ROD DROP AND SCRAM
IN
BOILING WATER REACTORS
Part I

D. J. Diamond, G. S. Lellouche
M. M. Levine

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Brookhaven National Laboratory
Upton, New York 11973

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I Introduction and Summary

One of the Design Basis Accidents (DBA) in Boiling Water Reactors (BWR) concerns the accidental dropout of a control rod. Under abnormal circumstances, such a rod could be worth several percent in reactivity and initiate a massive power and temperature increase. Some short time after such an accident starts, automatic safety equipment inserts the SCRAM bank and the accident is terminated. Typically, the time scale of interest is only a few (\(< 5\)) seconds. This report studies some of the methods of calculating the rod drop induced transient behavior of a BWR typified by the General Electric (G.E.) Oyster Creek power reactor. As we shall see, the methods can all be made to yield equivalent results if we first understand the physics of rod withdrawal and SCRAM bank insertion.

There are three postulated forms of rod drop accident: the first proceeds from a cold (68°F), 10^{-8} of full power condition, the second from a hot (525°F), 10^{-6} of full power condition, and the third from 10% of full power. We shall only deal with the second and third forms of the accident.

The hot, "zero" power accident envisages a rod worth 2.5% dropping out of the reactor. There are no voids to begin with and the effects of voids generated during the accident are neglected in the calculations. The predicted severity of the accident will then be quite conservative.
The 10% power accident envisages a rod worth 3.8% dropping out of the reactor. The flow rate is about 30% of full flow; the inlet temperature is taken as 543°F and there is significant voidage in the core. Because of the voidage, and the fact that axial power shaping is not started at this low power level*, the power is skewed strongly towards the inlet. This has a significant mitigating effect on the severity of the accident, even without counting the void reactivity feedback effects themselves. We examine this effect in some detail and are able to extend the methods of Ref. 1 to include skewed initial shapes.

Generally, we find that it is possible to accurately predict the hot spot fuel enthalpy using the one-group, 1D code NØAH and the theoretical methods of Refs. 1 and 2. Comparisons with four-group, 1D, and two-group, 2D (TWIGL) results are made, and the method of determining the hot spot enthalpy directly is shown to be valid.

Because the exact parameters (time delays, etc.) are somewhat difficult to predict, results of a parameter survey are also presented.

Broadly speaking, the 2.5% accident can lead (under the very special and conservative assumptions made) to peak,

*This may not be true in the new gadolinia containing fuel assemblies where part length rods are built in. These will worsen the consequences of all forms of the accident, but by how much is not clear yet.
average fuel pellet enthalpies significantly greater than previously reported and could, if actually reached, cause considerable core damage; one should not, however, assume that if these accidents actually occurred the peak fuel enthalpies would reach such damaging values. It seems quite reasonable to believe that the effects of void reactivity feedback alone would reduce the fuel enthalpies to acceptable values.

It is more difficult to predict the consequences of the 3.8% accident. If one neglects the effect that voidage has on the rod drop and SCRAM shapes, then the fuel enthalpy at the hot spot reaches the fully vaporized value (800 cal/g) almost independently of the rod drop and SCRAM parameters. Depending on what the actual void distribution is, and what the rod drop and SCRAM parameters are, the peak fuel enthalpy can be calculated as low as 100 cal/g. Because of the extraordinary effect that the void distribution has, the results of the 3.8% accident must be calculated with a realistic two-phase coolant model. This model and the results obtained using it will be reported in Part II of this report, which will be issued in the near future.
II Description of the Geometric and Neutronics Models

The physical arrangement of fuel assemblies in G.E. BWR's is correctly described by an $x,y,z$ geometry. In this geometry the worth of a control rod depends very strongly not only on how many other rods are out, but on the exact connectivity of the unrodded regions. Thus, Purohit showed that at cold zero power removing a central control rod which would connect two other unrodded assemblies could lead to anywhere from 0.0113 to 0.0266 addition in $\Delta k$. All such configurations would have the same $(x,y)$ unrodded areas both before and after. For this reason, the conversion of the $(x,y,z)$ geometry to $(r,z)$ geometry is not a trivial one. The reason for such a conversion (and indeed ultimately to an $(r)$ geometry alone) has to do with the availability of reactor dynamics codes and computers which can do the necessary calculations in a reasonable amount of time.

The basic $(r,z,t)$ geometry which we shall use in the analyses (along with $(r,t)$ and $(z,t)$ approximations) has been devised by G.E.

The model is to be used to determine the peak energy addition to the reactor core for a given reactivity addition. G.E. hypothesizes that this will occur when the reactivity is inserted in the center of the core. Furthermore, they assume that the basic three region geometry, shown in Fig. 1 produces the peak energy addition. The use of a region of thickness $R_2 - R_1 = \Delta R$ which contains no
control rods surrounded by a partially controlled region allows one to search for a core representation to obtain a desired reactivity change when the rod from the central region is removed.

In order to determine unique values of $\Delta R$ and $\alpha$, the fraction of supercells with rods in, in region 3, two conditions are used. The excursion of interest starts with the reactor just critical. Hence, calculations are performed using IDX, the one-dimensional diffusion theory code, to find pairs of values of $\Delta R$ and $\alpha$ which will give $k = 1.0$ when the rod is in, in the supercell represented by region 1. When this rod is removed, the reactor must be at $k = k^*$, where $k^*$ corresponds to the reactivity insertion desired. Again a search to find pairs of values of $\Delta R$ and $\alpha$ satisfying this condition is performed. The process is sketched in Figs. 1 and 2. Although no mention or use of the (z) coordinate has been made here, the same procedure applies using an (r,z) code such as 2DB or even TWIGL. In fact, in such large cores the changes in $\alpha$ and $\Delta R$ using an (r) representation rather than (r,z) are extremely minor.

