Science and Technology of Nuclear Installations

Natural Circulation in Nuclear Reactor Systems

Guest Editors: Dilip Saha and John Cleveland

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Editorial

Natural Circulation in Nuclear Reactor Systems

Dilip Saha[1] and John Cleveland[2]

[1] *Reactor Engineering Division, Bhabha Atomic Research Centre, Trombay, Mumbai 400085, India*
[2] *Nuclear Power Technology Development Section, Division of Nuclear Power, International Atomic Energy Agency, Wagramer Strasse 5, P. O. Box 100, 1400 Vienna, Austria*

Correspondence should be addressed to Dilip Saha, dsaha@barc.gov.in

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It gives us great pleasure to bring out this special issue on "Natural circulation in nuclear reactor systems" which assumes special significance in the context of present energy technology scenarios. Today nuclear energy produces about 15% of total world electricity. However, public concern about the safety of nuclear plants has resulted in sociopolitical constraints on its use in some countries. Now a worldwide renewed interest in nuclear energy is evident which is caused mainly by the following factors: (a) progressively dwindling world reserve of fossil fuel, (b) a deep-rooted concern about global warming, (c) increasing oil price, and (d) good performance of current plants. These factors are leading to rising expectations for nuclear energy for the future.

For the sustenance of this renewed interest, besides fuel resource, a number of important issues are being addressed leading to the development of advanced reactor designs as well as fuel cycle technologies. The major issues, which these advanced reactors and fuel cycle concepts are addressing, include economic competitiveness, achieving very high level of safety, waste disposal, environmental effects and proliferation resistance.

An important feature of several advanced reactors designs is the incorporation of passive safety systems. The IAEA conference on "The Safety of Nuclear Power: Strategy for the Future," convened in 1991, recommended that for new plants "the use of passive safety features is a desirable method of achieving simplification and increasing the reliability of the performance of essential safety functions and should be used wherever appropriate." Nuclear plant designers select active safety systems, passive safety systems, or combinations considering fulfilment of required safety functions with sufficient reliability, and the impact on plant operation and cost. A number of passive systems incorporated in advanced reactors employ natural circulation as the mode of energy removal underlining the importance of natural circulation in nuclear reactor design

By definition, natural circulation is a process in which the fluid motion is driven by a density gradient and no external source of energy is required. However, the driving head for natural circulation is low and can be influenced by small changes in operating conditions. Sometimes the flow is not fully developed and can be multidimensional in nature. All these have led to the need of thoroughly understanding the phenomena involved to ensure reliability of natural circulation systems. This has necessitated dissemination of knowledge in this complex and important area. This special issue is a timely and very effective step in this direction.

The papers in this issue cover most of the important aspects of natural circulation-modeling and code development, experimental investigations, development of performance evaluation tools, flow instabilities, safety analysis, and lastly reliability of natural circulation systems.

This issue has been a modest effort to bring to the readers an update on a subject of importance to the reactor designers. We are sure that the readers of this issue will find the papers of immense value and get provoked to explore further in this area.

Dilip Saha
John Cleveland

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Research Article

Numerical Analysis of General Trends in Single-Phase Natural Circulation in a 2D-Annular Loop

Gilles Desrayaud[1] and Alberto Fichera[2]

[1] *INSSET/LETEM, Université de Picardie Jules Verne, 48 rue Raspail, BP 422, 02109 Saint-Quentin, France*
[2] *Dipartimento di Ingegneria Industriale e Meccanica, Università di Catania, Viale A. Doria 6, 95125 Catania, Italy*

Correspondence should be addressed to Gilles Desrayaud, gilles.desrayaud@insset.u-picardie.fr

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The aim of this paper is to address fluid flow behavior of natural circulation in a 2D-annular loop filled with water. A two-dimensional, numerical analysis of natural convection in a 2D-annular closed-loop thermosyphon has been performed for various radius ratios from 1.2 to 2.0, the loop being heated at a constant flux over the bottom half and cooled at a constant temperature over the top half. It has been numerically shown that natural convection in a 2D-annular closed-loop thermosyphon is capable of showing pseudoconductive regime at pitchfork bifurcation, stationary convective regimes without and with recirculating regions occurring at the entrance of the exchangers, oscillatory convection at Hopf bifurcation and Lorenz-like chaotic flow. The complexity of the dynamic properties experimentally encountered in toroidal or rectangular loops is thus also found here.

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1. INTRODUCTION

Natural circulation is an important mechanism, and the knowledge of its behavior is of interest for cooling purposes in industrial processes, including solar water heaters, geothermal processes, gas turbine blade cooling, and as part of the emergency core cooling system in nuclear reactors. Heat removal by natural circulation provides a practical means to cool and depressurize the reactor primary coolant system after a reactor accident. But in advanced nuclear reactor designs, heat removal systems driven by natural convection are also a potentially important design feature. Indeed, some advanced nuclear plant designs rely on natural circulation to remove core power under normal operation (startup, normal power operation, and shutdown), and some designs rely on natural circulation to provide cooling of the containment. Because of their practical importance, thermosyphons have been the subject of a large number of theoretical and experimental studies. A review of the wide applications of natural circulation loops in engineering systems has been given by Zvirin [1]. These also attract attention because of the variety of fluid motions and the complexity of the dynamic properties encountered, in spite of the simplicity of their geometry. The research pioneered by Keller [2], Welander [3], and Malkus [4] has been reviewed by Greif [5]. The presence of a reverse flow region was first qualitatively reported by Creveling et al. [6] who also first observed the Lorenz-like chaotic flow in their experiments (see also [7–9]).

Previous studies of toroidal loops have utilized a one-dimensional approach by averaging the governing equations over the pipe cross-section, which required a priori specifications of the friction and the heat transfer coefficients. To the best of our knowledge, in the literature only one numerical experiment on steady 3D flow in a toroidal loop exists (Lavine et al. [10]).

The purpose of the present study is to investigate by direct numerical integration of the governing equations the steady and unsteady motions in a thermosyphon of simple well-defined geometry, a 2D-annular loop.

2. GOVERNING EQUATIONS AND RESOLUTION

Consider a 2D-annular loop of laminar fluid heated over one-half of its area at a uniform heat flux (q'') and cooled over the remaining half at a constant temperature (T_c) as shown in Figure 1. The inner (respectively outer) cylinder radius is r_i^* (resp., r_o^*), the channel gap being defined as

Figure 1: Coordinates and 2D-annular geometry.

$a = r_o^* - r_i^*$. The classical governing (Navier-Stokes plus energy) equations for incompressible fluid with the Boussinesq approximation used in cylindrical coordinates are not written here due to lack of space but can be found in the work of Desrayaud et al. [11]. The way in which the equations are nondimensionalized is the following for the dimension, time, velocity, and temperature:

$$r = \frac{r^* - r_i^*}{a}, \qquad \tau = \frac{\alpha t^*}{a^2}, \qquad u = \frac{a u^*}{\alpha}, \qquad \Theta = \frac{T - T_c}{\Delta T}, \qquad \Delta T = \frac{a q''}{\kappa}, \tag{1}$$

The thermophysical characteristics of the fluid are its thermal diffusivity α, its kinematic viscosity ν, and its thermal conductivity κ. The sign β is the coefficient of thermal expansion and the nondimensional radial and axial velocities are u and v.

The nondimensional boundary conditions are the following, the angular coordinate θ being measured from the downward vertical (see Figure 1):

$$\begin{aligned} &\text{Heating section at } \frac{-\pi}{2} \le \theta \le \frac{\pi}{2}, \quad r = 0 \text{ and } 1, \\ &\qquad u = v = 0, \ \frac{\partial \Theta}{\partial r} = -1, \\ &\text{Cooling section at } \frac{\pi}{2} \le \theta \le 3\frac{\pi}{2}, \quad r = 0 \text{ and } 1, \\ &\qquad u = v = 0, \quad \Theta = 0, \\ &2\pi\text{-periodicity in the } \theta \text{ direction}, \quad 0 < r < 1, \\ &\qquad u, v, \Theta|_{\theta=0} = u, v, \Theta|_{\theta=2\pi}. \end{aligned} \tag{2}$$

The motionless and isothermal solution used as the initial guess for computations is given by

$$\begin{aligned} &\text{for } \tau = 0 \quad \text{at } 0 \le r \le 1, \quad 0 \le \theta \le 2\pi, \\ &\qquad u = v = 0, \ \Theta = 0. \end{aligned} \tag{3}$$

The nondimensional parameters that govern the flow are the Rayleigh number Ra built on the channel gap a, the Prandtl number Pr, and the radius ratio R which are defined by

$$\mathrm{Ra} = g\beta a^3 \frac{\Delta T}{\alpha \nu}, \qquad \mathrm{Pr} = \frac{\nu}{\alpha}, \qquad R = \frac{r_o^*}{r_i^*}. \tag{4}$$

The dimensionless mass flow rate circulating inside the loop (which is also the dimensionless cross-average velocity) is defined as $Q = \int_0^1 v(r,\theta)dr$ and is angle independent owing to the laminar fluid incompressibility.

The control volume procedure is utilized to discretize on a staggered, uniform cylindrical grid the nonlinear system of governing equations and boundary conditions with the second-order centered scheme for the convective terms. The SIMPLER algorithm is employed for the velocity-pressure coupling. The momentum and energy equations are cast in transient form and the time-integration is performed using an alternating direction implicit (ADI) scheme. Boundary conditions of periodic type were applied at $q = 0$ and 2π. This consists of using the solutions calculated at a previous radial sweeping as the boundary conditions in the angular sweeping of the ADI procedure. A uniform grid with 30×320 control volumes in the r and θ directions was used to obtain all the results presented in this paper. The numerical code has been extensively validated on several cases (see [11, 12]).

3. RESULTS AND DISCUSSION

Over 100 runs have been carried out for steady flows and 50 for unsteady flows for radius ratios $R = 1.2, 1.4, 1.6, 1.8$, and 2.0. The working fluid was water with Prandtl number, Pr = 5, in all cases. Three videos accompany this article (see videos in Supplementary Material available online at doi:10.1155/2008/895695). These have been compressed in mp4 format and as a result, they are somewhat inferior in quality; however, in the resulting stylized representation, the important features show up rather well (although the colors do not correspond to those in the illustrations in the text).

A map of the various regimes exemplifying the "route to chaos" in such configuration is presented in Figure 2 for radius ratios $R = 2.0, 1.8, 1.6, 1.4$, and 1.2. These regimes are pseudoconductive (ps-*c*), convective (*conv*), secondary motion (*second*), oscillatory (*oscill*), and chaotic (*reverse*) regimes. It must be noted that this map only gives general trends of the flow. No attempt to find accurate transition Rayleigh numbers has been made because unstationary motions are very CPU-time consuming. For instance, it is a hard task to properly capture by brute force integration the Rayleigh number at which the monoperiodic motion occurs because the Ra-gap is very narrow. Moreover, this regime can degenerate after long time integration into chaotically reversing motion. In the following, the results used to illustrate the various regimes are for different radius ratios but, as shown in Figure 2, all these regimes have been found whatever the radius ratio is.

Ra/R	2	1.8	1.6	1.4	1.2
0.5					ps-c
1	ps-c		ps-c	ps-c	Conv
10	ps-c	ps-c	Conv	Conv	Conv
100	Conv	Conv	Conv	Conv	Conv
12000	Conv	Conv	Conv		
12500	Second				
13000	Oscill				
15000	Reverse	Conv	Conv	Conv	Conv
16000		Second			
17000		Oscill	Conv		
18500		Oscill	Conv		
19000		Reverse			
20000			Conv		Conv
22000			Conv	Conv	
22500			Second		
23000			Second		
23500			Second		
24600			Oscill		
25000			Oscill		Conv
26000			Reverse	Conv	
29000				Conv	
30000					Conv
32000				Conv	
35000				Second	Conv
42000				Second	
45000				Oscill	Conv
46000				Oscill	
47000				Reverse	
75000					Conv
80000					Second
100000					Second
120000					Oscill
145000					Oscill
200000					Reverse

Figure 2: Map of the various regimes: pseudoconductive (ps-*c*), convective (*conv*), secondary motion (*second*), oscillatory (*oscill*), and chaotic (*reverse*) regimes.

3.1. Steady flow

3.1.1. Pseudoconductive regime

At very low values of the Rayleigh number (Ra = 10, $R = 1.8$; see Figure 3), the stratified temperature which is almost symmetrically distributed in both halves of the annulus ($[0, -\pi]$ and $[0, \pi]$ in Figure 3(a)) reveals the importance of axial conduction, with convection playing only a minor role in the heat transfer. Hence, the heat is mainly transferred by conduction from the lower hot part to the upper cold part, and thus this regime can be appropriately qualified as pseudoconductive. A global counter-clockwise motion all around the loop is almost nonexistent and two very large recirculations of weak motion occur (see Figure 3(b)). These two large but weak vortices characterize the pseudoconductive regime, both being located at the heater, one along the inner wall while the other is along the outer wall. These two vortices occupied each almost one quarter of the loop, a weak global motion flowing in-between the vortices. A weak global motion always sets in around the loop, even at very low Rayleigh numbers, due to the cylindrical geometry of the loop, and a purely conductive solution was never found (i.e., with no global fluid motion). This configuration is like a Rayleigh-Bénard one (heating at the bottom and cooling at the top part) but contrarily to this well-known case for which no motion is expected until a critical Rayleigh number is reached, the heated fluid immediately proceeds upward along the inner bottom heated cylinder because no stable equilibrium can exist along a convex wall. Furthermore, due to the symmetry of the heating and cooling sections to the vertical axis, the global motion can be clockwise or counter-clockwise at random. Two convective solutions of very weak motion branch off symmetrically from the state of rest (i.e., Ra = 0) undergoing a pitchfork bifurcation [11]. Surprisingly, Figure 2 shows that decreasing the radius ratio decreases the Rayleigh number at which the pseudoconductive regime occurs. When the radius ratio decreases, one way in which this can occur being a decrease in the channel gap between the walls, the size of the cells becomes too large to permit the fluid to move, the *vena contracta* being too small. The cells are then pushed away by the flow and thus disappear.

3.1.2. Convective regime

For moderate values of the Rayleigh number, a quasi-one-dimensional flow exists along the loop and the flow is steady without undergoing any oscillatory process. Figure 4 shows the temperature and stream function fields on the two branches of the pitchfork bifurcation for a Rayleigh number equal to 1000 and a radius ratio $R = 2.0$. One of these two solutions has been obtained by increasing the Rayleigh number from 0 to 1000 (see Figure 4(a)) while the other has been obtained by increasing the Rayleigh number step by step (see Figure 4(b)). With increase in Ra, the fluid flow undergoes a short-term oscillation before becoming stable. The temperature and stream function fields for Ra = 22 000 and $R = 1.60$ are shown in Figure 5, with the fluid moving around the loop in a counter-clockwise direction. The streamlines that were concentric for low values of the Rayleigh number are now slightly deformed at the entrance region of the exchangers, while being more distorted at the heater (see Figure 5(b)). This distortion is due to a zone of a very strong temperature increase clearly visible at the entrance of the heater along the outer wall (see Figure 5(a)). These distorted streamlines are less pronounced at the cooler entrance due to the imposed temperature at the walls.

3.1.3. Secondary flow regime

At Ra = 22 500 and $R = 1.60$ (see Figure 6), it should be noted that, in addition to the circulatory main flow, two cells in the first and in the third quadrants can be seen. These two recirculating regions occur near the outer wall of the entrance of the exchangers (see Figure 6(b)), with the bigger vortex always being at the heater while the one at the cooler is very weak. As a consequence, the zone of the strong temperature increase that extended largely downstream along the outer walls is now reduced (see Figure 6(a)).

The dimensionless temperatures at the inner and outer walls are shown in Figure 7 only along the heater, the cooler being at an imposed temperature. The temperature of the

(a) (b)

Figure 3: Temperature and stream function fields in a pseudoconductive regime; Ra = 10, R = 1.8, and Pr = 5. (a) Isotherms, from 0.5 (blue) to 8.5 (red) in steps of 0.5. (b) Stream function field, from −0.005 (blue) to 0.1 (red) in steps of 0.005 (Vortex in the red zone: 0.102, 0.104).

(a) Counter-clockwise motion; isotherms, from 0.1 (blue) to 1.1 (red) in steps of 0.1

(b) Clockwise motion; stream function field, from 1 to 11 in steps of 1

Figure 4: Temperature and stream function fields in a convective regime; Ra = 1000, R = 2.0, and Pr = 5.

outer wall is always largely greater than the temperature of the inner wall. This is due to the boundary condition, imposed heat flux at the heater, combined with the curvature. Since the surface of the outer wall is R times greater than the surface of the inner wall, the total heat flux transferred to the convective fluid flow is also R times greater and there results a higher level of temperature at the outer wall. At Ra = 22 000 (22 500, resp.), the average temperature of the outer wall is $\overline{\Theta}_h = 0.496$ (resp., 0.527) while it is only 0.390 (resp., 0.394) at the inner wall. Following the main circulation from $\theta = -90^\circ$ to $\theta = 90^\circ$, Figure 7 (Ra = 22 000) clearly shows at the entrance of the heater a sharp temperature increase at the outer wall followed by a slight decrease of the temperature. This inverse temperature gradient explains why the fluid is able to proceed upstream along the outer wall at the entrance of the heat exchanger. At Ra = 22 500, a very large recirculation occurs at the entrance of the heater (see Figure 6(b)) amplifying the strong inverse temperature

(a) (b)

Figure 5: Steady motion in a convective regime; Ra = 22 000, R = 1.6, and Pr = 5. (a) Isotherms, from 0.05 (blue) to 0.55 (red) in steps of 0.05. (b) Stream function field, from 5 (blue) to 60 (red) in steps of 5.

(a) (b)

Figure 6: Steady motion in a secondary flow regime; Ra = 22 500, R = 1.6, and Pr = 5. (a) Isotherms, from 0.05 (blue) to 0.65 (red) in steps of 0.05. (b) Stream function field, from 5 (blue) to 60 (red) in steps of 5.

gradient (see Figure 7). Moreover, no increase of the average temperature at the inner wall is noticed from 22 000 to 22 500 while a large one occurs at the outer wall.

The secondary cellular structures appear at values of the Rayleigh number that are all high since the radius ratio is small, and are representative of the upper limit of the steady motion (see Figure 2). Decreasing the radius ratio has a very strong stabilizing effect, with the promotion of the secondary cells thus becoming very difficult for narrow channel gaps because a consequence of the occurrence of the cells is always a decrease of the mass flow rate, as can be seen in Figure 8(a). There is a competition between the strengthening of cells due to the temperature gradient and the main circulation of the flow which contains and limits the cells' growth.

3.2. Oscillatory regime

Time history of the mass flow rate is presented in Figure 8(a) from Ra = 22 000 to 23 500 for a radius ratio, R = 1.6. At Ra = 22 000 and for this radius ratio, the circulation flow is unidirectional (see Figure 5(b)) while secondary cells are de-

Figure 7: Inner and outer walls temperature at the heater; R = 1.6, Pr = 5.

(a) Time history of the mass flow rate; Ra = 23 500, R = 1.6, and Pr = 5

(b) Limit cycle; Ra = 24 600, R = 1.6, and Pr = 5

Figure 8: Oscillatory regime.

(a) Time history; Ra = 47 000, R = 1.4, and Pr = 5

(b) Enlargement of the time history; Ra = 15 000, R = 2.0, and Pr = 5

Figure 9: Chaotically reversing regime.

picted at Ra = 22 500 (see Figure 6(b)). After initial damped oscillations, the mass flow rate abruptly decreases owing to the occurrence of secondary cells along the outer wall at the entrance of the exchangers. This is followed by small sustainable oscillations of the secondary cells on the spot, with their strength (and size) varying slightly. The flow structure behavior of the stream function field during the same time history as in Figure 8(a) is well documented in Video 1 (see Supplementary Material), clearly showing the occurrence of the secondary cells followed by small oscillatory motion of these cells. At $Ra = 23500$, the amplitude of the periodic motion is very small and the oscillations are very slowly damped (not distinguishable in Figure 8(a) or in Video 1 (see Supplementary Material)). This is why the limit cycle to which the system is attracted is drawn for Ra = 24 600. At this Rayleigh number, the limit cycle illustrated in Figure 8(b) indicates that the motion is (almost) periodic with one frequency, $f = 6.66 \pm 0.39$. The phase portrait of $[\nu(0.5, 0), \Theta(0.5, 0)]$ is for a sampling interval 0.0025 during a time interval 1 representing 6 cycles with almost 310 iterations per cycle.

3.3. Chaotically reversing motion

Figure 9(a) presenting a very long time history of the mass flow rate from Ra = 45 000 to 47 000 for R = 1.4 clearly shows the aperiodic behavior of the flow. The nondimensional time interval is equal to 35 which represents 70 000 iterations. It must be remarked that the flow reversal process is only reached after a long time equal to 15 during which the flow rate oscillates with increasing amplitude until it reverses direction, whereupon oscillations initiate in a new flow direction. This variable clearly indicates the changes of flow direction with its change of sign. These reversals in flow directions are also accompanied by large temperature

Figure 10: Equally spaced snapshots of the temperature; Ra = 15 000, $R = 2.0$, and Pr = 5.

variations which can be detrimental to the safety operation of heat removal systems. Figure 9(b) presents, for Ra = 15 000 and $R = 2.0$, an enlargement of the time history of the axial component of the velocity at the middle of the cooler $(0.5, \pi)$ during a short period of time which presents three oscillations followed by four reverse flows. Figures 10 and 11 give eight snapshots of the temperature and of the stream function fields equally time-spaced (0.048) in clockwise progression around the figure at the center during one reverse flow process. The time locations of these snapshots are also indicated as filled red squares in Figure 9(b). The flow direction is clearly distinguishable on the temperature fields, with the isotherms following the flow direction (see Figure 10). Relatively to the flow field, the recirculating regions at the entrance of the exchangers shift their location at each flow reversal occupying the first and third quadrants for counter-clockwise motion (see Figure 11 snapshots 8,1) and the second and fourth quadrants for clockwise motion (see Figure 11 snapshots 4,5). In the center of Figures 10 and 11, we also show the average temperature and stream function fields which are obtained by adding all the instantaneous fields at each time step during the two quasiperiodic flow evolutions showing reverse flows during the time $[35.672, 36.440]$ (see Figure 9(b)). The average fields are not exactly symmetrical with respect to the vertical centerplane because the flow evolution is not periodic. Videos 2 and 3

Figure 11: Equally spaced snapshots of the stream function; Ra = 15 000, R = 2.0, and Pr = 5.

(see Supplementary Material), respectively, present the behavior of the temperature and stream function fields during the same time history as in Figures 9(b) and usefully complement Figures 10 and 11. The first three oscillations of the temperature and stream function fields and the way in which the four reverse flow directions occur are clearly seen.

Several experiments on thermal convection in a closed loop have been made. Although they were conducted in toroidal [6, 13, 14] or rectangular [7–9, 15] loops and not in an annular loop, some qualitative comparisons can nevertheless be made. First, Stern et al. [14], using the same boundary conditions as in the present numerical study but for a toroidal loop, experimentally demonstrated the flow reversal in the heating section to be stronger than the reversal in the cooling section. Moreover, all these experiments [6–9, 14, 15] showed that after steady state, oscillatory motions occurred followed by reverse flows as in the present numerical experiment. Finally, some of them also experienced Lorenz-like behavior [6–9] as in Figure 12 where the recognizable shape of the Lorenz attractor can be seen. The attractor projected in this plane resembles a butterfly. This phase portrait has been built at Ra = 48 000 for R = 1.4 with only one time series (horizontal axis), that of the mass flow rate and using two time step lags of $\Delta\tau$ = 0.04 and 0.06 for the other axis and during a total dimensionless time equal to 10. Only one iterative point (in red) in ten

FIGURE 12: Lorenz-like attractor; Ra = 48 000, $R = 1.4$, and Pr = 5.

is used to draw the Lorenz attractor. In Figure 12, there are only two excursions of the flow around the positive wing (the motion thus being in the counter-clockwise direction) while most of them swirl over the negative wing (the motion being in the clockwise direction). No better description of the phenomenon can be given than the following: "As the Lorenz attractor is plotted, a strand will be drawn from one point, and will start weaving the outline of the right butterfly wing. Then it swirls over to the left wing and draws its center. The attractor will continue weaving back and forth between the two wings, its motion seemingly random, its very action mirroring the chaos which drives the process." (http://zeuscat.com/andrew/chaos/lorenz.html). For those who wish to observe real-time Lorenz attractor movies, many examples can easily be found on the Web.

4. CONCLUSION

The behavior of a natural circulation 2D-annular loop that is heated uniformly over the lower half and cooled by maintaining a constant wall temperature over the upper half has been numerically investigated. Detailed numerical results for the flow in the thermosyphon are presented for various radius ratios and all these findings have been demonstrated by brute force integration of the complete Navier-Stokes equations.

It has been numerically demonstrated that the complexity of the dynamic properties experimentally encountered in toroidal and rectangular loops is also found here as follows:

(i) pseudoconductive regime for which heat is mainly transferred by conduction from the heater to the cooler;

(ii) steady flow showing a global motion all around the loop with heat being convected by the fluid from the heater to the cooler. The motion can be clockwise or anticlockwise according to chance due to a Pitchfork bifurcation;

(iii) steady flow with recirculating regions showing a slowing down of the mass flow rate due to the occurrence of a vortex at the entrance of the heater reducing the "*vena contracta*;"

(iv) periodic motion during which the cells oscillate on the spot with a constant amplitude of very small magnitude;

(v) Lorenz-like chaotic flow. The flow rate oscillates with increasing amplitude until it eventually reverses direction, whereupon oscillations initiate in a new flow direction.

This chaotic regime appears to be reached through a Ruelle-Takens scenario involving a sequence of Hopf bifurcations: from a fixed point to a periodic orbit, hence it is likely that the system possesses a strange attractor.

NOMENCLATURE

a: Gap width ($a = r_o^* - r_i^*$), m
g: Acceleration due to gravity, m/s^2
Pr: Prandtl number (Pr $= \nu/\alpha$)
q'': Specific heat flux at the heater, W/m^2
Q: Dimensionless mass flow rate
r: Dimensionless radial coordinate ($(r^* - r_i^*)/a$)
r_i^*: Inner cylinder radius, m
r_o^*: Outer cylinder radius, m
R: Radius ratio (r_o^*/r_i^*)
Ra: Rayleigh number (Ra $= g\beta a^3 \Delta T/\alpha\nu$)
T: Dimensional temperature, K
T_c: Dimensional cold temperature, K
u, v: Dimensionless radial and axial velocity

GREEK

α: Thermal diffusivity of the fluid, m^2/s
β: Coefficient of thermal expansion of the fluid, K^{-1}
κ: Thermal conductivity of the fluid, W/(mK)
ν: Kinematic viscosity of the fluid, m^2/s
τ: Dimensionless time ($\tau = \alpha t^*/a^2$)
θ: Angular coordinate measured from downward vertical
Θ: Dimensionless temperature ($\Theta = (T - T_c)/\Delta T$)
ΔT: Temperature difference ($\Delta T = aq''/\kappa$), K

SUPERSCRIPT

$*$: Variable with dimension

SUBSCRIPTS

c: Cold exchanger
h: Hot exchanger
i: Inner cylinder
o: Outer cylinder
w: At the walls (r_i^* and r_o^*)

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Research Article

SBWR Model for Steady-State and Transient Analysis

Gilberto Espinosa-Paredes[1] and Alejandro Nuñez-Carrera[2]

[1] *Area de Ingeniería en Recursos Energéticos, Universidad Autónoma Metropolitana-Iztapalapa, Avenida San Rafael Atlixco 186, Colonia Vicentina, 09340 México DF, Mexico*
[2] *Comisión Nacional de Seguridad Nuclear y Salvaguardias, Avenida Doctor Barragán 779, Colonia Vicentina, 03020 México DF, Mexico*

Correspondence should be addressed to Gilberto Espinosa-Paredes, gepe@xanum.uam.mx

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This paper presents a model of a simplified boiling water reactor (SBWR) to analyze the steady-state and transient behavior. The SBWR model is based on approximations of lumped and distributed parameters to consider neutronics and natural circulation processes. The main components of the model are vessel dome, downcomer, lower plenum, core (channel and fuel), upper plenum, pressure, and level controls. Further consideration of the model is the natural circulation path in the internal circuit of the reactor, which governs the safety performance of the SBWR. To demonstrate the applicability of the model, the predictions were compared with plant data, manufacturer_s predictions, and RELAP5 under steady-state and transient conditions of a typical BWR. In steady-state conditions, the profiles of the main variables of the SBWR core such as superficial velocity, void fraction, temperatures, and convective heat transfer coefficient are presented and analyzed. The transient behavior of SBWR was analyzed during the closure of all main steam line isolation valves (MSIVs). Our results in this transient show that the cooling system due to natural circulation in the SBWR is around 70% of the rated core flow. According to the results shown here, one of the main conclusions of this work is that the simplified model could be very helpful in the licensing process.

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1. INTRODUCTION

Recently, a joint project among three Agencies: the International Energy Agency (IEA), the OECD Nuclear Energy Agency (NEA), and the International Atomic Energy Agency (IAEA) prepared the report "Innovative nuclear reactor development, opportunities for International cooperation" [1], whose main objective is the identification of opportunities to establish joint projects in developing nuclear fission reactor technologies. This study remarks that the following areas are good candidates for developing new, broad based collaborative efforts:

(i) natural circulation;
(ii) high-temperature materials;
(iii) passive (safety) devices;
(iv) in-service inspection and maintenancemethods;
(v) advanced monitoring and control technologies;
(vi) delivery and construction methods;
(vii) safeguard technologies and approaches.

This paper focuses on the attention in natural circulation which is one of the good candidates for developing new reactor technologies. Natural convection circulation of coolant in the reactor cooling system is used to some extent in all reactor concepts, and many of these uses are innovative [2]. What is now needed is to develop or confirm the design basis and to develop and qualify computer codes to enable reliable analysis.

All the ordinary water-cooled and heavy water-cooled reactor designs rely on natural convection of the coolant to remove decay heat from the fuel after shutdown, even today's operating water reactors also rely on natural convection to remove decay heat when forced circulation is lost.

Among the new design with natural circulation, we can mention the CAREM-25 [3] and NHR-200 [4], both use

natural convection circulation of the ordinary water coolant at all power levels to remove heat from the reactor core. The energy amplifier and BREST 300 [5] use natural circulation of the lead coolant to remove decay heat from the core after shutdown, requiring the design of a reliable system to accommodate the potential solidification of the lead in regions outside the core. The energy amplifier also uses natural circulation of the coolant to remove heat produced by fission from the reactor core at all power levels; it is important to remark that this has not been done before. The FUJI [2] uses natural circulation of the molten salt reactor coolant to remove decay heat from the core after shutdown. Therefore, the ability to predict the performance of systems operating in natural convection circulation mode has fundamental importance.

One of the main design concepts is presently proposed by general electric (GE): the simplified boiling water reactor (SBWR) [6] and the economic and simplified boiling water reactor (ESBWR) [7]. According to the manufacturer, these new design reactors working with natural circulation provide simplification over previous BWRs. The improvements are accomplished by the removal of the recirculation pumps and associated motors, piping, valves, heat exchanges, control, and electrical support systems that exist with forced recirculation, eliminating flow disturbance by abnormal pump behavior. The main differences between natural and forced circulations are the additions of an exhaust pipe above the reactor core to stabilize and direct the steam and water flow above the core, and the correspondingly open and downcomer annulus that reduces the flow resistance and provides additional driving head pushing the water to the bottom of the core. Among the benefits of these designs are simplification of the operation, higher plant availability, 20% reduction in the cost of operation and maintenance, and the reduction of personnel dose by the elimination of recirculation pumps maintenance.

Simplified and convenient models are used in computer codes to perform analysis in early stages of the design of experimental reactor facilities and Generation IV reactors. The work of Tian et al. [8] showed a simplified neutronic and thermalhydraulic model used in the China Advanced Research Reactor (CARR) and its application with a simulation of loss of power accident without emergency cooling system. Other application is the engineering safety analysis for regulatory licensing process of light water reactors (LWR) that were performed in the past using simplified one-dimensional models of thermalhydraulics systems with the appropriate initial and boundary conditions for transient analysis in two-phase flow phenomena due that it can provide overestimated results.

This paper presents an SBWR model for transient analysis, where the thermalhydraulic model that describes the dynamic behavior of the lower and upper plena, and the reactor core, as well as fuel temperature model were based on distributed parameter approximations. The vessel dome, downcomers, recirculation loops, and neutron process models were based on lumped parameter approximations.

A multinode fuel pin model is developed to describe the heat transfer process. Three regions were identified as indispensable to be considered in the heat transfer analysis: the first region corresponds to heat transfer in the fuel; the second region corresponds to heat transfer in the gap; and the third region corresponds to heat transfer in the clad, whose temperatures are determined by the rate of heat convection due to core flow.

Figure 1: SBWR configuration and flow paths.

Reactor power is calculated from a point reactor kinetics model with six groups of delayed neutrons. The reactivity due to Doppler effect, void fraction, moderator temperature, and control rod drive was considered in this model.

This paper is organized as follows. The SBWR system description is presented in Section 2; mathematical models are described in Section 3; and Section 4 shows the results and discussions. The qualification assessment is presented in the appendix.

2. SYSTEM DESCRIPTION

The SBWR configuration and the flow paths are illustrated in Figure 1. The reactor natural circulation loop circulates the required coolant flow through the reactor core. The flow within the reactor vessel provides a continuous internal natural circulation path for a major portion of the core coolant flow. The core flow is taken from the vessel and discharged into the lower core plenum. The coolant water passes along the individual fuel rods inside the fuel channel where it boils and becomes a two-phase steam/water mixture. In the core, the two-phase fluid generates upward flow through the axial steam separators, while the steam continues through the dryers and flows directly out through the steam lines into the turbine generator. The condensate flow is then returned through the feed water heaters by the condensate feed water pumps into the vessel. Finally, the water, which is separated from the steam in the steam separators, flows

Table 1: Nominal parameters for SBWR [9].

Parameter	Units
Thermal power	1800 MWth
Total inlet core flow	6666 kg/sec
Pressure at the dome of vessel	7.07 MPa
Total steam flow	1100 kg/sec
Feed water temperature	488 K
Active fuel length	2.74 m
Pressure vessel internal height	22 m

Figure 2: Schematic diagram of the SBWR.

downward in the periphery of the reactor vessel and mixes with the incoming main feed flow from the turbine.

Table 1 shows the nominal values used in the simulation, which correspond to design characteristics of the SBWR [9].

