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AN ANALYSIS OF GCFBR TRANSIENTS WITHOUT SCRAM*

Hans Ludewig

February 1976

Fast Reactor Safety Division
Department of Applied Science
BROOKHAVEN NATIONAL LABORATORY
Upton, New York 11973

*Work carried out under the auspices of the United States Nuclear Regulatory Commission.

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SUMMARY

In this document the analysis of four transients imposed on a Gas Cooled Fast Breeder Reactor (GCFBR) plant are analyzed. These calculations were carried out using the HELAP code, which is based on RELAP 3B. The transient imposed on the system is a design basis depressurization accident (DBDA) with no scram. Furthermore, the rupture size causing the depressurization was varied over a range from $.6 \text{ ft}^2$ to zero. In the limit of no rupture the transient imposed on the system is due only to the transient behavior of the steam generator and circulator turbine during a DBDA.

The following observations can be made, based on this work:

- a) The time to clad failure as determined in this work is much shorter than in the previously published preliminary work⁽¹¹⁾. This reduction in time is due to the realistic steam generator and circulator models used, and the transient imposed on the steam generator.
- b) In the case of no rupture, the agreement between the current analysis and the previously carried out analysis⁽¹¹⁾ is better. However, the differences are again due to more realistic component models in the current case.
- c) In the case of smaller rupture sizes the superheater tube wall is subject to rapidly rising temperatures and pressures. The latter rise would be strongly affected by the capacity of the safety and relief valves. These temperatures and pressure transients should be evaluated with the tube wall strength in mind, since a steam generator tube failure during such a transient would be undesirable.
- d) The importance of the coupling between the primary and the secondary circuit is illustrated under items a) and b) above. These conclusions could be generalized to include

all transients lasting longer than approximately 10 seconds.

- e) Due to the large amount of computer time required the current code is only useful for short transient, such as those investigated here. Another use for the code would be to test component models which could be incorporated into a new, yet to be developed GCFBR system code. Such a code would be different from an HTGR system code since transients in the HTGR last much longer than in the GCFBR., due to the formers much larger thermal inertia. The design philosophy behind the two codes would thus be different.

I. INTRODUCTION

The analysis of a depressurization accident, despite the low probability of its occurring, plays a central role in the accident analysis of gas cooled reactors. Pressurized gas is required as a coolant since it has improved heat transfer characteristics and any deviation from operating conditions could lead to serious consequences. In the case of the gas cooled fast breeder reactor (GCFBR) the understanding of a depressurization accident is more important than in the case of the graphite moderated high temperature gas cooled reactor (HTGR) since the power density is relatively higher and the heat capacity is relatively lower in the former case. A detailed analysis of the design basis depressurization accident (DBDA) for a GCFBR is given in the preliminary safety information document⁽¹⁾ (PSID). Furthermore, additional studies describing this accident have been published, for both the steam generating GCFBR^(2,3,4) and the direct cycle GCFBR⁽⁵⁾. In all these studies, it is assumed that the reactor is scrammed and thus the auxiliary cooling system of the plant is able to prevent any overheating of the core.

In the present document, a depressurization transient similar to those mentioned above is to be treated with the added feature of no control or shutdown rod motion. Although such a double fault is even more unlikely than a DBDA (except possibly in the case when there is a common mode failure), it is thought that such a sequence could realistically lead to a core melt-down. Depending on the subsequent sequence of events, a hypothetical core disassembly accident (HCDA) could result. The analysis of such an accident sequence can be broken down into a number of stages. First, after the initiating event takes place, the sequence of events can be followed to the point of clad melting (usually the first point of failure). Second, the melting and relocation of the clad, fuel and other core material is analyzed to the point where large fractions of the core are disrupted. The subsequent step or steps will involve an analysis of the dynamics implied by a disrupted core which could include an HCDA.

The first step of the above sequence usually takes the longest to complete and thus its analysis should be carried out using a method which models the entire primary system, including the circulator and steam generator. The analysis of subsequent steps generally only concentrates on the core and finally on only part of the core, since the events potentially take place on a much shorter time scale. It is the first step in this sequence which will be addressed in the current manuscript.