In the course of the studies, we have made use of three different neutronics models (with all three yielding equivalent results):

1. A quasi-space-dependent model coupling a four-group, (r) module to a point kinetics (t) module.
2. A one-group (r,t) and (z,t) dynamics code NOAH.
3. A two-group (r,z,t) dynamics code (TWIGL).

The methods used to arrive at the cross sections used in these neutronics models are discussed in Appendix A.
III Rod and SCRAM Simulation

In an \((x,y,z,t)\) or \((r,z,t)\) model, the simulation of the rod dropping out and SCRAM bank moving in can be modeled quite accurately. A subroutine is constructed which calculates the distance traveled by the rod (or bank) tip. The rods-in cross sections are then applied in the regions where the rod or bank are actually inserted and the rods-out cross sections or partly rodded cross sections are used in the other regions. When the tip lies between mesh points (the usual case) the cross sections are weighted according to the fraction of the distance between the flux points actually covered. In a \((z,t)\) calculation the same procedure is used except that the cross sections are averaged radially and the result is often called a "window-shade" model. Such subroutines have been constructed and added to NØAH and TWIGL.

The most difficult modelling occurs with the \((x,y,t)\) or \((r,t)\) model. A function \(S(t)\) must be applied to the cross section changes due to rod and bank which yield (on editing the resulting flux distributions) the correct rod/bank reactivity versus time. This subject has been thoroughly discussed elsewhere. 

After determining the correct values of \(R_1, R_2\) and \(\alpha\) (cf. Sec. II and App. A), a series of static \((r)\) calculations are done using various fractions of the "rods-in" cross sections; these yield a relation between worth and fractional cross section. The "self-weight"*

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*The worth versus axial position curve in \((z)\) and \((r,z)\) models is essentially the same as has been shown in Ref. 1 and both are correctly mocked up by the "self-weight" method of calculating worth versus position.
worth versus position function is compared with the worth versus rods-in fraction relation and from this comparison the function $S(t)$ is determined. Fig. 3 shows the worth versus time curve and the associated $S(t)$ curves. Fig. 4 shows the similar curves for the SCRAM bank. The SCRAM bank is worth approximately 9% in reactivity (with the central rod out) in the NOAH model and approximately 8% in the TWIGL. The difference is due to the fact that the NOAH SCRAM model adjusts only the absorption cross section while the other model adjusts all cross sections and the diffusion constants as well. This difference does not significantly affect the results since almost all the energy is developed before the SCRAM bank is half inserted. G.E. states that the SCRAM bank is worth $^{7,8}$ between 14% and 15%.

The shape functions, $S(t)$, are derived from static calculations which - in effect - neglect the transient effects of the delayed neutrons. At first thought, such a procedure would be valid for the rod removal but invalid for the SCRAM bank insertion. Indeed, in Ref. 1 we have shown that inserting a SCRAM bank into a critical reactor leads to a greater negative reactivity effect than predicted by the "self-weight" method. The operative word here is "critical". In the rod drop accident the reactor is put on a positive period and the power rises by a factor of $10^8 - 10^9$; when the SCRAM bank is inserted the delayed neutron background

*G.E. (private communication) admits that their values are too high.
is far below the actual flux levels and its effect does not show up for a much longer period of time than would be expected from insertion in a critical system. A second effect which weakens the SCRAM is the fact that as the rod drops out the axial offset becomes large; thus, the relative increase in the delayed neutron background near the core inlet (where the SCRAM bank enters) is significantly less than in the upper half of the core. In Fig. 5 we show a comparison of \((r, t), (z, t)\) and \((r, z, t)\) calculations of the transient control eigenvalue* for the 2.5% accident. The degree of agreement is quite good, especially between the \((z, t)\) NØAH "window-shade" and the \((r, z, t)\) TWIGL, but even the \((r, t)\) calculation which uses the \(S(t)\) functions is not greatly different, indicating the validity of the derivation of the \(S(t)\) functions.

*By "control eigenvalue" we mean the actual eigenvalue with the feedback reactivity (in this case Doppler) added back.
IV Calculation of the Hot Spot Enthalpy

The \((x,y)\) plane of a BWR shows a fine structure of fuel rods inside a fuel assembly. Because of enrichment effects, there is a hottest rod inside the assembly. The direct determination of the transient heating of this hottest of all rods is not economically possible with the present computing machines and methods. Instead, what is normally done is to homogenize the assembly (see Appendix A) and determine (in the transient) the hottest "assembly averaged" fuel location. The increase in enthalpy during the transient is then multiplied by the peak-to-average power in the assembly as determined by auxiliary calculations. This assumes that the peak/average in the assembly (box factor) does not change during the transient. This method will work if we have the hottest "assembly averaged" fuel pin, and implies the use of an \((r,z,t)\) or \((x,y,z,t)\) dynamics model. In using an \((r,t)\) dynamics model one would have to compensate for the peak/average effects in the \"z\" direction, as well as the box factor. Unfortunately, the axial peak/average fuel temperature varies considerably during the transients and makes direct use of an \((r,t)\) model suspect.

In Ref. 2 we discussed a theoretical approach which should permit the use of an \((r,t)\) model. This approach makes use of a special axial Doppler weight factor related to the peak axial fuel enthalpy. When this is factored into the \((r,t)\) model, it allows the calculation to be performed for the radial plane at the axial hot spot. The validity of this approach rests on the comparison of the N\(\Phi\)AH \((r,t)\)
power distribution with the radial power distribution which is at the axial hot spot, edited from the equivalent TWIGL calculation. Some of these results are shown in Figs. 6 to 8 for the 2.5% rod drop accident; the solid lines are from NØAH and the histograms from TWIGL. The normalization used in each graph is to scale the peaks to unity.* Several of the graphs cover the time span 0.8 - .87 seconds; this is the range of time during which the power peaks (if we count the zero of time to be when the control rod tip enters the core). The good agreement is further emphasized by examining the transient enthalpy of the core hot spot.