3. MATHEMATICAL MODEL

Figure 2 is a schematic diagram of the SBWR where the arrangement of the computational cells of the SBWR model is shown. The reactor vessel was divided into five zones. Two of these zones, the vessel dome and the downcomer, have variable volume according to the vessel water level. The three fixed volume zones are the lower plenum, the upper plenum and steam separators, and the reactor core. Due to its importance on the model performance, the latter was subdivided into twelve one-dimensional nodes. The reactor model is completed by including natural recirculation loop as coolant, neutron kinetics, fuel rod temperature, and control models. In addition, this model uses a set of empirical correlations valid for the normal range of SBWR operating conditions. The model derivation is based in lumped and distributed parameters approximation; Table 2 presents the submodels and approximations used.

Table 2: SBWR submodels and approximations.

Mode*l	Approximation
Vessel dome and downcomer	Lumped parameters
Natural circulation loop	Lumped parameters
Neutron kinetics	Lumped parameters
Fuel rod	Distributed parameters
Lower plenum, upper plenum, and core	Distributed parameters

* The whole system includes pressure control and level control.

The following assumptions were adopted in the development of the SBWR model:

(i) the point-kinetic equations are used to describe the evolution of neutron population and precursor concentration;

(ii) delayed neutrons are modeled using six groups;

(iii) fuel rod is described by a distributed parameters model, which includes fuel, gap, and cladding. This model is crucial due that velocity of heat transfer from fuel to the coolant has important impact during transient behavior;

(iv) the multiple parallel channels in SBWR are lumped into one average channel. The drift-flux model is used to consider velocity difference between liquid and vapor phases;

(v) the axial power profile is modeled as a superposition of a profile space-dependent and a space-independent term, that is, time-dependent;

(vi) the downcomer section is assumed to be filled with single-phase fluid. The thermohydraulics system itself consists of five different sections: lower plenum, core, upper plenum, stem separators, and downcomer. The heated section is the core, and the others are unheated sections;

(vii) the gas phase is saturated at the vessel pressure.

3.1. *Thermalhydraulic model*

The thermalhydraulic model consists of five balance equations: (1) liquid-and gas-phase mass balances, (2) mixture momentum, (3) mixture energy, (4) liquid-phase energy, together with a drift flux approach [10], for the analysis of phase separation. The nonequilibrium two-phase flow for the volumetric vapor generation rate in subcooled boiling was considered using Saha and Zuber approximation [11, 12].

Nonequilibrium two-phase flow conditions are present in all two-phase flows, and the effects of nonequilibrium states can be important at steady-state and off-normal conditions encountered in operational transients. In boiling water reactor, for example, vapor voids are formed in the core when the bulk liquid is below the saturation state; this phenomena is known as subcooled boiling. These voids have a very strong effect relative to the power distribution in the

core both at quasi-steady conditions and during operational transient.

The thermalhydraulic model is used to describe the dynamic behavior of the lower and upper plena, downcomer, and the reactor core. The five-equation model was based on the following assumptions: (i) one-dimensional flow, (ii) no cross flow, and (iii) kinetic and potential energy effects are neglected in the energy equation.

The model is based on liquid- (denoted by subscript l) and gas- (denoted by subscript g) phase mass and energy balances, and a mixture momentum balance (denoted by subscript m):

$$\begin{aligned} \frac{\partial}{\partial t}(\rho_g \varepsilon_g) + \frac{\partial}{\partial z}(j_g \rho_g) &= \Gamma, \\ \frac{\partial}{\partial t}(\rho_l \varepsilon_l) + \frac{\partial}{\partial z}(j_l \rho_l) &= -\Gamma, \end{aligned} \tag{1}$$

$$\frac{\partial}{\partial t}(\rho_l h_l \varepsilon_l) - \varepsilon_l \frac{\partial p}{\partial t} + \frac{\partial}{\partial z}(\rho_l h_l j_l) = \frac{q_l'' P_H}{A_{x-s}} + q_l''' \varepsilon_l - \Gamma h_f, \tag{2}$$

$$\frac{\partial}{\partial t}(\rho_m h_m) - \frac{\partial p}{\partial t} + G_m \frac{\partial h_m}{\partial z} = \frac{q'' P_H}{A_{x-s}} + q''', \tag{3}$$

$$\frac{\partial G_m}{\partial t} + \frac{\partial}{\partial z}\left(\frac{G_m^2}{\rho_m}\right) = -\frac{\partial p}{\partial z} - \frac{f\, G_m^2}{2\rho_m D_h} - g\rho_m, \tag{4}$$

where

$$\rho_m = \rho_g \varepsilon_g + \rho_l \varepsilon_l, \tag{5}$$

$$\rho_m h_m = \rho_g h_g \varepsilon_g + \rho_l h_l \varepsilon_l, \tag{6}$$

$$G_m = \rho_g j_g + \rho_l j_l, \tag{7}$$

$$\varepsilon_g + \varepsilon_l = 1. \tag{8}$$

In these equations, ρ is density, ε_g is the void fraction; j is superficial velocity; Γ is the volumetric vapor generation rate; h is the enthalpy; p is the pressure; q'' is the heat flux; P_H is the heated perimeter; A_{x-s} is the cross-sectional area; f is the friction factor; D_h is the hydraulic diameter; and q''' is the volumetric heat generation by gamma radiation. For unheated sections (e.g., downcomer, lower and upper plenums) $q'' = 0$.

The equations of state are usually expressed in terms of two independent variables in the form $\rho_l = \rho_l(p, h_l)$ for density and $T_l = T_l(p, h_l)$ for temperature. If the fluid is at the saturation state, the state properties are functions of the pressure only, and the equations of state can be written as $\rho_k = \rho_k(p)$ to density and $T_k = T_k(p)$ where $k = l$ or g. For the gas phase and in all cases $h_g = h_g(p)$.

Depending on the mixture enthalpy h_m, the following two cases were considered.

Case 1. If $h_f(p) < h_m < h_g(p)$, the gas and liquid phases are saturated at reactor pressure and $h_l = h_f(p)$.

Case 2. If $h_m < h_f$ and $h_l < h_f$, liquid phase occurs first and, subsequently, subcooled boiling appears.

Here, h_f and h_g are the enthalpy of liquid and vapor saturated at the vessel pressure, respectively.

For bulk boiling condition, both the gas and liquid phases are in saturated condition (thermodynamic equilibrium condition) at the vessel pressure, then if $h_l = h_f(p)$; $\rho_l = \rho_f(p)$ in (3) and combining it with (1), the volumetric vapor generation rate Γ is given by:

$$\Gamma = \frac{1}{h_{fg}}\left[\frac{q'' P_H}{A_{x-s}} + q''' + \left(1 - \rho_g \varepsilon_g \frac{\partial h_g}{\partial p} - \rho_f \varepsilon_l \frac{\partial h_f}{\partial p}\right)\frac{\partial p}{\partial t} - \rho_f j_l \frac{\partial h_f}{\partial z}\right]. \tag{9}$$

In these equations, $\partial h_g/\partial z = 0$, due that the gas phase is saturated at the vessel pressure, h_{fg} is the difference in specific enthalpy of saturated vapor and liquid. For subcooled boiling, the volumetric vapor generation is given by [11]:

$$\begin{aligned} \Gamma = \frac{1}{h_{fg}}\Bigg[&\frac{q'' P_H}{A_{x-s}}\left(1 - \frac{h_f - h_l}{h_f - h_{ld}}\right) + q''' - H_o \frac{h_{fg}}{\nu_{fg}} \varepsilon_g (T_s - T_l) \\ &+ \varepsilon_g \left(1 - \rho_g \frac{\partial h_g}{\partial p}\right)\frac{\partial p}{\partial t}\Bigg], \end{aligned} \tag{10}$$

where H_o is the condensation parameter, and the liquid subcooling $(h_f - h_{ld})$ at the initiation of subcooled boiling is given by [12]

$$h_f - h_{ld} = \begin{cases} 0.0022 q'' D_h Cp_l / k_l, & \text{Pe} < 70{,}000, \\ 154 q''/G, & \text{Pe} > 70{,}000. \end{cases} \tag{11}$$

Here, k_l is the liquid thermal conductivity; Cp_l is the liquid specific heat; and T_s is the saturation temperature.

The mass flow rate of each phase is, respectively, given by

$$\begin{aligned} W_g &= j_g A_{x-s} \rho_g, \\ W_l &= j_l A_{x-s} \rho_l, \end{aligned} \tag{12}$$

where $j_l = j - j_g$. In order to calculate the amount of steam and liquid from mixture properties, the drift-flux model was used [10]. This model provides an algebraic relationship between the relative velocities of the flowing liquid and steam and the void fraction. The basic equation of the drift flux model is as follows:

$$j_g = (C_o j + \nu_{gj})\varepsilon_g. \tag{13}$$

The distribution parameter C_o and the average drift velocity ν_{gj} are represented by a correlation for each of the two-phase flow regimes, bubbly and slug in the present case, in vertical pipes. The superficial velocity is calculated by:

$$\begin{aligned} j = &\left[\left(\frac{1}{\rho_g} - \frac{1}{\rho_l}\right)\Gamma - \left(\frac{\varepsilon_g}{\rho_g}\frac{\partial \rho_g}{\partial p} + \frac{\varepsilon_l}{\rho_l}\frac{\partial \rho_l}{\partial p}\right)\frac{\partial p}{\partial t} - \xi_{\text{sub}}\right]\Delta z \\ &+ \left(\frac{W_{\text{gin}}}{\rho_g} + \frac{W_{\text{lin}}}{\rho_l}\right)\frac{1}{A_{x-s}}, \end{aligned} \tag{14}$$

Table 3: Thermal hydraulics parameters.

Parameter	Value
A_{x-s}	4.024 m^{2*}
D_h	0.0152 m
C_0	1.2–0.2 $(\rho_g/\rho_l)^{0.5}$; $\varepsilon_g < 0.65$
H_0	0.24 (s· ^{o}K)$^{-1}$
P_H	849.57 m
v_{gj}	$1.53g^{0.2}[\sigma g(\rho_l - \rho_g)/\rho_l^2]^{0.2}$; $\varepsilon_g < 0.65$

* 516 assemblies.

where

$$\xi_{\text{sub}} = \begin{cases} \dfrac{\varepsilon_l}{\rho_l}\left(\dfrac{\partial \rho_l}{\partial h_l}\right)\dfrac{\partial h_l}{\partial t}, & \text{Subcooled boiling,} \\ 0, & \text{Thermodynamic equilibrium.} \end{cases} \tag{15}$$

Here, Δz is the cell length in axial direction; W_{gin} and W_{lin} are the inlet mass flow rate of the gas and liquid phases, respectively.

Equations (1)–(15) represent the thermalhydraulic model, which consider nonequilibrium two-phase flow effects and velocities difference between liquid and vapor phases. The main advantage of this model is its simplicity requiring only and few constitutive equations. In fact, only well-known, wall-to-mixture heat transfer and friction constitutive relationships are required. The term $\partial p/\partial t$ in the equations of the thermalhydraulic model corresponds to vessel dome, which is presented in Section 3.5 The thermalhydraulic parameters are presented in Table 3.

For time discretization, an explicit Euler method was used, with a fixed time step of 0.01 second to solve the equations set of the thermalhydraulic models. This time step is highly recommended for transient with fast change in vessel pressure. Regarding the spatial discretization, the scheme used is known as a first-order, upwind finite difference scheme with a truncation error of order Δz.

3.2. Reactor power model

The reactor power is given by

$$P(t,z) = n(t)F(z)P_0, \tag{16}$$

where $F(z)$ is the axial power factor (which is given by Figure 3); P_0 is nominal power; and $n(t)$ is the normalized neutron flux, which is calculated from a point reactor kinetics model with six groups of delayed neutrons:

$$\frac{dn(t)}{dt} = \frac{\rho(t) - \beta}{\Lambda} n(t) + \sum_{i=1}^{6} \lambda_i c_i(t), \tag{17}$$

$$\frac{dc_i(t)}{dt} = \frac{\beta_i}{\Lambda} n(t) + \lambda_i c_i(t), \quad i = 1, 2, \ldots, 6, \tag{18}$$

where c_i is a delayed neutron concentration of the ith precursor group normalized with the steady-state neutron density; ρ is the net reactivity; β is the neutron delay fraction;

Figure 3: Axial power distribution.

Table 4: Neutron delay fraction and decay constant [13].

	β_i	λ_i
Group 1	2.470×10^{-4}	0.0127 s^{-1}
Group 2	1.355×10^{-3}	0.0317 s^{-1}
Group 3	1.222×10^{-3}	0.1150 s^{-1}
Group 4	2.646×10^{-3}	0.3110 s^{-1}
Group 5	8.320×10^{-4}	1.4000 s^{-1}
Group 6	1.690×10^{-4}	3.8700 s^{-1}
β	6.5×10^{-3}	
Λ	4.0×10^{-5} s^{-1}	

Λ is the neutron generation time; and β_i is the portion of neutrons generated by the ith group. The initial conditions are given by $n(0) = n_0$ and $c_i(0) = \beta_i n_0/\Lambda\lambda_i$. The parameters of the kinetics model are presented in Table 4.

The net reactivity of the nuclear reactor includes four main components: feedback reactivity due to the void fraction in the two-phase flow ρ_v, Doppler effect ρ_D due to fuel temperature, moderator temperature ρ_m, and reactor control rods ρ_{cr}. Therefore, we express the feedback reactivities as:

$$\rho(t) = \rho_v(t) + \rho_D(t) + \rho_m(t) + \rho_{\text{cr}}(t). \tag{19}$$

The kinetics point equations are stiff in the coefficients because they differ in several orders of magnitude, especially in (17). The variable implicit integration method [14] was used to solve (17), and the Euler method in an explicit form was used to solve the delayed precursor concentration given by (18). The reactivity correlations are presented in Table 5.

3.3. Fuel model

The fuel assembly temperature distribution was obtained considering each radial node at each of the twelve hydraulic axial nodes in the core. Two radial nodes are considered for the clad and the gap, two more nodes for the boundary condition evaluation, and four nodes are considered for one equivalent fuel element, as illustrated in Figure 4.

Table 5: Reactivity correlations.

Reactivity (pcm)
Void fractions reactivity
$\rho_v(\langle\varepsilon_g\rangle) = -4.0925 - 41.036\langle\varepsilon_g\rangle + 30.8223\langle\varepsilon_g\rangle^2.$
Doppler effect
$\rho_D = 1.3 - 0.005\langle T_f\rangle + 2.0\times10^{-6}\langle T_f\rangle^2 + -3.0\times10^{-11}\langle T_f\rangle^3 - 1.1\times10^{-3}\langle\varepsilon_g\rangle(\langle T_f\rangle - 325.0).$
Moderator temperature reactivity
$\rho_m = 1.656 - 1.035\times10^{-2}\langle T_m\rangle + 4.0\times10^{-5}\langle T_m\rangle^2 - 7.33\times10^{-8}\langle T_m{}^3\rangle.$
Control rod drive reactivity
$\rho_{\mathrm{cr}} = -11.235 + \dfrac{57.73(1-\mathrm{CRD})}{1+0.937(1-\mathrm{CRD})}.$

A detailed multinode fuel pin model is developed for this study. The fuel heat transfer formulation is based on the following fundamental assumptions: (i) axis-symmetric radial heat transfer; (ii) the heat conduction in the axial direction is negligible in respect to the heat conduction in the radial direction; (iii) the volumetric heat rate generation in the fuel is uniform in each radial node; and (iv) storage of heat in the fuel cladding and gap is negligible.

Under these assumptions, the transient temperature distribution in the fuel pin, initial, and boundary conditions are given by:

$$(\rho Cp)\frac{\partial T}{\partial t} = k\frac{1}{r}\frac{\partial}{\partial r}\left(r\frac{\partial T}{\partial r}\right) + q'''(t), \quad \text{at } r_0 \le r \le r_6, \tag{20}$$

$$\text{I.C.} \quad T(r,0) = T(r), \quad \text{at } t = 0, \tag{21}$$

$$\text{B.C.1} \quad -k\frac{\partial T}{\partial r} = H_\infty(T - T_m), \quad \text{at } r = r_6, \tag{22}$$

$$\text{B.C.2} \quad \frac{\partial T}{\partial r} = 0, \quad \text{at } r = r_0. \tag{23}$$

In these equations, r is the cylindrical radial coordinate; r_0 and r_6 are defined in Figure 4, $q'''(t) = P(t)/V_f$ at each axial node, where P is given by (16); T_m is the moderator temperature; and H_∞ is the convective heat transfer coefficient, which is presented in Section 3.4.

In the present solutions, the fuel element is represented by a one-dimensional, mesh-centred grid consisting of a variable number of radial elements at each axial position. The differential equations described previously are transformed into discrete equations using the control volume formulation technique in an implicit form [15].

Application of the control volume formulation enables the equations for each region (fuel, gap, and clad) to be written as a single set of algebraic equations for the sweep in the radial direction:

$$a_j T_{j-1}^{t+\Delta t} + b_j T_j^{t+\Delta t} + c_j T_{i,j+1}^{t+\Delta t} = d_j, \tag{24}$$

where $T_{j-1}^{t+\Delta t}$, $T_j^{t+\Delta t}$, and $T_{j+1}^{t+\Delta t}$ are unknowns; a_j, b_j, c_j, and d_j are coefficients, which are computed at the time t.

Figure 4: Arrangement of the computational cells of gap, clad, and fuel.

When these equations are put into a matrix form, the coefficient matrix is tridiagonal. The solution procedure for the tridiagonal system is the Thomas algorithm [15], which is the most efficient algorithm for this type of matrices. The coefficients a_j, b_j, c_j are dependents of thermophysical properties, that is, thermal conductivity, density, and specific heat, and since they are at function of $T_j^{t+\Delta t}$, at least one iteration is needed.

The interaction of the model with the neutron kinetics model is through evaluation of the average fuel temperature $\langle T_f\rangle$ and the volumetric heat rate generation q'''. The interaction with the nuclear thermalhydraulics is through evaluation of the coolant temperature and heat transfer coefficient. The average fuel temperature is given by:

$$\begin{aligned}\langle T_f\rangle &= \frac{1}{V_f}\int_{V_f} T_f\,dV \\ &\simeq \frac{1}{V_f}\sum_{i=1}^{n}\left(V_2T_{i,2} + V_3T_{i,3} + V_4T_{i,4} + V_5T_{i,5}\right),\end{aligned} \tag{25}$$

where $V_f = V_2 + V_3 + V_4 + V_5$ is the fuel volume. Here 2, 3, 4, and 5 represent the radial node according to Figure 4, while that i represents the axial index of the node. Fuel parameters are shown in Figure 4 and Table 6.

3.4. Convective heat transfer coefficients

The convective heat transfer coefficient for the single-phase liquid and vapor ($H_{1\phi}$) is obtained from the classical Dittus-Boelter correlation [16]. The heat transfer coefficient in both subcooled boiling and nucleate boiling regimes ($H_{2\phi}$) is calculated with the Chen correlation [17]. The convective

Table 6: Fuel model parameters [14].

Fuel
$K_f = \dfrac{3825.02}{\langle T_f\rangle + 129.41} + 6.08\times 10^{-11}\langle T_f\rangle^3$, J/m·s,
$(\rho Cp)_f = \dfrac{8.5103\times 10^{11} e^{535.28/\langle T_f\rangle}}{\langle T_f\rangle (e^{535.28/\langle T_f\rangle}-1)^2} + 2.4348\times 10^2\langle T_f\rangle + \dfrac{1.6609\times 10^{16} e^{-1.8970x10^4/\langle T_f\rangle}}{\langle T_f\rangle^2}$, J/m^3·K,
$V_f = 8.9335\,\mathrm{m}^3$.
Clad
$K_{\mathrm{cl}} = 7.51 - 0.0209T_{\mathrm{cl}} - 1.45\times 10^{-5}T_{\mathrm{cl}}^2 + 7.67\times 10^{-9}T_{\mathrm{cl}}^3$, J/m·s,
$(\rho Cp)_{\mathrm{cl}} = 1.8204\times 10^6 + 3.0386\times 10^5\theta_{\mathrm{cl}} - 1.0637\times 10^5\theta_{\mathrm{cl}}^2 + 2.8102\times 10^4\theta_{\mathrm{cl}}^3 + -2723.6\theta_{\mathrm{cl}}^4$, J/m^3·K,
$\theta_{\mathrm{cl}} = \dfrac{T_{\mathrm{cl}} - 300}{200}$.
Gap
$K_{\mathrm{ga}} = \dfrac{H_{\mathrm{gap}}}{\delta_{\mathrm{gap}}} = \dfrac{2936.8}{1.14\times 10^{-4}}$, J/m·s.

heat transfer coefficient (H_∞) is given by the maximum between $H_{1\phi}$ and $H_{2\phi}$ to avoid discontinuities, that is, $H_\infty = \mathrm{MAX}(H_{1\phi}, H_{2\phi})$.

The interaction of the convective heat transfer coefficients with the fuel heat transfer model is through the evaluation of the clad temperature $T|_{r=r_6}$ (Figure 4). The convective heat transfer coefficient H_∞ is used in the boundary condition given by (22).

3.5. Vessel dome model

The vessel dome is modeled as a two-region volume, one region being liquid and the other vapor. The two regions are assumed to be at the same pressure but not necessarily at the same temperature. The dynamic model used to obtain the pressure in the vessel dome is based on macroscopic balances of mass and energy:

$$\frac{dp}{dt} = -\frac{\nu_l\sum_j W_{jl} + (\partial\nu_l/\partial h_l)_p\sum_j W_{jl}\,(h_{jl}-h_l)}{Q+Q'} + \frac{\nu_v\sum_j W_{jv} + (\partial\nu_v/\partial h_v)_p\sum_j W_{jv}(h_{jv}-h_v)}{Q+Q'}, \tag{26}$$

where Q denotes $m_l[\nu_l(\partial\nu_l/\partial h_l)_p + (\partial\nu_l/\partial p)_{h_l}]$ and Q' denotes $m_v[\nu_v(\partial\nu_v/\partial h_v)_p + (\partial\nu_v/\partial p)_{h_v}]$, and W_{jl} and W_{jv} are the mass flow rates into or out of the liquid and vapor regions, respectively, and h_{jl} and h_{jv} are the enthalpy of the fluid entering or leaving the liquid and vapor regions, respectively. The mass of liquid m_l, mass of vapor m_v, enthalpy of liquid h_l, and enthalpy of vapor h_v are given by the following balance equations:

$$\frac{dm_l}{dt} = \sum_{jl} W_{jl}, \qquad \frac{dm_v}{dt} = \sum_{jv} W_{jv}, \tag{27}$$

$$\frac{dh_l}{dt} = \frac{1}{m_l}\sum_{jl} W_{jl}(h_{jl}-h_l) + \nu_l\frac{dp}{dt}, \qquad \frac{dh_v}{dt} = \frac{1}{m_v}\sum_{jv} W_{jv}(h_{jv}-h_v) + \nu_v\frac{dp}{dt}. \tag{28}$$

The flashing and condensation flows are given by:

$$W_f = \frac{m_l}{\Delta t}\left(\frac{h_l-h_f}{h_g-h_f}\right), \qquad W_{\mathrm{co}} = \frac{m_v}{\Delta t}\left(\frac{h_g-h_v}{h_g-h_f}\right). \tag{29}$$

Here Δt is the time step. The density and temperature in the downcomer is given by:

$$\rho_{\mathrm{dw}} = \rho_{\mathrm{dw}}(p, h_l), \tag{30}$$

$$T_{\mathrm{dw}} = T_{\mathrm{dw}}(p, h_l). \tag{31}$$

The level in the vessel is calculated as a function of liquid volume, that is, $N_l = N_l(V_l)$, where the liquid volume is given by $V_l = m_l\nu_l$.

An Euler method in an explicit form with a time step of 0.01 second was used to solve the ordinary differential equations of the vessel dome model.

3.6. Natural circulation loop

A natural circulation loop of the SBWR was considered in this analysis, as shown in Figure 1. Balancing the gravity head available and total loop pressure drop obtained by integration of the momentum balance leads to the model for the circulation natural. The circulation natural model includes the pressure drops and flows from the downcomer,

lower and upper plenum, reactor core, and steam separators, in order to obtain the following momentum balance:

$$\frac{dW_c}{dt} = \left(\sum_{i=1}^{n} \frac{l_i}{A_i}\right)^{-1} \left(-K_{\text{psn}} \frac{W_c^2}{\rho_{\text{dw}}} - K_{\text{sep}} \frac{W_{\text{sep}}^2}{\rho_{\text{sep}}} - \Delta p_c + \Delta p_g \right), \tag{32}$$

where $(\sum l/A)$ is the inertial term; ρ_{dw} is the downcomer density (30); ρ_{sep} is the steam-separator density; W_{sep} is the flow mass through the steam separator; K_{psn} is the support core plate loss coefficient; K_{sep} is the separator loss coefficient; Δp_c is the core pressure drop; and Δp_g is the pressure drop due to gravity.

The total core pressure drop is the sum of the frictional, acceleration, and gravitational components:

$$-\Delta p_c = \sum_{j=1}^{12} \left[\frac{\phi_{f0,j}^2 2C_{f0} W_c^2 \Delta z}{A_{x-s}^2 D_h \rho_f} + \frac{W_c^2}{A_{x-s}^2} \left(\frac{1}{\rho_{c,j+1}} - \frac{1}{\rho_{c,j}} \right) + \rho_{c,j} g \Delta z \right], \tag{33}$$

where $j = 1, 2, \ldots, 12$ are used to indicate the core node number; C_{f0} is the single-phase friction factor; Δz_j is the core node length; D_h is the hydraulic diameter; ρ_f is the density of saturated liquid; ρ_c is the core density in each node, which is given by (5); and $\phi_{f0,j}^2$ is the two-phase multiplier given by [18]:

$$\phi_{f0}^2 = 1 + x\left(\frac{\rho_f}{\rho_g} - 1\right), \tag{34}$$

where x is the vapor quality and ρ_g is the density of the saturated vapor.

The pressure drop due to gravity without core effects, which are considered in (32), is given by:

$$\Delta p_g = g N_l(V_l)\rho_{\text{dw}} - g(L_{\text{ps}} + L_{\text{sep}})\rho_{\text{sep}}, \tag{35}$$

where N_l, L_{ps}, and L_{sep} are the liquid level in the vessel, the length in the upper plenum and the length in steam separators. The parameters of this model are presented in Table 7.

The Euler method in an explicit form with a time step of 0.01 second was used to solve the ordinary differential equations (ODE) of the recirculation system model.

3.7. Control models to model the control systems of SBWR

The control models obtained considering the feed water system (FW) and the main steam line (MSL) are dummy models or auxiliary models. The interface with the process at the other end of the control loop is made by the final control element. In a vast majority of nuclear engineering processes, the final control element is an automatic control valve which throttles the flow of a manipulated variable [19].

The MSL mass flow can be calculated as [19]:

$$W_{\text{ms}} = C_{v\text{ms}} f(A_{\text{ms}}^*) \Delta p_{\text{ms}} \sqrt{\frac{\rho_v}{|\Delta p_{\text{ms}}|}}, \tag{36}$$

Table 7: Circulation natural parameters.

Parameter	Value
$(\sum l/A)$	$9\,\text{m}^{-1}$
K_{psn}	$1.3\,\text{m}^{-4}$
K_{sep}	0.01m^{-4}
L_{ps}	8 m
L_{sep}	5 m
C_{f0}	0.005
Δz	0.2283 m

where $C_{v\text{ms}} = 0.4158$ is the control valve size coefficient, whose value includes the MSL system; $f(A_{\text{ms}}^*)$ is the fraction of the total flow area; A_{ms}^* is the fraction of wide open; $\Delta p_{\text{ms}} = p_{\text{Rx}} - 6.7203 \times 10^6$ is the MSL pressure drop; p_{Rx} is the reactor pressure (see Section 3.5); and 6.7203×10^6 is an estimated pressure. This equation allows reverse flow.

An analogous formulation is applied to calculate the FW mass flow:

$$W_{\text{fw}} = C_{v\text{fw}} g(A_{\text{fw}}^*) \sqrt{\rho_l \Delta p_{\text{fw}}}, \tag{37}$$

where $C_{v\text{fw}} = 0.04595$ is the control valve size coefficient, whose value includes the FW system; $\Delta p_{\text{fw}} = 7.9976 \times 10^6\,\text{Pa} - p_{\text{Rx}}$ is the feed water pressure drop; and $7.9976 \times 10^6\,\text{Pa}$ is an estimated pressure. The FW enthalpy is calculated as a function of the MSL mass flow rate, which was estimated using a typical BWR [20]:

$$\begin{aligned} h_{\text{aa}} = {} & -187903.0 + 50281.0(W_{\text{ms}}^*) - 983.3(W_{\text{ms}}^*)^2 \\ & + 8.88(W_{\text{ms}}^*)^3 - 0.03(W_{\text{ms}}^*)^4, \end{aligned} \tag{38}$$

where $W_{\text{ms}}^* = (W_{\text{ms}}/W_{\text{nom}}) \times 100$, W_{ms} is given by (43), and W_{nom} is 100% of mass flow rate. The dynamic effects are considered as a response of first-order system [19]:

$$\frac{dh_{\text{fw}}}{dt} = \frac{h_{\text{aa}} - h_{\text{fw}}}{\tau_{\text{fws}}}, \tag{39}$$

where τ_{fws} is the FW time constant. Table 8 presents the time constant.

The dynamic of the valves was modeled as a response of a first-order system:

$$\begin{aligned} \frac{dA_{\text{ms}}^*}{dt} &= \frac{S_{\text{cp}} - A_{\text{ms}}^*}{\tau_{\text{ms}}}, \\ \frac{dA_{\text{fw}}^*}{dt} &= \frac{S_{\text{cl}} - A_{\text{fw}}^*}{\tau_{\text{fw}}}, \end{aligned} \tag{40}$$

where τ_{ms} and τ_{fw} are time constants of the MS valve and FW valve, respectively; S_{cp} and S_{cl} are control signals. The time constant values are presented in Table 8.

The proportional-integral-derivative (PID) control is used for pressure control in the reactor vessel. The proportional action changes its output signal S_{cp} in direct proportion to the error signal $e_p(t)$ [19]:

$$S_{\text{cp}} = K_{\text{po}} + K_p e_p(t) + K_D \frac{de_l}{dt} + K_I \int e_p(t)dt, \tag{41}$$

Table 8: Time constant used in this work.

τ_{fws}	1000.0 s
τ_{ms}	1.25 s
τ_{fw}	10.0 s

Table 9: Control parameters.

K_{po}	0.75
K_p	18.4
K_I	18.4
K_{pl}	0.90
K_{Il}	0.02

where K_{po} is the polarization and the controller value output when there is no error; K_p, K_D, and K_I are, respectively, the proportional, derivative, and integral control gains. The tracking error is given by:

$$e_p(t) = \frac{p_{Rx} - 7.07 \times 10^6 \text{ Pa}}{p_{nom}}. \tag{42}$$

Here, p_{Rx} is the reactor pressure; 7.07×10^6 Pa is the set point of pressure; and p_{nom} is the nominal pressure. The proportional-derivative feedback control action $K_p e_p + K_D(de_l/dt)$ is used to induce certain stability margin in the controlled pressure. However, the stability of pressure does not ensure that the tracking error $e_p(t)$ can be made as small as desired. To this end, the integral action $K_I \int e_l(t)dt$, which contains the memory of the tracking error, is introduced to reduce the size of the steady-state tracking error.

The proportional-integral (PI) control is used for level control in the reactor vessel, which is given by [19]:

$$S_{cl}(t) = K_{pl} e_l(t) + K_{Il} \int e_l(t)dt, \tag{43}$$

where K_{pl} is the proportional gain; K_{Il} is the integral gain; and the tracking error is given by:

$$e_l(t) = \frac{W_{ms} - W_{fw}}{W_o} + \frac{N_{inst} - 0.97 \text{ m}}{N_o}, \tag{44}$$

where W_o is the mass flow rate; N_o is the normal level; N_{inst} is the instrument level; and 0.97 m is the set point of the level. The proportional action moves the control valve in direct proportion to the magnitude of the error, while the integral action moves the control valve based on the time integral of the error. If there is no error, the controller output does not move. As the error goes positive or negative, the integral of the error drives the controller output either up or down, depending on the action of the controller. The integral action eliminates the steady error. The control parameters used in this work are presented in Table 9.

4. RESULTS AND DISCUSSION

To demonstrate the model applicability, first, the model was validated under steady-state and transient behavior using manufacturer's predictions [20], plant data [21, 22], and RELAP5 code [23]. Afterwards, the model was applied to the SBWR to analyze steady-state conditions at 100% of the rated power and to analyze the transient behavior during the closure of all main steam line isolation valves (MSIVs). Table 2 shows the nominal values of the SBWR used in the simulation.

4.1. Validation

The qualification assessment of the simplified model is presented in Appendix. The model predictions were compared with plant data and manufacturer's predictions under steady-state behavior [20, 22]. To demonstrate the model applicability under transient behavior, an event occurred in the Laguna Verde nuclear power plant (LVNPP) was selected [21]. The plant data is compared with that obtained with RELAP5 [23] and the simplified model described in this work. The comparison results between RELAP5 and simplified model against plant data show that both computer codes have the capability to adequately predict the reactor behavior.

4.2. Steady-state behavior

Table 2 shows the nominal values of the SBWR used in the simulation. The axial power distribution in the core (Figure 3) shows a high peak value in nodes 4 and 5 that correspond to the middle part of the fuel assembly. The lower power factor corresponds to the top and bottom of the assembly, where the neutron leakage is higher. This is a quite standard profile in a BWR, but using different schemes of the control rod the axial power shape is flatted along the fuel assembly in order to decrease the hot point in case of operational transient.

Figure 5 shows the behavior of the mixture density in the core. This is one of the most important parameters for the reactor that works under natural circulation, where the differences of the density of the coolant between the top and bottom of the core establish the natural flow.

The void fraction distribution (Figure 6) along the fuel assembly corresponds to a quiet standard BWR. Here, the heat transfer to the coolant increases the void fraction 70% before the dry step.

The radial and axial temperature distribution in the fuel is shown in Figure 7. The first radial node corresponds to the surface of the fuel cladding that is keeping about 560 K by the coolant flow. This temperature is the typical value during normal operation in the standard BWR. The node 8 corresponds to the center of the fuel pellet. The higher temperature corresponds to the nodes 4 and 5 because these nodes have the higher axial power factor (see Figure 3). Figure 7 shows that the natural circulation works efficiently to cool the surface temperature, far from the thermal limit even during transient conditions as show in Section 4.3.

The change in the enthalpy from subcooled liquid to saturated liquid during the natural circulation of the liquid

Figure 5: Axial distribution of the mixture density.

Figure 6: Axial distribution of the void fraction.

Figure 7: Fuel rod temperature distribution. (a) Surface graphic, (b) contour lines.

phase is shown in Figure 8. This effect corresponds to the heating of the coolant during its travel along the core.