An analysis of the above type, which includes the first two steps, has been reported elsewhere^(6,7,8). The current analysis will include more realistic models for the circulator and steam generator than were included in the previous analysis⁽⁶⁾ and will include feedback effects not used in the other analysis⁽⁷⁾. The final work⁽⁸⁾ is a phenomenological analysis of the accident.

In the following four sections, the plant transient, method of analysis, results and conclusion are outlined.

II. DESCRIPTION OF THE PLANT AND THE TRANSIENT TO BE ANALYZED

The plant to be considered in this manuscript is a GCFBR similar to the one being considered by the General Atomic Company or the Kernforschungs Zentrum Karlsruhe. Neither of these designs has been finalized; however, both are similar in principle and any detail changes in the design will not change the overall conclusions of this study. The entire nuclear steam supply system in these designs is contained within a prestressed concrete reactor vessel (PCRVR). This system is made up of the following main components, a reactor core, three steam generators, three circulators and three auxiliary cooling loops. A circulator consists of a steam turbine driven helium compressor and a steam generator consists of an economizer, evaporator, superheater and a resuperheater. The auxiliary cooling loops contain a heat exchanger and an electrically driven helium compressor.

The reactor core is located within the central cavity of the PCRVR and is suspended at its upper end from a grid plate. The core consists of stainless steel clad fuel rods arranged in a hexagonally shaped fuel element. The clad surface is roughened over the length of the core region to enhance the heat transfer coefficient, at the expense of increasing the core pressure drop. Control and shutdown rods enter the core from the cooler top end.

Helium coolant enters the core at the upper end, from the inlet plenum, flows downward and exits into a large outlet plenum. The hot helium enters three steam generators in which the ducting is arranged in such a manner that the hottest helium first flows through the resuperheater then the superheater, evaporator and finally the economizer. Helium leaving the steam generator enters the compressor inlet plenum, is compressed and exhausts into the reactor inlet plenum. On the secondary side, water enters the economizer, passes through the evaporator and superheater and is then used to drive the helium circulator turbine. The steam subsequently returns to the resuperheater before it

flows to the main turbine. Water/steam flows upwards on the tube side and helium flows downwards across the tubes on the shell side of the heat exchanger.

The transient to be analyzed is an unprotected DBDA. This implies both a transient on the primary side and the secondary side. The primary side transient consists of a depressurization of the coolant and an unscrammed reactor; on the secondary side, the shutdown transient imposed on the steam generator during a DBDA is assumed⁽¹⁾. A rupture in the inlet plenum is assumed to cause the depressurization event. This point in the circuit is chosen for the rupture site since the helium coolant is at its densest at this point (highest pressure and lowest temperature) and thus the highest leak rate possible for a given rupture size results. Four rupture sizes will be assumed, ranging from 0.6 ft² to zero, i.e. no rupture. The power level of the reactor is allowed to vary, subject to coolant density and Doppler feedback. On the secondary side, the transient is identical to that imposed on the plant during a protected depressurization transient. In the latter case, the circulator is slowed down rapidly to prevent over cooling of the core; furthermore, the reduction in coolant density would lead to an overspeed if the steam flow were not reduced. This implies that the compressor slows down, resulting in a steadily reducing flow rate of helium in the primary circuit. Thus, a reduction in the heat transfer coefficient at the clad surface is due to both a reduction in coolant density and flow rate, except in the case where there is no rupture. In the latter case, the reduction in heat transfer is due only to a reduction in flow rate and is thus a pure loss of flow accident.