An edit of the peak TWIGL enthalpy does not yield the core hot spot enthalpy because TWIGL makes use of the thermal-hydraulic region (T.H.R.) concept for its feedback modules. This means that the TWIGL results must be corrected for what would have been the true enthalpy distribution in the T.H.R. where the peak occurs. If the peak/average power in the hottest T.H.R. does not vary greatly in time, then one may assume that peak enthalpy would follow from using that value as a multiplier. Fortunately, for the 2.5% rod drop study, this multiplier has very little variation (between 1.12 and 1.16) during the important portion of the transient. The results of this comparison are shown in Fig. 9. The difference of 8 cal/g at 1.8 seconds can be reasonably neglected as an unimportant difference between the two calculations. The difference in cost of the calculation (the basic reason

*The comparison could have been made to appear much better if we had scaled to equal areas.
for terminating the TWIGL computation at 1.8 seconds) is somewhat higher. The NØAH calculation up to 13.9 seconds cost $4.10 while the TWIGL calculation to 1.8 seconds cost about $1300 on the BNL CDC 6600.

The TWIGL feedback model is a two-temperature model** with constant thermo-physical properties; NØAH*** has several feedback models available, one which is like the TWIGL model and another which uses variable (i.e. temperature and pressure dependent) thermo-physical properties. The comparison in Fig. 9 shows the results for the case where the feedback modules are the same. On the basis of this comparison, and others discussed in Ref. 2, we assume that the 2D, two-group TWIGL results can be essentially calculated by the 1D, one-group code NØAH. More details of the TWIGL calculation are presented in Appendix C.

As has been discussed elsewhere,² it is necessary to explain how the (r,t) NØAH calculation can yield such good

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* Much of the TWIGL calculation was run at night under reduced cost considerations; had it been run at the regular rates (as NØAH was), the cost would have been closer to $2000.

** A more realistic temperature module is currently being installed in TWIGL.

*** The current edition of NØAH has little relation to the published version; a modified manual is being prepared and the new version will be published in the near future.
agreement with the highly nonseparable $(r,z,t)$ transient calculated using TWIGL. As we noted in our earlier report, it is likely that local separability might exist. What we mean by this is that the $(r,z)$ region of importance during the transient is not the whole reactor but a small subregion in which separability is meaningful. Figs. C3-C12 show the TWIGL $(r,z)$ power distribution in perspective for various times during the 2.5% rod drop. The power in each figure is normalized to unit integral so that the relative importance of a location $(r_i,z_i)$ can be seen at a glance. It is clear that only a very small subregion of the reactor has any importance during the transient and it seems reasonable to assume that it is for this reason that the $(r,t)$ calculation is so accurate.
V A Comparison of Methods

1. Dynamic versus Quasi-Space-Dependent in Radial Geometry

By quasi-space-dependent (QSD) we mean that the neutronics model calculates a normalized equilibrium distribution for the power \( P(r) \) with a time varying set of cross sections (criticality being maintained by changing the eigenvalue). The cross sections are varied according to the \( S(t) \) function, and feedback effects on the cross sections are made through an estimate of the temperature distribution. The actual time-dependent calculations are done in a point kinetics module which produces (presumably) the core average power and temperature rise (plus delayed neutrons, etc.). When the average temperature has increased some prespecified amount, a new normalized spatial power distribution is calculated. Edits of the power distribution yield the Doppler weight factor and other parameters needed for the point kinetics module. A more detailed discussion of the (QSD) model is found in Appendix B.

For the 2.5\% rod drop, Figs. 10 and 11 show a comparison (without SCRAM) between the NØAH \( (r,t) \) and the Q.S.D. \( (r,t) \) calculations of the total power and the core average fuel temperature. These results do not account for the axial Doppler weight factor (D.W.F.) and are meant only to show the relative differences between calculational models. There are several minor differences between the two models (among others, the Q.S.D. is adiabatic, while NØAH accounts for heat transfer to clad and coolant) but these do not hide the essential agreement between them.
2. One-Dimension (NØAH) versus Two-Dimension (TWIGL)

We have in part (see Sec. IV) already discussed the excellent agreement between NØAH and TWIGL at the core hot spot. Here we only add to the weight of evidence that there is good agreement between the two models.

Fig. 12 shows the total power (Mwt) during the transient. The NØAH calculation is an (r,t) transient using an average axial D.W.F. of 1.5. Fig. 13 shows the TWIGL D.W.F.* in comparison with the NØAH D.W.F. The solid line is the product of the NØAH (r,t) and (z,t) D.W.F.'s, while the dashed line is the NØAH D.W.F. using 1.5 as the axial value. The value of 1.5 is determined by finding what numbers used in the (r,t) and (z,t) calculations lead to the same maximum core average fuel enthalpy (the average radial D.W.F. is ≈ 3.5). The dashed line explains the differences in the power traces of Fig. 12; too low a D.W.F. near the peak means the power must rise higher to produce more energy (higher fuel temperature) to turn over the power. The other portions of the transients are just as easily analyzed in terms of the D.W.F. Fig. 14 shows the core average fuel temperature for these transients. The too small D.W.F. requires a too high temperature rise to compensate, but near the peak the error is only 20°F. Nonetheless, this would imply an error at the core hot spot of about 300°F. For this reason, we make use of

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*The method of editing TWIGL for the D.W.F. is discussed in Ref. 2.
the special axial D.W.F. discussed in Sec. IV which leads (Fig. 9) to errors of only about 6 cal/g at the hot spot; this implies an error in core average temperature of about \(7^\circ F\) (factoring out the box factor, average axial power peak/average and the radial fuel temperature peak/average) which we take to be in excellent agreement with the TWIGL results.
VI The 2.5% Rod Drop Accident

The rest of the body of this report discusses a survey of the possible consequences of the 2.5% rod drop accident based on calculations using NØAH.

