer analyses to predict the power level at which a nominal thermal output of 17.0 kw/ft will be obtained.

4. The second set of power increases will be toward the predicted nominal thermal output of 17.0 kw/ft but not to exceed a maximum of 19.1 kw/ft or a power level of 35 MWt. The same procedure as described in step 3 above will be repeated for these power increases.
5. The reactor will continue to operate at 35 MWt or the maximum thermal output of 19.1 kw/ft.

VIII. Provide additional detail on your program for power escalation. Include in your discussion, the size of each step in power increase (and specific power increase), the length of time at each power and the measurements and evaluations required after each step.

ANSWER

The general testing description for the proposed power escalation program is outlined in "Safeguards Report for the Saxton Reactor Operating at 35 MWt". Additional detail on the program for power escalation is presented below.

1. Startup to 23.5 MWt will be accomplished utilizing the variable frequency motor-generator set to provide the main coolant pump power at a flow rate of 3.21×10^6 lbs/hr. The reactor will be operated at 23.5 MWt at an average temperature of 510°F to establish equilibrium conditions. A full core flux map, as well as thermal and hydraulic measurements will be obtained to verify core parameters are within predicted limits.
2. Control rod No. 2 will then be adjusted to 27.5 inches by boron dilution and the average temperature will be decreased to 500°F. A full core flux map and thermal-hydraulic measurments will be obtained. Control rod No. 2 will then be inserted to 18 inches and 13 inches respectively. Full core flux maps and thermal-hydraulic measurements will be made at these positions. This data will establish the curve of hot channel factors at the expected control rod position of 27.5 inches.
3. Control rod No. 2 will then be returned to 27.5 inches and the core inlet temperature reduced to a maximum of 480°F by boron addition. The fission product monitor will be put into service at this time.

IX. In our letter to you dated December 30, 1966, we requested that you consider the emergency core cooling provisions to determine the need for additional provisions to limit the release of fission products from the core. In addition, we requested that you perform suitable analyses to determine the degree to which emergency core cooling must be relied upon to maintain containment integrity. Please inform us of the progress made thus far and your schedule for submittal of the reply.

ANSWER

We expect our report covering review and analyses of Saxton's emergency core cooling provisions will be submitted for your review and evaluation by August 1, 1967.

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