Reduction of the circulator speed is affected by throttling the flow of superheated steam to the drive turbine. This throttling action has an additional implication - it drastically reduces the flow rate out of the steam generator. The flow out of the steam generator is reduced to 12% of full flow in 3 seconds and the inlet flow to the steam generator is reduced to zero in 1 second. This implies that the contents of the

steam generator is essentially contained, and at the same time, a relatively large amount of heat (since the reactor is close to full power) is being added to it. The steam generator pressure thus rises quite rapidly to levels which might be unacceptably high. In some transients (to be discussed in a following section), the helium temperature rises, causing the steam tube temperature to rise. Under normal conditions, the safety or relief valves would open to relieve the rapid pressure rise; however, since they had not been specified at the start of this analysis, they were omitted. In keeping with accepted engineering design practice, these valves should open at approximately 3200 psia. The rise in pressure would not be entirely eliminated, however, since in all likelihood their capacity would not be specified to accommodate such extreme and highly unlikely situation.

In summary, the transient imposed on the plant may be characterized by the following steps:

- 1) A rupture is simulated in the inlet plenum, resulting in the onset of a depressurization.
- 2) The reactor is unscrammed; the power level being free to vary, subject to feedback constraints.
- 3) Feedwater flow to the steam generator is reduced from full flow to zero in 1 second.
- 4) Steam flow from the superheater to the circulator driven turbine is reduced from full flow to 12% of full flow in 3 seconds, implying a coast down of the coolant flow rate in the core.

In addition, there is no allowance for a safety or relief valve in the secondary side, since their location and capacity has not been clearly specified yet.

III. METHOD OF ANALYSIS

The method of analysis is based on the RELAP3B code⁽⁹⁾, which describes the behavior of a light water reactor system during postulated transients. The program determines flows, mass, and energy inventories, pressure, temperature, fluid qualities along with variables associated with the reactor core and heat exchangers. It is written in a very versatile manner and is designed in such a way that changes can be made to the various models with relative ease. The hydraulic network is divided into "volumes" which are connected by "junctions". Volumes specify a region of fluid within a given set of fixed boundaries; "junctions" are common flow areas connecting volumes. A time-dependent description of the thermal-hydraulic behavior of the reactor system is obtained by integrating a set of differential equations which control the mass and energy balance. These balances are obtained from junction conditions, heat gains in the reactor volume, heat losses and gains in the heat exchanger volumes and heat added by pumping power. By using the equation of state for the fluid, a pressure, temperature and quality is obtained for the fluid within each volume. A one-dimensional momentum balance is then obtained for each function. Using reactivity feedback from the reactor condition, the space-independent reactor kinetics equations are then advanced to determine the new reactor power level. A new cycle is then begun with a new mass and energy balance. More detailed descriptions of the system of equations to be solved can be found in Reference 9.

In the remainder of this section, these models in the original code which had to be changed and new models which had to be added will be summarized. Details of these changes have been described in detail elsewhere^(10,11,12).

A fundamental change that was introduced into RELAP3B was the added capability to treat two different loops containing fluids of different thermodynamic properties, i.e. helium and steam/water⁽¹⁰⁾. This was primarily a programming change and will not be described any further.

The thermodynamic data⁽¹³⁾ used for the helium is based on closed form expression. These formulas express enthalpy, specific heat, viscosity and density in terms of pressure and temperature. In addition to these expressions, an equation of state is used which includes the second virial coefficient, i.e. the helium gas is treated as a real gas. The properties for steam/water appear in the code as tables, which are based on the code package Steam-67. RELAP3B is versatile enough that these tables can be re-computed before embarking on a transient calculation. This facility has the advantage of being able to change the pressure and temperature mesh, depending on the path the transient is expected to follow. Finally, the possibility of choked flow is allowed for at all junctions connecting the various volumes. This final point is a departure from the TAP code⁽¹⁴⁾ which is customarily used to analyze gas cooled reactor systems.