Basically, we shall establish our estimates of the parameters entering the calculation, the G.E. estimates and technical specifications and some experimentally derived estimates as well. We shall present graphs covering the full parameter survey and compare the peak fuel enthalpies for the following five parameter sets:

1. Our estimate of the parameters.
2. The G.E. estimate of the parameters.
3. The experimental rod time with the technical specifications SCRAM bank time.
4. The design basis rod time with the experimental bank time.
5. The design basis rod time with the technical specifications bank time.

1. **SCRAM Delay Time**

The amount of time which elapses between the activation of a reactor trip and the start of motion of the SCRAM bank comprises the delay time. This time interval has two basic parts. The first takes place in the protection system which determines whether the SCRAM trip signal is spurious. This
time interval is variously given as 0.05 sec (Ref. 7, Fig. 3.6-15) or 0.07 sec (Ref. 7, p. 3.4-18). The second part of the time interval occurs in the rod drive system itself. In order to initiate SCRAM, two pilot valves must de-energize (both, not either one); afterwards, the SCRAM valves themselves open. This later event allows high pressure water to accelerate the control rods into the core. In several of the earlier BWR FSAR's (e.g. Ref. 8), a time of 0.05 sec is given for the de-energizing of the pilot valves. Since both of these valves must open fully before the next stage of SCRAM can occur, the proper (conservative) time to use is 0.10 sec. It should be noted that in the newer FSAR's, no opening time is given for the pilot valves but all FSAR's give the SCRAM valves' opening time as 0.1 sec. From the above results, we can assume that between 0.15 and 0.17 sec must elapse before the SCRAM valves start to open. As soon as the SCRAM valves start to open, some pressurized drive water will start acting on the rod (this assumes that any valve motion at all allows water through to the rod) but conservatively we should not account for this effect and hence, take the delay time as 0.25 to 0.27 sec.

The conclusion arrived at above is also arrived at in many of the newer FSAR's (e.g. Ref. 7, p. 3.4-18) where the larger number of 0.27 is stated to be observed.

Further additions to the delay time could be made to account for the fact that the SCRAM bank tip lies one inch below the core-reflector interface* and the acceleration

*G.E. private communication.
time to full velocity is 30 milliseconds. These are accounted for by adding 45 milliseconds to the delay time yielding a total delay of 0.315 sec. G.E. takes the delay time (for accident analysis purposes) to be 0.40 seconds.*

2. **SCRAM Time**

The SCRAM time is more easily determined since it is part of the technical specifications. Oyster Creek, for example, specified that it would meet the following restrictions:

<table>
<thead>
<tr>
<th>Fractional Insertion</th>
<th>Time, sec</th>
</tr>
</thead>
<tbody>
<tr>
<td>.10</td>
<td>0.7</td>
</tr>
<tr>
<td>.50</td>
<td>2.05</td>
</tr>
<tr>
<td>.90</td>
<td>5.00</td>
</tr>
</tbody>
</table>

Experimentally, (Ref. 8 the following has been observed (time measured from start of motion):

<table>
<thead>
<tr>
<th>Fractional Insertion</th>
<th>Time, sec</th>
</tr>
</thead>
<tbody>
<tr>
<td>.10</td>
<td>0.33</td>
</tr>
<tr>
<td>.90</td>
<td>3.05</td>
</tr>
</tbody>
</table>

Extrapolating, we have as well

<table>
<thead>
<tr>
<th>Fractional Insertion</th>
<th>Time, sec</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.00</td>
<td>3.40</td>
</tr>
</tbody>
</table>

*G.E. private communication.
In the newer product line (Ref. 7, p. 3.4-17) the Technical Specifications are

<table>
<thead>
<tr>
<th>Fractional Insertion</th>
<th>Time, sec</th>
</tr>
</thead>
<tbody>
<tr>
<td>.05</td>
<td>.375</td>
</tr>
<tr>
<td>.20</td>
<td>.90</td>
</tr>
<tr>
<td>.50</td>
<td>2.0</td>
</tr>
<tr>
<td>.90</td>
<td>5.0</td>
</tr>
</tbody>
</table>

These are all shown in Fig. 15. The Technical Specifications define time zero at the time when the SCRAM pilot valve solenoids are de-energized; hence, include all of the delay time except that part in the protection system.

Assuming a straight-line relationship for the initial motion of the bank, the SCRAM delay time may be calculated (from the Technical Specifications data but not from the APED 5446 data) as:
<table>
<thead>
<tr>
<th>Delay Time (excluding Protection System), Sec.</th>
<th>Curve</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.3625</td>
<td>Oyster Creek, Tech. Spec.</td>
</tr>
<tr>
<td>0.2000</td>
<td>Browns Ferry, Tech. Spec.</td>
</tr>
</tbody>
</table>
From the technical specifications we see that the insertion time is about 5.7 sec and has two constant velocity sections; experimentally, the insertion time is about 3.40 sec and exhibits a nearly constant velocity during insertion. A single velocity Tech. Spec. SCRAM time (based on the initial-higher-velocity) would yield about 3.6 sec. G.E. uses 2.2 sec as the SCRAM time, but currently proposes to increase it to about 3.1 sec.

3. Rod Drop Velocity

The rod drop velocity depends strongly on the type of accident assumed, from 0.25 ft/sec for a single malfunction to 15 ft/sec for multiple simultaneous component failure. This accident study assumes a single component failure and under these circumstances the rod drop velocity is found to be 2.86 ft/sec \(^9\) (\(\approx\) 4.2 sec drop time).