The transfer of energy from the fuel to the coolant due to heat transfer increases the enthalpy of the mixture node by node as shown in Figure 9. When $h_f < h_m$, two-phase saturated exist, this takes place approximately at core node 3. If $h_m < h_f$ and $h_l < h_f$, liquid phase occurs in the core node 1, and subcooled boiling appears in the core nodes 2 and 3, respectively.

Figure 10 shows the convective heat transfer coefficient. The drastic increment of this parameter in the bottom of the core shows the importance of this mechanism to remove heat from the nuclear fuel during natural circulation. Specifically, in node 1, there is heat transfer in single liquid phase, and in nodes 2, 3, and 4 the heat transfer is governed by subcooled boiling. For the rest of the nodes the heat transfer regime is nucleate boiling.

The superficial velocity of the gas and liquid is shown in Figures 11 and 12, respectively. The superficial velocity of the gas increases due to the heating of the liquid following the curve of the void fraction. Therefore, the superficial velocity of the liquid tends to go down.

4.3. Closure of MISVs

The analysis of this transient assumes normal functioning of plant instrumentation and controls plant protection and reactor protection systems (RPSs). The sequence of the events of this transient is presented in Table 10.

The main steam isolation valves start to close in the first second with a delay of 3 seconds before closing completely, and the position switches on the valves initiate a reactor scram when the valves are less than 90% open. According to the behavior shown in Figures 13 and 14, the closure of these valves inhibits steam flow to the feed water turbines terminating feed water flow. Mitigation of pressure increase is accomplished by initiation of the reactor scram via MSIV position switches and the protection system (Figure 15) and

Figure 8: Axial distribution of the enthalpy of the liquid.

Figure 9: Axial distribution of the mixture. h_f (= 1271026) liquid saturated enthalpy.

Table 10: Sequence of events of closure of main steam isolation valves.

Elapsed time (sec.)	Event
0.0	Reactor under steady-state condition rate at full power.
1.0	Closure of all main steam isolation valves.
2.0	Maximum power peak is reached (120% of rated).
2.0	The peak of pressure is maximum, and the SRVs opening to relief pressure.
2.0+	Reactor lever decreases until the emergency core cooling system actuation.
10.0+	Core flow tend to stabilize at new equilibrium conditions (natural circulation).

Figure 10: Axial distribution of the convective heat transfer during natural circulation.

the opening of the safety relief valves (SRVs) to limit system pressure (Figure 16). The peak of thermal power reaches 120% of rated power after approximately 3 seconds as shown in Figure 15, which is due to the drastic reduction of the void fraction in the core (Figure 17). This peak of power is limited by the set point scram at 118% of neutronic power. The peak pressure will still remain considerably below the ASME code limit of 1375 psig (9.48 MPa) as shown in Figure 18. The loss of feed water flow produces a reduction in the water lever in the reactor vessel as shown in Figure 19; the initiation of the emergency core cooling systems (ECCSs) occurs at approximately 33 seconds after detection; and the low level is not shown here. The scram of the reactor causes a reduction of the core flow (Figure 20). Figure 21 shows the heat transfer from the fuel to the coolant, where the shape follows the scram curve.

Figure 22 shows the behavior of the core flow in respect to void fraction. After the closure of the MISVs, the void fraction decreases suddenly due to pressure raise in the reactor vessel, which produces unbalance between the gravity head and the total loop pressure drop; therefore, the core flow decreases. This decrease can be observed in the figure, where finally the core flow reaches 70% of the rated core flow with 20% of the rated void fraction. This behavior is very important to core cooling. Figure 23 shows the neutronic power behavior in respect to core flow. In general terms, this figure shows that after shutdown the core flow keeps maintaining high values for low power values, which must guarantee the core cooling due to residual heat. The expanded graphic (Figure 23) shows that at the maximum power level the core flow increases slightly as expected.

5. CONCLUSIONS

A simplified model for transient analysis on boiling water reactor (BWR) working with natural circulation is described

Figure 11: Superficial velocity for the gas phase.

Figure 12: Superficial velocity for the liquid phase.

Figure 13: Feed water behavior.

Figure 14: Steam flow behavior.

Figure 15: Neutronic power behavior during the closure of MSIVs.

Figure 16: SRV'S steam flow by closure of MSIVs.

Figure 17: Average void fraction behavior.

Figure 18: Pressure change in the dome of the reactor vessel.

Figure 19: Water lever reduction due to loss of feed water.

Figure 20: Core flow behavior.

Figure 21: Heat flux behavior.

Figure 22: Core flow versus void fraction.

Figure 23: Neutronic power versus core flow.

Figure A.1: Profile of the void fractions in comparison with manufacturer's predictions [20].

in this paper. This model was based on lumped and distributed parameter approximations, which includes vessel dome and downcomer, neutron kinetics, fuel temperature, lower and upper plena reactor core, and pressure and level controls.

The steady state and the transient of closure of main steam valves were presented and analyzed. The numerical results obtained with the model show a behavior physically consistent both in steady-state and transient behavior. Appendix A is a comparison of the simplified model predictions with plant data, manufacturer's predictions, and RELAP5 under steady-state and transient conditions, showing good predictions between plant data and codes calculations. Therefore, the simplified model described in this paper provides reliable results to predict the behavior of a BWR working with natural circulation under normal and abnormal conditions.

Our results in the transient of closure of main steam valves show that the cooling system due to natural circulation in the SBWR is around 70% of the rated core flow. This can guarantee the core cooling due to residual heat.

Natural convection circulation of coolant in the reactor cooling system is used to some extent in all the reactor concepts, and many of these uses are innovative. Therefore, the need is to develop or confirm the design basis and to develop and qualify computer codes to enable reliable safety analysis.

Simplified model as shown in this work can be used to predict phenomena on SBWR behavior during the evaluation of the safety of a nuclear power plant which requires analysis of the plant's responses to postulate equipment failures or malfunctions. Such analysis helps to determine the limit conditions for operation, limit safety system settings, and design specifications for safety-related components and systems to protect public health and safety.

Figure A.2: Steam flow comparison with data design [22].

APPENDICES

A. QUALIFICATION ASSESSMENT FROM STEADY STATE

The profile of the void fraction prediction was compared with the manufacturer's predictions [20]. The numerical results obtained with the simplified model show that void

Figure A.3: Nodalization of Laguna Verde nuclear power plant with RELAP5 [21].

fraction behavior is similar to the results reported by the final safety analysis report [20] for a typical BWR. This assessment is shown in Figure A.1. In this figure, it can be observed that manufacturer's results underpredict the void fraction in the subcooled boiling region (approximated of 0.15 to 0.35 of core length). After this region, the profile is predicted with an accuracy of 0.323%.

The steam flow data design [22] in the reactor for a typical BWR is predicted with a maximum accuracy of 2.4%. This assessment is shown in Figure A.2 for different load lines.

B. QUALIFICATION ASSESSMENT OF THE SIMPLIFIED MODEL UNDER TRANSIENT CONDITIONS

For the validation of the model under transient conditions, a transient occurred in the Laguna Verde nuclear power plant (LVNPP) was selected [21]. The plant data is compared with those obtained with RELAP5 [23] and the simplified model described in this work. This transient occurred in LVNPP Unit 2 on June 25th 1999, during the maintenance activities in the filters of the reactor water cleanup system (RWCU). A wrong sequence in the opening of valves of the RWCU caused a differential pressure and small pieces of the filter going up inside the reactor vessel. The signal of high radiation in the main steam lines causes a reactor SCRAM. This transient occurs at 90% of the full power.

Figure A.3 shows the nodalization diagram of the LVNPP for the RELAP5 computer code. This is a typical boiling water reactor 5. In this model, the feed water, turbine, suppression pool, control rod drive system, and emergency cooling systems are represented by boundary conditions. Four fuel rod channels represent the core of the reactor [23].

Figure B.4: Thermal power behavior during the shutdown of the reactor.

The signal of high radiation in the main steam lines causes the scram of the reactor and this is shown in Figure B.4. This Figure shows a comparison among plant data, RELAP5, and simplified model results. The prediction of the simplified model is between RELAP5 calculation and plant data.

The pressure of the reactor vessel is shown in Figure B.5. The maximum peak of pressure according to the plant data

Figure B.5: Pressure rise of the reactor due to closure of main steam valves.

Figure B.6: Core flow reduction by recirculation pump trip.

is 75.69 Kg/cm^2, and the simplified model shows an error of 2.5% below that value. RELAP5 calculation shows that the peak of pressure is well predicted. The opening of the safety relief valves causes a fast reduction of the pressure. In the simplified model, this reduction is slower than RELAP5 prediction.

The core flow reduction due to the trip of the recirculation pumps by high pressure in the reactor vessel is shown in Figure B.6. In this case, RELAP5 and the simplified model overpredicts the core flow, but during the first 5 seconds of the transient the results obtained with the simplified model

Figure B.7: Steam flow reduction during the closure of main steam valves.

are closer to the plant data, with a maximum difference of 12%.

Figure B.7 shows the behavior of the steam flow. Again, the prediction of the simplified model is between plant data and RELAP5 calculation. The highest difference during the first 5 seconds of the transient is lower than 3%.

The comparison results between RELAP5 and simplified model against plant data shows that both computer codes have the capability to adequately predict the reactor behavior. One of the main advantages of the simplified model is that it spends less time to simulate different transients with a high reliability in results.

NOMENCLATURE

a, b, c, d:	Coefficients (see (24))
A^*_{fw}:	Fraction of wide open of the FW control valve
A^*_{ms}:	Fraction of wide open of the MSL control valve
A_{x-s}:	Cross-sectional area (m^2)
c_i:	Delayed neutron concentration
C_o:	Distribution parameter
C_{f0}:	Single-phase friction factor
$C_{v\mathrm{fw}}$:	FW control valve-size coefficient (m^2)
$C_{v\mathrm{ms}}$:	MSL control valve-size coefficient (m^2)
Cp_l:	Liquid specific heat (J/kg·K)
D_h:	Hydraulic diameter (m)
$e_p(t)$:	Error signal
F:	Convective boiling factor
$F(z)$:	Axial power factor
f:	Friction factor
G:	Mass flux (kg/s·m^2)
H_o:	Condensation parameter (1/s·K)
H_∞:	Convective heat transfer coefficient (J/s·m^2·K)

h: Enthalpy (J/kg)
h_{fg}: Difference in specific enthalpy between saturated vapor and liquid (J/kg)
h_l: Liquid enthalpy (J/kg)
h_v: Vapor enthalpy (J/kg)
j: Superficial velocity (m/s)
K_D: Derivative control gain (s)
K_I: Integral control gain (s^{-1})
K_{Il}: Integral gain (s^{-1})
K_p: Proportional control gain
K_{pl}: Proportional gain
K_{po}: Polarization
K_{psn}: Support core plate loss coefficient (m^{-4})
K_{sep}: Separador loss coefficient (m^{-4})
k_l: Liquid thermal conductivity (J/s·m·K)
L_{ps}: Length in the upper plenum (m)
L_{sep}: Length in the steam separators (m)
m_l: Mass of liquid (kg)
m_v: Mass of vapor (kg)
$n(t)$: Normalized neutron flux
N_{inst}: Instrument level (m)
N_l: Liquid level in the vessel(m)
N_o: Normal level(m)
P_0: 100% of rated power(J/s)
P_H: Heated perimeter(m)
p_{Rx}: Reactor pressure(Pa)
p: Pressure (Pa)
q'': Heat flux (J/s·m^2)
q''': Volumetric heat generation by gamma radiation(J/s·m^3)
S_{cp}: Output signal
$\langle T_f \rangle$: Average fuel temperature(K)
T_m: Moderator temperature(K)
T_s: Saturation temperature(K)
v_{gj}: Average drift velocity(m/s)
W_o: 100% of rated of the mass flow(kg/s)
W_c: Core mass flow rate(kg/s)
W_{gin}: Gas mass flow rate (kg/s)
W_{jl}: Liquid mass flow rate (kg/s)
W_{jv}: Vapor mass flow rate (kg/s)
W_{lin}: Liquid mass flow rate (kg/s)
W_{nom}: 100% of mass flow rate (kg/s)
W_{sep}: Mass flow rate through the steam separator (kg/s)
x: Vapor quality
X_{tt}: Martinelli factor.

GREEK SYMBOLS

β: Neutron delay fraction
β_i: Portion of neutrons generated by the ith group
Γ: Volumetric vapor generation rate (kg/m^3)
Δp_c Core pressure drop (Pa)
Δp_g: Drop pressure due to gravity (Pa)
Δt: Time step (s)
Δz: Cell length in axial direction (s)
ε_g: Void fraction
Λ: Neutron generation time (s)
ρ: Density (kg/m^3)
ρ_c: Core density in each node (kg/m^3)
ρ_D: Doppler effect (pcm)
ρ_{dw}: Downcomer density (kg/m^3)
ρ_{cr}: Reactor control rods (kg/m^3)
ρ_f: Density of saturated liquid (kg/m^3)
ρ_g: Density of the saturated vapor (kg/m^3)
ρ_m: Rmoderador temperature reactivity (pcm)
ρ_{sep}: Steam-separator density (kg/m^3)
τ_v: Void fraction reactivity (pcm)
$(\sum l/A)$: Inertial term (m^{-1})
τ_{fws}: Time constant of the FW enthalpy (s)
τ_{ms}: Time constant of the MS valve (s)
τ_{fw}: Time constant of the FW valve (s)
$\phi^2_{f0,j}$: Two-phase multiplier.

SUBSCRIPTS

f: Fuel, saturated liquid
g: Gas phase
l: Liquid phase
m: Mixture in two-phase flow.

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Project Report

Natural Circulation Characteristics at Low-Pressure Conditions through PANDA Experiments and ATHLET Simulations

Domenico Paladino,[1] Max Huggenberger,[1] and Frank Schäfer[2]

[1] *Laboratory for Thermal-Hydraulics, Paul Scherrer Institute (PSI), 5232 Villigen, Switzerland*
[2] *Research Center Dresden-Rossendorf, P.O. Box 510119, 01314 Dresden, Saxony, Germany*

Correspondence should be addressed to Frank Schäfer, f.schaefer@fzd.de

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Natural circulation characteristics at low pressure/low power have been studied by performing experimental investigations and numerical simulations. The PANDA large-scale facility was used to provide valuable, high quality data on natural circulation characteristics as a function of several parameters and for a wide range of operating conditions. The new experimental data allow for testing and improving the capabilities of the thermal-hydraulic computer codes to be used for treating natural circulation loops in a range with increased attention. This paper presents a synthesis of a part of the results obtained within the EU-Project NACUSP "natural circulation and stability performance of boiling water reactors." It does so by using the experimental results produced in PANDA and by showing some examples of numerical simulations performed with the thermal-hydraulic code ATHLET.

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1. INTRODUCTION

In the framework of the EU-Project NACUSP, experiments on thermo-hydraulic characteristics of natural-circulation-cooled boiling water reactors (BWR) have been performed in four sophisticated thermo-hydraulic test facilities which complement each other, ranging from small-scale to large-scale, and from low-pressure/low-power operating conditions to the nominal operating conditions for BWRs. The ultimate goal of the project was to improve the economics of operating and future plants through improved operational flexibility, enhanced availability, and increased confidence levels on the safety margins regarding the stability issues in BWRs [1].

This paper presents a synthesis of the results obtained from the experiments performed in the large-scale PANDA facility [2] and from the numerical simulations performed with the thermal-hydraulic code ATHLET. The range of parameters covers a large spectrum of conditions for the low-pressure/low-power range, therefore, being of interest especially with regard to the start-up procedures for natural-circulation-cooled BWRs (e.g., for the European simplified boiling water reactor (ESBWR)).

2. PANDA EXPERIMENTAL INVESTIGATIONS

2.1. PANDA facility

The multipurpose thermal-hydraulic test facility PANDA is located at Paul Scherrer Institute (PSI), Villigen, Switzerland. The facility is designed and used for investigating at large-scale system behavior and phenomena for different light water reactor (LWR) designs. PANDA has a modular structure, which is based on six cylindrical pressure vessels with a total volume of 460 m^3 and four open pools with a total capacity of 60 m^3 (Figure 1). Four of these pressure vessels are arranged in two vertical columns. The two lower vessels simulating the wetwell or suppression chamber are interconnected with two large pipes. The wetwell vessels support the two vessels, simulating the drywell. They are interconnected at about midplane elevation with one large pipe. The fifth vessel simulates the core flooding (GDCS) pool. The sixth vessel is configured to simulate the reactor pressure vessel (RPV). Its dimension allows for performing natural circulation tests at roughly 1 : 1 scale in height. The vessel has an inner diameter of 1.23 m and is 19.2 m in height. The RPV includes a heated section, a riser, and a downcomer (DC). The heated section has a height of 1.3 m and does

not reproduce the geometry of a real core. The power is generated by 115 electrical heater elements divided into six individually controlled groups. This allows for conducting tests with any specified power up to 1.5 MW. The heaters are distributed over the cross-section to obtain a uniform radial distribution over the whole power range. The riser has a height of 9.5 m (starting at the top of the heated section). Four rectangular open pools are placed on top of the drywell vessels and are equipped with heat exchangers/condensers. The facility is well insulated in order to minimize heat losses: an insulation layer of 300 mm is applied on the RPV and 100 mm on system lines and pools. The overall height of the facility is about 25 m. The maximum operating conditions are 10 bar and 200°C.

The basic instrumentation of the facility includes about 1000 sensors to measure temperatures, pressures, pressure differences, water levels, flow rates, gas concentrations, fluid phases, and electrical heater power. The data acquisition system maximum scanning rate is 5 Hz. The standard operating scanning rate is 0.5 Hz. The facility is remotely controlled from a graphical-display man-machine interface with process visualization.

2.1.1. *PANDA facility configuration for natural circulation tests*

For the NACUSP project only parts of the PANDA facility were used. Figure 2 shows the test configuration consisting of the RPV natural circulation loop and the condensation/cooling loop. The steam produced in the RPV flows to the condenser and condenses due to the temperature difference with the secondary side (water pool at saturation temperature). The pool is open to the atmosphere and water evaporates from the boiling pool. The condensate flows through the drain line back to the RPV downcomer. A major modification to the RPV was related to the core inlet region. A movable ring was attached to the lower edge of the shroud (separating the core from the downcomer) to allow for varying the core-inlet hydraulic resistance. The gap height between the lower edge of the ring and the bottom of the RPV can be varied in order to obtain the required core-inlet flow resistance. The PANDA auxiliary systems are used for preconditioning the facility and for adding water to keep the pool at the required level along the test duration.

The instrumentation in the RPV has been significantly improved. In total about 110 sensors are used for the natural circulation tests. Additional K-type thermocouples inside riser and downcomer have been installed to measure local fluid and wall temperatures. In the range of 100°C–150°C a measurement error less than 0.5°C can be expected. For higher temperatures the error is still less than 1°C. Three ultrasonic flow meters have been added to measure local fluid flow velocities in the downcomer. The measurement error (for pipe flow) is typically less than 1% of the reading. Absolute pressure and heater power are measured as well. Differential pressure sensors in the riser and downcomer allow for the assessment of void fraction in different vertical sections of the RPV. A time domain reflectometry (TDR) probe is used to measure the level swelling due to the void production in the RPV.

Figure 1: PANDA facility.

2.2. *PANDA test matrix*

The test matrix allowed the RPV power and pressure to be varied, as well as other parameters influencing the natural circulation behavior such as core-inlet hydraulic resistance and RPV water level. The tests were performed at constant power, balanced by a corresponding condenser heat removal capacity. The specified RPV power was selected to match the desired steady state pressure in the RPV. The basic test matrix is shown in Table 1. For a given RPV pressure level, the maximum energy is removed if the condenser is fully submerged in the pool. The corresponding power for the four specified pressure levels is given in Table 1 (column "high"). For running tests at lower power, the condenser heat removal capacity is adjusted by lowering the water level in the pool, thereby reducing the active condenser area. Three tests with different power ("low," "middle," and "high") were performed for each of the four different pressure levels.

The core-inlet hydraulic resistance coefficient k has been varied as follows: for the basic tests a BWR typical value was used ($k_{\text{basic}} = 30$). Selected tests were performed with a low inlet resistance ($k_{\text{low}} = 7$) and with high inlet resistance ($k_{\text{high}} = 500$).

Figure 2: PANDA configuration for natural circulation tests.

(a) Test H2.1

(b) Test H8.1

Figure 3: Measured temperature profiles in the vertical axis of the RPV.

(a) Measured flow velocities in the downcomer

(b) Normalized power spectra of velocity signal

(c) ACF processed from one velocity signal

Figure 4: Analysis of flow velocity signals in the downcomer (Test L8.3).

Table 1: Basic test matrix (variation of pressure and power).

RPV pressure [bar]	RPV power [kW]		
	Low	Middle	High
2.0	135	200	245
3.0	250	380	480
5.0	430	660	885
8.0	594	917	1306

The RPV water level has also been varied: most of the tests were performed with the collapsed water level at 12.8 m above bottom of RPV. This level represents about the nominal value for the ESBWR. For few tests the water level was reduced close to top of riser which is at 11.0 m above RPV bottom. The selected values were 11.1 m and 11.4 m.

The following three series of tests with totally 25 experiments have been performed:

(i) B-series tests (BWR-typical core-inlet flow resistance $k = 30$):

(a) 12 tests with nominal RPV water level (12.8 m),

(b) 2 tests with low RPV water level (11.1 m and 11.4 m);

(ii) L-series tests (Low core-inlet flow resistance $k = 7$):

(a) 3 tests with nominal RPV water level (12.8 m),

(b) 1 test with low RPV water level (11.1 m);

(a) Measured flow velocities in the downcomer

(b) Normalized power spectra of velocity signal

(c) ACF processed from one velocity signal

Figure 5: Analysis of flow velocity signals in the downcomer (Test B3.3).

(iii) H-series tests (High core-inlet flow resistance $k = 500$):

(a) 6 tests with nominal RPV water level (12.8 m),
(b) 1 test with low RPV water level (11.1 m).

An overview of all PANDA tests is included in Table 2.

2.3. Experimental results and analysis

Natural circulation modes

The coolant temperature increases in the heated section (core), but it may not reach saturation conditions under the given conditions. A rough estimation of the position of the boiling boundary in the core by means of a heat balance was performed. The temperature measured at the inlet of the core was used as the reference temperature, and the coarsest assumptions were made (no heat losses, uniform radial and vertical distribution of power in the core, no subcooled boiling). The nondimensional value $z^*_{\text{boil}.b}$ reported in Table 2 corresponds to the distance (measured from the inlet of the core, and divided by the length of the core) at which this boiling boundary can be expected ($z^*_{\text{boil}.b} \leq 1$: boiling in the core; $z^*_{\text{boil}.b} > 1$: no boiling in the core). The result is that boiling in the core did not occur in any test except may be in tests with high core-inlet resistance and relatively high power for which $z^*_{\text{boil}.b} \cong 1$ is found.

In the same way, a coarse estimation of the height in the riser at which flashing may be expected to occur has been made. Using the same inlet temperature, calculating the temperature at the outlet of the core, and assuming it is

Table 2: Overview of the PANDA tests and analysis results.

Test name	k	P (bar)	Power (kW)	Level RPV (m)	Level pool (m)	T_{sub} (K)	$z^*_{boil.b}$ (–)	z^*_{flash} (–)	V_1 (m/s)	V_2 (m/s)	V_3 (m/s)	V_{123} (m/s)	Var_{123} (m^2/s^2)	^{1123} (%)	f (Hz)	T (sec.)	Ap (deg)	DR (–)	Analysis of RPV pressure signal	Class
LOW k SERIES:																				
L8.3	7	7.96	1313	12.81	4.21	5.8	2.7	0.75	0.54	0.51	0.52	0.52	0.0135	22	0.0160	63	0	0.64	1 major peak, $f = 0.01455$ ($T = 65$ s); DR = 0.63	2
L5.3	7	5.04	909	12.82	4.20	7.7	3.2	0.79	0.32	0.31	0.32	0.32	0.0030	17	0.0084	120	0	0.68	1 major peak, $f = 0.0084$ ($T = 1\,20$ s); DR = 0.68	2
LL5.3	7	5.02	911	11.11	4.20	5.3	2.6	0.43	0.35	0.43	0.37	0.38	0.0037	16	0.0111	90	0	0.74	1 major peak, $f = 0.011$ ($T = 90$ s); DR = 0.74	2
L2.3	7	2.02	247	12.83	4.20	13.8	12.5	1.02	0.16	0.20	0.21	0.19	0.0030	29	?	?	?	?	no major peak	1
BASIC k SERIES:																				
B8.1	30	8.04	594	12.75	1.73	5.8	3.0	0.82	0.29	0.23	/	0.26	0.0027	20	?	?	?	?	1 major peak, $f = 0.0077$ ($T = 130$ s); DR = 0.5?	1
B8.2	30	8.06	917	12.81	2.17	5.8	2.7	0.77	0.36	0.31	0.41	0.36	0.0052	20	0.0115	87	0	0.6	1 major peak, $f = 0.0115$ ($T = 87$ s); DR = 0.4?	2
B8.3	30	7.98	1306	12.80	4.20	5.8	2.7	0.75	0.52	0.47	0.57	0.52	0.0085	18	0.0158	63	0	0.61	1 major peak, $f = 0.0158$ ($T = 63$ s); DR = 0.4?	2
B5.1	30	5.15	391	12.82	1.73	7.5	4.0	0.86	0.19	0.18	0.18	0.18	0.0015	21	?	?	?	?	no major peak	1
B5.2	30	5.04	637	12.84	2.17	7.6	3.1	0.77	0.22	0.22	0.23	0.22	0.0017	18	?	?	?	?	1 major peak, $f = 0.0065$ ($T = 154$ s); DR = 0.85	1
65.3	30	4.85	896	12.77	4.20	7.8	2.8	0.72	0.30	0.27	0.26	0.28	0.0037	22	0.0080	125	0	0.66	1 major peak, $f = 0.08$ ($T = 125$ s); DR = 0.65	2
BL-5.3	30	5.02	896	11.11	4.20	7.0	3.3	0.70	0.37	0.33	0.39	0.36	0.0055	20	?	?	?	?	1 major peak, $f = 0.01225$ ($T = 82$ s); DR = 0.4	1
BLL5.3	30	4.95	897	11.39	4.20	7.2	3.2	0.71	0.35	0.32	0.36	0.34	0.0085	27	0.0106	95	0	0.85	1 major peak, $f = 0.01055$ ($T = 95$ s); DR = 0.81	2
B3.1	30	3.02	231	12.82	1.73	10.4	8.0	0.96	0.15	0.16	0.14	0.15	0.0026	34	?	?	?	?	no major peak	1
B3.2	30	3.02	361	12.80	2.17	10.5	5.8	0.91	0.18	0.17	0.16	0.17	0.0018	25	?	?	?	?	1 peak, $f = 0.0055$ ($T = 182$ s);	1
63.3	30	2.97	502	12.78	4.21	10.7	4.7	0.87	0.20	0.18	0.19	0.19	0.0016	21	?	?	?	?	1 peak, $f = 0.055$ ($T = 200$ s);	1
B2.1	30	2.02	111	12.81	1.73	14.0	17.9	1.05	0.10	0.13	0.12	0.12	0.0017	35	?	?	?	?	no major peak	1
B2.2	30	2.02	183	12.80	2.17	13.8	12.5	1.01	0.13	0.15	0.13	0.14	0.0024	35	?	?	?	?	no major peak	1
B2.3	30	2.00	241	12.80	4.20	13.9	10.2	0.99	0.14	0.16	0.16	0.15	0.0029	35	?	?	?	?	no major peak	1
HIGH k SERIES:																				
H8.1	500	7.9	595	12.81	1.73	5.2	1.4	0.25	/	0.13	0.16	0.14	0.0023	34	?	?	?	?	no major peak	3
H8.3	500	7.83	1309	12.78	4.21	4.6	1.2	0.03	/	0.26	0.31	0.29	?	?	?	?	?	?	no major peak	3
H5.1	500	5.06	419	12.83	1.73	7.1	1.8	0.41	0.08	0.08	0.10	0.09	0.0014	44	?	?	?	?	no major peak	3
H5.3	500	4.97	902	12.81	4.20	6.6	1.3	0.12	0.15	0.12	0.17	0.15	0.0034	40	?	?	?	?	no major peak	3
HL5.3	500	5.00	907	11.10	4.21	6.0	1.5	0.23	0.22	0.16	0.23	0.20	0.0170	64	?	?	?	?	no major peak	3
H2.1	500	2.03	131	12.85	1.73	13.6	7.4	0.95	0.08	0.05	0.05	0.06	0.0006	41	?	?	?	?	no major peak	1
H2.3	500	2.00	246	12.83	4.20	13.6	4.6	0.84	0.08	0.06	0.07	0.07	0.0080	126	?	?	?	?	no major peak	1

Figure 6: Period of oscillation versus fluid transit time in core and riser.

conserved all along the riser (no heat losses), a distance z^*_{flash} (measured from inlet of riser, and divided by length of riser) is estimated and reported in Table 2. For most of the tests, it is expected that flashing should only occur in the upper region or above the riser. Of course, in tests at high core-inlet resistance and relatively high power, and also in tests with low RPV water level (and hence lower pressure head), this basic calculation predicts flashing in lower sections of the riser.

These estimations do not take into account the 3D and local effects that might affect the physical phenomena and the flow regime actually occurring in the RPV. Some interesting information can be retrieved by plotting some of the temperature profiles measured in the central axis of the RPV. Two examples are given in Figure 3, for which, a curve corresponding to the saturation temperature is also plotted on each graph. The saturation temperature has been estimated by using the time-averaged RPV pressure and calculating the pressure head at each height, without considering the possible presence of void.

From Figure 3, it is clear that the validity of the comparison with the saturation temperature may be limited due to the error in temperature measurements. Due to the data reduction procedure, this error slightly increases with temperature (Figure 3). The saturation temperature decreases along the vertical axis as the system operates at low pressure. Flashing should occur when the coolant reaches saturation conditions in the riser. Following this approach, in test H2.1 (shown in Figure 3(a)), the average height of the flashing boundary can be roughly estimated to be at about the level corresponding to the top of the riser. In this case, flashing should be responsible for the shape of the profile above this height, where measured temperatures seem to lie in the vicinity of the saturation curve. This basically confirms the previous coarse prediction (Table 2, $z^*_{\text{flash}} = 0.95$). In test H8.1, a different situation is shown (Figure 3(b)): the fluid temperature clearly following the saturation curve indicates that two-phase flow should have occurred along the complete riser length. To summarize, these different plots help to qualitatively distinguish different cases and define three classes:

Class 1. flashing above the riser, or perhaps no flashing at all (Figure 3(a));

Class 2. flashing at a lower elevation in the riser;

Class 3. two-phase flow all along the riser (Figure 3(b)). Rough estimations of time-averaged void fraction values processed from differential pressure measurements also indicate that the highest void fraction was assessed at the top of the riser, and especially for cases with high core-inlet resistance/high power (Class 3). These cases showed the largest difference between the swell level (measured by the TDR probe) and the collapsed water level retrieved from differential pressure measurements. For example, in tests HL5.3 and H8.3, a void fraction of the order of 10% was estimated in the upper region of the riser. In the lower sections, values lower than a few percent were found.

Analysis of flow velocity measurements in RPV downcomer

The natural circulation flow rate is of main interest. Hence, this short analysis concerns the velocity measurements from 3 ultrasonic flow meters in the RPV DC. The sensors are located at elevation 5.00 m above RPV bottom with an angle of 120° azimuthally between each other.

The velocity signals were sampled at a frequency of 0.5 Hz for the whole duration of the test (5 hours). It should be noted that the time constant of the sensors was set to 6 seconds in order to avoid aliasing problems. The autopower spectral density (APSD) of the signals was calculated to identify resonance frequencies of the system. The autocorrelation function (ACF) was used to calculate the decay ratio (DR) of the system. The DR is a widely used parameter to quantify the stability of the system: if DR < 1, the system is linearly stable; if DR > 1, it is unstable. In practice, the DR values presented in Table 2 were estimated by simply calculating the ratio between the second and the first maxima of the ACF (Figure 4(c)). Cross-power spectral densities, coherence, and cross-correlation functions were also calculated to examine coherence, possible phase shifts, and so forth between signals from different flow meters.

An overview of the results of the analysis performed can be found in Table 2. The basic parameters of each test are given in the first columns of this table. Mean values of the velocities calculated over the test period (V_1, V_2, and V_3) are also reported. V_{123} is the average of the three mean velocities. The quantity I_{123} should provide an estimate of the intensity of the velocity fluctuations, and was calculated as follows:

$$I_{123}(\%) = 100.\frac{(\text{Var}_{123})^{1/2}}{V_{123}}, \tag{1}$$

where Var_{123} represents the average of the 3 variances of the signals. It should be realized that this relation could result in very high values for the tests with very low natural circulation

Table 3: Initial conditions for the selected PANDA tests (Exp. = Experiment, Cal. = ATHLET Calculation, Add. Calc. = Additional ATHLET calculation).

Test	Power MW.RP.7 (kW)		RPV pressure MP.RP.1 (bar)		RPV level ML.RP.1 (m)		DC velocity MVE.DC.1 (m/s)		DC temperature MTL.RP.1 (°C)	
	Exp.	Cal.	Exp.	Cal.	Exp.	Cal.	Exp.	Cal.	Exp.	Cal.
B3.3	502	502	2.97	2.98	12.78	12.78	0.19	0.19	133.7	133.4
B5.2	637	637	5.04	5.06	12.84	12.85	0.22	0.22	152.1	152.3
L5.3	909	909	5.04	5.05	12.82	12.83	0.32	0.32	152.0	152.3
LL5.3	911	911	5.02	5.02	11.11	11.23	0.38	0.38	151.5	152.1
B8.3	1306	1306	7.98	7.99	12.80	12.86	0.52	0.52	169.7	170.5
Add. Calc.	—	1308	—	4.9	—	11.43	—	0.60	—	151.2

Table 4: Oscillation period and decay ratio calculated from the DC mass flow (Exp. = experiment, Cal. = ATHLET calculation).

Test	Period (s)		Decay ratio (–)	
	Exp.	Cal.	Exp.	Cal.
B3.3	200.0	204.8	—	0.66
B5.2	154.0	170.7	—	0.61
L5.3	120.0	113.8	0.68	0.62
LL5.3	90.0	102.4	0.74	0.55
B8.3	63.0	68.3	0.61	0.60

velocities, for which the signal-to-noise ratio is expected to be higher (e.g., test H2.3).