Heat transfer on the primary side takes place in two areas. First, in the core and second in the steam generator. In the first case, a heat transfer coefficient is determined which is a function of the Reynolds number and the Prandtl number. In addition, the clad surface roughness, which enhances the heat transfer coefficient was accounted for by a Reynolds number dependent function. In the steam generator, the heat transfer coefficient on the helium side is computed, based on a correlation due to Grimison⁽¹⁵⁾. This correlation was coded in such a fashion that allowance could be made for variations in transverse and longitudinal pitch of the helical coil. This variation is allowed for by letting the coefficient multiplying the correlation and the power of the Reynolds number to be specified at input. Furthermore, the correlations used on the tube side are those usually used for straight tubes; these are then corrected to allow for the curvature of the tube. The heat transfer in curved tubes is enhanced due to secondary flows which are set up in the tube. Six regions are used in determining the heat transfer coefficient on the secondary side. In the forced convective region, the Seider-Tate⁽¹⁶⁾ correlation is used. For subcooled nucleate boiling, the Thom⁽¹⁷⁾ correlation is used; in the region where

the quality is greater than 0.1 and less than 0.6, the Schrock-Grossman⁽¹⁸⁾ correlation is used; and in the range of quality between zero and 0.1, the coefficient is obtained by extrapolating on quality between the Thom and Schrock-Grossman correlations. Finally, for qualities above 0.6, the Dittus-Boelter⁽¹⁹⁾ correlation is used.

The steam generator and circulator models couple the primary and secondary sides. In the case of the steam generator, this coupling is achieved by exchange of thermal energy and in the case of the circulator, the coupling is due to the exchange of mechanical work. In all the transients considered to date, the flow of thermal energy has been from primary to secondary and the flow of mechanical work has been from secondary to primary. In the case of the steam generator, the quantities of prime interest are the heat lost by the primary fluid and the heat gained by the secondary fluid. At steady state, these two quantities are equal; however, during a transient, this equality is not maintained since the tube wall acts as a thermal inertia. This inertia is accounted for by solving the time dependent heat conduction equation for the tube wall with spatial boundary conditions which are specified as a function of film heat transfer coefficient, wall temperature and the bulk fluid temperature and an initially specified temperature distribution. The initial condition is obtained by solving the steady state heat conduction equation and knowing the steady state heat flux. Finally, the interface between the vapor phase and the liquid phase is allowed to move, and within the volume in which this interface occurs, the phase separation model coded into RELAP3B is used.

The steam turbine driven compressor model is set up in such a manner that it allows for the inertial effects of the rotor and it uses operating tables, which are read in as input. Control of the steam flow to the turbine is carried out by using the check valve model in RELAP. Briefly, this allows the user to reduce the flow in a junction, containing a check valve, to zero at a given point in time. Thus by having a number of junctions connecting the steam generator and the turbine, each one

with a check valve, and arranging for these valves to close sequentially, the flow to the turbine can be reduced in a step wise manner. This method of control has two shortcomings. First, the flow will redistribute itself and the desired rate of reduction will not be achieved. Second, it is not possible to allow the flow to the turbine to be a function of some variable, i.e. clad temperature. These shortcomings did not prove to be very serious for the transients being considered. However, the second shortcoming does impose limits on the usefulness of the code. A realistic control system for the turbine does not exist and would have to be created as a future model.

The leak model used for simulating the break in the inlet plenum is based on adiabatic compressible flow and allows for both choked and unchoked flow. Finally, the feedback expressions used to describe the reactivity changes to be used in the point reactor equations were adapted to the GCFBR. Two modes of feedback were assumed: Doppler feedback due to fuel temperature changes, and feedback due to coolant density change. The latter phenomenon shifts the neutron spectrum up or down in energy. Expressions for these relationships are based on Reference 20.

IV. RESULTS OF ANALYSIS

In this section, the consequences of the accidents outlined in Section II will be discussed. These accidents will be analyzed by the techniques outlined in Section III. A schematic diagram of the model used to analyze the accidents is shown in Figure 1. The connection between the primary helium and secondary steam/water loops is illustrated on this figure. A rupture is modeled in the inlet plenum and the three steam generators are modeled as one. Furthermore, no hot channel is allowed for in the core; the core volume represents an average core channel. All computed core parameters are thus indicative of the average core channel.