G.E. takes the rod drop velocity to be \(7,8\) 5 ft/sec. This value appears in the Bases for the Tech. Specs. but is not itself a Technical Specification; G.E. currently proposes to reduce this value to 2.86 ft/sec. These results are summarized in Table II.
<table>
<thead>
<tr>
<th>BNL Best Estimate</th>
<th>General Electric</th>
</tr>
</thead>
<tbody>
<tr>
<td>Old</td>
<td>New</td>
</tr>
<tr>
<td>SCRAM Delay, sec</td>
<td>0.315</td>
</tr>
<tr>
<td>SCRAM Time, sec</td>
<td>3.40</td>
</tr>
<tr>
<td>Rod Velocity, sec</td>
<td>2.86</td>
</tr>
</tbody>
</table>
4. **Other Parameters**

Table II does not indicate which set of data should yield the worst response but a further parameter of interest is the SCRAM shape. Fig. 4 shows that our calculations yield a very skewed shape with only about 11% of the total worth introduced by 50% of insertion. The old G.E. calculations use a straight-line representation which introduces 50% of the worth at half insertion. It is this last point mainly which causes the great discrepancy between our calculations and theirs. The new G.E. calculations have not as yet been published. Figs. 16-18 show the results of the parameter survey for the 2.5% BWR rod drop accident. Figs. 17, 18 show progressively more specific cuts through the data yielding (in Fig. 18) the peak enthalpy as a function of rod drop velocity for a 3.0 sec SCRAM time and 0.20/0.40 sec SCRAM delays. These calculations use the NOAH feedback model incorporating fuel-clad-coolant temperatures with the thermophysical properties, being themselves functions of temperature. Heat transfer is allowed and only 96% of the heat enters directly into the fuel (4% to the moderator). Auxiliary calculations show that heat transfer and 4% direct yield to the moderator are worth 27 cal/g at the hot spot. In Refs. 5 and 7 G.E. reported a peak of 220 cal/g for the 2.5% accident (column 2 of Table II). Cross-plotting the results in Fig. 16 we find:
Table III

Core Peak Fuel Enthalpies for the Parameters in Table II

<table>
<thead>
<tr>
<th>Case</th>
<th>Fuel Enthalpy, cal/g</th>
</tr>
</thead>
<tbody>
<tr>
<td>BNL</td>
<td>283.</td>
</tr>
<tr>
<td>G.E., old</td>
<td>322.</td>
</tr>
<tr>
<td>G.E., new</td>
<td>283.</td>
</tr>
</tbody>
</table>
Adding the 27 cal/g due to model differences, we see that the G.E. old parameters lead to 349 cal/g considerably higher than the 220 previously reported. The difference is (almost) entirely due to the use of an incorrect SCRAM shape. For either the BNL or G.E. new parameter set, we find the same peak enthalpy, 283 cal/g (or 310 excluding heat transfer, etc.), the change in SCRAM time being offset by the change in delay time. Table IV lists the parameters for the three other cases and the peak enthalpy, as well.
Table IV

Accident Parameters for Cases 3 - 5
SCRAM Delay = 0.40

<table>
<thead>
<tr>
<th>Case</th>
<th>SCRAM Time, sec</th>
<th>ROD Time, sec</th>
<th>Peak Fuel Enthalpy</th>
</tr>
</thead>
<tbody>
<tr>
<td>3</td>
<td>5.3</td>
<td>4.2</td>
<td>345.</td>
</tr>
<tr>
<td>4</td>
<td>3.4</td>
<td>2.4</td>
<td>363.</td>
</tr>
<tr>
<td>5</td>
<td>5.3</td>
<td>2.4</td>
<td>411.</td>
</tr>
</tbody>
</table>
If we do not allow for changes in Technical Specifications, then cases 3 or 5 yield the peak/fuel enthalpy required for the F.S.A.R.; if one allows for more realistic parameters, then case 1, we feel, yields the more meaningful result. In any case, these numbers drop drastically when void feedback is accounted for.
References


RADIAL MODEL FOR B.W.R. DROP

REGION 1. CENTRAL SUPERCELL \( \left\{ \begin{array}{c} k = 1: \\ k = k^* \end{array} \right\} \)

\( \Sigma_1 = \Sigma^\text{IN} \)

\( \Sigma_1 = \Sigma^\text{OUT} \)

\( \Sigma_2 = \Sigma^\text{OUT} \)

\( \Sigma_3 = a \Sigma^\text{IN} + (1-a) \Sigma^\text{OUT} \)

REGION 2. UNCONTROLLED

REGION 3. PARTIALLY CONTROLLED

REGION 4. REFLECTOR

FIGURE 1
OUTER REGION ROD FRACTION, $a$

\[ \Delta p = \frac{k^* - 1}{k^*} = 0.025 \]

$\Delta p = \frac{\Delta R}{R} = \frac{\Delta R}{25}$

UNRODDED ZONE $\Delta R, \text{cm}$

FIGURE 2
CHARACTERISTICS OF THE 2.5% ROD DROP IN THE OYSTER CREEK BOILING WATER REACTOR

THE ROD FUNCTIONS

FRACTION OF THE ROD INSERTION TIME

FRACTION OF FULL VALUE

FIGURE 3
CHARACTERISTICS OF THE 2.5% ROD DROP IN THE OYSTER CREEK BOILING WATER REACTOR

THE SCRAM BANK FUNCTIONS

WORTH

S(t)

FRACTION OF FULL VALUE

0.0

0.25

0.50

0.75

1.00

FRACTION OF THE SCRAM BANK INSERTION TIME

FIGURE 4
ROD-SCRAM BANK INTERACTION, RESULTS FROM NØAH AND TWIGL INCLUDING FEEDBACK 2.5% ROD DROP ACCIDENT

FROM THE RADIAL TRANSIENT (NØAH)

FROM THE AXIAL TRANSIENT (NØAH)

+ SYMBOLS FROM TWIGL (r, z, t)

TIME INTO THE TRANSIENT, sec

FIGURE 5
Figure 6: Radial Power Distribution Comparisons from TWIGL and NTHA.