Concerning the spectral analysis, the frequency and the corresponding period of the major peak are reported when possible. The value "0" in the column "$\Delta\varphi$" of Table 2 means that no significant phase shift between the signals was observed, that is, the three velocity signals were oscillating in phase at one given frequency. When possible, a DR was estimated from the ACF and is also reported in Table 2. One column of the table refers to the analysis of signals from the sensor measuring the total pressure in the RPV. These additional observations may help to characterize some of the tests. Looking at the spectra and at the results of this analysis based on the ACF and the estimation of DR values, the PANDA tests can be categorized in two different groups as follows.

1st group

The rows in Table 2 corresponding to these tests are greyed. An illustration of this case is made using test L8.3 results. An excerpt of the raw velocity signals recorded during this test is shown in Figure 4(a). A major peak can be observed on the processed spectra of the 3 sensors reported in Figure 4(b). The time traces of the velocity signals show that the three sensors oscillate "together," that is, with no significant phase shift between each other. This was confirmed by looking at the phase of the complex cross-power spectral densities between the different signals. Just simply looking at a number of raw signals recorded over the test period, it can also be seen that the other measured variables (temperatures, pressure in the RPV, condenser feed, and drain flows, etc.) show fluctuations with the same frequency. The ACF obtained from velocity signal 1 is given in Figure 4(c). Clearly, a DR value indicating stable behavior (DR = 0.64) can be estimated from this graph.

2nd group

An illustration of this group is given using test B3.3 results. Observations made on the raw signals (Figure 5(a)) in this case show rather *random* behavior. In some other tests belonging to the same group, it is sometimes possible to observe significant phase shifts between the sensors. Figure 5(b) shows that there is no longer a major peak present in the power spectrum. Using a logarithmic scale, the low-frequency part of the spectrum appears to be *flatter*. Moreover, the ACF does not really allow a DR value to be derived (Figure 5(c)), this DR appearing to be much smaller (actually, very close to 0) compared to the tests of the first group.

It was also observed that these two characteristic modes of behavior, indicated by the ACF plots, did not vary as a function of time during the course of the tests. For tests L8.3 and B3.3, this was done by calculating ACFs over 5 successive (1-hour long) time periods and it clearly showed that, in both cases, all the ACFs for one test at different times were very similar.

Some data from other sensors (condenser feed and drain flow rates, RPV pressure, etc.) have also been analyzed using the same approach as for the velocity data. For the tests from the 1st group, a resonance peak can be observed at the same oscillation frequency. No peak can be found for the tests belonging to the 2nd group. However, in some of these tests (e.g., test B3.3), the condenser feed flow and RPV pressure signals may exhibit a peak in their spectrum whereas no such peak was observed for the velocity signals (see also the second last column of Table 2, concerning the analysis of RPV pressure signals). This indicates that flashing possibly takes place above the top of the riser, not influencing the main circulation flow. These cases fall consistently into Class 1 (see Figure 3).

From the expected mechanism of the flow oscillations, the fluid travelling time in the heated section, and the transit time of the enthalpy perturbations in the adiabatic section, should play a role in determining the period of the

oscillations. From Figure 11, it is clear that the oscillation period decreases with increasing average flow rate. The total transit time can be evaluated as the sum of the transit time in the riser section plus half of the transit time in the heated section [3]. In PANDA, no velocity measurement is available in the riser. An estimation of the riser velocity was made by simply using the time-averaged value of the velocity in the DC reported in Table 2, and just considering the flow area ratio between the riser and the DC.

Following this simplified approach, the oscillation period was found to be in the order of 1.6 times the transit time of the fluid (Figure 6). The simple way in which the transit time was estimated did not consider the occurrence of two-phase flow in the riser. Hence, it is likely that our assessment overestimated this time. Thus, these observations seem to be quite consistent with those presented for the CIRCUS facility [1], for which the period of oscillation was found to be twice the transit time.

Discussion

The differentiation between the two groups of tests is based on DR values derived from the velocity signals. However, the validity and the accuracy of this method can be questioned. Many other methods have been used to extract stability parameters and can be compared to the ACF-based method. For this reason, some of our experimental velocity time traces were processed using a more sophisticated method, namely, the autoregressive method. This method properly applied to our signals yielded about the same DR values as those presented in Table 2. This confirms the main result from this analysis that all tests are stable.

However, by having a closer look to the tests, the different flow regimes that can be assumed, and the three classes defined, can be related to the previous findings regarding the stability of the system. Information reported in Table 2 was used for this purpose. The plots in Figure 3 partly illustrate the following classification.

Class 4 (Figure 3(a)). High inlet subcooling and relatively low (or even very low) flow rates characterize this class. It is likely that flashing occurred only above the riser, the natural circulation flow still being in a single-phase regime. This would explain why, in some of these tests, contrary to the velocity signals in the DC, some RPV pressure signals show a peak in their spectrum and allow the derivation of DR values (see, e.g., test B5.2). It is also possible that in some of these tests (notably those performed at the lowest powers), no flashing occurred at all in the whole system.

Class 5. These tests, performed at relatively high power and low inlet resistance, have been characterized by a well-identified oscillation period and DR values could be derived from the velocity signals. The highest natural circulation flow rates were measured in these tests, which indicate the combined effect of the flow enhancement by steam production and of a low inlet resistance. According to our estimations and analyses, no boiling occurred in the core but flashing did in the upper half of the riser.

Class 6 (Figure 3(b)). Very stable behavior was found for these tests performed with an increased inlet resistance. Under these conditions, the flashing front fluctuates close to the bottom of the riser, which stabilizes very much the two-phase natural circulation. The measured flow rates are among the lowest of the test series, as a result of the high flow resistance. Looking at the parameters (notably the expected position of the boiling boundary) reported in Table 2 and at the characteristic temperature profiles (Figure 3(b)), it seems that stable two-phase natural circulation flow can be assumed in these cases.

The presented classification gives a rather coherent picture of the PANDA test series. It was possible to link the experimental observations with the expected phenomenology. No unstable tests were recorded. However, the characterization of the tests (from single-phase to two-phase flow conditions) is satisfying and quite consistent with what is reported in the literature. Test BLL5.3, which shows a DR of about 0.85, should be very close to unstable conditions.

The test matrix allowed the variation of the RPV water level. However, the actual influence of this parameter on the flow behavior cannot be clearly assessed. Comparing tests B5.3 and BL5.3, it seems that a lower level would have a stabilizing effect.

3. ATHLET SIMULATIONS

3.1. Thermal-hydraulic model

The thermal-hydraulic code ATHLET [4], which has been developed by GRS (Gesellschaft für Anlagen- und Reaktorsicherheit mbH), was used for the calculation of selected experiments from the PANDA test matrix (see Section 2). The ATHLET input dataset models all main parts of the PANDA configuration used for the NACUSP natural circulation experiments (Figure 7).

The ATHLET model consists of the lower plenum (P1-LP-1), the core section (P1-CORE) with 115 electrical heater elements, the riser (P1-RIS1), upper plenum (P1-UP-1), downcomer (P1-DC), and the upper part of the RPV (P1-RIS2). The isolation condenser (IC) is not modeled. Therefore, a bypass (CIRC-ENT, CIRC-EIN) is connected to the upper part of the RPV and to the downcomer, modeling the IC-feed and drain lines by fills with constant mass flows and given enthalpy (only drain line). In all control volumes, the 5-equation model (separate conservation equations for liquid and vapour mass and energy, mixture momentum equation) and the full-range drift-flux model of ATHLET are used. A valve at the lower end of the downcomer (VLV, see Figure 7) is used to model different k-factors of the core inlet. The cross-section of this valve can be changed to adjust the core inlet flow resistance.

3.2. Steady state calculation

At first, a steady state calculation with constant boundary conditions was performed for each PANDA test. With help of these calculations, the pressure losses, heat losses, the RPV

Figure 7: Schematic representation (a) and ATHLET model (b) of PANDA test facility.

water level, and the temperature distribution were adjusted. The steady state calculation starts with zero power. After a few seconds, the core power is switched on and the power is increased with time. For the calculations, it was not possible to specify a certain k-factor for the core inlet, because the form-loss coefficients are changed within the algorithm of the ATHLET code during the steady state calculation. Therefore, a valve at the lower end of the downcomer with changing cross-section was used to adjust the core-inlet flow resistance. The cross-section of this valve was reduced to a value, which leads to DC velocities corresponding to the measured data. The steady state calculation is stopped if stable conditions are reached. As an example, the results of the steady state calculation for Test B 8.3 and the corresponding experimental data are shown in Figure 8.

3.3. Transient calculations

From the PANDA test matrix, 5 experiments were selected for the ATHLET simulations. Table 3 shows the initial conditions calculated with ATHLET in comparison to the measured data. For the experiments the data were recorded over a period of 5 hours. The experimental data in Table 3 represent average values. The initial conditions for the ATHLET simulations correspond to the end of the steady state calculations.

Each transient calculation starts from a steady state calculation as described in Section 3.2. To simulate the system behavior with respect to natural circulation stability a short disturbance of the drain line mass flow was used in order to stimulate oscillations in the loop (see Figure 9). The response of the system can be seen in the calculated riser mass flow.

As in the experiments, the DC velocities were used to calculate the spectrum and the decay ratio. Figure 10 shows the downcomer velocity, the spectrum, and the autocorrelation function for two ATHLET calculations (Test B 8.3 and Test B 3.3). Both calculations predict stable behavior and no limit-cycle oscillations occur. The disturbance of the drain line mass flow leads to flow oscillations with decreasing

Figure 8: Core power (MW.RP.7), system pressure (MP.RP.1), RPV level (ML.RP.1), and downcomer velocity (MVE.DC.1) for test B 8.3: experiment and ATHLET steady state calculation.

Figure 9: Mass flow in IC-feed and drain lines (CIRC-EIN, CIRC-ENT) and riser mass flow (P1-RIS1).

Figure 10: DC velocity, spectrum, and autocorrelation function for the ATHLET calculations of tests B 8.3 and B 3.3.

amplitude. In both cases, the oscillations were damped due to the higher core-inlet loss coefficient ($k = 30$) used in the B-series experiments. Although the oscillations were damped in the ATHLET simulations, an oscillation period and also a decay ratio can be calculated from the DC mass flow. Table 4 gives a comparison between measured and calculated period and decay ratio for the selected PANDA tests.

The experiments and also the ATHLET calculations show that the oscillation period decreases with increasing DC velocity. This behavior is illustrated in Figure 11. The results of the ATHLET calculations show a good agreement with the experimental data.

To demonstrate the ability of ATHLET to calculate an unstable behavior with the PANDA model, an additional calculation with a core-inlet loss coefficient of $k = 7$, a higher core power, and a lower RPV level was performed. The initial conditions for this calculation are given in Table 3. The results of the simulation show an unstable behavior with

Figure 11: Oscillation period as a function of the DC velocity for the PANDA tests and the ATHLET simulations.

strong oscillations caused by flashing in the riser section. Figure 12 shows the calculated DC mass flow, the spectrum, and the autocorrelation function. The oscillations have a period of 62 seconds with a decay ratio >0.9.

In Figure 13, the calculated temperature distribution over the height of the PANDA model is illustrated for t = 5000 seconds. At this time, the dynamic behavior becomes unstable. The subcooling in the core is high and no void production takes place. Only above the middle of the riser section, the temperature reaches saturation conditions and flashing-induced oscillations occur.

4. CONCLUSIONS

In the PANDA facility, 25 tests have been carried out in order to investigate natural circulation flow behavior and stability, under low-pressure/low-power conditions. Some parameters influencing the natural circulation flow were varied, such as the RPV power and pressure, the core-inlet hydraulic resistance, and also the water level in the RPV.

An analysis of the data from three ultrasonic sensors installed at three circumferential locations in the downcomer was presented. The power spectra show, in a few tests, a major and unique resonance peak. The period of oscillation of the flow rate seems to be of the order of twice the transit time of the fluid in the riser. Analyses of the time series show that these cases are stable. The tests for which more flat spectra were obtained show a behavior that is even more stable.

A phenomenological classification has been applied according to three circulation modes that could be assumed: single-phase circulation (with possibly flashing above the riser), two-phase circulation with flashing in the upper half of the riser, or two-phase flow along the complete riser length. The new experimental data about the behavior of the natural circulation flow under low-pressure conditions represent a valuable extension of the available database,

(a)

(b)

ATHLET additional calculation	
Power	1308 kW
RPV pressure	4.9 bar
RPV level	11.43 m
DC velocity	0.6 m/s
Oscillation period	61 s
Decay ratio	> 0.9

(c)

Figure 12: DC velocity, spectrum, and autocorrelation function for the additional ATHLET calculation with higher core power and lower RPV level.

Figure 13: Temperature distribution over the height of the PANDA model (additional ATHLET calculation).

notably because the height of the riser in PANDA is approximately preserved with respect to a real reactor. The data have been used for assessing the capabilities and limitations of thermal-hydraulics codes in predicting natural circulation characteristics at low pressure/low power.

The ATHLET simulations show stable behavior for the selected PANDA tests. In all simulations, the oscillations were damped and no limit-cycle oscillations occur. The calculated periods and decay ratios show a good agreement with the experimental results. With help of the additional calculation, it could be demonstrated that ATHLET can also simulate an unstable behavior. In this case, strong oscillations occur, caused by flashing in the riser section.

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Research Article

Flow Stagnation under Single and Two-Phase Natural Circulation Conditions in the APEX-CE Test Facility

José N. Reyes Jr.

Department of Nuclear Engineering, The College of Engineering, Radiation Center, Oregon State University, Corvallis, OR 97331-4501, USA

Correspondence should be addressed to José N. Reyes Jr., jose.reyes@oregonstate.edu

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Natural circulation experiments were conducted at Oregon State University using the advanced plant experiment (APEX-CE) integral system test facility as configured to simulate a typical 2×4 Combustion Engineering nuclear steam supply system. This paper describes the mechanisms by which natural circulation flow was interrupted under single-phase and two-phase natural circulation conditions in APEX-CE.

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1. INTRODUCTION

Natural circulation is an important means of cooling the nuclear core in the event of a loss of primary system pumps. This paper describes the mechanisms that can interrupt natural circulation in loops transporting single- and two-phase fluids. The investigation of these mechanisms was conducted in the APEX-CE test facility, at Oregon State University [1–7]. A schematic of the test facility is shown in Figure 1. APEX-CE was configured to model a 2×4 loop Combustion Engineering PWR. It included the reactor vessel with an electrically heated rod bundle, a pressurizer, two inverted U-tube steam generators, four cold legs and reactor coolant pumps, two hot legs, and a safety injection system. The facility had a length scale ratio of approximately 1 : 4 and a volume ratio of 1 : 274. It was operated at decay powers ranging down from 6%. Therefore, the tests were conducted after reactor scram with the reactor coolant pumps tripped in a natural circulation mode of operation.

The motivation for the studies in APEX-CE was an issue known as pressurized thermal shock (PTS). In the event of an emergency that results in a significant loss in system pressure or fluid inventory, cold borated water is typically injected into the primary system via the cold legs. If the flow rate in the primary loop is significant, the cold injected fluid will thoroughly mix with the hot water in the primary loop. However, at very low flow rates, the cold injected fluid will stratify in the loops and form cold plumes in the downcomer. Should a pre-existing flaw in the vessel wall or welds exist at a location experiencing prolonged contact with a cold plume, while at high pressure, there is a potential for the flaw to grow into a "through-wall" crack. Sections 2 and 3 describe mechanisms leading to a loss of primary loop flow.

2. SINGLE-PHASE NATURAL CIRCULATION STAGNATION MECHANISMS

This section describes the mechanisms that interrupt single-phase natural circulation. Figure 2 presents a simple sketch describing the single-phase fluid natural circulation flow paths. Each loop in the Nuclear Steam Supply System shown in Figure 2 contains a hot leg, a steam generator, a loop seal (i.e., a cross-over leg), a pump, and a cold leg. The flow areas will vary around the loop. Under certain conditions, involving a reactor scram, the reactor coolant pumps in the plant may trip causing a coast down in the loop flow. Subsequent to a reactor scram, reactor decay power continues to heat the fluid in the vessel, while the steam generators continue removing heat at an elevation well above the core. The density difference and elevation difference produce a buoyancy force that drives the fluid around the loop. This buoyancy driven flow is known as natural circulation. The natural circulation flow rate is limited by the friction and form losses in the loop piping and components.

Figure 1: Schematic of the APEX-CE test facility.

The dominant pressure drops in the loop occur in the core and in the steam generator as a result of form losses, usually expressed in terms of loss coefficients, K. As shown Figure 2, the primary loop is divided into: a hot fluid side having an average temperature T_H and a cold fluid side having an average temperature T_C.

2.1. Loss of heat sink (steam generator reverse heat transfer)

One mechanism for losing single-phase natural circulation flow is a loss of heat sink. This can occur as a result of a loss of main and auxiliary feedwater supplies. This could also occur as a result of a main steam line break (MSLB) in a single steam generator in a multiloop plant. In the event of a MSLB, the operators isolate the feedwater to the steam generators and close the main steam isolation valves. The affected steam generator, however, will continue to vent steam and depressurize. The blowdown of a steam generator may result in a rapid cooling of the primary system fluid. As a result, the primary loop fluid temperatures may drop below the secondary side temperatures of the "unaffected" steam generators. The result is a loss of heat sink in the loops not experiencing the steam line break. Figure 3 shows that stagnation occurs in cold legs no.1 and no.3, connected to the unaffected Steam Generator no.1 for this test. Figure 4 shows the flow rates for cold leg no.1 and no.3. When primary side temperature exceeds the secondary side temperature, natural circulation flow is restored.

Figure 2: Control volume for single-phase natural circulation in a two-loop PWR.

2.2. Negatively buoyant regions in loop (loop seal cooling)

Another mechanism that can interrupt single-phase natural circulation flow is loop seal cooling. The piping that connects the steam generator lower channel head to the reactor coolant pump is known as the cross-over leg or the reactor pump loop seal as shown in Figure 2. Overcooling transients

Figure 3: Illustration of steam generator reverse heat transfer during a main steam line break simulation and recovery (OSU-CE-0012).

Figure 4: Illustration of loss of natural circulation flow during a main steam line break simulation and recovery (OSU-CE-0012).

Figure 5: Flow visualization of injected coolant mixing with fluid in a transparent loop seal.

Figure 6: Asymmetric loop seal cooling (OSU-CE-0008).

such as main steam line breaks, result in a primary side cooldown. If the primary side pressure drops below the safety injection actuation setpoint, cold-borated water will be injected into the loop. This water is typically injected into the cold legs of a PWR between the reactor coolant pump and the reactor vessel. Figure 5 is a picture of the transparent separate effects test loop at Oregon State University used to visualize fluid mixing in a side-injection cold leg.

The dense injected fluid simulated, using fluorescent salt-water, falls to the bottom of the cold leg where it spreads out toward the vessel and the reactor coolant pump loop seal. Countercurrent flow is established with hot water at the top of the cold leg pipe flowing toward the injection point. The dense-injected water mixes with the less dense water in the loop seal creating a negatively buoyant region in the loop, effectively increasing the resistance to flow in that loop. For multiloop plants, the flow is preferentially diverted to the adjacent cold leg through the SG lower provided the same condition does not also exist there. Figure 6 shows the asymmetric cooling of two loop seals attached to a single steam generator in APEX-CE. Loop seal no.4 cools faster than loop seal no.2 because the dense fluid back flows over the lip of RCP no.4 earlier during this particular transient. Figure 7 shows that loop flow in cold leg no.4 stagnates earlier than the flow in cold leg no.2.

3. TWO-PHASE NATURAL CIRCULATION STAGNATION MECHANISMS

During a small break loss of coolant accident (SBLOCA) in a PWR, steam generator tube draining will result in a gradual decrease in primary side natural circulation flow until it transitions to a boiling-condensing mode of operation. Figure 8 shows the results of a test conducted at OSU to investigate this phenomenon. The test, OSU-CE-0002, was a stepped reduction in inventory test. In essence, it is a quasisteady SBLOCA. It was conducted at a constant core

Figure 7: Stagnation of a primary loop due to loop seal cooling (OSU-CE-0008).

Figure 8: Cold leg flow rates as a function of primary side inventory during a stepped-inventory reduction test (OSU-CE-0002).

Figure 9: Cold leg flow rates as a function of primary side inventory during a stepped-inventory reduction test (OSU-CE-0002) and semiscale Mod 2A data [6].

Figure 10: Liquid levels in the longest and shortest tubes of steam generator no.2 during SLOCA test (OSU-CE-0008).

power of 275 kW and initiated from steady-state single-phase natural circulation conditions. A break valve on the reactor vessel was opened to remove primary fluid in stepped intervals. After a short period, the break valve was closed and the loop was allowed to reach a new quasisteady state flow rate. The cold leg flow rates were measured at each interval. These tests were similar to tests performed at the Semiscale test facility at the Idaho National Engineering Laboratory as shown in Figure 9 [6].

As liquid mass is removed from the system, the loop void fraction increases. This resulted in a rise in the loop flow rates above those observed for single-phase natural circulation as shown in Figure 8. At approximately 70% inventory in APEX-CE, the flow reaches a maximum value. This corresponds to the maximum two-phase buoyancy driving head for the test. Eventually, the steam generator tubes begin to drain causing a decrease in flow rate because the distance between the core and steam generator thermal centers has decreased and interruption of flow in the longest tubes.

Figure 10 illustrates the significant difference in draining time for the longest U-tubes at the top of the bundle and the shortest U-tubes at the bottom of the bundle. The long tubes drained much earlier than short tubes. However, some primary loop natural circulation continued until the short tubes drained. These results suggest that to obtain faithful simulations of the steam generator tube draining requires modeling multiple steam generator tubes.

4. CONCLUSIONS

Experiments in the APEX-CE integral system test facility indicate that natural circulation under single-phase fluid condition can be interrupted as a result of a loss of heat sink during a main steam line break (i.e., reverse steam generator heat transfer), or as the result of the formation of negatively buoyant conditions in the loop seal. Under two-phase natural circulation conditions, loop stagnation arises as a result of steam generator tube voiding. The loop flow transitions from two-phase natural circulation to a boiling-condensing mode of operation. The APEX-CE tests indicate that the long steam generator tubes drain before the short tubes suggesting that computer code models include multiple steam generator tubes. The formation of negatively buoyant conditions in plants with loop seals can also result in asymmetric loop stagnation under two-phase natural circulation flow conditions.

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Research Article

Use of the Natural Circulation Flow Map for Natural Circulation Systems Evaluation

M. Cherubini, W. Giannotti, D. Araneo, and F. D'Auria

Department of Mechanical, Nuclear and Production Engineering, University of Pisa, 56126 Pisa, Italy

Correspondence should be addressed to M. Cherubini, m.cherubini@ing.unipi.it

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The aim of this paper is to collect and resume the work done to build and develop, at the University of Pisa, an engineering tool related to the natural circulation. After a brief description of the different loop flow regimes in single phase and two phase, the derivation of a suitable tool to judge the NC performance in a generic system is presented. Finally, an extensive comparison among the NC performance of various nuclear power plants having different design is done to show a practical application of the NC flow map.

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1. INTRODUCTION

The natural circulation (NC) is an important mechanism in several industrial systems and the knowledge of its behaviour is of interest to nuclear reactor design, operation, and safety. In the nuclear technology, this is especially true for new concepts that largely exploit the gravity forces for the heat removal capability. The NC takes place due to the presence of a heat source and of a heat sink constituted in a nuclear power plant (NPP) by the core and the steam generators, respectively. In a gravity environment, with the core located at a lower elevation than the steam generators, driving forces occur such that to generate suitable flow rate for removing nuclear fission power. While the NC core power removal capability was only exploited for accident situations basically to demonstrate the inherent safety features of the plants, recently within the passive reactor concept the NC performance assumes major importance having a large impact on the design phase.

The NC behaviour has been (and it is still now) object of several experiments aimed at addressing issues like scaling or characterization test in a test rig. In this view, a quite large database has been created collecting the experimental measurements from various facilities. The NC scenarios occurring for different values of the primary system mass inventory were considered (reference is made to both single-phase and two-phase natural circulation) by gathering and analyzing data from the following PWR simulators: Semiscale, Spes, Lobi, Bethsy, Pkl, and Lstf [1].

In order to evaluate the natural circulation performance (NCP) of the mentioned facilities, significant information comes from the analysis of the trend of the core inlet mass flow rate and the primary loop mass inventory. The flow rate and the residual masses have been normalised taking into account the volume of each facility and the corresponding power level (typically ranging between 1 and 5% of the nominal core power) utilized in the selected experiment. Four main flow patterns were characterized depending upon the value of the mass inventory of the primary loop.

2. THE NATURAL CIRCULATION FLOW REGIMES

2.1. *Single-phase NC (SPNC)*

SPNC regime implies no void occurrence in the upper plenum of the system. Therefore, coolant at the core outlet will be subcooled up to nearly saturated. Core flow rate is derived from the balance between driving and resistant forces. Driving forces are the result of fluid density differences occurring between {descending side of U-tubes and vessel downcomer} and {core and ascending side of U-tubes}. Resistant forces are due to irreversible friction pressure drops along the entire loop. Resulting fluid velocities are sufficient for removing core power in (subcooled) nucleate boiling or

Figure 1: Characterization of natural circulation flow regimes based on experimental data and system code calculations.

forced convection heat transfer regimes: no film boiling condition is experienced in the core. It may be noted that the secondary side of SG is also a natural circulation system working in two-phase conditions.

SPNC may occur at any primary system pressure, consistently with SG pressure. However, typical primary system pressures range between 8 and 16 MPa with secondary pressure close to the nominal operating condition. It is expected from the NPP design that SPNC, provided the availability of SG cooling, is capable to remove the nuclear decay heat from the core. Experimental database, including NPP tests, confirms this capability.

2.2. Stable two-phase NC (TPNC)

TPNC regime occurs as a consequence of coolant loss from the primary system. Owing to this, both driving and resistant forces increase when decreasing mass inventory of primary system. Assigned the typical geometrical layout of PWR, the former effect, that is, increase of driving forces, is prevalent at small decreases of mass inventories. The opposite occurs for larger decreases of mass inventories. The net result is a "peak" in core mass flow rate versus primary system inventory (when primary mass flow rate decreases) as can be observed in Figure 1. Forced convection, subcooled, and saturated heat transfer regimes occur in the core. Condensation occurs inside the U-tubes of SG. The average core void fraction is typically less than 30%, whereas at the outlet values around 50% can be reached without occurrence of thermal crisis in the considered pressure range.

2.3. Siphon condensation NC (SCNC)

The decreasing of NC driving forces, the small temperature difference across U-tubes of the steam generators, and the occurrence of the countercurrent flow limiting phenomenon (CCFL) at the entrance of U-tubes (e.g., see [2]) are at the origin of wide system oscillations of core inlet flow rate. This phenomenon has been investigated in [3, 4] and based on a natural circulation experiment performed in Lobi facility in [5]. However, evidence of the siphon condensation has been found also in other facilities.

At mass inventories of the primary system around 70% of the nominal value, the efficiency of the condensation heat transfer across U-tubes causes the release of almost all core thermal power in the ascending side of U-tubes. Liquid level builds up and is prevented to drain down by the steam-liquid mixture velocity at the tube entrance, that is, the CCFL condition occurs. Therefore, liquid level rises in the U-tubes till reaching the top. During this period, typical duration of the order of 10 seconds, the flow rate at the core inlet is close to zero hence the core boiloff occurs. Once the liquid level reaches the upper bend of U-tubes, the siphon effect occurs and causes the emptying of the ascending side of U-tubes and the reestablishment of core inlet flow rate. A new cycle starts. The phenomenon is made more complex by the interaction of the several thousands of U-tubes that constitute a SG tube bundle. Different groups of tubes may stay at a different stage of the oscillation at the same time, also causing flow reversal in the tube bundle. Suitable core cooling still can be achieved in these conditions.

2.4. Reflux condensation (RCNC)

At "low" mass inventories of primary coolant and/or at low core power, the steam velocity in the upper part of the system including hot legs and steam generator entrance is low. Weak interactions occur at the steam-liquid interface that are not enough to cause CCFL. In these conditions, the liquid that is condensed or entrained in the ascending side of the U-tubes may flow back to the hot leg and to the core. Stratified countercurrent steam and liquid flow simultaneously in the hot legs. The mass flow rate at the core inlet is close to zero, although a "minor" natural circulation path may establish between core, and downcomer inside the vessel. However, upward two-phase mixture and downward liquid flows occur at the core outlet. Core thermal power can be removed by boiloff in the saturated nucleate boiling heat transfer regime.

2.5. Dryout occurrence

The terms "dryout occurrence" appear in the right part of Figure 1, when primary system mass inventory is roughly lower than 40% of the nominal value. Dryout is caused by the combination of low flow and high void fraction. As a consequence, film boiling heat transfer regime is experienced with low coefficient for heat transfer. Rod surface temperature increases in various zones of the core and the overall process of thermal power transfer from fuel rods to the fluid may become unstable. The system operation in these conditions

Table 1: Relevant hardware characteristic of the PWR simulators considered for the NCFM creation.

Item	1 Semiscale Mod2A	2 Lobi Mod2	3 Spes	4 PKL-III	5 Bethsy	6 Lstf
Reference reactor and power (MWt)	W-PWR 3411	KWU-PWR 3900	W-PWR 2775	KWU-PWR 3900	FRA-PWR 2775	W-PWR 3423
No. of fuel rod simulators	25	64	97	340	428	1064
No. of U-tubes per SG	2/6	8/24	13/13/13	30/30/60	34/34/34	141/141
Internal diameter of U-tubes (mm)	19.7	19.6	15.4	10.0	19.7	19.6
Actual Kv	1/1957	1/589	1/611	1/159	1/132	1/48

is not acceptable from a technological point of view. It can be noted that the temperature excursion is strongly affected by primary system pressure and thermal power levels: the linear rod power plays a role in these conditions. At primary system pressure around 15 MPa (nominal operation for PWR), "post-dryout" surface temperature jumps may be as low as a few tens of Kelvin, tolerable for the mechanical resistance of the rod-clad material.

All the regimes cited above are summarized in Figure 1 which reports both experimental and calculated data. On the vertical axis, the percentage of the nominal core power (P) is reported, while the residual mass inventory in the primary circuit (RM) is reported on the horizontal axis.

3. THE NATURAL CIRCULATION FLOW MAP

The database gathered from ten experiments performed in the six integral test facilities (ITFs) listed in Table 1 has been used ([6, 7]) to establish a natural circulation flow map.

In all the considered ITF, NC experiments with similar modalities have been performed, and the NC regimes discussed in the previous chapter are experienced. The linear power of the fuel rod simulators, the fraction of nominal core power, and the primary system pressure constitute the main differences for the boundary conditions of the considered experiments. In relation to the primary side pressure, PKL experiments have been performed at a pressure value roughly half of the value adopted in the other facilities. The range of design and operational parameters of the ITF (e.g., pipe diameter, system volume, number of the steam generators, heat losses to the environment), not explicitly discussed here, and the identified differences are assumed to produce an envelope for any expected NC situation in a typical PWR when decay heat removal is concerned.

Measured values of core inlet flow rate (G, kg/s), core power (P, MW), primary system fluid mass inventory (RM, kg), and net volume of the primary system (V = const., m^3) have been used for setting up the natural circulation flow map (NCFM). The diagram G/P versus RM/V has been preferred for the NCFM over other possible choices including nondimensional quantities.

Figure 2: Natural circulation system behaviour measured in ten experiments performed in six PWR simulators.

The experimental database from ITF (six ITFs, ten experiments) and the envelope of the curves are given in Figures 2 and 3, respectively. The envelope in Figure 3 is assumed to constitute the NCFM of PWR at core decay power.

A first demonstration of the use of the NCFM has been done in [8], where seven commercial NPP systems and three ITFs, not used for setting up the database presented in Figure 2, have been analyzed. Main characteristics of the NPP and ITF can be drawn from Tables 2 and 3, respectively. The considered NPP include U-tube, once through and horizontal SG design. The ITFs are Pactel and RD14M (see Table 3) experimental simulators of WWER-440 and CANDU NPP, respectively. Their geometric layout is different from those

Table 2: Relevant characteristics of NPP considered for the application of the NCFM.

	PWR	PWR	PWR	WWER-1000	EPR	AP-600	EP-1000
Nominal power (MWt)	1877	870	2733	3000	4250	1972	2958
Primary system volume (m^3)	167	150	330	359	459	211	339
SG type	U-tubes	U-tubes	Once-through	Horizontal	U-tubes	U-tubes	U-tubes
No. of loops	2	4	2	4	4	2	3
No. of pumps	2	4	4	4	4	4	6
Nominal mass inventory (Mg)	108	108	224	240	307	145	227
Nominal core flow (kg/s)	9037	3150	17138	15281	20713	8264	14507
Pressurizer and SG pressure (MPa)	15.6 6.	14.0 3.1	15.0 6.4	15.7 6.3	15.5 7.2	15.5 5.5	15.8 6.4

Table 3: PACTEL and RD14M.

	Pactel (original)	Pactel (with CMT)°	RD14M
Reference reactor and power (MWt)	WWER-440 1375	WWER-440 1375	CANDU 1800
No. of rods	144	144	70
No. of SG	3	3	2
SG type	Horizontal	Horizontal	U-tubes
Actual Kv+	1/433	1/462	1/378

°CMT = core makeup tank.
+Definition introduced for database in Table 1.

Figure 3: Natural circulation flow map achieved from the envelope of measured curves in PWR simulator.

of a PWR. In the case of WWER-440, six loops equipped with HTSG are connected to the vessel, though only three are simulated in Pactel. Horizontal core configuration characterizes the CANDU design, that is, equipped with UTSG.

Comparing the calculated data with the NCFM, authors concluded that

(i) PWR equipped by OTSG have poor natural circulation performance;

(ii) NPP designed around passive system concept showed a quite extend SPNC even at low mass inventory;

(iii) Russian design reactors equipped by horizontal SG are also suitable for NC mode;

(iv) RD-14m database was not fully qualified (e.g., the documentation was not so exhaustive), this brought to a large deviation from the map.

The NCFM was used to characterize the behaviour of the CNA-I PHWR NPP in NC flow conditions, in a reduced primary mass inventory scenario [6]. The simulations have been performed using three different nodalizations of increasing detail. The coarser one (SET I) was used as a basic set just to represent a plant layout similar to a PWR (see Figure 4). This permitted to verify that the trends known for most ITFs working in similar situations give an envelope to the CNA-I behaviour, considering appropriate trips of some of its safety systems.

4. RECENT USE OF THE NC FLOW MAP

4.1. Application to the PSB-VVER facility

As already showed by the previous examples (other can be got from, e.g., [8, 9]), the NCFM constitutes an advantageous tool to depict the NC capability in removing the core power. Starting from this point more applications have been done that cover practically all the NPP types.