Figures 2 - 14 show the results for various computed parameters of interest. Figures 2 - 9 refer to parameters computed for the core, Figures 10 - 12 refer to parameters on the secondary side of the steam generator and Figures 13 - 14 refer to the compressor performance. All the calculations, except one, were terminated when the computed clad temperature reached 2200°F ; in the case of no rupture (pure loss of flow), the calculation was stopped when the clad temperature reached approximately 2100°F . The time to reach 2100°F in the latter case was estimated by extrapolation.

Figure 2 shows the variation of inlet plenum pressure with time. It is seen that in the cases where a finite rupture is considered, the pressure drops with time. The rate of decrease increasing with rupture size. In the case of a pure loss of flow, the pressure drops slightly for the first five seconds and then starts increasing steadily. The initial drop is due to the compressor coast down, which occurs in the first four seconds, but then the subsequent increase in pressure can be ascribed to the increasing helium temperature. The pressure increase is not felt to be very large and is well within the design safety margins of a pressure vessel.

Figure 3 illustrates the variation of average flow rate into the core as a function of time for various rupture sizes. The overall trend is the same in all cases and follows the behavior of the compressor coast down. Differences between the curves are due to the difference in helium density from case to case. The density being much lower for the larger ruptures compared to the smaller ruptures at any given time.

Shown on Figures 4 and 5 are the variations of core coolant density and average fuel temperature with time. These two quantities are used in determining the reactivity feedback used in the point reactor kinetics equations, which determines the reactor power. The average fuel temperature rises monotonically in all cases, thus increasing the negative effects due to Doppler feedback. As the rupture size increases, it is seen that the rate of increase in temperature increases, implying a higher rate of negative reactivity insertion. Average helium density in the core drops off in all cases. In the cases with finite rupture sizes, the drop off is highest for the larger rupture sizes. However, in the case of a pure loss of flow, the density levels off after about ten seconds. All the density variations illustrated on Figure 5 imply a positive insertion of reactivity, the highest rate of insertion corresponding to the largest rupture sizes.

The variation of total reactivity with time is shown on Figure 6. The two competing effects outlined in the above paragraph are combined to produce the net reactivity change. It is seen that for all cases, the positive component due to density changes overwhelms the negative component due to Doppler broadening initially. However, the increasing fuel temperature causes the negative component of reactivity to increase sufficiently to reverse the increasing reactivity and in two cases the total reactivity finally becomes negative. Finally, it can be seen that the magnitude of the reactivity change s involved (approximately 15¢) are relatively small compared to reactivity insertion in the latter stages of the accident when clad relocates and core motion takes place.

The power changes resulting from the reactivity changes shown on Figure 6 are shown on Figure 7. It is seen that the power variation with time roughly follows the shape of the reactivity variation with time. Furthermore, it is seen that the maximum power increase is approximately 30% before the transient is terminated. In three of the four cases the power has passed its peak value. This reduction in power is due to the Doppler feedback effect overwhelming the positive reactivity of the density variation. In the case of the largest rupture size, the rate of increase has slowed considerably. Finally, in the case of the loss of flow accident, after a slight initial rise the power drops off monotonically.

One of the parameters which is most sensitive to the type of transient described above under Section II is the clad surface temperature at the hot spot. This is one of the areas where a failure could be initiated, i.e. clad melting. Since the clad surface temperature at the hot spot was not one of the parameters that was computed, the clad temperature for the average channel will be considered. Shown on Figure 8 is the variation with time of the average channel clad surface temperature for the four transients considered. In the case where finite rupture sizes were considered, the temperature is seen to rise rapidly and reach levels at which melting could occur within seconds. Even a modest value for the peak to average power indicates that there would certainly be melting at the hot spot, and in all likelihood the entire core would be affected shortly afterward. In the case of the loss of flow accident, the situation is not as clear. The temperature rises quite sharply in the initial stages, but since the power level is dropping off, the rate of increase in the clad surface temperature slows down. A surface temperature of 2000°F is still reached within approximately 10 seconds and this coupled with a hot spot factor of 1.25 could yield a temperature high enough to cause melting at the hot spot. However, since the power is dropping off, the rate of increase will continue to drop and the average channel may not melt. This calculation will have to be carried out for a longer period

to determine the peak temperature of the average channel. Damage at the hot spot is essentially certain; however, the spatial extent of the damage within the core is not clearly understood.