- Time = 0.0 seconds
- Time = 0.669 seconds

The graph shows the radial power distribution over time, with the radial distance measured in centimeters (cm).
RADIAL POWER DISTRIBUTION COMPARISONS FROM TWIGL AND NØAH

Figure 7

TIME = 0.8016 SECONDS

TIME = 0.8136 SECONDS

RADIAL POWER DISTRIBUTION

RADIAL DISTANCE, cm

FIGURE 7
RADIAL POWER DISTRIBUTION COMPARISONS FROM TWIGL AND NSAH

FIGURE 8

TIME = 0.8665 SECONDS

TIME = 1.439 SECONDS

RADIAL POWER DISTRIBUTION

RADIAL DISTANCE, cm

FIGURE 8
A COMPARISON OF THE CORE PEAK FUEL ENTHALPY CALCULATED FOR THE 2.5% ROD DROP ACCIDENT IN A B.W.R. BY NOAH AND TWIGL (DELAY TIME = 0.2 sec; SCRAM TIME = 2.5 sec)

NOAH RESULT; CONSTANT SPECIFIC HEAT, TWO TEMPERATURE MODEL. THE SAME AS IN TWIGL.

TWIGL RESULT CORRECTED

TWIGL RESULT UNCORRECTED FOR THE THERMAL-HYDRAULIC REGION EFFECT

SCRAM BANK ENTERS THE CORE

TIME INTO THE TRANSIENT, sec

FIGURE 5
POWER TRANSIENT DUE TO A 2.5% ROD DROP

FIGURE 10
FUEL TEMPERATURE TRANSIENT DUE TO A 2.5% ROD DROP

FIGURE II

CORE AVERAGE FUEL TEMPERATURE, °C

TIME, sec

NOAH RESULT

QSD RESULT
A COMPARISON BETWEEN NŒAH AND TWIGL: THE POWER TRANSIENT

![Graph showing the comparison between NŒAH and TWIGL results for the power transient.](image)

**Figure 12**

**Graphic Title:** REACTOR POWER, MW

**X-Axis:** TIME INTO THE TRANSIENT, sec

**Y-Axis:**
- 10^2
- 10^3
- 10^6
- 10^8

**Lines:****
- NŒAH RESULT
- TWIGL RESULT

SOLID LINE FROM THE NOAH CALCULATIONS
+ SYMBOLS FROM THE TWIGL CALCULATION
DASHED LINE FROM THE NOAH \( r, t \) CALCULATION
USING 1.5 AS THE AXIAL WEIGHT FACTOR

FIGURE 13
Figure 14
Time into the transient, sec

A comparison between NGAH and TIGL: Core average fuel temperature

Core average fuel temperature, °F

Core average fuel temperature, °F

Core average fuel temperature, °F

Core average fuel temperature, °F
SOME B.W.R. SCRAM TIME-DISTANCE CHARACTERISTICS

A = EXPERIMENTAL
B = OYSTER CREEK TECHNICAL SPECIFICATIONS
C = BROWNS FERRY TECHNICAL SPECIFICATIONS

FIGURE 15
THE 2.5% B.W.R. ROD DROP ACCIDENT, A PARAMETER SURVEY

SCRAM CHARACTERISTICS
A 2.5 SECOND SCRAM TIME
B 3.0 " " "
C 4.0 " " "
D 6.0 " " "

ROD CHARACTERISTICS
— 2.4 SECOND DROP TIME
— 3.0 " " "
— 4.0 " " "

CORE PEAK FUEL ENTHALPY, cal/g

SCRAM DELAY TIME, sec.

FIGURE 16
THE 2.5% B.W.R. ROD DROP ACCIDENT, SELECTED RESULTS FROM A PARAMETER SURVEY

SCRAM DELAY TIME
A = 0.2 SECONDS
B = 0.4 SECONDS

ROD DROP TIME
- 2.4 SECONDS
- 3.0 SECONDS
- 4.0 SECONDS

FIGURE 17

CORE PEAK ENTHALPY, cal/g

SCRAM TIME, sec
THE 2.5% B.W.R. ROD DROP ACCIDENT, A PARAMETER SURVEY. SELECTED RESULTS

SCRAM TIME = 3.0 SECONDS

SCRAM DELAY TIME, sec

0.4

0.2

CORE PEAK FUEL ENTHALPY, cal/g

DROPPED ROD VELOCITY, ft/sec.

FIGURE 18
Appendix A

Calculation of BWR Cross Sections

In order to represent a BWR for a reactor calculation, it is necessary to obtain homogenized cross sections for the basic supercells which are repeated throughout the core lattice. The supercell being considered is shown in Fig. A1 and consists of four fuel assemblies surrounding a cruciform control rod. In addition, the same supercell with the control rod replaced with water must be considered. A quadrant of the supercell is represented as shown in Fig. A2 for use in the homogenization procedure.

The HAMMER code is used to generate four-group cross sections for the nine regions of the supercell marked on Fig. A2. Straightforward cell calculations for a cell containing fuel, gap, clad and associated moderator are performed for the three different enrichments. These cross sections are for regions 1, 2 and 3. Cell calculations in slab geometry are used to obtain cross sections for most of the non-fuel regions. A semi-infinite slab of thickness $X_1$ represents the control blade region 4C containing boron carbide, stainless steel and water. Next to this slab is the region representing the water of region 5C having outer dimension $X_2$. Completing the cell are a region representing the zircalloy can with thickness $X_3 - X_2$ and a slab of thickness
$X_{10} - X_3$ containing the homogenized composition of the fuel cells. A regionwise edit of this cell calculation yields the cross sections for regions 4C and 5C. For the supercell without control rod, a similar calculation is performed with a region of thickness $X_2$ containing water replacing regions 4C and 5C. The edit of this cell calculation gives cross sections for regions 4, 5 and 7, as well as for region 8. A similar type of cell calculation with slabs representing the control curtain, adjacent water, zircalloy can, and homogenized fuel region yields cross sections for regions 6 and 9. For the control blade hub, region 7C, a cylindrical cell calculation is performed with the stainless steel and water hub region surrounded by a region containing control blade material and additional water to represent its environs. The above calculations were performed at several fuel and moderator temperatures to allow for the calculation of the cross section temperature coefficients.