In Figure 5 (taken from [10]) it can be seen the use of the NCFM for a VVER-1000 simulator whose scale factor is 1 : 300. The experiment has been carried out in the frame of an OECD project in which the core power and the SG pressure are maintained constant, while the mass is stepwise

Figure 4: Natural circulation map for Atucha-I at different core power level and in case of SBLOCA.

Figure 5: Natural circulation map for the PSB-VVER experimental and calculated data.

drained. Both measured (in blue) and calculated date (in violet) are reported. The experimental data goes out from the map because the flow rate measurement systems were not qualified in two-phase flow giving a wrong indication. However, the calculation confirms on one hand the suitability of the NCFM and on the other hand the good NC performance of the VVER reactor type.

4.2. Revisiting the RD-14m facility

In Section 3, the RD14m data were considered but achieving results far from what expected by the NCFM. Recently, a calculation has been repeated fixing constant the core power and the SG pressure, while the primary side mass is stepwise reduced, following a typical way to perform such kind of NC test. The nodalization had been qualified against LBLOCA test (B-9401) carried out in the framework of an IAEA project [11]. Analyzing the code results (summarized in Figures 6 and 7), two main conclusions can be drawn.

(i) The CANDU facility seems not so suitable for NC status (Figure 6). However it should be noted that the NCFM reasonably resumes the SG NC, it does not consider any other mechanism simply because they are outside of its definition.

(ii) Figure 7 shows that in the CANDU installation other type of NC are present. The channel to channel mode seems relevant from the core power removal point of view. This behaviour explains why even at value of RM/V greater than 500 kg/m^3, where the NCFM predicts low flow condition (hence degraded capability in removing the core power) the dryout is not experienced.

Regarding the latter point (i.e., the channel to channel NC mode), Figure 7 reports the ratio channel mass flow rate over power versus time of two different horizontal channels. It can be seen that once one core channel has positive mass flow rate (i.e., the water follows the normal flow path entering from the inlet and going out from the outlet of the channel), the other is cooled by water circulating in the opposite path. This means that only a part of the total core flow rate (sum of the core channels mass flow rate) reaches the SG to be cooled.

4.3. Consideration of BWR data

The last use of the NCFM regards the consideration of boiling water reactor type coming from plant measurement (BWR) and calculation results (RBMK).

Figure 8 reports the typical power-flow map of a BWR in which the NC curve is marked. The maximum removable power in NC mode is roughly 40% with a core flow rate of 25%.

In Figure 9 the BWR data are reported into the NCFM (green dots and green square). Four points are present in the map: full power condition (green square) at full pump speed, 25%, 4%, and 3% of the nominal power (green dots). It can be seen that the points are practically aligned along a line that marks the RM/V of the considered plant confirming the suitability of the BWR to work in NC mode. Values of G/P comparable to what measured in TP-flow conditions in PWR simulators are experienced in BWR but with a RM/V ratio almost halved. The degradation of the performance in core removable power typical of PWR is not present in BWR that in turns can be operated at 25% of its nominal power.

Again in Figure 9 it can be seen also the RBMK data. Those calculated values (red and blue stars are obtained by

Figure 6: Natural circulation map for the RD-14m calculated data.

Figure 7: Channel-to-channel natural circulation mode for the RD-14m calculated data.

Figure 8: Typical BWR power-flow map.

Figure 9: BWR and RBMK data and comparison with the NCFM.

the use of a qualified nodalization developed and tested during an EC funded project [12]. The following consideration come out when calculated data are put inside the NCFM and compared with BWR values.

(i) The RBMK data stay almost aligned at various power levels as occur in the BWR.

(ii) The full power point of both boiling water reactors (western and eastern project type, resp.) has the (practically) same G/P ratio, but differs by a factor of two in the RM/V ratio. This stresses the diverse project type (vessel type versus channel type reactor), the different nominal point of work, and the impact on the primary side (PS) mass of the two steam drums used in the RBMK as separator.

(iii) The G/P ratio remains comparable between the two reactors at various power levels up to 4%.

(iv) Deviation from the consideration of the item above is experienced, when the bypass between the pressure header and the suction header is closed (blue star in Figure 9) and when the power level is 20% of the nominal core power. The former case has an important negative impact on the NC performance of the RBMK; the latter case seems to represent the last possible NC operational point for the RBMK, lower than the BWR corresponding point.

(v) When power level around 3% is considered, the two reactors behave in a different way: it can be seen a lower mass flow rate in the RBMK enhancing a better performance of the BWR with a low void fraction.

5. CONCLUSIONS

A system scaling study was the precursor of this work in which the main objective was the setup of an engineering tool aimed at evaluating the NC performance of an NPP. The NCFM was created gathering experimental data from six PWR simulators.

Recently, the use of the NCFM has been extended to practically all types of reactor: VVER-1000 by the use of experimental data available after an NC experiment carried out at the PSB-VVER test rig; CANDU revisiting the RD-14m data after its nodalization qualification; BWR and RBMK comparing measured and calculated data, respectively.

Once more, the NCFM results in a very helpful tool to judge the NC performance of different reactor types. It should be noted that the NCFM is suitable to catch the SG NC type hence not able to deal with other NC type, such as the channel-to-channel circulation experienced in the RD-14m. Considering the characteristic and the way the NCFM has been derived, other applications may regard support on scaling analysis, design and/or optimization of NC system, and validation of computational tools.

As mentioned above, this engineering tool has been directly derived from the analysis of experimental data. However, a theoretical and generalized relationship between the system inventory and the flow per unit power could be the objective of a future research work.

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Review Article

Flow Instabilities in Boiling Two-Phase Natural Circulation Systems: A Review

A. K. Nayak and P. K. Vijayan

Reactor Engineering Division, Bhabha Atomic Research Centre, Trombay, Mumbai 400085, India

Correspondence should be addressed to A. K. Nayak, arunths@barc.gov.in

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Several decades have been spent on the study of flow instabilities in boiling two-phase natural circulation systems. It is felt to have a review and summarize the state-of-the-art research carried out in this area, which would be quite useful to the design and safety of current and future light water reactors with natural circulation core cooling. With that purpose, a review of flow instabilities in boiling natural circulation systems has been carried out. An attempt has been made to classify the instabilities occurring in natural circulation systems similar to that in forced convection boiling systems. The mechanism of instabilities occurring in two-phase natural circulation systems have been explained based on these classifications. The characteristics of different instabilities as well as the effects of different operating and geometric parameters on them have been reviewed.

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1. INTRODUCTION

Natural circulation (NC) systems are susceptible to several kinds of instabilities. Although instabilities are common to both forced and natural circulation systems, the latter is inherently more unstable than forced circulation systems due to more nonlinearity of the NC process and its low driving force. Because of this, any disturbance in the driving force affects the flow which in turn influences the driving force leading to an oscillatory behavior even in cases where eventually a steady state is expected. In other words, a regenerative feedback is inherent in the mechanism causing NC flow due to the strong coupling between the flow and the driving force. Even among two-phase systems, the NC systems are more unstable than forced circulation systems due to the above reasons.

Before we proceed further, let us define the term "instability." Following a perturbation, if the system returns back to the original steady state, then the system is considered to be stable. If on the other hand, the system continues to oscillate with the same amplitude, then the system is neutrally stable. If the system stabilizes to a new steady state or oscillates with increasing amplitude, then the system is considered as unstable. It may be noted that the amplitude of oscillations cannot go on increasing indefinitely even for unstable flow. Instead for almost all cases of instability, the amplitude is limited by nonlinearities of the system and limit cycle oscillations (which may be chaotic or periodic) are eventually established. The time series of the limit cycle oscillations may exhibit characteristics similar to the neutrally stable condition. Further, even in steady state case, especially for two-phase systems with slug flow, small amplitude oscillations are visible. Thus, for identification purposes especially during experiments, often it becomes necessary to quantify the amplitude of oscillations as a certain percentage of the steady state value. Amplitudes more than $\pm$ 10% of the mean value is often considered as an indication of instability. However, some authors recommend the use of $\pm$ 30% as the cutoff value [1].

Instability is undesirable as sustained flow oscillations may cause forced mechanical vibration of components. Further, premature CHF (critical heat flux) occurrence can be induced by flow oscillations as well as other undesirable secondary effects like power oscillations in BWRs. Instability can also disturb control systems and cause operational problems in nuclear reactors. Over the years, several kinds of instabilities have been observed in natural circulation systems excited by different mechanisms. Differences also exist in their transport mechanism, oscillatory mode, and analysis methods.In addition, effects of loop geometry

Figure 1: Typical stable, unstable, and neutrally stable behaviour for Ledinegg-type instability.

and secondary parameters also cause complications in the observed instabilities. Under the circumstances, it looks relevant to classify instabilities into various categories which will help in improving our understanding and hence control of these instabilities.

2. INSTABILITY CLASSIFICATION

Mathematically, the fundamental cause of all instabilities is due to the existence of competing multiple solutions so that the system is not able to settle down to anyone of them permanently. Instead, the system swings from one solution to the other. An essential characteristic of the unstable oscillating NC systems is that as it tries to settle down to one of the solutions, a self-generated feedback appears making another solution more attractive causing the system to swing toward it. Again, during the process of settling down on this solution, another feedback of opposite sign favoring the original solution is self-generated and the system swings back to it. The process repeats itself resulting in perpetual oscillatory behavior if the operating conditions are maintained constant. Although this is a general characteristic it hardly distinguishes the different types of instabilities found to occur in various systems. In general, instabilities can be classified according to various bases as follows:

(a) analysis method;

(b) propagation method;

(c) number of unstable zones;

(d) nature of the oscillations;

(e) loop geometry;

(f) disturbances or perturbations.

2.1. *Based on the analysis method (or governing equations used)*

In some cases, the occurrence of multiple solutions and the instability threshold itself can be predicted from the steady-state equations governing the process (pure or fundamental static instability). The Ledinegg-type instability is one such example occurring in boiling two-phase NC systems. The occurrence of this type of instability can be ascertained by investigating the steady-state behavior alone. The criterion for this type of instability is given by

$$\frac{\partial \Delta p_{\text{int}}}{\partial w} - \frac{\partial \Delta p_{d\nu}}{\partial w} \leq 0, \tag{1}$$

where Δp_{int} is the internal pressure loss in the system and $\Delta p_{d\nu}$ is the driving head due to buoyancy. The internal pressure loss of the system includes the losses due to friction, elevation, acceleration and local in the heated portion, the riser pipes and the steam drum, and all the losses except the elevation loss in the downcomers. The driving head is basically the gravitational head available from the steam drum to the bottom of the heated section. Figures 1(a) and 1(b) show an example of occurrence of Ledinegg-type instability at different powers [2] in a boiling two-phase NC

system. The instability is found to occur when the power is more than 285 MWth and less than 460 MWth if the operating pressure is 0.1 MPa and the subcooling is 30 K. When the power is in between the above specified range, the internal pressure loss curve intersects the driving buoyancy curve at three points (i.e., three operating points at a given power level) which makes the system unstable. Thus at 30 K subcooling, the system can have two threshold points of instability.

Like the Ledinegg instability, the flow pattern transition instability is another static instability caused by the excursion of flow due to differences in the pressure drop characteristics of different flow patterns. To analyze this type instability, it is required to predict the pressure drop characteristics of the system against the flow rate similar to the Ledinegg-type instability [3]. Figure 2 shows an example of the steady-state pressure drop characteristics of the system for analysis of flow pattern transition instability. The gravitational head, which depends on the density of the single-phase fluid, remains constant at a particular core inlet temperature. The different flow patterns in the vertical and horizontal portions of the riser pipes are shown in the two-phase region at the operating conditions. It can be observed from Figure 2 that there can be multiple steady-state flow rates (point at which the driving head intersects the internal loss curve) at this operating condition. The number of flow excursions is seen to be five, unlike that of the Ledinegg-type instability. The type of flow excursion in different flow regimes are observed to be as follows: there can be one flow excursion in the annular region itself due to reduction of pressure drop with reduction in quality as in the Ledinegg-type instability. The next flow excursion occurs due to rise in pressure drop when the flow pattern changes from annular to slug flow in the vertical portion of the riser pipes. The other flow excursion occurs when the flow pattern changes from the annular to dispersed bubbly flow in the horizontal portion of riser pipes due to reduction of pressure drop with flow rate. The last flow excursion occurs when the flow becomes single phase and the pressure drop increases with increase in flow rate. Thus, there can be five different flow rates for a particular operating condition of power and subcooling as indicated in Figure 2 by points A–E. The existence of multiple flow rates as a particular operating power and subcooling makes the system unstable. For example, if the system is initially operating at point C, any slight disturbance causing the flow to increase will shift the flow rate to point D and the to point E. Similarly, any slight disturbance causing the flow rate to decrease will shift the operating point to B and then to point A. Thus, the flow rate can jump from one value to the other even though the operating power and pressure are constant. This makes the system unstable.

However, there are many situations with multiple steady-state solutions where the threshold of instability cannot be predicted from the steady-state laws alone (or the predicted threshold is modified by other effects). In this case, feedback effects are important in predicting the threshold (compound static instability). Besides, many NCSs with only a unique steady-state solution can also become unstable during the approach to the steady state due to the appearance of

H: Horizontal riser pipe
V: Vertical riser pipe

Figure 2: Typical flow pattern transition instability in boiling natural circulation systems.

competing multiple solutions due to the inertia and feedback effects (pure dynamic instability). Neither the cause nor the threshold of instability of such systems can be predicted purely from the steady state equations alone. Instead, it requires the full transient governing equations to be considered for explaining the cause and predicting the threshold. In addition, in many oscillatory conditions, secondary phenomena get excited and they modify significantly the characteristics of the fundamental instability. In such cases, even the prediction of the instability threshold may require consideration of the secondary effect (compound dynamic instability). A typical case is the neutronic feedback responding to the void fluctuations resulting in both flow and power oscillations in a BWR. In this case, in addition to the equations governing the thermalhydraulics, the equations for the neutron kinetics and fuel thermal response also need to be considered.

Thus we find that the analysis to arrive at the instability threshold can be based on different sets of governing equations for different instabilities. Boure et al. [4] classified instabilities into four basic types as follows:

(a) pure static instability;

(b) compound static instability (it may be noted that Boure et al. [4] named this instability as compound relaxation instability);

(c) pure dynamic instability;

(d) compound dynamic instability.

2.2. Based on the propagation method

This classification is actually restricted to only the dynamic instabilities. According to Boure et al. [4], the mechanism of dynamic instability involves the propagation or transport of disturbances. In two-phase flow, the disturbances can be transported by two different kinds of waves: pressure (acoustic waves) and void (or density) waves. In any two-phase system, both types of waves are present, however, their

(a) Typical low-power and high-power unstable zones for two-phase NC flow

(b) Typical stability map for two-phase density wave instability [2]

Figure 3

velocities differ by one or two orders of magnitude allowing us to distinguish between the two.

2.2.1. Acoustic instability

Acoustic instability is considered to be caused by the resonance of pressure waves. Acoustic oscillations are also observed during blowdown experiments with pressurized hot-water systems possibly due to multiple wave reflections. Acoustic oscillations are characterized by high frequencies of the order of 10–100 Hz related to the pressure wave propagation time [4]. Acoustic oscillations have been observed in subcooled boiling, bulk boiling, and film boiling. The thermal response of the vapor film to passing pressure wave is suggested as a mechanism for the oscillations during film boiling. For example, when a compression (pressure wave consists of compression and rarefaction) wave passes, the vapor film is compressed enhancing its thermal conductance resulting in increased vapor generation. On the other hand when a rarefaction wave passes, the vapor film expands reducing its thermal conductance resulting in decreased vapor generation. The process repeats itself.

2.2.2. Density-wave instability (DWI)

A density-wave instability is the typical dynamic instability which may occur due to the multiple regenerative feedbacks between the flow rate, enthalpy, density, and pressure drop in the boiling system. The occurrence of the instability depends on the perturbed pressure drop in the two-phase and single-phase regions of the system and the propagation time delay of the void fraction or density in the system. Such an instability can occur at very low-power and at high-power conditions. This depends on the relative importance of the respective components of pressure drop such as gravity or frictional losses in the system. Fukuda and Kobori [5] have classified the density-wave instability as type I and type II for the low power and high-power instabilities, respectively. The mechanisms can be explained as follows [6].

Type I instability

For this type of instability to occur, the presence of a long riser plays an important role such as in a boiling two-phase natural circulation loop. Under low quality conditions, a slight change in quality due to any disturbance can cause a large change in void fraction and consequently in the driving head. Therefore, the flow can oscillate at such low-power conditions. But as the power increases, the flow quality increases where the slope of the void fraction versus quality reduces. This can suppress the fluctuation of the driving head for a small change in quality. Hence, the flow stabilises at higher power (Figures 3(a) and 3(b)).

Type II instability

Unlike the type I instability, the type II instability occurs at high-power conditions. This instability is driven by the interaction between the single and two-phase frictional component of pressure losses, mass flow, void formation, and propagation in the two-phase region. At high power, the flow quality or void fraction in the system is very large. Hence, the two-phase frictional pressure loss may be high owing to the smaller two-phase mixture density. Having a large void fraction will increase the void propagation time delay in the two-phase region of the system. Under these conditions, any small fluctuation in flow can cause a larger fluctuation of the two-phase frictional pressure loss due to fluctuation of density and flow, which propagates slowly in the two-phase region. On the other hand, the fluctuation of the pressure

drop in the single-phase region occurs due to fluctuation of flow alone since the fluctuation of the density is negligible. The pressure drop fluctuation in this region travels much faster due to incompressibility of single-phase region. If the two-phase pressure drop fluctuation is equal in magnitude but opposite in phase with that of the single-phase region, the fluctuation or oscillation is sustained in the system since there are no attenuating mechanisms. Divergent oscillations can occur depending on the magnitude of the pressure-loss fluctuation in the two-phase and single-phase regions and the propagation time delay.

Because of the importance of void fraction and its effect on the flow as explained above, this instability is sometimes referred to as flow-void feedback instability in two-phase systems. Since transportation time delays (related to the spacing between the light and heavy packets of fluid as explained above) are crucial to this instability, it is also known as "time-delay oscillations". Density-wave instability (DWI) or density-wave oscillations (DWO), first used by Stenning and Veziroğlu [7], is the most common term used for the above described phenomenon as it appears that a density wave with light and heavy fluid packets is traveling through the loop.

2.3. Based on the number of unstable zones

Fukuda and Kobori [5] gave a further classification of density-wave instability based on the number of unstable zones. Usually, there exists a low-power and a high-power unstable zone for density wave instability in forced as well as NC two-phase flows (Figure 3(a)). For the two-phase flow density-wave instability, the unstable region below the lower threshold occurs at a low power and hence at low quality and is named as type I instability by Fukuda and Kobori [5]. Similarly, the unstable region beyond the upper threshold occurs at a high power and hence at high qualities and is named as type II instability. However, in certain cases depending on the geometry and operating conditions, islands of instability have been observed to occur [8–10]. In these cases, more than two zones of instability were observed. Chen et al. [11] also observed hysteresis in a two-phase loop. As an unstable single-phase system progresses through single-phase NC to boiling inception and then to fully-developed two-phase NC with power change, it can encounter several unstable zones. In view of the existence of more than two unstable zones, this method of classification could be confusing at times.

2.4. Based on the nature of the oscillations

All instabilities eventually lead to some kind of oscillations. The oscillations can be labeled as flow excursions, pressure drop oscillations, power oscillations, temperature excursions or thermal oscillations, and so on. Besides, classifications based on the oscillatory characteristics are sometimes reported for dynamic instability. For example, based on the periodicity sometimes oscillations are characterized as periodic and chaotic. Based on the oscillatory mode, the oscillations are characterized as fundamental mode or higher harmonic modes [12]. In boiling NC systems with multiple parallel channels, inphase and out of phase modes are present depending on the geometry of the channels and heating conditions. Sometimes, dual oscillations also are possible. In natural circulation loops, flow direction can also change during oscillations. Based on the direction of flow, the oscillations can be characterized as unidirectional, bidirectional, or it can switch between the two. Such switching is often accompanied by period doubling, tripling, or n-tupling.

2.5. Based on the loop geometry

Certain instabilities are characteristic of the loop geometry. Examples are the instabilities observed in open U-loops, symmetric closed loops, and asymmetric-closed loops. In addition, pressure-drop oscillations and the parallel-channel instability are also characteristic of the loop geometry. Another type instability which can occur in systems with a compressible volume (e.g., a pressurizer) at the inlet of the heated channel is the pressure-drop-type instability. Similarly, interaction among parallel channels can also lead to various complex instabilities as discussed above.

2.6. Based on the disturbances

Certain two-phase flow phenomena can cause a major disturbance and can lead to instability or modify the instability characteristics significantly. Typical examples are boiling inception, flashing, flow pattern transition, or the occurrence of CHF. Cold water injection can also cause a major disturbance and instability in natural circulation systems.

3. CHARACTERISTICS OF INSTABILITIES

Several types of flow regimes can be associated in a natural circulation loop as heating proceeds. Some of these flow regimes are stable while others are unstable. For example, [13] observed seven different types of flow modes in a boiling two-phase natural circulation loop with increase in heater power such as (i) surface evaporation, (ii) a static instability characterized by periodic exit large bubble formation, (iii) a steady flow with continuous exit of small bubbles, (iv) a static instability characterized by periodic exit of small bubbles, (v) another static instability characterized by periodic extensive small bubble formation, (vi) a steady natural circulation, and (vii) the density-wave oscillation (dynamic instability). The static instabilities observed in their loop are due to the high heat flux and subcooled boiling occurring in the heated section, which are ideal for the cause of chugging-type instability. The characteristics of the instabilities are different from one to the other due to the differences in the physical mechanism associated with their initiations.

3.1. Characteristics of Instabilities associated with boiling inception

Boiling inception is a large enough disturbance that can bring about significant change in the density and hence the

buoyancy driving force in an NCS. A stable single-phase NCS can become unstable with the inception of boiling. Boiling inception is a static phenomenon that can lead to instability in low-pressure systems. However, feedback effects also are paramount in the phenomena. Hence the instability belongs to the class of compound-static instability. In this case, however, the instability continues with limit cycle oscillations. The oscillatory mode during boiling inception can also be significantly affected by the presence of parallel channels.

3.1.1. *Effect of boiling inception on unstable single-phase NC*

With increase in power, subcooled boiling begins in an unstable single-phase system leading to the switching of flow between single-phase and two-phase regimes. Experiments in a rectangular loop showed that subcooled boiling occurs first during the low flow part of the oscillation cycle [14]. The bubbles formed at the top horizontal-heated wall flows along the wall into the vertical limb leading to an increase in flow rate. The increased flow suppresses boiling leading to single-phase flow. Several regimes of unstable flow with subcooled boiling can be observed depending on the test section power such as (a) instability with sporadic boiling (boiling does not occur in every cycle), (b) instability with subcooled boiling once in every cycle; (c) instability with subcooled boiling twice in every cycle, and (d) instability with fully developed boiling. The change in power required from the first to the last stage is quite significant and it may not be reached in low-power loops.

U-tube manometer-type instabilities have been observed in boiling NC systems at reduced downcomer level [15] when the loop is heated from single-phase condition. The flow is found to reverse even before boiling is initiated (Figure 4). However, with initiation of boiling, no flow reversal is observed (Figure 5). The characteristics of oscillation were similar as previous cases (i.e., periodic large amplitude oscillation with few small amplitude oscillations in between). However, regular flow stagnation is observed, which is of concern for the safety of nuclear reactors. Also the amplitude of oscillation was found to be larger than that under single-phase conditions.

3.1.2. *Effect of boiling inception on steady single-phase NC*

A common characteristic of the instabilities associated with boiling inception is that single-phase conditions occur during part of the oscillation cycle. With the bubbles entering the vertical tubes, the buoyancy force is increased which increases the flow. As the flow is increased, the exit enthalpy is reduced leading to suppression of boiling. This reduces the buoyancy force and the flow, increasing the exit enthalpy resulting in boiling and leading to the repetition of the process. Krishnan and Gulshani [16] observed such instability in a figure-of-eight loop. They found that the single-phase circulation was stable. However, with power increase, the flow became unstable as soon as boiling was initiated in the heated section. Other examples of instabilities

Figure 4: Typical flow instability behavior at low power under single-phase condition (power 10 kW).

Figure 5: Typical flow instability behavior at low power under two-phase condition (initiation of boiling, power 12 kW).

associated with boiling inception in stable single-phase NCs are the following.

(a) Flashing instability

Flashing instability is expected to occur in NCSs with tall, unheated risers. The fundamental cause of this instability is that the hot liquid from the heater outlet experiences static pressure decrease as it flows up and may reach its saturation value in the riser causing it to vaporize. The increased driving force generated by the vaporization, increases the flow rate leading to reduced exit temperature and suppression of flashing. This in turn reduces the driving force and flow causing the exit temperature to increase once again leading to the repetition of the process. The necessary condition for flashing is that the fluid temperature at the inlet of the riser is greater than the saturated one at the exit [17]. The instability is characterized by oscillatory behavior and gets suppressed with rise in pressure [18, 19]. Furuya et al. [18] did systematic analyses to characterize the difference between

(a) For the entire range of qualities

(b) For low qualities

Figure 6: Effect of pressure on the void fraction.

flashing instability from other flow instabilities in boiling systems such as geysering, natural circulation instabilities like a DWI, and flow pattern transition instability. Their main observations were that the oscillation period in flashing instability correlates well with the passing time of single-phase liquid in the chimney section regardless of the system pressure, the heat flux, the inlet subcooling, and the wave-form. Out of phase flashing, oscillations were observed in the parallel channels of the CIRCUS facility by Marcel et al. [20].

(b) Geysering

Geysering was identified by both Boure et al. [4] and Aritomi et al. [21] as an oscillatory phenomenon which is not necessarily periodic. The proposed mechanism by both the investigators differ somewhat. However, a common requirement for geysering is again a tall riser at the exit of the heated section. When the heat flux is such that boiling is initiated at the heater exit and as the bubbles begin to move up the riser they experience sudden enlargement due to the decrease in static pressure and the accompanying vapor generation, eventually resulting in vapor expulsion from the channel. The liquid then returns, the subcooled nonboiling condition is restored, and the cycle starts once again. The main difference with flashing instability is that the vapor is produced first in the heated section in case of geysering, whereas in flashing the vapor is formed by the decrease of the hydrostatic head as water flows up.

The mechanism as proposed by Aritomi et al. [21] considers condensation effects in the riser. According to him, geysering is expected during subcooled boiling when the slug bubble detaches from the surface and enters the riser (where the water is subcooled), where bubble growth due to static-pressure decrease and condensation can take place. The sudden condensation results in depressurization causing the liquid water to rush in and occupy the space vacated by the condensed bubble. The large increase in the flow rate causes the heated section to be filled with subcooled water suppressing the subcooled boiling, and reducing the driving force. The reduced driving force reduces the flow rate. Increasing the exit enthalpy and eventually leading to subcooled boiling again and repetition of the process. Geysering involves bubble formation during subcooled conditions, bubble detachment, bubble growth, and condensation. Geysering is a thermal nonequilibrium phenomenon. On the other hand, during flashing instability, the vapor is in thermal equilibrium with the surrounding water and they do not condense during the process of oscillation. Both these instabilities are observed during low-pressure conditions only.

Instability due to boiling inception usually disappears with increase in system pressure due to the strong influence of pressure on the void fraction and hence the density (Figure 6).

3.2. Characteristics of two-phase static instability

Static instability can lead either to a different steady state or to a periodic behavior. Commonly observed, static instabilities are flow excursion and boiling crisis.

3.2.1. Flow excursion or excursive instability

The characteristics of the flow excursion instability or Ledinegg type instability depend very much on the geometry as well as the system pressure, power, and channel inlet subcooling [22]. Figure 7 shows an example of the stability maps for Ledinegg type instability at different pressures for a natural circulation boiling water reactor [2]. The Ledinegg-type instability decreases with an increase in pressure. This may be due to the fact that with an increase in pressure, the void fraction decreases with quality significantly in the

Figure 7: Characteristics of Ledinegg-type instability in NCS.

Figure 8: Comparison between Ledinegg and flow pattern transition instability maps for NC.

two-phase region, which can reduce the S-shaped variation of the irreversible losses (i.e., $\partial \Delta p_{\text{int}}/\partial w$) responsible for the occurrence of the Ledinegg-type instability. Similar to the type I and type II density-wave oscillations, two types of Ledinegg instabilities are observed at any subcooling depending on the operating power. With increase in pressure, the threshold power for the lower instability boundary moves to much higher power and the upper threshold boundary does not change significantly. The interesting thing which can be observed from the figure is that this instability almost vanishes when the operating pressure is more than 0.7 MPa.

3.2.2. *Flow pattern transition instability*

While there are several experimental and analytical studies to understand the characteristics of Ledinegg-type instability, there are not many studies on flow pattern transition instability. Nayak et al. [3] were probably the first to clarify some characteristics of this type of instability theoretically. They compared the stability maps between the Ledinegg and the flow pattern transition instability (Figure 8). The Ledinegg-type instability is found to occur at a lower power as compared to the flow-pattern transition instability at any subcooling. However, both instabilities increase with rise in subcooling. More experimental and theoretical studies are required to further clarify this instability.

The problems associated with static instability is that the amplitude of oscillations can be very high and sometimes the static instability can initiate the dynamic oscillations in the system [17]. There are limited studies on the excursive instability behavior of a parallel downward flow system (Babelli and Ishii [23]). While the mechanism of instability is same for upward- and downward-flow systems, however, one important finding is that the flow excursion can be the dominant mode of instability as compared to the density-wave instability in boiling NCs.

3.2.3. *Boiling crisis*

Following the occurrence of the critical heat flux, a region of transition boiling, may be observed in many situations as in pool boiling (see Figure 9(a)). During transition boiling a film of vapor can prevent the liquid from coming in direct contact with the heating surface resulting in steep temperature rise and even failure. The film itself is not stable causing repetitive wetting and dewetting of the heating surface resulting in an oscillatory surface temperature. The instability is characterized by sudden rise of wall temperature followed by an almost simultaneous occurrence of flow oscillations. This will not be confused with the premature occurrence of CHF during an oscillating flow, in which case the oscillations occur first followed by CHF (see Figure 9(b)).

3.3. *Characteristics of two-phase dynamic instabilities*

Unlike the static instabilities, there are several investigations in the area of dynamic instabilities, particularly the density-wave oscillations. In fact, numerous experiments and analytical studies are found in literature to clarify the characteristics of the density-wave oscillations.

3.3.1. *Experimental investigations*

Experimental investigations in two-phase natural circulation loops having single boiling heated channel have been carried out by [13, 17, 24–27]. They observed density-wave instability in their experiments, which was found to increase with increase in channel exit restriction and inlet subcooling. Also low water level in downcomer and low system pressure increases the density-wave instability in NCs. Such behavior was also observed in parallel heated channels of a two-phase natural circulation loop by Mathisen [28]. Lee and Ishii [25] found that the nonequilibrium between the phases created flow instability in the loop. Kyung and Lee [26]

(a) Pool boiling heat transfer regimes

(b) CHF occurrence during unstable oscillations

Figure 9: Instability due to boiling crisis.

investigated the flow characteristics in an open two-phase natural circulation loop using Freon-113 as test fluid.They observed three different modes of oscillation with increase in heat flux such as (a) periodic oscillation (A) characterized by flow oscillations with an incubation period. The mean circulation rate and void fraction at the riser section were found to increase with heat flux, (b) continuous circulation which is maintained with the churn/wispy-annular flow pattern. This was found to be a stable operation mode in which the flow was found to increase with heat flux first and then decrease with increase in heat flux, and (c) periodic circulation (B) characterized by flow oscillations with continuous boiling inside the heater section (i.e., there is no incubation period) and void fraction fluctuates from 0.6 to 1.0 regularly. In this mode, mean circulation rate was found to decrease with increase in heat flux, although the mean void fraction kept on increasing. Jiang et al. [17] observed three different kinds of flow instability such as geysering, flashing, and density-wave oscillations during startup of the natural circulation loop. Wu et al. [27] observed that the flow oscialltory behavior was dependent on the heating power and inlet subcooling. Depending on the operating conditions, the oscillations can be periodic or chaotic. Fukuda and Kobori [5] observed two modes of oscillations in a natural circulation loop with parallel heated channels. One was the U-tube oscillation characterized by channel flows oscillating with 180^0 phase difference, and the other was the inphase mode oscillations in which the channel flow oscillated alongwith the whole loop without any phase lag among them. Out of phase oscillations were also observed in the parallel channels of the CIRCUS facility by Marcel et al. [20].

3.3.2. Theoretical investigations

Linear analyses of boiling flow instabilities in natural circulation systems with homogeneous flow assumptions have been carried out by Furutera [29], S. Y. Lee and D. W. Lee [22], Wang et al. [30], and Nayak et al. [2]. Advantage of homogeneous flow assumption is that it is easier to apply and the model is also found to predict the stability boundary or the threshold of instability with reasonable accuracy. Linear stability analyses with homogeneous flow assumption and empirical model for the slip to calculate void fraction as a function of mixture quality have been carried out by Fukuda et al. [31]. Linear stability analysis using a four-equation drift flux model has been carried out by Ishii and Zuber [32], Saha and Zuber [33], Park et al. [34], Rizwan-Uddin and Dorning [35], van Bragt et al. [36], and Nayak et al. [37]. These models are based on kinematic formulation which considers the problem of mechanical nonequilibrium between the phases by having a relationship between the quality and void fraction through superficial velocities of liquid and vapor phases, vapor drift velocity, and void distribution parameter. The adoption of drift flux model allows to replace two separated momentum equations for liquid and vapor as used in the rigorous two-fluid models, by one momentum equation for the mixture plus a nondifferential constitutive law for the relative velocity. Besides, it considers equilibrium phasic temperature as in case of homogeneous model. Saha and Zuber [33] considered subcooled boiling in the drift flux model and applied the model to the stability of a natural circulation system. They found that consideration of thermal nonequilibrium condition results in a more stable system at low subcooling and a more unstable system at high subcooling as compared to the thermal equilibrium model. Rizwan-Uddin and Dorning [35] found that the threshold power for stability in boiling channel is sensitive to the void distribution parameter considered in the analysis. They found that with an increase in void distribution parameter, the stability of boiling channel increases. Similar results were also reported by Park et al. [34] and van Bragt et al. [36] for boiling channel systems. Nayak et al. [37] observed that the results are true not only for forced convection boiling systems, but also for the type I and type II instabilities observed in boiling natural circulation systems (Figures 10 to 12).

Similar results were also found for the effect of drift velocity on both type I and type II instabilities [37]. For any mixture quality, the void fraction is smaller for larger

Figure 10: Effect of void distribution parameter on threshold of Type I instability observed in HPNCL.