Figure 9 is a different representation of the results shown on Figure 8. In this case, a specific temperature is chosen and the time to reach this temperature is plotted against rupture size. It seems that in all cases, the average channel reaches 2000°F and thus with a hot spot factor of 1.25, a temperature is reached (2500°F) which could cause melting at the hot spot. It is also seen that the curves approach an asymptotic lower limit in time for any given temperature; this limit is due to the thermal inertia of the system.

Figure 10 is an illustration of the variation of the primary side heat transfer coefficient with time in the steam generator. The example shown is for the superheater; however, all primary side steam generator volumes react in essentially the same way. After starting off at an operating point, there is a sharp drop in the value of the coefficient for approximately five seconds and then it levels off to some much lower value, approximately 35 - 40% of the operating value. The spread in the curve is due to the variation in pressure, and thus density, from case to case.

Shown on Figure 11 is the variation with time of the average superheater tube wall temperature on the primary side. This temperature is not only a function of the heat transfer coefficient, shown on Figure 10, but also a function of the bulk helium temperature. The bulk helium temperature is a function of the heating rate in the core, which is determined by the heat transfer coefficient and the power. In the case of the largest rupture, the power is increasing; however, at the same time, the heat transfer coefficient drops off rapidly, the net result being a slight rise in the helium temperature in this case. Coupling the slight rise in the helium temperature to the falling heat transfer coefficient in the steam generator causes the tube temperature to drop off in this case. As the rupture size decreases to zero, the heat transfer coefficient does

not drop off as rapidly; consequently, the bulk helium temperature is higher. Coupling this with increasing coefficients in the steam generator leads to ever increasing steam generator tube temperatures at any given time with decreasing rupture size. As can be seen from the figure, the temperature increase is quite rapid in the case of a loss of flow accident.

The average pressure in the evaporator, which is indicative of the average steam generator pressure, is shown on Figure 12. In view of the transient imposed on the steam generator, it is seen that after approximately 2 - 3 seconds, there is a sharp increase in three of the transients. This pressure increase is due to the virtual closure of the steam generator, since only 12% of the steam is allowed to escape to drive the circulator turbine, and to an increasing bulk helium temperature in these cases. It is clear that in the two most extreme cases, the safety and relief valves would open and slow the rapid pressure rise. However, the safety valve performance was not specified at the time of the analysis and was thus not included. In the case of the largest rupture size, the low rate of heat transfer limits the build-up of pressure on the secondary side.

Figure 13 shows the variation with time of the circulator shaft speed. It is seen that it drops off rapidly, in approximately four seconds, to a value of 28% of the operating value. The slightly lower rate decreases in shaft speed in the case of the largest rupture size is due to the lower helium density in this case. This is not an obvious result since the steam pressure feeding the turbine is lower in the larger rupture cases, as shown on Figure 12. However, since the turbine operates at choked or near choked condition, the difference in steam pressure does not affect it very markedly.

Shown on Figure 14 is the operating map used for the compressor. It shows the variation of a dimensionless compressor pressure rise with flow rate for various compressor shaft speeds. Imposed on this map are the trajectories followed by the compressor during the four transients considered. It is seen that they all follow quite different paths,

making it essentially impossible to predict ahead of time the way the compressor will behave under any given transient condition. The importance of modeling the steam generator together with the steam turbine is thus illustrated by these curves. Since only with the coupled representation used here, where the various controlling equations are solved simultaneously, can the response of the compressor be approximated with any certainty. Finally, the importance of an accurate compressor response is illustrated by the fact that all heat transfer coefficients on the primary side depend very strongly on the flow rate which is dictated by the compressor pressure rise.