The regionwise cross sections were then used in the supercell configuration of Fig. A2 within the DS$_N$ code TWOTRAN. A2. Regionwise flux-volume integrals calculated by this code for a case with blade in and a case with blade out were then used to weight the regionwise cross sections in order to obtain the homogenized supercell values. These same integrals were also used to obtain homogenized cross sections at the other temperatures.
Once supercell cross sections are obtained, the reactor can be represented for a radial calculation as described in Section II. This radial representation is used to calculate the fluxes needed to collapse the four-group cross sections to two-group values necessary for the \((r,z,t)\) code TWIGL. This provides initial two-group constants, as well as the changes needed to represent the absence of a control rod in the inner zone and the presence of a SCRAM bank in the outer two core zones. The calculations are also performed at an elevated temperature to obtain the change in cross sections with fuel temperature.
References


OYSTER CREEK NUCLEAR POWER PLANT
UNIT NO. 1 - FACILITY DESCRIPTION
AND SAFETY ANALYSIS REPORT

CORE SUPERCELL

FIGURE A-1
The quasi-space-dependent (QSD) model couples space-independent kinetics calculations with steady state one-dimensional spatial calculations. The latter are used to obtain the properly weighted radial Doppler feedback which is needed in the kinetics calculations. The model assumes that the spatial and temporal calculations can be done separately and therefore, that the power due to both prompt and delayed neutrons has the same spatial distribution. It also assumes that one-dimensional radial calculations account for the important spatial effects and that the importance weighting of the Doppler effect does not change radically during a temperature increment $\Delta T$. This allows the kinetics calculation to proceed with static calculations only necessary periodically after a reasonable $\Delta T$ rise.

The flow chart shown in Fig. B1 sketches the basic steps involved in the QSD model and identifies the important parameters which are either input to, or output from, the component calculations of the model. The model is fashioned after the adiabatic model used by GE which is described in general terms in references B1 - B4. Some of the more important features of the present model are now discussed.
The kinetics calculation solves the eight time-dependent differential equations which describe the reactor power, \( P \), the six delayed neutron precursor concentrations and the average fuel temperature, \( T \). Since the time scale is short, heat transfer is neglected. The GE model also solves a ninth equation for the Doppler reactivity \( \delta k_D \) which is a function of the average fuel temperature. In the present model it is not necessary to solve this equation for reasons which will become evident.

The time dependent reactivity needed as input to the point kinetics equation is written as

\[
\frac{k-1}{k} = \frac{\Delta k_R}{k} + \frac{\Delta k_D}{k} \tag{B-1}
\]

where each quantity is time dependent. The component \( \Delta k_R/k \) is the change in reactivity due to control rod motion. SCRAM rods are neglected in the present calculation and hence, the reactivity refers only to the central rod being withdrawn. The time dependence of \( \Delta k_R \) is shown in terms of fractional worth in Fig. 3 and is obtained as explained in Section III.

The component \( \Delta k_D/k \) is the change in reactivity due to Doppler feedback. It may be considered as containing a Doppler weighting factor, DWF, which accounts for the spatial distribution of the Doppler effect due to the spatial distribution of the temperature. The DWF is defined as the ratio of the reactivity change obtained by
spatially weighting the temperature rise $\Delta T$ to the change obtained through a uniform $\Delta T$ rise. The Doppler feedback may then be written as $\Delta kD/k = (\delta kD)(DWF)$, where $\delta kD$ is the reactivity obtained through a uniform $\Delta T$ rise. In the GE model this is the quantity calculated by the differential equation which relates $\delta kD$ to $T$. Since $\delta kD$ should be in the denominator of $DWF$, and hence, cancel out, it is only necessary to calculate the reactivity change due to spatially distributing the temperature for use in the kinetics calculation.

To explain how this calculation is done, consider what happens upon exiting from the nth kinetics calculation at time $t_n$ and temperature $T_n$ (see Fig. B1). Static calculations are now used to obtain the effect of the temperature increase from $T_{n-1}$ to $T_{n-1} + \Delta T = T_n$. If the central rod has moved during the last kinetics step, a calculation is done with the temperature at $T_{n-1}$ and with the cross sections in the central zone, $\Sigma_1(t_n)$, representing the blade position at this time. This yields the eigenvalue $\lambda_n$ and a power shape which is to be used to weight the Doppler effect in going to temperature $T_n$. If $\Sigma_1(t_n) = \Sigma_1(t_{n-1})$ then $\lambda_n = \lambda_{nn-1}$, the eigenvalue from the previous calculation at $(t_{n-1}, T_{n-1})$ and that power shape is used in the next calculation. The next calculation for time $t_n$ at temperature $T_n$ yields the eigenvalue $\lambda_{nn}$. The spatially weighted Doppler feedback is then $\lambda_D = \lambda_D_{n-1} + \lambda_{nn} - \lambda_n$ which is extrapolated forward in the next kinetics calculation to give the required Doppler reactivity:
\[ \Lambda kD(t) = \lambda D_n + \frac{\lambda_{nn} - \lambda_n}{\sqrt{T_n - T_{n-1}}} \sqrt{T - \sqrt{T_n}} \]  

(B2)

where temperature is in absolute degrees.

The core cross sections needed in the static calculations to obtain \( \lambda_{nn} \) are assumed to have a temperature dependence given by

\[ \gamma^i = \Sigma^i_0 + \gamma^i \left( T_i - \sqrt{T_o} \right) \]  

(B3)

for each of twenty subregions.