Figure 11: Effect of void distribution parameter on threshold of Type II instability observed in the Apparatus-A of Furutera.

drift velocity. If the quality is disturbed by a small amount, the void fraction with smaller drift velocity can have larger fluctuation than the other due to larger slope of void fraction versus quality.Hence,the flow will be disturbed larger for a smaller fluctuation in quality in this case. That is the reason for the reduction of type I instability with increase in drift velocity (Figure 13). An increase in drift velocity is also found to reduce the unstable region in the type II instability observed in Figures 14 to 15. With increase in drift velocity, the vapor propagation time lag in two-phase region reduces, which has a stabilizing effect.

Moreover, from these results it is interpreted that the homogeneous model for void fraction, which considers a zero drift velocity and unity void distribution parameter, predicts the most unstable region as compared to the slip models. Limited studies by Nayak et al. [38] and Bagul et al. [39] have shown that the homogeneous model predicts a more unstable region even as compared to the two-fluid models such as RAMONA-5 and RELAP/MOD3.2 (Figures 16 and 17). Usually, the homogeneous model predicts a larger void fraction than the two-fluid model for the same mixture quality due to the absence of slip between the water and steam in this model. The larger the void fraction in the system, the greater the buoyancy force, and consequently a higher flow rate will be encountered. At high flow rate, the frictional and local pressure drop in the two-phase region become greater, which has a destabilizing effect.

3.3.3. *Effect of geometric and operating parameters on instability*

Almost all the theoretical and experimental studies agree well that the DWI can be suppressed in boiling two-phase NC systems by increasing the system pressure. This is true

Figure 12: Effect of void distribution parameter on threshold of Type I and Type II instabilities observed in the Apsara loop.

both for type I and type II instabilities. An increase in power suppresses the type I instabilities, while enhances the type II instabilities according to the basic classification of these instabilities. The effects of subcooling on these instabilities are always debatable. While type I instabilities are always found to enhance with rise in subcooling, type II instabilities may enhance or reduce with subcooling depending on its magnitude and the system geometry and

Figure 13: Effect of drift velocity on threshold of Type I instability observed in high pressure natural circulation loop (HPNCL).

Figure 14: Effect of drift velocity on threshold of Type II instability observed in the Apparatus-A of Furutera.

heat input [1, 36, 38]. Invariably, it has been observed that with increase in local losses in two-phase region, both type I and type II instabilities increase. On the other hand, with increase in local losses in the single-phase region (such as orificing at the inlet of channels), the improvement in stability has been found to be conditional [2, 40] unlike in forced circulation systems wherein it has been observed that with increase in local losses in the single-phase region always improves the flow stability. In a natural circulation system, the flow rate in the channel depends on the heating power and the channel resistance. With increase in inlet throttling coefficient for same heating power, the channel flow rate decreases, which in turn causes an increase in channel exit quality. This may reduce the threshold power for instability for that channel which may cause the other channel to be unstable. So increase in orificing at channel inlet does not always increase the stability of a natural circulation system with multiple parallel channels (Figure 18).

The effect of riser geometry such as riser height and area on flow stability is important. In a natural circulation system, the low-power type I instability increases with increase in riser height. But the type II instability may increase or decrease depending on the flow resistance and heating power. For smaller riser height, lesser is the channel flow rate and larger is the channel exit quality for same heating power. This gives larger two-phase pressure drop due to large channel exit quality. Larger the riser height, larger is the channel flow rate which may cause larger two-phase pressure drop due to larger riser length. So a reduction or an increase in riser height on type II instability of natural circulation system is competitive [2].

The effect of riser area on flow instability has been discussed in great detail in a companion paper by the authors

Figure 15: Effect of drift velocity on the Type I and Type II instabilities threshold observed in the Apsara loop.

and hence will not be repeated here. However, for sake of completeness, only a brief discussion is presented below. For smaller riser flow area, the flow rate is smaller due to larger resistance in small riser pipes. As the flow area is increased, the flow rate increases, which gives rise to small frequency oscillations, typical of low quality type I density-wave instability (Figure 19) due to reduction in void fraction.

Figure 16: Comparison of stability maps between the homogeneous model and the two-fluid model (RAMONA5).

Figure 17: Comparison of stability maps between the homogeneous model and the two-fluid model (RELAP5/MOD3.2).

Hence, with increase in riser flow area, the type I instability appears [38]. However, the type II instability, which occurs at high power or void fraction, disappears with increase in riser diameter [38] due to reduction in void fraction or decrease in two-phase pressure drop.With an increase in riser area,the time period of oscillation reduces due to the increase in flow rate in the system.

3.4. Characteristics of compound dynamic instability

Instability is considered compound when more than one elementary mechanisms interact in the process and cannot be studied separately. If only one instability mechanism is at work, it is said to be fundamental or pure instability. Examples of compound instability are (1) thermal oscillations, (2) parallel channel instability (PCI), (3) pressure-drop oscillations, and (4) BWR (boiling water reactor) instability.

3.4.1. Thermal oscillations

In this case, the variable heat transfer coefficient leads to a variable thermal response of the heated wall that gets coupled with the DWO. Thermal oscillations are considered as a regular feature of dryout of steam-water mixtures at high pressure [4]. The steep variation in heat transfer coefficient typical of transition boiling conditions in a post CHF scenario can get coupled with the DWO. During thermal oscillations, dryout or CHF point shift downstream or upstream depending on the flow oscillations. Hence thermal oscillations are characterized by large amplitude surface temperature oscillations (due to the large variation in the heat transfer coefficient). The large variations in the heat transfer coefficient and the surface temperature causes significant variation in the heat transfer rate to the fluid even if the wall heat generation rate is constant. This variable heat transfer rate modifies the pure DWO.

3.4.2. Parallel channel instability (PCI)

Interaction of parallel channels with DWO can give rise to interesting stability behaviors as in single-phase NC. Experimentally, both inphase and out of phase oscillations are observed in parallel channels. However, inphase oscillation is a system characteristic and parallel channels do not generally play a role in it. With inphase oscillation, the amplitudes in different channels can be different due to the unequal heat inputs or flow rates, but there is no phase difference among them. Occurrence of out of phase oscillations is characteristic of PCI. The phase shift of out-of-phase oscillations (OPO) is known to depend on the number of parallel channels. With two channels, a phase shift of 180° is observed. With three channels, it can be 120° and with five channels it can be 72° [41]. With n-channels, Aritomi et al. [42] report that the phase shift can be $2\pi/n$. However, depending on the number of channels participating, the phase shift can vary anywhere between π and $2\pi/n$. For example, in a 3-channel system one can get phase shift of 180° or 120° depending on whether only two or all the three channels are participating.

3.4.3. Pressure-drop oscillations (PDO)

Pressure-drop oscillations are associated with operation in the negative sloping portion of the pressure drop-flow curve of the system. It is caused by the interaction of a compressible volume (surge tank or pressurizer) at the inlet of the heated section with the pump characteristics and is usually observed in forced circulation systems. DWO occurs at flow rates lower than the flow rate at which pressure-drop oscillation is observed. Usually, the frequency of pressure-drop oscillation is much smaller and hence it is easy to distinguish it from density-wave oscillations. However, with a relatively stiff system, the frequency of PDO can be comparable to DWO making it difficult to distinguish between the two. Very long test sections may have sufficient internal compressibility to

Figure 18: Effect of orificing of channels on the stability of boiling natural circulation systems (K_1 and K_2 refer to the throttling coefficients of channel 1 and 2, resp.).

Figure 19: Effect of an increase in riser area on stability.

initiate pressure drop oscillations. Like Ledinegg instability, there is a danger of the occurrence of CHF during pressure drop oscillations. Also inlet throttling (between the surge tank and the boiling channel) is found to stabilize PDO just as Ledinegg instability.

3.4.4. *Instability in natural circulation BWRs*

The flow velocity in natural circulation BWRs is usually smaller than that of forced circulation BWR. Besides, due to the presence of tall risers in natural circulation BWRs, the frequency of density-wave oscillation can be much lower due to longer traveling period of the two-phase mixture in the risers. The effects of negative void reactivity feedback are found to stabilize the very low frequency type I instabilities [43, 44]. But it may stabilize or destabilize type II instabilities depending on its time period [44].

In case of a natural circulation BWR, the existence of a tall riser or chimney over the core plays a different role in inducing the instability. Series of experiments carried out by van der Hagen et al. [45] in the Dodewaard natural circulation BWR in The Netherlands showed that instabilities could occur at low as well as at high powers in this reactor. From measured decay ratio, it was evident that at very low power there is a trend of increase in decay ratio and similar results are seen at higher power also. The low-power oscillations are induced by the type I density-wave instabilities and high power oscillations are induced by the type II density-wave instabilities. type I and type II instabilities have been predicted to occur in the Indian AHWR which is a natural circulation pressure tube type BWR, away from the nominal operating condition [44]. It may be noted that in case of forced circulation BWRs, instabilities observed under natural circulation conditions are due to pump trip transients when the core exit quality is high due to low flow and high power. Hence these are induced by the type II density-wave instabilities only.

4. CONCLUDING REMARKS

Several decades have been spent on the study of flow instabilities in boiling two-phase natural circulation systems. A large number of numerical and experimental investigations in this field have been carried out in the past. Many numerical codes in time domain as well as in frequency domain have been developed using various mathematical modelling techniques to simulate the flow instabilities occurring in the NCSs. It is felt to have a review and summarize the state-of-the-art research carried out in this area, which would be quite useful to design and safety of current and future light water reactors with natural circulation core cooling. With that purpose, a review of flow instabilities in boiling natural circulation systems has been carried out. An attempt has been made to classify the instabilities occurring in natural circulation systems similar to that in forced convection boiling systems. It was found that the instabilities can be classified based on the mechanism of their occurrence into broadly two groups such as static and dynamic instabilities. The analytical tools based on the above mechanisms predicts the stability threshold and characteristics of instabilities reasonably well. Other classifications are in fact subcategories of a particular class of the instabilities covered under this classification. While classifying instabilities of NCSs, a need was felt to consider the instabilities associated with single-phase condition, boiling inception, and two-phase condition separately as a natural circulation system progresses through all these stages before reaching the fully developed two-phase circulation. Most instabilities observed in forced circulation systems are observable in natural circulation systems. However, natural circulation systems are more unstable due to the regenerative feedback inherent in the mechanism causing the flow. While most of the work has been devoted to generate data for steady state and threshold of flow instabilities in NCSs, however, it was felt that more investigations on characteristics of

these flow instabilities must be conducted in future, which is not understood enough. Moreover, it is found that these instabilities do not occur in isolated manner in NCSs, however, many times, they occur together which are known as compound instabilities. Different models of two-phase flow have been used for modelling these flow instabilities, which range from the simplest HEM to more rigorous two-fluid model. While the HEM is found to model the threshold of instability of density-wave type in NCS with reasonably accuracy, however, there are concerns for using this model since the drift velocity and void distribution parameters which are indications of slip between the phases, are found to affect the stability threshold. Computer codes developed considering more rigorous models such as RELAP5 are yet to be established for their applicability for simulation of stability in boiling NCS. In view of this, more research needs to be conducted to explore the capability of existing mathematical models for prediction of these instabilities in NCSs in future.

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Research Article

Effect of Loop Diameter on the Steady State and Stability Behaviour of Single-Phase and Two-Phase Natural Circulation Loops

P. K. Vijayan, A. K. Nayak, D. Saha, and M. R. Gartia

Reactor Engineering Division, Bhabha Atomic Research Centre, Trombay, Mumbai 400085, India

Correspondence should be addressed to P. K. Vijayan, vijayanp@barc.gov.in

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In natural circulation loops, the driving force is usually low as it depends on the riser height which is generally of the order of a few meters. The heat transport capability of natural circulation loops (NCLs) is directly proportional to the flow rate it can generate. With low driving force, the straightforward way to enhance the flow is to reduce the frictional losses. A simple way to do this is to increase the loop diameter which can be easily adopted in pressure tube designs such as the AHWR and the natural circulation boilers employed in fossil-fuelled power plants. Further, the loop diameter also plays an important role on the stability behavior. An extensive experimental and theoretical investigation of the effect of loop diameter on the steady state and stability behavior of single- and two-phase natural circulation loops have been carried out and the results of this study are presented in this paper.

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1. INTRODUCTION

Natural circulation systems find application in many nuclear reactor systems. Single-phase natural circulation is the mode of coolant circulation in the current pressurized water and heavy water reactors (PWRs and PHWRs) following a pumping power failure. Certain reactor systems like the heating reactors (normally referred to as district heating reactors [1]) and the CAREM PWR [2] use single-phase natural circulation as the normal mode of coolant circulation in the primary system. Two-phase natural circulation systems are extensively used in fossil-fuelled power plants, thermosyphon reboilers, and steam generators including those in nuclear power reactors like PWRs, VVERs, and PHWRs. Two-phase natural circulation is the mode of coolant circulation in the current generation of nuclear power plants following a small break LOCA associated with pumping power failure. It was used as the normal mode of coolant circulation in low power boiling water reactors (BWRs) like Humbholdt Bay 3 (63 MWe), Dodewaard (65 MWe), and VK-50 (50 MWe). In many new designs under development like the ESBWR, AHWR [3], and VK-300 [4], two-phase natural circulation is the normal mode of coolant circulation. Natural circulation based reactor designs are being studied even with supercritical water as coolant [5, 6].

In view of their wide application, both single-phase and two-phase natural circulation systems are being extensively studied. State-of-the-art reviews on single-phase [7, 8] and two-phase [9, 10] natural circulation have been provided by several authors. Most studies are concerned with instability as natural circulation systems are more unstable than forced circulation systems. For example, both single-phase and two-phase NCSs exhibit instability whereas only two-phase forced circulation systems are known to exhibit instability. Apart from the steady-state performance, operational transients like start-up, power raising and step back are also required to be stable for natural circulation reactors. This requires specification of a stable operating procedure which in turn requires deeper understanding of the parametric trends due to the operational (i.e., inlet subcooling, pressure, and power) and the geometric parameters (i.e., height, diameter, and orientation of source; and sink) affecting natural circulation.

In the present paper, the effect of loop diameter on the steady state and stability behavior of single- and two-phase natural circulation loops was studied both experimentally

Figure 1: Schematic of the experimental loops.

and theoretically. Single-phase natural circulation is found to destabilize with increase in loop diameter. On the other hand, the two-phase natural circulation is found to stabilize with increase in loop diameter. Both the type-I and type-II instabilities in two-phase loops are found to be significantly influenced by the loop diameter. For example, the threshold value of pressure at which type-I instability disappears is found to decrease with increase in loop diameter. Type-II instability was observed only in small diameter loops. Linear analysis showed that type-II instability does not occur in the two-phase region (0 < quality < 1) if the loop diameter is above a critical value. Depending on the loop diameter, the maximum operating power of a natural circulation system can be either stability controlled or critical heat flux (CHF) controlled. For stability controlled systems, the minimum decay ratio (DR) line can be an ideal operating line. Premature occurrence of CHF and burnout is a serious concern during low-frequency and high-amplitude oscillatory flows in small diameter loops.

2. SINGLE-PHASE NATURAL CIRCULATION

Even for natural circulation based boiling water reactors, single-phase natural circulation is encountered during the heat-up phase. Hence experiments were carried out in four single channel uniform diameter loops (UDLs) of rectangular shape made of borosilicate glass tube having horizontal heater and cooler with diameter varying from 6 mm to 26.9 mm. These studies were carried out at near atmospheric conditions. Three of the loops studied had the general dimensions as shown in Figure 1(a) and the dimensions of the fourth loop are shown in Figure 1(b). The cooler was tube-in-tube type and the heater was made by winding nichrome wire uniformly on the outside of the tube. The experimental loops had a small expansion tank open to the atmosphere to accommodate the swell and shrinkage. The measurements included the heater power, loop temperatures and the cooling water flow rate and its inlet temperature. For the loop shown in Figure 1(b), the pressure drop across 1075 mm of the bottom horizontal pipe was also measured. A data logger with a scanning time of 1.5 seconds recorded all the measurements. The measurement accuracy was 0.4% (±1.1°C) for thermocouples, ±2% for flow rate and ±0.5% of the span (0–1250 W) for power and pressure drop (−100 to +100 Pa).

2.1. Steady state behavior

Steady state flow prevails when the driving buoyancy force is balanced by the retarding frictional force, that is, $\rho_0 \beta g \oint T dz = (f L_t/D)(W^2/2\rho_0 A^2)$. Integrating over the closed loop and replacing $\Delta T_h = Q_h/WCp$ and $f = p/\mathrm{Re}^b = pA^b\mu^b/D^b W^b$, we obtain

$$W = \left[\frac{2D^b \rho^2 \beta_T g A^{2-b} Q_h \Delta z}{pCp\mu^b N_G}\right]^{1/(3-b)}. \quad (1)$$

The above equation is valid for uniform diameter loops with negligible local pressure losses filled with an incompressible Boussinesq fluid, where

$$\beta_T = \frac{1}{v}\left(\frac{\partial v}{\partial T}\right)_p. \quad (2)$$

From the steady state experimental data, a heat balance using the measured heater power and the temperature difference across the heater yielded the mass flow rate. For the same

(a) Measured and predicted flow

(b) Effect of loop diameter on predicted flow rate

Figure 2: The steady state flow rate variation with power for different loop diameters.

operating conditions (heater power, coolant flow rate and its inlet temperature), the flow rate is found to increase with power and loop diameter (Figure 2). The steady state flow rate can be expressed by the following dimensionless correlation [11]:

$$\mathrm{Re}_{ss} = C\left[\frac{(\mathrm{Gr}_m)_{\Delta z}}{N_G}\right]^r, \tag{3}$$

where $\mathrm{Re}_{ss} = DW_{ss}/A\mu$, $(\mathrm{Gr}_m)_{\Delta z} = D^3\rho^2\beta_T g Q_h \Delta z/A\mu^3 Cp$, $N_G = L_t/D$, $C = (2/p)^r$, $r = 1/(3-b)$, p and b are the constants in the friction factor correlation of the form $f = p/\mathrm{Re}^b$. The p and b values for laminar, transition and turbulent flows were, respectively, 64 & 1, 22.3 & 0.67, and 0.316 & 0.25. Corresponding C and r values for laminar, transition and turbulent flow are, respectively, 0.1768 & 0.5, 0.3568 & 0.43, and 1.96 & 0.364. The dimensionless steady state flow rate data is compared with (3) for various flow regimes in Figure 3. The literature data on rectangular natural circulation loops [12–19] are also included in Figure 3. The steady state flow rate data agrees reasonably well with the proposed steady state correlations.

Figure 3: Steady state performance of single-phase loops differing in diameter (dimensions in the bracket denote the loop diameter used by different investigators).

2.2. Stability behavior

Both theoretical and experimental studies were carried out to understand the stability behavior.

2.2.1. Test results

Before stability runs the fluctuations in the loop instrument readings at zero power and steady state operation were established. The fluctuations in the controlled coolant flow rate and its inlet temperature were also noted. The experiments showed that the loops with small diameters were highly stable. However, instability could be observed in 23.2 mm and 26.9 mm diameter loops. Extensive investigations of the unstable behavior were carried out for the 26.9 mm i.d. loop [20–22]. An interesting observation is that the instability threshold is not a unique value but depends on the operating procedure (characteristic of the hysteresis phenomenon).

Based on the operating procedure, the natural circulation flow can be classified as unconditionally stable, conditionally stable, and unconditionally unstable (Figure 4). In the conditionally stable (hysteresis) region, stable or unstable flow can prevail depending on the operating procedure.

Three different oscillatory modes are observed in the experiment depending on the power. These are unidirectional pulsing (UDP), bidirectional pulsing (BDP), and chaotic switching. UDP is characterized by a bean-shaped limit cycle, whereas BDP is characterized by a dumbbell-shaped limit cycle (Figure 4). Between the two, an oscillatory regime with chaotic switching between UDP and BDP is observed. At high powers, the BDP becomes chaotic. The loop resistance and the coolant flow significantly affect the thresholds of a specified oscillatory flow regime.

2.2.2. Stability analysis

Derivation of the characteristic equation for the stability parameter n (n is a complex variable) and its solution procedure are described based on the linear stability method in Vijayan and Nayak [23]. The characteristic equation for the loop shown in Figure 1 can be expressed as

$$n - \frac{(p/2)^{1+m}}{(\mathrm{Gr}_m I_{\mathrm{ss}})^m (D/L_t)^{1+m}} \left[\left(\frac{\overline{I}}{\overline{\omega}} \right) - (2-b) \right] = 0, \tag{4}$$

where

$$m = \frac{b}{3-b}, \qquad I_{\mathrm{ss}} = \oint \theta_{\mathrm{ss}} dZ,$$
$$\frac{\overline{I}}{\overline{\omega}} = \frac{\phi}{n}\left(1 - e^{-n/\phi}\right) e^{-nL_x/L_t} \left\{ \frac{\overline{\theta}_h - \overline{\theta}_c}{\overline{\omega}} \right\}, \tag{5}$$

$$\frac{\overline{\theta}_h - \overline{\theta}_c}{\overline{\omega}} = \frac{\mathrm{St}_m (\theta_{\mathrm{hl}})_{\mathrm{ss}}\, \alpha + (L_t/L_h)\, \alpha^*}{n\left(e^{(\mathrm{St}_m L_c + nL_t)/L_t} - 1\right)}, \tag{6}$$

where $\alpha = \{e^{nL_{\mathrm{hl}}/L_t}(e^{nL_c/L_t} - 1) - e^n(1 - e^{-nL_c/L_t})\}$ and $\alpha^* = \{e^{(\mathrm{St}_m L_c + nL_t)/L_t}(e^{-nL_h/L_t} - 1) - e^{nL_{\mathrm{cl}}/L_t}(1 - e^{nL_h/L_t})\}$. Using the characteristic equation, the stability map was generated for the loops with different diameters. Figures 5(a) and 5(b) show a comparison of the predicted stability map with test data. Considering that the effects of wall conduction, axial conduction in the fluid, the local pressure losses, the heat losses, and multidimensional effects are neglected in these calculations, the agreement with test data is reasonable. The unstable zone is found to shift up with reduction in loop diameter. Thus, increasing the loop diameter is found to destabilize single-phase natural circulation. A literature review has revealed that the L_t/D ratio is an important parameter affecting instability. In fact to our knowledge, instability has not been observed experimentally in uniform diameter loops with $L_t/D > 300$. The stability map, however, is a strong function of the friction correlation (Figure 5(b)).

Figure 4: Unstable oscillatory regions in single-phase loops.

3. TWO-PHASE NATURAL CIRCULATION

Two-phase natural circulation experiments were conducted in four uniform diameter loops of inside diameter 7, 9.1, 15.74, and 19.86 mm with the length scales as shown in Figure 6(a). The steam separator, the condenser, and the associated piping (the portion inside the rectangular box in Figure 6(a)) were the same for all the loops. The steam separator was made of 59 mm inside diameter pipe. The vertical heater section was directly heated with a high current low voltage power supply. The steam-water mixture produced flows to the separator and the separated steam was condensed and the condensate returns to the steam drum by gravity. The loop was extensively instrumented to measure heater power, temperatures (of heater surface and fluid), steam drum pressure, differential pressure at various sections of the single-phase and two-phase pipes, SD level, flow rates (loop, condensate, and cooling water flows), void fraction and its distribution. The void fraction was measured using both neutron radiography (NRG) and conductance probe (CP). The neutron radiography also helped to visualize the flow patterns. To facilitate neutron radiography, the loop was installed in the Apsara reactor hall in front of the neutron beam hole (Figure 6(b)). With the neutron radiography, void fraction was measured at the outlet of the heated section, whereas conductance probes were located at the center line in the vertical tube above the neutron window as well as in the top near horizontal tube. During the experiments, the system was initially pressurized in the cold condition to the required value with the help of nitrogen cylinders. During the heat-up and boiling, the system pressure increase was compensated by bleeding nitrogen-steam mixture from the condenser. Further details of the loop are available in Vijayan et al. [24] and Dubey et al. [25]. In addition, experimental data were generated in a 49.3 mm inside diameter loop shown in Figure 6(c). The inside diameter of the heated section was 53.4 mm in this loop. Further details of this facility are available in Kumar et al. [26].

(a) 6, 11 and 23.2 mm loops

(b) 26.9 mm loop

Figure 5: Comparison of predicted stability map with data.

3.1. Steady state behavior

Steady state data on flow pattern, void fraction, and flow rate were collected for the various loops as a function of power at different pressures. The 7 mm loop was mostly unstable and reliable steady state data could not be generated due to frequent burnout of the heated section. The steady state data generated in all other loops fall in the following range of parameter: loop diameter: 9.1–49.3 mm, circulation length: 8.7 and 13.5 m, SD level: 90% ± 5%, pressure: 0.1–7 MPa, quality: 0.4–24%, and power: 0.3–40 kW.

3.1.1. Flow patterns

Steady state flow patterns at the exit of the heated section were recorded with a CCD camera, details of which are available in Sinha et al. [27]. The predominant flow pattern observed in all the loops (of geometry given by Figure 6(a)) was bubbly or bubbly-slug. Typical recorded images and the expected flow pattern change with increase in power are shown in Figures 7(a) and 7(b), respectively.

3.1.2. Void fraction

The void fraction measured by the neutron radiography and the conductance probe were close to each other. The void fraction data were compared with various correlations giving reasonable agreement. A typical comparison with Ishii correlation [31] is shown in Figure 8. More results on the void fraction are available in [24, 32].

3.1.3. Steady state flow

The steady state flow rate was measured with the help of calibrated pipe taps located in the single-phase bottom horizontal pipe. Assuming balance of the driving buoyancy force to the retarding frictional forces at steady state, the following equation was derived to estimate the steady state flow rate in a UDL [33]:

$$W_{ss} = \left[\frac{2}{p}\,\frac{D^b\,\rho_l^2\,\overline{\beta}_h g A^{2-b} Q_h \Delta z}{\mu_l^b\,N_G}\right]^{1/(3-b)}, \tag{7}$$

which can be expressed in dimensionless form as

$$\mathrm{Re}_{ss} = C\left(\frac{\mathrm{Gr}_m}{N_G}\right)^r, \tag{8}$$

where

$$\mathrm{Gr}_m = \frac{D^3\rho_l^2\overline{\beta}_h g Q_h \Delta z}{A\mu_l^3}; \quad \overline{\beta}_h = \frac{1}{v}\left(\frac{\partial v}{\partial h}\right)_p; \tag{9}$$

$$N_G = \frac{L_t}{D}\left[(l_{\mathrm{eff}})_{\mathrm{sp}} + \overline{\phi}_{\mathrm{LO}}^2\,(l_{\mathrm{eff}})_B + \phi_{\mathrm{LO}}^2\,(l_{\mathrm{eff}})_{\mathrm{tp}} + \overline{\phi}_{\mathrm{LO}}^2\,(l_{\mathrm{eff}})_c\right] \tag{10}$$

with

$$\begin{aligned}\phi_{\mathrm{LO}}^2 &= \frac{\rho_l}{\rho_e}\left[\frac{1}{1 + x_e(\mu_l/\mu_g - 1)}\right]^b,\\ \overline{\phi}_{\mathrm{LO}}^2 &= \frac{\rho_l}{\rho_m}\left[\frac{1}{1 + (x_e/2)(\mu_l/\mu_g - 1)}\right]^b.\end{aligned} \tag{11}$$

Figure 6: Schematic of the different test loops.

Figure 7: Flow patterns obtained for a typical run.

Figure 8: Comparison of measured void fraction with that predicted using Ishii model [31]

It may be noted that for single-phase flows $\phi_{LO}^2 = 1$, and (8) reduces to that applicable to single-phase natural circulation. Also, similarity in the definitions of the thermal expansion coefficients β_h and β_T valid for two-phase and single-phase flows, respectively, may be noted. The constants C and r have the same definitions as in single-phase flow. The measured steady state flow rate data from the present test loops are plotted in Figure 9 in dimensionless form. The data are found to be within ±40% of the above theoretical correlation from Figure 9.

3.2. Parametric effects on steady state flow

One of the drawbacks of dimensionless correlations is that it often masks the parametric trends. In fact the steady state performance of two-phase loops is significantly influenced by the pressure, power, and loop diameter. Effect of pressure on two-phase NC flow was studied experimentally and theoretically in the 49.3 mm i.d. loop and the results are shown in Figure 10. The results are in agreement with the predictions of (7). The measured flow rate as a function of power is plotted in Figure 11 for various loops. The

FIGURE 9: Comparison of steady state data with correlations.

FIGURE 10: Effect of pressure on flow rate.

predictions with (7), TINFLO-S [34], and RELAP5/MOD3.2 are also given in Figure 11. Again, the data are in reasonable agreement with the predictions.

3.3. Two-phase NC flow regimes

Unlike single-phase natural circulation, it is observed that the trend of the steady state data can be significantly different for two-phase loops. Based on the nature of the variation of the steady state flow with power, three different natural circulation flow regimes can be observed for two-phase loops. These flow regimes are designated as gravity dominant, friction dominant, and the compensating regimes. In

(a) 9.1 mm i.d. loop

(b) 49.3 mm i.d. loop

(c) 9.1 mm i.d. loop

FIGURE 11: Effect of power on the two-phase NC flow rate.

Figure 12: Effect of quality on void fraction.

Figure 13: Effect of pressure on flow regime.

(a)

(b)

Figure 14: Effect of pressure on flow regime for different loop diameter.

a natural circulation loop, the gravitational pressure drop (being the driving pressure differential) is always the largest component of pressure drop and all other pressure drops (friction, acceleration, and local) must balance the gravity (or buoyancy) pressure differential at steady state. However, the natural circulation flow regimes are differentiated based on the change of the pressure drop components with quality (or power).

3.3.1. Gravity dominant regime

The gravity dominant regime is usually observed at low qualities. In this regime, for a small change in quality there is a large change in the void fraction (see Figure 12) and hence the density and buoyancy force. The increased buoyancy driving force is to be balanced by a corresponding increase in the retarding frictional force that is possible only at a higher flow rate. As a result, the gravity dominant regime is characterized by an increase in the flow rate with power (see Figures 13 and 14).

3.3.2. Friction dominant regime

Friction dominant regime is observed at low to moderate pressures when quality is high. At higher qualities and moderate pressures, the increase in void fraction with quality is marginal (Figure 12) leading to almost constant buoyancy force. However, the continued conversion of high density water to low density steam due to increase in power requires that the mixture velocity must increase resulting in an increase in the frictional force and hence a decrease in flow rate. Thus, the friction dominant regime is characterized by a decrease in flow rate with increase in power (see the curve for 15 bar in Figure 13).

3.3.3. Compensating regime

Between the gravity dominant and friction dominant regimes, there exists a compensating regime, where the flow rate remains practically unaffected with increase in power. In

(a) Experimental stability map

(b) Nature of type-I oscilation

(c) Type-I oscilation

Figure 15: Experimental stability map and some typical type-I oscillation.

this regime, the increase in buoyancy force is compensated by a corresponding increase in the frictional force leaving the flow unaffected (see Figure 11(a) and the curve for 7 MPa in Figure 13) in spite of increase in quality.

3.3.4. *Effect of pressure and loop diameter*

The NC flow regimes depend strongly on the system pressure. In fact, at high pressures only the gravity dominant regime (as in single-phase natural circulation) may be observed if the power is low. The friction dominant regime shifts to low pressures with increase in loop diameter (see Figure 14). Knowledge of the flow regimes is important to understand the stability behavior of two-phase loops.

3.4. *Stability behavior*

Two-phase natural circulation systems are susceptible to a large number of instabilities. Instability is undesirable as sustained flow oscillations may cause forced mechanical vibration of components. Further, premature critical heat flux (CHF) occurrence can be induced by flow oscillations as well as other undesirable secondary effects like power oscillations in BWRs. Instability can also disturb control systems and pose operational problems in nuclear reactors. The instabilities are broadly classified into three groups: static, dynamic, and compound dynamic instabilities. A more detailed description of natural circulation instabilities can be seen in [23].

3.4.1. *Experimental stability map*

An experimentally generated stability map for the 9.1 mm i.d. loop is shown in Figure 15(a), which shows two unstable regions for two-phase natural circulation. It may be noted that all the two-phase loops tested had stable single-phase natural circulation flow. However, it appears possible to get an unstable zone in the single-phase region also for certain loop geometries. Thus overall, three unstable zones (one in the single-phase region and two in the two-phase region) are possible for natural circulation loops with increase in power (excluding the single-phase vapour region). The first unstable zone in the two-phase region (Figure 15(a)) occurs at a low power and hence at low quality and is named as type I instability by Fukuda and Kobori [35]. Similarly, the second unstable zone in the two-phase region (Figure 15(a)) occurs at high powers and hence at high qualities and is named as type II instability. Theoretical analysis by the same authors has shown that the gravitational pressure drop plays a dominant role in type I instability where as frictional pressure drop is dominant in type II instability. In other words, type-I instability occurs in the gravity dominant regime whereas type-II instability occurs in the friction dominant regime. Instability is not found in the compensating regime during the present tests.

3.4.2. *Parametric effects on DWI*

Both type-I and type-II instabilities are affected by a large number of parameters out of which the effects of power, pressure, and loop diameter are studied both experimentally and theoretically in the present work.

3.4.3. Characteristics of type-I instability

A general characteristic of type-I instability is that it occurs right from boiling inception. Flashing and geysering induced instability also belong to this category. Broadly speaking, the amplitude of type-I oscillations first increases, reaches a peak and then decreases with increase in power eventually leading to stable flow (Figure 15(b)). However, amplitude variation is a complex function of power presumably due to flow pattern transitions (Figure 15(b)). For certain type-1 unstable regions, the flow alternates between a stable and oscillatory regime (Figure 15(c)). For large diameter loops, a unidirectional oscillation depicting a bean shaped limit cycle is observed (Figure 16). However, the dynamics is quite rich showing many different oscillatory patterns (see Figures 15(c), 16(a), and 16(c) for a few sample patterns).

The amplitude of type-I oscillations reduces significantly with increase in pressure (see Figures 15(a) and 17(a)). Further, type-I instability is not observed beyond a critical value of the system pressure (Figure 17(c)). While experimenting with uniform diameter loops, it was observed that the critical value of pressure beyond which the instability disappears is found to decrease with increase in the loop diameter (Figure 18). However, beyond a certain diameter, the effect of loop diameter on the critical pressure is found to be marginal.

3.4.4. Characteristics of type-II instability

In the present experiments, type-II instability (Figure 19) is found to occur after the flow starts to decrease with increase in power (characteristic of friction dominant regime). The threshold of type-II instability is found to increase with pressure as well as loop diameter. Type-II instability is observed in large diameter loops only at low pressures. A general characteristic of the type-II instability is that the oscillation amplitude keeps increasing monotonically with power. When the amplitude reaches a critical value, CHF occurs causing test section burnout. Burnout of the test section occurred frequently in the 7 mm loop and less frequently in the 9.1 mm loop.