V. CONCLUSIONS

The following conclusion can be reached regarding the preceding work:

a) In comparing this analysis with the previously carried⁽¹¹⁾ out analysis of a DBDA without scram, it can be seen that for the current case the time to reach failure is very much shorter. This reduction in time scale is due to the fact that the steam flow to the circulator turbine is throttled in the same way as it would be in the case of a scrammed DBDA. In the previous case, this reduction in steam flow and subsequent behavior of the circulator was impossible to model since the circulator and steam generator model was very preliminary. However, since the circulator slowed down, this would imply that an optimal circulator shaft speed was not used for this particular transient. In order to avoid circulator overspeed, a more sophisticated model would have adjusted the steam flow to the turbine to maintain a constant shaft speed in this case. This speed would have been the operating speed and in this way a higher core heat transfer coefficient could have been maintained for a longer time. In this way, clad overheating and eventual melting could be postponed for a longer time. In some of the less severe transients, this extra time may be sufficient to introduce preventative measures, at least to prevent the entire core from melting.

b) The transient in which the steam flow to the turbine was reduced to 12% of full operating flow and held constant, with no rupture in the inlet plenum can be compared to a loss of flow accident analyzed previously⁽¹¹⁾. In the original analysis, the helium flow rate was coasted down to 12%. A comparison of the times required to melt the hot spot, i.e. approximately 2000°F for the average channel, is of the same order. In the current analysis, the time required was 10 seconds and in the previous analysis, the time required was approximately 5 seconds. The difference can again be ascribed to the inadequate circulator model in the previous analysis. Furthermore, on comparing the shape of the clad

temperature versus time curve, it can be seen that the rate of increase has slowed markedly in the current analysis. This points up the necessity to use a realistic circulator model in the analysis of this accident, since the probability exists that core melting at the hot spot would occur, but nowhere else as is the case in the other transient.

c) In the case of the smaller rupture sizes and the loss of flow, it is seen that the steam generator tube walls are subject to a rapidly increasing temperature (approximately 200°F in the superheater in 10 seconds) and in the case of no safety and relief valve action, a large increase in pressure (approximately 50% increase in 10 seconds). This increased load on the superheater tube wall would be quite severe and if any weak spots exist, they could lead to a tube failure. Such a failure coupled with a loss of flow accident would be very undesirable. Thus, the capacity of the safety and relief valves would have to be of such a magnitude to prevent this possibility from occurring.

d) In the second conclusion above, the importance of the coupled steam generator and circulator was illustrated in connection with the loss of flow accident. That conclusion can be generalized to apply to any transient lasting more than approximately 10 seconds. This is particularly important when analyzing such marginal situations where clad temperature rises and before melting starts, the probability of a temperature decrease exists, i.e. due to a power reduction or an increase in the heat transfer coefficient. Finally, this explicit steam generator and circulator model gives the variation of temperatures and pressures in the steam generator, which normally would not be known, and could be important as shown in conclusion (c).

e) Due to the requirement of a short time step imposed on the calculation by the steam generator model (due to rapid and large changes in secondary side heat transfer coefficients) these calculations were very time consuming. The code is thus limited to the analysis of the types of transients outlined here, i.e. DBDA and LOF with no scram. A further use of the code would be its usefulness as a test bed for the

development of new component models to be used in a new advanced, yet to be developed, system code. This code would be specific for GCFBRs and could not efficiently be used for HTGRs. This is because the types of transients of interest in the two reactors are sufficiently different. In an HTGR, the scrammed transients last for many hours, whereas in a GCFBR, they are over in essentially 30 minutes. Furthermore, because of the large thermal inertia of a HTGR core, the variation in time of most parameters of interest are not as steep as in the GCFBR. Thus, the complexity of the component models and the time step algorithm would of necessity be different to analyze these two reactor systems.