\( \Sigma^i_0 \) is the base cross section for subregion \( i \) at the initial fuel temperature \( T_o \) in °C. \( T^i = T_o + \Delta T \sum_{m=1}^{n} P^i_m \) is the temperature of subregion \( i \) when the core average temperature is \( T_n \). \( P^i_m \) is the fractional power density in subregion \( i \) at time \( t_m \) obtained from the same static calculation which yields \( \lambda^i_m \). The coefficient \( \gamma^i \) is the change in cross section between two fuel temperatures divided by the difference in the square roots of these temperatures. Changes occur in both groups 2 and 3, although the most significant change is in the group 3 absorption cross section.

Since the DWF used in the \((n+1)\)th kinetics calculation is obtained from extrapolating results obtained at temperatures \( T_n \) and \( T_{n-1} \), the initial value must be obtained differently. This is done by calculating the eigenvalue at a temperature slightly above the initial temperature, with the blade in,
using the power distribution at the base temperature to weight the temperature rise. The value of \( k_D \) during the first kinetics calculation is then obtained by interpolating between this \( \lambda \) and the initial just critical condition. Subsequent kinetics calculations are then performed using the extrapolation routine explained above.

One further quantity needed for the static calculations is the function \( S(t) \) which is applied to the cross section changes to yield the correct rod reactivity vs time. This function is obtained as explained in Section III.
References


FLOW CHART FOR THE QSD MODEL

BASIC DATA

INITIAL CONDITIONS

KINETICS CALC.

P(t) T(t)

DWF

\[ \lambda_n = \lambda_{nn-1} \]

\[ \Sigma_0(t_n)^2 = \Sigma_0(t_{n-1}) \]

YES

NO

\[ \lambda_n \]

\[ \Sigma_1(t_n), T_{n-1} \]

\[ \Sigma_1(t_n), T_n \]

\[ \lambda_{D_n} = \lambda_{D_n-1} + \lambda_{nn} - \lambda_n \]

FIGURE B-1
Appendix C

Transient Analysis With the TWIGL Code

The transient analyzed with TWIGL is for a central control rod worth 0.025 $\Delta\rho$ being withdrawn in 2.4 sec. The SCRAM bank worth 0.078 $\Delta\rho$ moves into the core 0.21 sec after the overpower trip at 1920 Mw with an insertion time of 2.5 sec. The $(r,z)$ representation used is shown in Fig. C1.

In addition to the results already discussed in Sections IV and V, there are several other quantities of interest which will now be considered. Figure C2 shows the time-dependent total reactivity of the system and its components due to the movement of control rods and Doppler feedback. The Doppler component is given in terms of its absolute value. It is seen to have the same shape as the fuel temperature shown in Fig. 14. Note that the power peaks when the total reactivity drops to a value equal to the delayed neutron fraction, 0.0065. This can be seen in Fig. 12.

The control reactivity follows the "S" shape shown in Fig. 3 until the SCRAM bank begins to contribute to the curve. There is a slight dip in this curve at about 0.83 sec. This occurs when both the total and Doppler reactivities are changing most rapidly and is probably due to the accumulation of slight errors from each of these quantities.
because the time steps about this sensitive point may have been too large. However, it should be noted that no other quantity analyzed showed any similar discontinuity and the time increments were monitored through the transient to assure reasonable accuracy.

The next ten plots (Figs. C3-C5) show the spatial power distributions at 0.2 s intervals. Each perspective plot is normalized to a core average power density of unity and is plotted on the same scale in order to be more easily compared. In the radial direction the first three grid lines are within the innermost core zone (cf. Fig. C1), the next three are within the middle, initially uncontrolled, zone, and the rest in the outer zone.

Initially, (Fig. C3a) the power in the axial direction is seen to have a generally sinusoidal shape. In the radial direction the peak power is initially in the uncontrolled middle zone. As the transient progresses (Figs. C3b-C3d), the power begins to peak at the top of the core and along the core centerline where the central blade has been removed. In Fig. C4a the power peaking is at its maximum. Doppler feedback causes this peak to drop to a lower value in Fig. C4b but in the following plots (Figs. C4c-C5b) it does not decrease much further. As the blade is withdrawn the power peaking broadens and the maximum value moves down the core (Figs. C3d-C5b). This spreading is attenuated more sharply at the bottom of the core at latter times (Figs. C4d-C5b), as the effect of the SCRAM bank is felt.
Numerical values for the peak/average global power are shown in Fig. C6 and may be related to the corresponding points on the perspective plots. Also shown in this figure are the radial and axial peak/average and their product. The axial quantity is for the power integrated radially \textit{i.e.}, \( P(z) = \int P(r,z)2\pi rdr \) and the radial quantity the converse. The product does not equal the global peak/average due to the non-separability of \( P(r,z) \).
Reference

I ZONE CONSISTING OF CENTRAL SUPERCELL WITH CONTROL BLADE.
II ZONE WITHOUT CONTROL BLADES.
III PARTIALLY CONTROLLED ZONE.
IV REFLECTOR.
V TYPICAL THERMAL-HYDRAULIC REGION.
VI TYPICAL NUCLEAR MESH WITHIN THERMAL-HYDRAULIC REGION.

FIGURE C-1
REACTIVITY EDITS FROM THE TWIGL CALCULATION

FIGURE C-2
NORMALIZED POWER DISTRIBUTIONS FROM TWIGL CALCULATIONS

Fig. C-3
NORMALIZED POWER DISTRIBUTIONS FROM TWIGL CALCULATIONS

Fig. C-4
NORMALIZED POWER DISTRIBUTIONS FROM TWIGL CALCULATIONS

Fig. C-5
POWER EDITS FROM THE TWIGL CALCULATION

FIGURE C-6