Figure 16: Some typical type-1 oscillations and the limit cycle for observed for 49.3 mm i.d. loop.

3.5. Theoretical results

The stability of the test loops was studied with a linear stability code TINFLO-S based on the drift flux model (Nayak et al. [36]). The drift flux parameters (C_o and V_{gi}) for slug flow were used as it was the most frequently observed flow pattern during the tests. The Martinelli-Nelson two-phase friction multiplier model was used in the computations. Figures 20(a) and 20(b) compare the predicted stability map with experimental data. Figure 21(a) reveals that the stability is enhanced by increasing the loop diameter which is consistent with the test results. The effect of loop diameter on the lower threshold of instability (type-I instability) is less significant compared to that on the upper threshold (type-II instability). However, when plotted in terms of power (Figure 21(b)) both the lower and upper thresholds are found to be significantly influenced by the loop diameter. Increase in inlet subcooling destabilizes type-I instability irrespective of the loop diameter. For type-II instability, however, similar behavior is observed only at low loop diameter. For large loop diameters, (see the stability map for 25 mm) type-II instability stabilizes initially and then destabilizes. Similar behavior is also reported by Boure et al. [37].

4. DESIGN CONSIDERATIONS

The above results are significant to the design of the pressure tube type BWRs. For example, the experiments show that it is possible to avoid type-I instability by a pressurized start-up (Figure 17). Figure 21(a) shows that the type-II instability threshold shifts to higher qualities with increase in loop diameter. For 40 mm loop diameter, the upper threshold is not found in the two-phase region (i.e., $0 < \text{quality} \leq 1$). Thus, by the appropriate choice of the loop diameter, it

Figure 17: Effect of pressure on the type-I instability in the 9.1 mm inside diameter loop.

Figure 18: Effect of pressure on the instability due to boiling inception in 19.86 mm i.d. loop.

is possible to entirely eliminate the type-II instability in the two-phase loops. However, this is yet to be established experimentally as the maximum test section power was limited to 40 kW. Experiments by Mochizuki [38] indicate that this instability occurs when the quality is close to unity at high pressure and large inlet subcooling. It may be noted that the stable zone (difference in power between the upper and lower thresholds) is very low for small diameter loops, whereas it is significantly large in large diameter loops (Figure 21(b)).

4.1. Design types for NCSs

Since both instability and CHF (critical heat flux) need to be avoided in the design of two-phase natural circulation systems, two types of designs are possible depending on which of them is limiting the maximum power that can be extracted. These are designated as stability-controlled and CHF-controlled designs.

4.1.1. Stability-controlled design

In this type of loops, the maximum power is limited by the stability, as the threshold of type-II instability is lower than the CHF threshold. Figure 22 shows that this is normally the case in small diameter loops. These predictions were obtained by incorporating the look-up table (Groeneveld et al. [39]) in the TINFLO-S code. The heat balance method was used for the CHF prediction. In

Figure 19: The upper threshold of instability at different pressures.

Figure 20: Comparison of measured and predicted stability maps.

Figure 22(c), stability-controlled design is found on the right of the dotted line.

4.1.2. CHF-controlled design

Characteristic of this design is that the CHF threshold is much below the type-II instability threshold and hence CHF limits the maximum power that can be extracted. Figure 23 shows that this situation arises in large diameter loops. In Figure 23(a), the design is CHF controlled only to the left of the vertical dotted line, while in Figures 23(b) and 23(c) the design is CHF controlled over the entire subcooling-power plane. Design of forced circulation BWRs is usually CHF controlled. By appropriate choice of the loop diameter, the design of an NCS can shift from stability controlled to CHF controlled (Figures 22 and 23).

4.1.3. Operating line for stability-controlled designs

Two-phase NCSs are not completely stable over the entire subcooling-power map and an operating line needs to be

Figure 21: Predicted stability map for various loop diameters.

Figure 22: Stability controlled designs and loop diameter.

specified for ensuring stability for all anticipated operations like start-up, power raising, and step back. One would expect the decay ratio to go through a minimum while moving from the lower to the upper threshold for a fixed subcooling (Figure 24). Ideally, the operating line shall pass through the minimum decay ratio line (locus of all minimum decay ratio points (see also Figure 24)) so that all oscillations will die down in the quickest possible manner. In stability-controlled designs, one could choose the operating line as the minimum decay ratio line if found feasible from feed water inlet temperature considerations (see also Vijayan et al. [40]).

4.1.4. *Unstable oscillations and CHF*

Premature occurrence of CHF and test section burnout can be an issue during oscillatory flows in two-phase loops. In the present experiments, premature CHF occurrence was

observed only at low pressures when the amplitude of flow oscillation is large. Although Figures 22(a) and 22(b) show that the CHF threshold is significantly higher than the instability threshold, burnout occurred only for the test section diameters of 7 and 9.1 mm in the present experiments. The occurrence of burnout during instability was more frequent in 7 mm diameter loop than in 9.1 mm loop. A typical observed temperature excursion following the occurrence of CHF in the 7 mm diameter test section is shown in Figure 25(a), where a heater trip based on high heater surface temperature terminated the transient. However, occurrence of CHF away from the thermocouple locations resulted in the burnout of the test section. Inspection of the burntout test sections revealed that the burnout is not restricted to the test section outlet. Premature occurrence of CHF was studied using RELAP5/MOD3.2 code for sinusoidal fluctuations in flow rate. The effect of amplitude and frequency was also studied for the 9.1 mm i.d. test section. Figure 25(b) shows that premature occurrence of CHF is a concern at low frequencies and high amplitudes.

5. CONCLUDING REMARKS

Effect of loop diameter was studied for both single-phase and two-phase natural circulation loops.

Single-phase loops

Four single-phase loops differing in diameter were studied. Instability was observed only in the two large diameter loops. The instability threshold is found to decrease with increase in loop diameter. Also, the threshold of instability is not a unique value as it depends on the operating procedure (hysteresis phenomenon). Linear analysis showed that the unstable region shifts up with decrease in loop diameter. Thus, small diameter loops are more stable than large diameter loops.

Two-phase loops

The steady state and stability behaviour of five loops differing in diameter were studied. Besides, the well known gravity and friction dominant regimes a compensating regime, where the flow is practically invariant with power is also observed under steady state. Both type-I and type-II instabilities are observed in small diameter loops, whereas only type-I instability was observed in large diameter loops. Type-I instability can be avoided by a pressurized start-up. However, the pressure beyond which the type-I instability disappears is found to decrease with increase in the loop diameter. Contrary to the observations in single-phase loops, the stability of two-phase loops is found to enhance with increase in loop diameter. Linear analysis showed that increase in subcooling destabilized type-I instability irrespective of loop diameter whereas type-II instability is found to destabilize for small diameter loops. For large diameter loops, type-II instability stabilizes initially with increase in subcooling and then destabilizes with further increase in subcooling. Stability analysis also revealed that the type-II instability

Figure 23: CHF controlled designs and loop diameter.

is not observable in the two-phase region ($0 < x < 1$) for large diameter loops at moderate pressures (70 bar). Premature occurrence of CHF and burnout is a serious problem at low-frequency high-amplitude oscillatory flow in small diameter loops. The study also shows that the

Figure 24: Variation of decay ratio (DR).

(a) Premature CHF occurrence

(b) Effect of amplitude and frequency

Figure 25: Premature CHF occurrence with oscillatory flow.

maximum operating power of NCSs can be CHF controlled or instability controlled depending on the loop diameter. For stability controlled designs, the minimum decay ratio (DR) line could be a feasible operating line.

NOMENCLATURE

A: Flow area, m^2
b Exponent in the friction factor equation
Cp: Specific heat, J/(kg K)
D: Hydraulic diameter, m
f: Darcy-Weisbach friction factor
g: Gravitational acceleration, m/s^2
Gr_m: Modified Grashof number
h: Enthalpy, J/kg
l: Dimensionless length, L_i/L_t
L: Length, m
L_x: Length of adiabatic horizontal section (see Figure 1(a)), m
p: Constant in the friction factor equation
P: Pressure, Pa
Q_h: Total heat input rate, W
v: Specific volume, m^3/kg
St_m: Modified Stanton number
W: Mass flow rate, kg/s
x: Quality
Z: Dimensionless elevation
Δz: Center line elevation difference, m.

GREEK SYMBOLS

α: Void fraction
β: Thermal expansion coefficient, K^{-1}
ϕ: Dimensionless parameter, AH/L_t
ϕ^2_{LO}: Two-phase friction multiplier
$\overline{\phi}^2_{LO}$: Mean value of ϕ^2_{LO}
ρ: Density, kg/m^3
$\Delta\rho$: Density difference across the heater, kg/m^3
Δh: Enthalpy difference over the heater, J/kg
μ: Dynamic viscosity, Ns/m^2
θ: Dimensionless temperature.

SUBSCRIPTS

B: Boiling length
c: Cooler
cl: Cold leg
e: Heater exit
eff: Effective
g: Vapor
h: Heater
hl: Hot leg
i: ith segment
in: Heater inlet
l: Liquid
m: Mean
sp: Single phase
ss: Steady state
t: Total
tp: Two-phase.

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Research Article

Analyses of Instability Events in the Peach Bottom-2 BWR Using Thermal-Hydraulic and 3D Neutron Kinetic Coupled Codes Technique

Antonella Lombardi Costa,[1] Alessandro Petruzzi,[2] Francesco D'Auria,[2] and Walter Ambrosini[2]

[1] *Departamento de Engenharia Nuclear, Universidade Federal de Minas Gerais, Avenida Antônio Carlos 6627, Campus UFMG, PCA1—Anexo Engenharia, Pampulha, 31270-90 Belo Horizonte, MG, Brazil*
[2] *Dipartimento di Ingegneria Meccanica, Nucleare e della Produzione, Università di Pisa, Via Diotisalvi 2, 56126 Pisa, Italy*

Correspondence should be addressed to Antonella Lombardi Costa, lombardicosta@gmail.com

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Boiling water reactor (BWR) instabilities may occur when, starting from a stable operating condition, changes in system parameters bring the reactor towards an unstable region. In order to design more stable and safer core configurations, experimental and theoretical studies about BWR stability have been performed to characterise the phenomenon and to predict the conditions for its occurrence. In this work, contributions to the study of BWR instability phenomena are presented. The RELAP5/MOD3.3 thermal-hydraulic (TH) system code and the PARCS-2.4 3D neutron kinetic (NK) code were coupled to simulate BWR transients. Different algorithms were used to calculate the decay ratio (DR) and the natural frequency (NF) from the power oscillation predicted by the transient calculations as two typical parameters used to provide a quantitative description of instabilities. The validation of the code model set up for the Peach Bottom Unit 2 BWR plant is performed against low-flow stability tests (LFSTs). The four series of LFST have been performed during the first quarter of 1977 at the end of cycle 2 in Pennsylvania. The tests were intended to measure the reactor core stability margins at the limiting conditions used in design and safety analyses.

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1. INTRODUCTION

In the last four decades, the nuclear power industry has been upgrading and developing light water reactor technology, and preparing to meet the future demand for energy. The presently operating BWRs contribute with about 21% of the total produced nuclear power worldwide. These plants have reached very ambitious goals of safety and reliability, together with high availability factors, notwithstanding the flow instability and thermal-hydraulic oscillations that may affect BWRs under particular operating conditions.

The stability of BWR systems has been of great concern from the safety and the design point of view at the beginning of the nuclear era; nowadays, the design of reactors having appropriate stability margins, the adoption of operating procedures avoiding possible unstable regions, and the development of mitigation strategies to cope with inadvertent instability occurrences have strongly limited safety concerns in this regard. This is a direct consequence of the large operating experience gained with BWRs and of the increased knowledge of instability phenomena obtained from both experimental and computational activities aimed at simulating reactor behaviour.

BWR instabilities occur when an operating condition becomes unstable after some change in system parameters. As a consequence, state variables identifying the reactor working conditions are observed to oscillate in different ways depending on the modalities of the departure from the stable operating point.

Figure 1 shows an example of the power-flow map for a BWR. The lower right side of the plot marks the allowed operating region, the grey region can only be entered if special measures are taken, and finally, the black regime is forbidden due to stability concerns. In the figure, the 100% rod line, also called 100% flow-control line, is a line on the BWR power-flow map which passes through the normal operating point of the BWR (100% power and 100% flow rate). The control rods do not move on this line. Therefore, operating

Figure 1: Instability region in the power-flow map for a typical BWR.

points on the line are identified by a fixed reactivity value. BWRs operate on this line during startup and shutdown operations by flow control via the recirculation pumps [1].

Conditions corresponding to about 50% core nominal power and 30% core inlet flow rate may be seen as the area in the power-flow map where the highest probability of oscillations occurs. The operation in this area is avoided by means of adequately defined control and trip conditions. Nevertheless, certain perturbed transient conditions can still result in time windows, in which operation in this area will occur, usually accompanied with the observation of oscillating core behavior. The core two-phase flow itself provides a potential for oscillatory behavior and the strong feedback between moderator coolant density and core power enhances the effect under certain conditions. In-phase and out-of-phase power oscillations have been actually observed and both modes, for large amplitudes, can have an unwanted influence on the fuel integrity. Control systems for the RPV (reactor pressure vessel) pressure and downcomer levels can influence the oscillatory behavior in unfavourable way. Therefore, the main aims of BWR stability analyses could be summarized as follows [2]:

(i) to assess the stability margins in the reactor plant, including normal and off-normal conditions;
(ii) to predict the transient behavior of the reactor, an unstable condition should occur;
(iii) to help in designing and assessing the effectiveness of countermeasures adopted to prevent and mitigate the consequences of instabilities.

2. COUPLED NEUTRONIC/THERMAL-HYDRAULIC INSTABILITIES

BWR transient scenarios, that involve considerable reactivity changes, are described, for example, in [2, 10]. These documents address overpressurisation events, large break loss of coolant accidents (LBLOCAs), feedwater temperature decrease, increase of core flow, main circulation pump flow rate increase, anticipated transient without scram (ATWS), turbine trip (TT), and control rod removal.

From the point of view of the BWR safety, the most important type of power instability is the reactivity oscillations excited by thermal-hydraulic mechanisms. Two types of instability by reactivity have been characterized.

(i) *In-phase (core-wide) instability.* In this case, all the variables (power, mass flow, pressure, etc.) oscillate in phase determining a limit cycle; from the point of view of safety, this type of instability has relatively small relevance, unless it is associated with an ATWS.
(ii) *Out-of-phase instability.* In this case, the instabilities occur when a neutronic azimuthal mode is excited by thermal-hydraulic mechanisms causing asymmetric power oscillations; at a given time, while part of the reactor operates at high-mass flow and low-power level, in the other part the opposite happens; this behaviour must be studied in detail because of safety implications.

A very complex type of power instability in BWRs consists of out-of-phase regional oscillations, in which, normally, subcritical neutronic modes are excited by thermal-hydraulic feedback mechanisms. The out-of-phase mode of oscillation is a very challenging type of instability and its study is relevant because of the safety implications related to the capability to promptly detect any such inadvertent occurrence by in-core neutron detectors, thus triggering the necessary countermeasures in terms of selected rod insertion or even reactor shutdown.

Power oscillations can, for large amplitudes, have an unwanted influence on the fuel integrity. In the fuel temperature limitation, it is essential to prevent the exceeding of the melting point (3073.15 K for UO_2). Fuel elements subjected to temperatures sufficiently high to induce centreline melting will experience a significantly higher probability of failure (loss in the functional behavior caused by a change in the physical properties). Furthermore, the low thermal conductivity of ceramic fuels leads to high temperature gradients that can cause fuel cracking and swelling [12].

3. INADVERTENT AND INDUCED BWR INSTABILITIES

Some of the several occurred instability events in BWR plants were inadvertent and other ones were induced intentionally as experiments. These instabilities were identified as periodic oscillations of the neutron flux via instrumentation readings. Essentially, neutronic power signals from local power range monitors (LPRMs) and average power range monitors (APRM) have been used to detect and study the power oscillations.

One of the inadvertent instability events happened in LaSalle NPP, in 1988. During a routine surveillance test, an instrument technician inadvertently caused the shutdown of both recirculation pumps. As a consequence, the core flow rate was rapidly reduced from 76% to 29% of the rated value, corresponding to natural circulation conditions; this, in turn, led to the isolation of some of the steam extraction lines leading to the pre-heaters. The result of this action was a colder FW supply to the core. Between four and five minutes after the pump trip, the operators observed power oscillations

with amplitude range from 25% to 50% of the rated value. The reactor scram occurred automatically on high neutron flux at 118% of the rated power at about seven minutes after the pumps tripped. This accident was analyzed in many works, as in [3, 4], for example.

In 1995, in Laguna Verde BWR/5, an instability event occurred during the startup process. The analysis of neutron noise showed that the transition from stability to instability is a gradual process that can be stopped by an earlier alarm indication [5]. No damage to the plant was reported. In addition, the Oskarshamn-3 BWR experienced power oscillations in February 1998. A review of the possible causes suggested that the oscillations resulted from the particular used control rod sequence and the power distribution obtained as a result [6].

In November 2001, an in-phase neutron flux oscillation occurred at Philippsburg-1 NPP after an FW temperature transient. A similar event occurred at the Swedish BWR Oskarshamn-2 in February 1999. In both events, a scram terminated the neutron flux oscillation, but only at the fixed scram set point at 120% and 132% power levels, respectively. In both cases, control rod insertion was activated too late to limit the oscillations effectively before reaching the scram set points [7].

After the first instability events, authorities in all countries with BWRs required a review of the stability features of their reactors. The authorities include the requirements of analyses in the safety analysis reports and changes in the procedures and plant safety systems. The major safety concern associated with instability is the cooling of the fuel and cladding integrity.

Several occurred instability events in BWR plants were induced intentionally as experiments. For example, series of turbine trips and stability tests were conducted in the Peach Bottom-2 BWR in 1977. The low flow stability tests (LFST) were done along the low-flow end of the rated power-flow line, and along the power-flow line corresponding to a minimum recirculation pump speed in the region of the power-flow map where oscillations have a higher probability [8]. The main aim of these tests was to provide a database for the qualification of transient design methods used for reactor analyses at operating conditions.

A stability test was performed at Forsmark-1 BWR in January, 1989, during a startup operation after the scram due to a turbine trip. In total, 36 signals including those from APRM and LPRM, total core flow, and local channel flows were recorded on a digital computer. These data were useful to the validation of analysis techniques as verified, for example, in [9]. In 1990, a stability test was conducted in the BWR Leibstadt to test the ability of the monitoring system to cope with demanding operation situations; the power oscillations were transformed from the in-phase mode into the out-of-phase mode by the removal of some control rods. When the maximal oscillation amplitude was reached, it was suppressed by decreasing the power [10].

The European Commission's NACUSP project, started in December 2000, investigates natural circulation and stability performance of BWRs. One of its main aims is understanding the physics of the phenomena involved during the startup phase of natural-circulation-cooled BWRs, providing a large experimental database and validating state-of-the-art thermal-hydraulic codes in the low-pressure low-power operational region of these reactors [11].

4. METHODS AND TOOLS TO STUDY BWR INSTABILITIES

To study instabilities in BWRs, many numerical models and computer codes have been developed. The methods have been validated using data provided by signals of several experimental tests and by inadvertent events. The good agreement found between computational analyses and the available experimental data has contributed to better understanding of the BWR instability phenomena.

4.1. Mathematical models

Many literature works are related to numerical methods to study the BWR instabilities. Several authors use the modal method to investigate the phenomena, as in [13–15], for example. The modal analysis is a process conceived to determine the dynamic characteristics of a system, in the form of natural frequencies, decay factors, and oscillating modes. This information is used to formulate a mathematical model of the dynamic behavior of the system, denominated as the "modal model."

The decay ratio (DR) and the natural frequency (NF) of the oscillations are typical parameters used to evaluate the instabilities. Other parameters can also provide valuable information, such as the Lyapunov exponents associated to the time series. In fact, Lyapunov exponents are also used as a measure of the stability of the neutronic time series [16].

Parametric or nonparametric methods can be used to evaluate the decay ratio. For nonparametric methods, DR is evaluated from the autocorrelation function of the signal. For parametric methods, it is evaluated from the impulse response of the system or from its effective transfer function [17]. Different parametric models are actually used, being that the autoregressive moving average (ARMA), the autoregressive (AR) or the moving-average (MA) are the most common ones. For the same time series signal, DR can have significant variation on its result depending on the method selected for its calculation.

Two different algorithms, ADRI (analysis of decay ratio instability), a parametric method, and DRAT (decay ratio), a nonparametric method, described in the two next sections, respectively, were used in this work to perform DR and NF calculations.

4.1.1. ADRI

ADRI code [18] is a package written with MATLAB scripts and it can evaluate DR and natural frequency for both signals obtained from transient initiated by short perturbation and noise during stationary operation. It may be applied to all types of signal, with or without noise, with high or low DR. ADRI was applied to calculate DR and NF of Ringhals-1 NPP stability benchmark presenting results in good agreement with the experimental DR and NF.

At the base of ADRI, there are (i) the "AR" MATLAB function that estimates the parameters of an autoregressive (AR) model, (ii) the "IDSIM" MATLAB function simulating a dynamic system, and (iii) the evaluation of DR and NF as the average of decay ratios and natural frequencies, respectively, of the considered couple of peaks.

4.1.2. DRAT

The method proposed here by Ambrosini is based on the form of the general differential equation for a second-order system in free evolution, being

$$\ddot{y} + b\dot{y} + cy + d = 0. \tag{1}$$

The basic idea is to extract from the available transient data estimates of y and its time derivatives to optimise the parameters b, c, and d with the minimum square technique. This amounts to solve, by the minimum square technique, the overspecified system of equations

$$\begin{aligned} f_k(x_1, x_2, x_3) &= x_1\dot{y}_k + x_2 y_k + x_3 + \ddot{y}_k \\ &= 0, \quad (k = 2, \ldots, N-1), \end{aligned} \tag{2}$$

in which N is the number of the available data of y_k; the elements x_1, x_2, x_3 are the coefficients of the second-order model, and also the unknown of the problem that will permit to find the angular frequency and the damping factor of the system. Estimates of the involved derivatives can be found as finite difference approximations, for example, of the kind

$$\begin{aligned} \ddot{y}_k &\approx \frac{2}{t_{k+1} - t_{k-1}}\left(\frac{y_{k+1} - y_k}{t_{k+1} - t_k} - \frac{y_k - y_{k-1}}{t_k - t_{k-1}}\right); \\ \dot{y}_k &\approx \frac{1}{2}\left(\frac{y_{k+1} - y_k}{t_{k+1} - t_k} + \frac{y_k - y_{k-1}}{t_k - t_{k-1}}\right). \end{aligned} \tag{3}$$

Making use of the minimum square technique, the following definitions apply:

$$\begin{aligned} Q(x_1, x_2) &= \sum_{k=2}^{N-1} f_k^2(x_1, x_2, x_3); \\ \frac{\partial Q}{\partial x_i} &= \sum_{k=2}^{N-1} f_k \frac{\partial f_k}{\partial x_i} = 0, \quad (i = 1, 2, 3); \\ \frac{\partial f_k}{\partial x_1} &= \dot{y}_k, \qquad \frac{\partial f_k}{\partial x_2} = y_k, \qquad \frac{\partial f_k}{\partial x_3} = 1, \end{aligned} \tag{4}$$

leading to the linear algebraic system

$$\begin{aligned} &\sum_{k=2}^{N-1} \dot{y}_k[x_1\dot{y}_k + x_2 y_k + x_3 + \ddot{y}_k] = 0 \\ &\sum_{k=2}^{N-1} y_k[x_1\dot{y}_k + x_2\hat{y}_k + x_3 + \ddot{y}_k] = 0 \\ &\sum_{k=2}^{N-1} [x_1\dot{y}_k + x_2\hat{y}_k + x_3 + \ddot{y}_k] = 0 \end{aligned} \tag{5}$$

or

$$\begin{aligned} &\left[\sum_{k=2}^{N-1} \dot{y}_k^2\right]x_1 + \left[\sum_{k=2}^{N-1} \dot{y}_k y_k\right]x_2 + \left[\sum_{k=2}^{N-1} \dot{y}_k\right]x_3 = -\left[\sum_{k=2}^{N-1} \dot{y}_k\ddot{y}_k\right] \\ &\left[\sum_{k=2}^{N-1} \hat{y}_k\dot{y}_k\right]x_1 + \left[\sum_{k=2}^{N-1} y_k^2\right]x_2 + \left[\sum_{k=2}^{N-1} y_k\right]x_3 = -\left[\sum_{k=2}^{N-1} \hat{y}_k\ddot{y}_k\right], \\ &\sum_{k=2}^{N-1} [\dot{y}_k]\, x_1 + \sum_{k=2}^{N-1} [y_k]\, x_2 + \sum_{k=2}^{N-1} [1]\, x_3 = -\left[\sum_{k=2}^{N-1} \ddot{y}_k\right] \end{aligned} \tag{6}$$

which represents the minimum square version of the overspecified system introduced above.

Solution of this system allows calculating the damping factor and the frequency of oscillation of the system, interpreted as a second-order one. In fact, from the characteristic equation of the original differential equation

$$z^2 + x_1 z + x_2 = 0, \tag{7}$$

it is found that

$$z_{1,2} = -\frac{x_1}{2} \pm \sqrt{\frac{x_1^2}{4} - x_2}, \tag{8}$$

and then the general solution of the equation becomes

$$y(t) = Ae^{[-x_1/2 + \sqrt{(x_1^2/4) - x_2}]\,t} + Be^{[-x_1/2 - \sqrt{(x_1^2/4) - x_2}]\,t}. \tag{9}$$

As usual, two cases can be considered.

(i) $x_1^2/4 - x_2 > 0$, that is, nonoscillatory behaviour, putting

$$\begin{aligned} z_R &= -\frac{x_1}{2}, \\ \hat{z}_R &= \sqrt{\frac{x_1^2}{4} - x_2}, \end{aligned} \tag{10}$$

the general solution is

$$y(t) = e^{z_R t}[\overline{A}\sinh(\hat{z}_R t) + \overline{B}\cosh(\hat{z}_R t)]. \tag{11}$$

(ii) $x_1^2/4 - x_2 < 0$, that is, oscillatory behaviour, putting

$$z_R = -\frac{x_1}{2}, \qquad z_I = \sqrt{\left|\frac{x_1^2}{4} - x_2\right|}. \tag{12}$$

From the above, it can be clearly understood that the computed time evolution is consistent with the following theoretical, purely second-order time evolution:

$$y(t) = e^{z_R t}[\overline{A}\sin(z_I t) + \overline{B}\cos(z_I t)]. \tag{13}$$

It is clear that the algorithm can be applied only while considering the behavior of a system that, for small oscillations, can be considered approximately linear after a perturbation, with no explicit forcing.

4.2. System analysis computer codes

Computer programs developed for the modelling and the transient simulation of a complete nuclear-power plant with a high degree of detail are called system codes. Different choices are adopted for neutron kinetics and two-phase flow modelling. The application of thermal hydraulics (TH) and neutron kinetics (NK) codes to LWR analyses was discussed, for example, in the three volumes edited by the project CRISSUE-S[1] [10, 19, 20]. Specifically, the project CRISSUE-S treated the interactions between neutron kinetics and thermal hydraulics that affect neutron moderation and influence the transient performance of the NPPs.

Nowadays, the nuclear industry and the scientific community turned their attention to the development of coupled 3D neutron-kinetics and thermal-hydraulic system codes to investigate BWR instabilities, in particular, the regional (out-of-phase) type. The coupled system codes can model accurately not only reactivity-initiated accidents (RIAs), but also typical reactor operational transients as turbine trips. These programs are often called "best-estimate" analysis tools and describe, in a more realistic way, the local core effects, coupled reactor core, and plant dynamics interactions.

Different coupling code methodologies have been used as, for example, TRAC-BF1/ENTREÉ [21], RELAP5-3D [22, 23], TRAC-BF1/RAMONA [24], MARS/MASTER [25], RETRAN-3D [26], TRAC-BF1/NEM [27], RELAP5/PANBOX/COBRA [28], and RELAP5/PARCS [29, 33].

In this work, simulations of in-phase and out-of-phase instabilities in a BWR are being presented. The thermal-hydraulic system code RELAP5 [30] and the 3D neutron-kinetic code PARCS [31] have been used in a coupled way for performing the transient simulation. In particular, the PARCS code is used to evaluate the 3D space-time core power history; it uses a nonlinear nodal method to solve the two energy group neutron diffusion equations. In the calculation, PARCS makes use of the moderator temperature and density and of the fuel temperature calculated by RELAP5 to evaluate the appropriate feedback effects in the neutron cross sections. Likewise, RELAP5 takes the space-dependent power calculated in PARCS and solves the heat conduction in the core heat structures. The coupling process between RELAP5 and PARCS codes is done through a parallel virtual machine (PVM) environment.

The temporal coupling of RELAP5 and PARCS is explicit in nature, and the two codes are locked at the same time step. For this implementation, the RELAP5 solution lags the PARCS solution by one time step. Specifically, the advancement of the time step begins with RELAP5 obtaining the solution to the hydrodynamic field equations using the power from the previous time step. The property data obtained from this solution is then sent to PARCS and the power at the current time step is computed.

[1] The acronym CRISSUE-S project stands for Critical Issues in Nuclear Reactor Technology, a state-of-the-art report.

*PVM-parallel virtual machine
**MAPTAB-TH to neutronic nodes file

Figure 2: Scheme of coupling among RELAP5 and PARCS codes.

The two processes are loaded in parallel and the PARCS process transfers the nodal power data to the TH process. The TH process then sends the temperature (fuel and coolant) and density data back to the PARCS process.

The adopted calculation sequence is sketched in Figure 2. The user must run two programs simultaneously; the following sequence can be used during RELAP5/PARCS coupling:

(i) RELAP5 runs in the stand-alone mode for flow initialization (invoking no PARCS calculations) and generates a restart file at the end of the calculation (RELAP5 steady state stand alone);

(ii) PVM is launched; using the above restart file, the coupled steady-state case runs and generates the steady-state restart files for both PARCS and RELAP5 (RELAP5/PARCS coupled steady state);

(iii) using the restart files, the coupled transient case is launched (RELAP5/PARCS coupled transient).

The original mapping between neutronic and thermal-hydraulic codes was explicit in that the fractions of different TH nodes belonging to a neutronic node had to be specified in the MAPTAB file for all the neutronic nodes. The postprocessor WinGraf has been used to read the unformatted binary output data from the RELAP5. Two different algorithms were then used to calculate the DR from the power oscillation signals obtained from the transient calculations.

5. ANALYSED EVENTS AND RESULTS

In this work, data from experimental low flow stability tests (LFSTs) have been compared with results obtained with coupled 3D simulations. Other transient cases, requiring the use of a 3D coupled analysis, have been also simulated (feedwater temperature decrease, recirculation pump trip, and control rod banks movement) using the same operating conditions of the LFST. These cases must be considered as sensitivity analyses with no possibility of comparison with measured data. All the simulated events are being described next, as well as the obtained results.

Figure 3: Peach bottom BWR modelled by RELAP5.

5.1. Thermal-hydraulic and neutronic model

Peach Bottom Unit 2 is a direct-cycle BWR/4 of General Electric type that has been subjected to stability testing. Three turbine trip tests and four series of low flow stability tests (LFST) have been performed during the first quarter of 1977 at the end of cycle 2. The LFST were performed in the region of the power-flow map where the highest probability of oscillations occurs.

The Peach Bottom nodalisation for RELAP5 and PARCS was based on the benchmark specification document for the turbine trip test (TT) [32], and on data in the related tests report [8]. Details of the adopted nodalization methodology, developed by the University of Pisa, are described in [36]. The methodology was validated in relation with the TT test [33, 36] and also for pressure perturbation stability tests [29]. The Peach Bottom NPP core was divided into 33 heated regions representing the 764 real core fuel assemblies, modelled according to the RELAP5 code requirements; channels with common characteristics were grouped together. In particular, each channel groups a certain number of fuel assemblies; they were chosen according to their thermal-hydraulic and kinetic properties, taking into account the lattice type, the relative power, the inlet flow area, and the relative position within the core.

Figure 3 shows the main elements of the RELAP5 nodalization. Figure 4 represents part of the nodalization corresponding to the reactor core; in the figure, the identification number is related to the pipe component in the nodalization. The core active zone was axially subdivided into 24 meshes with 15.24 cm each. In a recent study, Ambrosini and Ferreri [39] investigated stability boundaries obtained from a RELAP5 model for a boiling channel of 3.6 m with 48 and 24 meshes. The results showed that the stability boundaries predicted with 48 and 24 nodes are very similar. Therefore, the use of 24 meshes limits the complexity of the model reducing the calculation time and conserving the accuracy of the results.

Figure 4: Detail of the plant nodalisation with the 33 TH channels in the reactor core.

Figure 5: Peach bottom-2 Low-Flow stability tests in the power/flow map.

To represent the reactor core neutronic behavior by the PARCS code, the reactor core was discretized into parallelepipedal nodes, where the nuclear properties are assumed to be constant. Radially, 18 fuel types and one reflector node were defined, whereas, axially, the core was subdivided into 26 axial nodes; the first and the last nodes represent the reflector zones. In total, 435 compositions or neutronic nodes were considered to represent the kinetic behavior of the core.

5.2. Low flow stability tests, pressure perturbation (PP)

The LFST were intended to measure the reactor core stability margins at the limiting conditions used in design and safety analyses. Table 1 gives the test condition for the four stability points (PT1, PT2, PT3, and PT4), and Figure 5 shows the location of the respective test points in the Peach Bottom power-flow map.

For all the four stability points, the steady-state simulations were firstly performed using the RELAP5 code stand alone in order to estimate the thermal-hydraulic operating conditions under the assumption of fixed and uniform axial power distribution. These initial conditions are then used to perform the coupled calculations. In the coupled steady-state calculation, results of the axial power profile were obtained to the four cases and compared with the available experimental curves with good agreement in all the four cases as it can be verified in Figure 6.

In the transient experiments, the magnitude of the pressure set point steps was selected at approximately 8 psi (0.055 MPa), which gave a good signal-to-noise ratio in the neutron flux response and did not cause operational difficulties during the testing. Then the series of small pressure perturbation tests conducted at each of the LFST conditions were composed of pseudorandom binary switching of small step inputs to the pressure-regulator reference set point.

Typical reactor-core and vessel-pressure responses, and the average and local neutron flux signals, were taken. The neutron flux to pressure transfer functions was estimated from the data using the fast Fourier transform (FFT) algorithm. From this transfer function, the stability margin of the core, in terms of the decay ratio of the fundamental oscillatory mode of response, was determined [8].

Therefore, DR and NF calculated from the experimental power oscillations data are available (Table 1) and being compared, in this work, with the data calculated from the simulations.