PRIMARY SIDE SUPERHEATER HEAT TRANSFER COEFFICIENT

(Btu/hr - ft² - °F)

1000

900

800

700

600

500

400

300

200

100

RUPTURE SIZE (ft²)

0.0

0.15

0.30

0.60

TIME (sec)

2

4

6

8

10

12

14

AVERAGE PRIMARY SIDE SUPERHEATER
TUBE WALL TEMPERATURE (°F)

REFERENCES

- (1) "Gas Cooled Fast Breeder Reactor Preliminary Safety Information Document", Gulf General Atomic, GA-10298 (1971).
- (2) D. R. Buttemer, J. A. Larrimore, "Response of Gas-Cooled Fast Breeder Reactors to Depressurization Accidents", Nuc. Eng. and Design, 26, 195 (1974).
- (3) J. A. Larrimore, T. R. Moffetti, "Safety Evaluations for Gas-Cooled Reactors", Proc. ANS Topical Meeting Gas-Cooled Reactors: HTGR and GCFBR, 418 (1974).
- (4) M. Dalle-Donne, C. A. Goetzmann, "Gas-Cooled Fast Breeder Reactor Design, Development and Safety Features", Proc. ANS Topical Meeting - Gas-Cooled Reactors: HTGR and GCFBR, 319 (1974).
- (5) A. Tiberini, "Das Dynamische Verhalten eines Gasgekühlten Schnellen Brutreaktors mit direktem Gasturbienen Kreislauf, dargestellt am Beispiel des Rohrbruch-Unfalles in einer 1000 MWe - Anlage mit Rekuperator", Eidg. Inst. für Reaktorforschung, EIR-263 (1974).
- (6) R. E. Alcouffe, H. Ludewig, "Core Disruptive Accident Analysis of a Gas Cooled Fast Breeder Reactor", Trans. Amer. Nucl. Soc., 21, 296 (1975).
- (7) D. R. Buttemer, A. Torri, "Response of the Gas-Cooled Fast Breeder Reactor to Flow Reduction Accidents", Trans. Amer. Nucl. Soc., 21, 295 (1975).
- (8) A. Torri, D. R. Buttemer, "Loss-of-Flow Accident Phenomenology in GCFR", Trans. Amer. Nucl. Soc., 21, 295 (1975).
- (9) RELAP3B Manual - A Reactor System Transient Code, Brookhaven National Laboratory, BNL-20065 (1974).
- (10) B. A. Martin et al., "NALAP, An LMFBR System Transient Code", Brookhaven National Laboratory, BNL 50457 (1975).

- (11) H. Ludewig, L. G. Epel, "Preliminary GCFR Plant Transients Analysis Using the System Code HELAP", Brookhaven National Laboratory, BNL 19854 (1974).
- (12) H. Ludewig, "Steam Generator and Circulator Model for the HELAP Code", Brookhaven National Laboratory, BNL 20583 (1975).
- (13) K. Bammert, "The Thermodynamic Properties of Helium as a Working Medium for Nuclear Power Plant Gas Turbines", Kerntechink, 11, 88 (1969).
- (14) A. M. Baxter, L. L. Swanson, "HTGR Plant Dynamics Analysis Under Accident Conditions", Nuc. Eng. and Design, 26, 103 (1974).
- (15) E. D. Grimison, "Correlations and Utilization of New Data on Flow Resistance and Heat Transfer for Cross Flow of Gases over Tube Banks", Trans. ASME, 59 (1937).
- (16) E. W. Seider, G. E. Tate, "Heat Transfer and Pressure Drops of Liquids in Tubes", Ind. and Eng. Chem. 28 #12, 1429 (1936).
- (17) TRS Thom et al., "Boiling in Sub-Cooled Water During Flow up Heated Tubes or Annuli", Proc. Inst. Mech. Eng., 180, pt. 3C, 226 (1965).
- (18) V. E. Schrock, L. M. Grossman, "Forced Convective Boiling Studies Final Report on Forced Convective Vaporization Project", TID-14632 (1959).
- (19) F. W. Dittus, L. M. K. Boelter, "Heat Transfer in Automobile Radiators of the Tubular Type", Univ. of Calif. Publ. in Eng. 2 #13 (1930).
- (20) A. Torri, M. T. Driscoll, "Reactivity Insertion Mechanism in the GCFR", General Atomic Company, GA-A 12934 (1974).