The coupled transient calculations were performed considering boundary conditions in which the reactor is disturbed with one pressure spike of 0.055 MPa in the turbine. The turbine corresponds to a *tmpdvol* (time-dependent volume) component type in the RELAP5 input deck (element number 675 in the nodalization) that permits to impose pressure variation in time.

The pressure perturbation propagates through the steam line (SL) and reaches the core disturbing the mass flow rate and bringing the power to an oscillatory behavior. Figures 7, 8, 9 and 10 show the power behavior after the pressure perturbation for the cases PT1, PT2, PT3, and PT4,

Figure 6: PT1, PT2, PT3 and PT4—experimental and calculated mean axial power profile.

Table 1: LFST-experimental conditions.

Tests	Power		Mass flow rate (Core Inlet)		Enthalpy (Core Inlet)	Pressure (Core Inlet)	DR	NF
	%	MW	kg/s	(%)	(kJ/kg)	(MPa)		(Hz)
PT1	60.6	1995	6753.6	52.3	1184.6	7.06	0.121	0.441
PT2	51.7	1702	5657.4	43.8	1187.8	7.01	0.121	0.471
PT3	59.2	1948	5216.4	40.4	1184.6	7.10	0.344	0.437
PT4	43.5	1434	5203.8	40.3	1183.8	7.06	0.296	0.402

respectively. In all the cases, the transient begins at the time zero of calculation.

5.2.1. Case PT1

As it is shown in Figure 7, power oscillates after the pressure wave perturbation disturbs the core flow. The power oscillation reaches the maximum value of 64% (7.4% higher than the initial condition) and after about 40 seconds, the steady-state conditions are re-established.

The core mass flow rate oscillates reaching the maximum amplitude of 0.7%. The variation in the mass flow rate is about ten times smaller than that of power, that is, a small perturbation of mass flow rate can cause a great variation in the power.

DR and NF from the power signal have been calculated using both algorithms ADRI and DRAT and different time intervals. It has been observed that both algorithms give results of DR and NF that can vary largely in accordance with that of the time window considered in the calculation [29].

Table 2: DR and NF results in comparison with the experimental data for PT1.

Data from experimental results			Calculation			
			ADRI		DRAT	
			Time window from 37.5 to 70.0 s			
LFST	DR	NF (Hz)	DR	NF (Hz)	DR	NF (Hz)
			0.216	0.355	0.323	0.377
			Time window from 0.0 to 55.0 s			
PT1	0.121	0.441	0.729	0.416	1.001	0.383
			Time window from 36.5 to 55.0 s			
			0.351	0.424	0.540	0.431

Figure 7: Power evolution—PT1.

Figure 8: Power evolution—PT2.

Figure 9: Power evolution—PT3.

Figure 10: Power evolution—PT4.

The oscillation in the case PT1 presents a behavior very different in comparison with the other three cases, PT2, PT3, and PT4, where the oscillations involve few peaks and are quickly damped. Observing, in detail, the final oscillation in power for PT1, it is more similar to the linear oscillatory behavior of the cases PT2, PT3, and PT4. Therefore, DR and NF were recalculated considering only the last power peaks for the case PT1 because the initial oscillations could be interpreted as a noise signal. Table 2 gives calculation and experimental data of DR and NF; calculated values by ADRI and DRAT correspond to different time windows. As it can be seen, DR predicted by ADRI and DRAT tends to values closer to the experimental one when only the last peaks are considered (time window from 37.5 to 70.0 s) in spite of DRAT giving values of DR slightly higher than that from ADRI.

5.2.2. *Case PT2*

A fast decrease in the amplitude of power oscillations is observed and, after approximately 30 seconds, oscillations are terminated (Figure 8). In this case, the system has a very stable behavior, the oscillations are completely damped and the

Table 3: DR and NF results in comparison with the experimental data for the PT2.

Calculation (from 3.8 to 30 s)						
	ADRI		DRAT			
LFST	DR	NF (Hz)	DR	NF (Hz)	DR	NF (Hz)
PT2	0.121	0.471	0.498	0.275	0.481	0.278

Table 4: DR and NF results in comparison with the experimental data for PT3.

Calculation (from 5 to 30 s)						
	ADRI		DRAT			
LFST	DR	NF (Hz)	DR	NF (Hz)	DR	NF (Hz)
PT3	0.344	0.437	0.454	0.289	0.467	0.275

power and the other analysed parameters return to the steady state values.

DR and NF, from the power signal, were calculated using both algorithms ADRI and DRAT. Table 3 presents the numerical results. As it can be observed from the table, both algorithms give values of DR and NF very close to each other. However, the calculation results are not in agreement with the experimental data. In fact, the experimental DR for the point PT2 is very small and it is exactly the same as for the point PT1 ($DB_{exp} = 0.121$) though PT1 and PT2 are operating points relatively far from each other in the power-flow map.

The tests PT1 and PT2 were investigated in two previous works [34, 35] from the point of view of the gain between the pressure and power during the PP event. In the results, the gains for the tests PT1 and PT2 are underestimated by the first work and overestimated for the second one. This seems to indicate that the two operating points are somehow critical for simulation.

It is possible that the strong transient xenon-concentration change taken place between test conditions PT1 and PT2 [8] could have "masked" the DR results for these cases. The xenon concentration affects the stability and, in particular, the DR value. The redistribution of the Xe concentration, following a large scale power change, apparently may cause decrease in the DR [2].

5.2.3. Case PT3

Also in this case, the process presents a fast decrease in the power amplitude oscillation and, after about 30 seconds, oscillations are terminated, as it can be seen in Figure 9. The system presents good stability to the PP transient. After the perturbation, power and the other parameters return to the steady state values.

DR and NF, from the power signal, were calculated using both algorithms ADRI and DRAT considering a time window from 5.0 to 30.0 seconds. Table 4 presents the results. The results show a very good agreement between ADRI and DRAT for the DR calculations. However, both algorithms tend to overestimate the experimental value of DR. Calculated NF presents a value smaller than that for the experimental one, as occurred for the case PT2.

Table 5: DR and NF results in comparison with the experimental data for PT4.

Calculation (from 4.2 to 30 s)						
	ADRI		DRAT			
LFST	DR	NF (Hz)	DR	NF (Hz)	DR	NF (Hz)
PT4	0.296	0.402	0.217	0.282	0.349	0.285

A sensitivity case for the test PT3 was performed, in which the core nodalization was modified obtaining a new configuration. The number of heated thermal-hydraulic channels in the core changed from 33, in the original nodalisation, to 132 [29]. A single pressure spike of 0.055 MPa was applied during 1 second in the turbine. In this case, DR decreases slightly and NF increases with respect to the base case. The values obtained are DR = 0.438 and NF = 0.312 Hz, and these results are closer to the experimental DR and NF.

5.2.4. Case PT4

The perturbation starts at the time zero. As it can be observed in Figure 10, the pressure perturbation brings the power to oscillate. In a short time (10 seconds), the oscillations are damped and the power oscillates around the mean value with amplitude of 0.61%. This "residual" power oscillation, ±0.6%, is very small and cannot be considered as an actual indication of instability from the view point of the oscillation amplitude: only amplitudes greater than 10% of the mean value are considered as an indication of instability [37].

Table 5 presents calculated DR and NF in comparison with the experimental data for PT4. The results are in reasonable agreement with the experimental one though NF is a little smaller in the prediction. The results given by the algorithms are in reasonable agreement between each other for DR as well as for NF, considering the time window from 4.2 to 30.0 seconds.

The main conclusions from all the pressure-perturbation investigations are the following.

(i) ADRI and DRAT are capable of predicting DR and NF, in most cases, in reasonable agreement between them, in spite of the fact that the algorithms are based on very different mathematical assumptions.

(ii) The results obtained for DR showed that this value changes in dependence of the time window considered in the analysis. It is, therefore, very important to pay attention to selecting an adequate signal time interval, representing the linear dependencies of the system.

(iii) Performing DR calculations, using the algorithm ADRI, needs having a sampled signal with a minimum of about 500 points. On the other hand, DRAT is capable of evaluating DR for any number of points sufficient to depict a reasonably complete swinging of the considered parameter. Anyway, a higher number of points give a more realistic result for DR.

(iv) The results of DR, from the LFST experimental data, were obtained with a completely different calculation method with respect to those adopted by ADRI and DRAT, and this fact can be a cause of the discrepancies found between experimental and calculated DRs.

Figure 11: Relative power evolution considering two cases of FW temperature decrease: 10 K and 50 K.

Figure 12: Relative power evolution considering one recirculation pump stopped for two different time intervals: 5 seconds and 1 second.

Figure 13: Relative power evolution after recirculation pump trip.

(v) The calculated DR for the point PT4 is in good agreement with the experimental one. However, the calculations showed a small constant power oscillation that is observed after the perturbation. This confirms that some nonlinear effects come into play in the analyses.

(vi) For all the analyzed cases, the frequencies of the oscillations varied between 0.3 and 0.5 Hz, which is a typical frequency range for this kind of instability events.

5.3. Feedwater (FW) temperature decrease

A fault of FW preheaters (e.g., due to sudden depressurization in one preheater on the heating side) and of FW pumps may cause an FW temperature decrease that results in colder water at core inlet. This creates the potential for reducing the volume occupied by steam in the core and a consequent increase in moderation and fission power.

The transient calculation was performed considering only the point PT3 at the base of an operating condition. Considering that there are no experimental data for comparison, this calculation represents only a sensitivity analysis.

The event has been simulated in the Peach Bottom by the coupled RELAP5/PARCS. To perform the transient calculation, the FW temperature value was reduced by 10 and 50 K (two separated cases) during five seconds. The feedwater (element number 500 in the nodalization) corresponds to a *tmpdvol* component in the RELAP5 input deck, in which it is possible to vary the temperature as a function of time.

It was observed that no significant variation in the power evolution has occurred for the two analyzed cases. Power experienced an increase of 11% (case $\Delta T_{FW} = -50\,K$) and, in few seconds, it returned to the steady-state level as it can be seen in Figure 11.

5.4. Recirculation pump trip (RPT)

In similarity with the previous case of FW temperature perturbation, there are no experimental data available for comparing the results of the calculations performed for this type of transient, which must be again regarded as a sensitivity analysis.

The stop of a recirculation pump causes a sharp decrease in the core flow, which generates a significant negative reactivity insertion that tends to reduce power and, consequently, the amount of steam generated.

To simulate the event, the recirculation pump speed was brought to zero (in the RELAP5 input deck) for one and five seconds, respectively, in two different analyses. In the transient, the pump is shut down for a short time interval and then it is switched on again. The relative power evolutions for the two cases are shown in Figure 12. One of the two pumps is stopped at the time zero. As it can be seen, the variation in the pump trip duration causes a small variation in the power oscillation amplitude, and the oscillations are terminated at the same time.

In addition, another case was considered in which both pumps were stopped, at the same time, for one second. As it can be noted in Figure 13, the amplitude of the power oscillation in this case is higher, as it is expected to occur. The

Table 6: DR and NF calculated by ADRI.

Calculation case	DR	NF (Hz)
(1) One pump stopped for 1 s	0.427	0.278
(2) Two pumps stopped for 1 s	0.345	0.280

Figure 14: Initial control rods distribution (point PT3—steady state).

periods of oscillation, for both cases, are practically the same; the reactor reaches stability nearly in the same time for these two cases.

DR and NF calculations were performed for both cases using the algorithm ADRI. The core power exhibits damped in-phase oscillations with a decay ratio value less than 1.0, characterizing a stable system after the transient event. The results of DR and NF, presented in Table 6, are very similar to those found for the case of the pressure perturbation.

5.5. Control rod bank movement

To simulate this hypothetical transient, the control rod banks were withdrawn beginning from the steady state positions for the case PT3. Figure 14 shows the initial control rods distribution. The position identified by "48" represents the bank totally removed. The simulation was performed using the *MOV_BANK* card in the PARCS input data. The rod banks were continuously withdrawn, during the period from 20 to 100 seconds. Then the calculation carried on until 200 seconds with the same rod banks configuration. During the analysis, out-of-phase oscillations were observed. It must be emphasized that the purpose of this study is not to simulate a realistic reactor transient, but just to investigate the possible mode of oscillations that could be observed in a hypothetical case in which the reactor was brought to unstable conditions by raising its power.

Figure 15 shows the power evolution during the calculation. Power oscillations last until the end of the calculation, but the amplitude falls drastically after 160 seconds of calculation and the reactor, due to subsequent feedback effects,

(a) Overall transient

(b) 100 to 120 s time window

Figure 15: Total power evolution.

reaches less unstable conditions. Only after about 90 seconds of calculation, strong oscillations in the power are observed. The analyses showed that out-of-phase oscillations are dominant after about 100 seconds in the transient calculation.

Two TH core channels, 11 and 22, were taken as references to demonstrate the out-of-phase phenomenon during the transient. In the TH nodalization, channels 11 and 22 are localized in two different parts of the core with respect to the core center. Figure 16 presents the trend of mass-flow rate in the channels 11 and 22. Observing the mass-flow evolution in a greater detail (Figure 16(b)), it is possible to check the out-of-phase behavior: when the mass flow reaches a maximum value in a channel, in the other, it presents a minimum value. In addition, Figure 17 represents the void fraction for the same channels in different quarters of the core; also this parameter is found to behave out of phase in the two considered positions.

Figure 18 shows the cladding temperature evolution at the axial level 3 in six core TH channels. The variable was observed for all 33 TH channels. Channel 11 reached the most elevated values of temperature. The cladding temperature increases drastically in one extreme of the fuel assembly (axial level 3) after the rod banks are removed. This phenomenon is directly connected with the change

(a) Overall transient (b) 100 to 120 s time window

Figure 16: Inlet mass flow rate evolution for two selected channels located in different quarters of the core.

(a) Overall transient (b) 100 to 120 s time window

Figure 17: Void fraction evolution at mid height (axial level 12) in two selected channels.

Figure 18: Cladding temperature at the axial level 3 in six different core channels.

Figure 19: Evolution of the axial power profile during the removal of the control rod banks.

Figure 20: Relative power evolution during a period of oscillation of 1.82 seconds (from 121.101 to 122.921 seconds in the transient calculation).

in axial power distribution, which is drastically affected by the rod banks withdrawn as it can be seen in Figure 19. Since after rod withdrawal, the coolant density is much higher at the bottom core inlet, the expected bottom-peaked power profile is obtained. The temperature difference between the cladding and coolant, in time, was verified and can reach for about 700°C testifying for the occurrence of dry out.

Figure 20 illustrates five moments of power evolution during one cycle of transient (from 121.101 to 122.921 seconds). As it can be seen, 3D power plots provide clearer visualization of the time evolution of the phenomenon.

The TH nodalization has been modified from 33 to 132 TH core channels. The main aim of this change was to eliminate the existence of identical channels in different halves of the core. In this way, the TH mapping is independent on each half of the core permitting to verify the independent behavior of each one during the transient. The obtained new results for the control rod banks movement can be seen in the currently published work [38].

6. CONCLUSIONS

RELAP5/MOD3.3 thermal-hydraulic system code and the PARCS-2.4 3D neutron kinetic code were coupled to simulate transients in a BWR. DR and NF calculated from the experimental power oscillations data from the LFST were compared, in this work, with the data calculated from the simulations.

The coupled system was firstly validated for the test points PT1, PT2, PT3, and PT4 in steady-state conditions. Thereafter, simulations of PP events to the four cases were shown and compared with the available experimental data. Two algorithms, ADRI and DRAT, were used to perform the calculations. ADRI and DRAT predicted DR and NF values in most of the cases in reasonable agreement between them in spite of the fact that the algorithms are based on very different mathematical assumptions.

Calculated DR is in reasonable agreement with experimental results for the tests PT3 and PT4, but it is overestimated for the tests PT1 and PT2. One possible cause for the

low value of DR found in the experiments for PT1 and PT2 is the change of the xenon concentration taking place between those first test conditions.

Other interesting transient cases, considered as sensitivity analyses (feedwater temperature decrease, recirculation pump trip and control rod banks movement), have been simulated using the same operating conditions of the LFST PT3. Their interest is, anyway, related to the possibility of quantifying the margin to unstable behavior bypassing the uncertainty introduced by different definitions adopted for DR.

In the simulations, the feedwater temperature decrease did not represent a significant variation in the power evolution and the reactor seems to be very stable in the analyzed case of FW temperature variation. In the case of the sudden pump trip event, the core power exhibited damped in-phase oscillations with a decay-ratio value less than 1.0, characterizing a stable system after the transient event. The obtained results of DR and NF were close to those found in the case of the pressure perturbation.

The control rod bank movement transient was considered to study the out-of-phase behavior occurring in the reactor as a consequence of raising power by the removal of the control rod banks from the BWR core. The analyses taking into account two TH channels, localized in two different quarters of the core, demonstrated that the mass-flow rate and the void fraction are totally out of phase after 100 second of calculation. Obviously, in the calculation, the scram intervention was not considered because the main interest was to assess the core parameters evolution during an out-of-phase oscillation. The simulation showed that, if there is not a scram intervention, the cladding temperature can reach high values and dry-out can occur.

In summary, in-phase and out-of-phase modes of power oscillation in a BWR were presented in this work. Though uncertainties still remain in relation to the definition of DR to be adopted for comparison with experimental data and about the effect of thermal-hydraulic radial discretization of the core on the obtained results, the information obtained in this work will contribute for further more detailed studies of such complex phenomena having considerable interest in the safety of boiling-water nuclear reactors.

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Research Article

Effect of Coolant Inventories and Parallel Loop Interconnections on the Natural Circulation in Various Heat Transport Systems of a Nuclear Power Plant during Station Blackout

Avinash J. Gaikwad,[1] P. K. Vijayan,[2] Sharad Bhartya,[3] Kannan Iyer,[4] Rajesh Kumar,[1] A. D. Contractor,[1] H. G. Lele,[1] S. F. Vhora,[5] A. K. Maurya,[5] A. K. Ghosh,[1] and H. S. Kushwaha[1]

[1] *Reactor Safety Division, Bhabha Atomic Research Centre, Trombay, Mumbai 400085, India*
[2] *Reactor Engineering Division, Bhabha Atomic Research Centre, Trombay, Mumbai 400085, India*
[3] *Chemical Engineering Department, Indian Institute of Technology (IIT), Powai, Mumbai 400076, India*
[4] *Mechanical Engineering Department, Indian Institute of Technology (IIT), Powai, Mumbai 400076, India*
[5] *Nuclear Power Corporation of India (NPCIL), NUB, Anushaktinagar, Mumbai 400094, India*

Correspondence should be addressed to Avinash J. Gaikwad, avinashg@barc.gov.in

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Provision of passive means to reactor core decay heat removal enhances the nuclear power plant (NPP) safety and availability. In the earlier Indian pressurised heavy water reactors (IPHWRs), like the 220 MWe and the 540 MWe, crash cooldown from the steam generators (SGs) is resorted to mitigate consequences of station blackout (SBO). In the 700 MWe PHWR currently being designed an additional passive decay heat removal (PDHR) system is also incorporated to condense the steam generated in the boilers during a SBO. The sustainability of natural circulation in the various heat transport systems (i.e., primary heat transport (PHT), SGs, and PDHRs) under station blackout depends on the corresponding system's coolant inventories and the coolant circuit configurations (i.e., parallel paths and interconnections). On the primary side, the interconnection between the two primary loops plays an important role to sustain the natural circulation heat removal. On the secondary side, the steam lines interconnections and the initial inventory in the SGs prior to cooldown, that is, hooking up of the PDHRs are very important. This paper attempts to open up discussions on the concept and the core issues associated with passive systems which can provide continued heat sink during such accident scenarios. The discussions would include the criteria for design, and performance of such concepts already implemented and proposes schemes to be implemented in the proposed 700 MWe IPHWR. The designer feedbacks generated, and critical examination of performance analysis results for the added passive system to the existing generation II & III reactors will help ascertaining that these safety systems/inventories in fact perform in sustaining decay heat removal and augmenting safety.

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1. INTRODUCTION TO 700 MWe PRESSURISED HEAVY WATER REACTOR (PHWR)

In the 700 MWe PHWR, the primary coolant heavy water under pressure removes (with partial boiling at channel exit) the fission heat generated in the reactor core and transfers it to the secondary coolant (light water) in the steam generators (SGs). The primary heat transport (PHT) system consists of 392 fuel channels. The PHT system is divided into two identical loops. Each loop consists of two primary circulating pumps (PCPs) and two SGs in a figure of eight loop configuration as shown in Figure 1. There are two passes through the core for each loop. As the primary coolant flows over the fuel bundles placed inside the channels, it picks up the fission heat in four passes through the reactor core. In each pass, 98 channels are connected to a common header at each end of the reactor. After picking up heat from the reactor core, the coolant flows through the reactor outlet header (ROH) into the tube side of the SGs. After transferring heat in the SGs, the primary coolant is pumped (by the primary

Figure 1: 700 MWe PHWR primary heat transport system.

circulating pumps (PCPs)) back to the reactor core through reactor inlet header (RIH).

The SG provides the thermal linkage between the PHT system and the secondary coolant system. The SGs deployed in the 700 MWe reactors are of the inverted U-tube type with integral drum. The secondary fluid flows in the shell side, and the hot primary coolant from the reactor core (ROH) flows inside the U tubes. The total boiler feed is given at the top of the downcomer. The recirculation flow from the steam drum, after mixing with the feed water, flows down the annulus (downcomer). Then, it rises up through the main boiling zone, as it picks up the heat. After extracting the heat, the secondary fluid (steam-water mixture) rises through the riser and then passes through the steam separator and dryers. Here, the two-phase mixture gets separated into saturated water and steam. The former is led downwards, after mixing with the feed water, to the annular downcomer of the SG, while the latter goes to the steam outlet and then to the turbine.

The average of the two ROH pressures is controlled around a set point of 101.0 kg/cm^2, to avoid excessive boiling and over-pressurisation in the PHT system. In the 700 MWe PHWR, for controlling the PHT system pressure, a pressuriser (surge tank) is also provided along with the Feed/Bleed system for maintaining the coolant inventory. The feed/bleed system is provided for controlling the water level in the pressuriser. Steam bleed valves (SBVs) are provided on top of the pressuriser vapor volume to control the increase in the PHT system pressure, by relieving the heavy water steam into the bleed condenser (BC) through the PHT system pressure controller. Electrical heaters are provided to take care of the low-pressure transient, by switching on the heater banks to increase the pressuriser pressure. The hot bleed from the RIH, and the relief from the south ROH flashes into a two-phase mixture inside the BC. The BC pressure is controlled at 34 Kg/cm^2. In the event of an increase in the PHT system pressure, the SBVs also start relieving heavy water steam into the BC. At 100% full power steady state, the reactor core inlet temperature is 266 C, the core outlet temperature at the ROH is 310 C, and the reactor core exit quality is around 3% only.

2. EXPERIENCE DURING 220 MWe PHWR NAPS FIRE INCIDENT

An incident of fire in the generator at one of the units of Narora Atomic Power Station had led to gradual loss of class-IV and class-III power supplies for all the plant loads such as primary coolant pumps, pressurising pumps, shutdown cooling pumps and main and auxiliary boiler feed pumps on secondary side resulting in a station blackout like scenario. Fire water had to be manually hooked up by going to boiler room. Thermosyphon in the primary and the secondary systems did work, and there were no fuel failures,

with no activity release. Nuclear power plants have safety systems which are designed to be highly reliable. In spite of the various built-in provisions for very high reliability, requirement of analysis and suitable provisions for beyond design basis event (BDBE) scenario of station blackout, LOCA without ECCS actuation, and so forth, are necessary.

A debate on the concepts to be adopted for nuclear power plants to be built in the future has been underway with several different approaches being in vogue. One approach being the "evolutionary" approach which recommends that the plant design is similar to well proven design with some enhancements in safety. Other approach is to go for passive design. The "evolutionary" approach considers greater redundancy and diversity whereas latter relies on features such as lower-core power density, greater RB volumes, and greater reliance on thermosyphon. Broad lessons from Chernobyl, NAPS fire incident, and even recent Tsunami incident with respect to Kalpakkam show that continued availability of heat sink is the major issue to be addressed even if provision of this is done in somewhat "simple" manner.

3. PASSIVE DECAY HEAT REMOVAL SYSTEM (PDHRS) FOR 700 MWe PHWR

Long term removal of decay heat is essential to avoid fuel heat up even after reactor trip or shutdown. Different heat sinks are available for various states of reactor shutdown such as a normal shutdown with class IV available or accident conditions such as LOCA. During normal shutdown, initially the decay heat is removed by steam generators with steam being dumped to condenser and/or through atmospheric steam discharge valves (ASDVs). Feed water make up to steam generators is by main or auxiliary boiler feed pumps (MBFPs or ABFPS). Further primary cool down to room temperature is by shutdown cooling system with the heat getting transferred to active process water system (APW) and subsequently to service water loop and then to atmosphere.

In case of station blackout, the envisaged heat sink is the passive decay heat removal system (PDHRS) for recirculating the steam generator secondary side inventory through the U-tube condenser inside PDHRS tanks.

For removing the heat generated by the PHT, PDHRS is provided for condensing the steam and recirculating the steam generator inventory during station blackout scenario (see Figure 2). This system consists of a horizontal U-tube condenser inside a tank having inventory of 125 m^3 of water. The U-tube condenser is connected to a 150 mmNB line taken off from the main steam line, this steam gets condensed inside the 50 mmNB tubes of the condenser, and the condensate returns back to steam generator. During this process, the decay heat from primary side is given up to the tank inventory which would initially heat up, later starts boiling and the steam gets vented to the atmosphere. Four sets of such PDHR tanks, gets, and piping are provided one set connected to each of the four steam generators. The stored inventory in the tank is adequate to provide decay heat removal for more than 8 hours during which inventory make up to the tank can be initiated. During the normal operation

Figure 2: Layout for the four SGs and the 4 PDHRs.

of plant, periodic make up to this tank is envisaged by DM water connection. During the station black out scenario, the make up to this tank is envisaged from the firewater.

As a design practice, it is always followed that the tested PHT system layouts (with multiple loops and parallel paths) of the earlier generation and operating power reactors are augmented with new concepts/systems such as the PDHRS and other passive systems. It is conveniently assumed that addition of these systems will enhance the NPP safety by continued removal of decay heat under adverse conditions. The performance analysis and the present studies point to another aspect which is very important, and it points out at the degradation/failure of heat removal in the presence of more than adequate coolant inventories in the primary, secondary, and the PDHRS. With detailed parametric studies and analysis of all the anticipated scenarios, this problem can be overcome and the effective use of all the available safety systems and coolant inventories can be achieved for the SBO case.

It has been reported in recent literature that RELAP5/MOD3.2 is capable of simulating natural circulation phenomena [1–4]. The SG boil-off and SBO response for a PWR are described in detail [5]. Reference [6] describes the incorporation of PDHRS, its design, and modeling. Reference [7] describes the application of RELAP5 for SBO analysis. The present study deals with boil-off in PDHRS connected to the secondary side of SGs and the effects of inter-loop connection leading to depletion of heat removal in the presence of large coolant inventories in the SGs and the PDHRS. Such study aims at analysing all the worst possible SBO scenarios and design verification to avoid severe accident conditions [8]. The thermal hydraulics modeling methodology and simulation philosophy of 700MWe PHWR for the present study are based on [9–16], though RELAP5/MOD3.2 code has been used here. Sensitivity studies were carried out to finalize the present nodalisation, which are not presented due to space limitation.

4. MODELING & NODALISATION

Primary heat transport (PHT) system model has been developed with two loops connected to the pressuriser and four passes through the core (see Figures 3 and 4). In each pass, 98 channels are modeled using 10 axial volumes. 10 heat

Figure 3: 700 MWe PHWR loop-1 nodalisation.

Figure 4: 700 MWe PHWR loop-2 nodalisation.

slabs are connected to the fuel in each pass of core. The feed and bleed systems are connected to the headers on one side of the reactor, and on the other side the pressuriser is connected to both outlet headers through the surge line. Surge line is modeled using two pipe volumes and a branch. All the headers are modeled as branches. The pressuriser is modeled using 12 control volumes with 1.5 MW electrical heaters and 10 heat slabs. The switching logic for pressuriser heaters has been developed based on the error in the PHT pressure, the steam bleed valves (SBVs) open following an increase in the PHT system pressure. The secondary system model (see Figure 5) includes simulation of the steam generator with pressure controller, level controller, and all the steam lines. The heated riser region is simulated with 10 control volumes; the unheated riser volume is also modeled. 20 heat slabs are used for connecting the primary and secondary systems thermally. The steam drum model includes 10 control volumes. The downcomer model also includes 10 control volumes. The PDHR system (see Figures 6 and 7) model is also integrated with all the 4 steam generators. All the steam lines up to governor, CSDV, and ASDV are simulated using several pipe and single volumes. Steady state conditions were

Figure 5: Steam generator nodalisation.

Figure 6: Schematic of a PDHR.

achieved on both the PHT (ROH quality ~3.5%) and SG side integrated together for a plant simulation model.

5. RESULTS AND DISCUSSION

SBO scenario includes the loss of all the operating pumps, that is, 4 primary circulating pumps (PCPs), primary pressurising pumps (PPPs), and all the boiler feed pumps (BFPs). The reactor trip signal based on the loss of all the PCPs; is generated within one second. To study the effect of the various inventories and the parallel multiple loop interconnections, three case studies carried out, are is, (i) effect of initial coolant inventory in the SGs prior to cooldown, (ii) effect of the steam line interconnection, and (iii) effect of the PHT loop isolation.

5.1. *Effect of initial SG inventory*

Two cases are presented in this category, that is, (a) SBO analysis with PDHR valving in after 6 minutes. (b) SBO analysis with no delay in valving of the PDHR. Because of the

Figure 7: PDHR nodalisation.

delay in valving in of the PDHRS (which is a closed system), certain amount of SG inventory is lost through the ASDVs in the first case. In the second case, the initial inventory of the SG coolant is higher prior to the initiation of cooldown with the PDHRS.

5.1.1. *Station blackout analysis with PDHR valving in after 6 minutes*

Station blackout was initiated by tripping all the PCPs, PPPs, and the boiler feed pumps (BFPs) at $t = 0$ second. The reactor trip on no PCP available signal was delayed by one second. The actual reactor power reduction was further delayed by one second considering the delays for rod insertions on conservative side. Hot shutdown condition was maintained for initial six minutes with the help of atmospheric steam discharge valves (ASDVs) mounted on the SG steam lines, after this all the four PDHRs valves were opened to condense the steam from SGs, that is, at $t = 361$ seconds. Once the PCPs are tripped, the differential pressure across the headers/channel decreases, and all header pressures start falling together.

Following the reactor trip and the valving in of the PDHR, the PHT system pressure (see Figure 8) starts falling (80 bar at $t = 508$ seconds), and the pressuriser level falls below 1.7 m, which leads to isolation of the pressuriser. After this, the PHT system pressure falls rapidly to 8 bar at $t = 7000$ seconds, then it remains around this value approximately up to 27000 seconds. Later on the PHT system pressure, Pressure shows an increasing trend again following depletion of shell side inventory in the PDHRs. It comes down to about 43 ton from 121 ton. At $t = 47000$ seconds, it falls to 11 ton, and thereafter it remains almost constant as the liquid level in the PDHRS falls below the tube bundle.

Figure 8: PHT system pressure case-6 minutes.

Figure 9: SG Pressure variation case-6 minutes delay in valving in PDHRS.

The SG pressure (see Figure 9) shows a peak of 49.3 bar at $t = 45$ seconds initially following the turbine trip initiated due to reactor trip, it falls to 41.8 bar, at $t = 7000$ seconds, then it comes down to 7.03 bar at $t = 27000$ seconds.

All the four SGs are connected through the steam lines. Any PHDR through the steam lines can draw the steam from all the four SGs, but it sends back condensate only to the SG to which it is connected. Though the difference between the four SG pressures is very small, the SG with maximum SG pressure sends more steam to the other

Figure 10: SG level variation case-6 minutes delay in valving in PDHRS.

PDHR in addition to the PDHRs to which it is directly connected. The return flow to this SG is only equal to the steam flow which was going to the corresponding PDHR (directly connected), the steam flow to other PDHRs sends the condensate to the other SGs. This initiates an inventory transfer, beyond $t = 1800$ seconds, due to low-driving forces encountered during natural circulation at low pressure in the PHTs, SGs, and PDHR, it is observed that the SG level (see Figure 10) in two SGs goes down and the other two SGs, it shows an increasing trend. Inventory transfer through steam lines is observed but the total SGs inventory remains constant. Another initiation cause (for difference in the four SG pressures) is the difference in the PHT flow through all the four SGs during natural circulation.

The total primary core flow (see Figure 11) remains around 7% at $t = 7000$ seconds after this it shows a slow-decreasing trend. The PHT system core exit quality remains low up to 15000 seconds, then it shows lot of oscillation (also observed in the core flow), it even reaches values up to 50% and above, up to 30000 seconds, then it comes back to lower values (<2%). It can be concluded that the PDHRs can remove the decay heat safely up to 10 hours, during this period additional water inventories can be lined up.

5.1.2. *SBO with no delay in valving in of the PDHRs*

In the previous station blackout case, it was assumed that the PDHRs valves are opened after 6 minutes delay. In the present case, it is assumed that the PDHRs valves are opened without any delay immediately after sensing the station blackout at $t = 1$ second. The results obtained are similar to the earlier predictions (see Figures 12, 13, 14, 15, and 16) but the oscillation/fluctuation in the PHT and the SG flows are relatively dampened, and more stable flow conditions

Figure 11: Core flow variation case-6 minutes delay in valving in PDHRS.

Figure 13: SG level variation case-no delay in valving in PDHRS.

Figure 12: PHT pressure variation case-no delay in valving in PDHRS.

Figure 14: Core exit quality variation case-no delay in valving in PDHRS.

are observed due to higher SG inventories. In the 6 minutes PDHR valving delay cases, the SG inventories go down from 32 ton to almost 27 ton, a loss of 5 ton through atmospheric steam discharge valves (ASDVs) in the initial 6 minutes. For the no delay case, the ASDVs open only for a short period, and the SG inventory remains around 32 ton with negligible loss. As the steam lines were not isolated, diverging trend was observed for SG levels (see Figure 13). It can be concluded that valving in of the PDHR should not be delayed if a SBO is confirmed.

5.2. SBO with steam line isolation

All the steam lines interconnections were isolated to avoid any inter loop inventory transfer through the parallel paths available in the steam lines. Each of the SGs is connected only

Figure 15: Clad temperature variation case-no delay in valving in PDHRS.

Figure 16: PDHR shell inventory variation case-no delay in valving in PDHRS.

to the corresponding PDHR, and all other connections are snapped.

Station blackout analysis with isolation of steam lines

In the previous station blackout transient analysis, it was observed that the level in two SGs was increasing significantly after 30000 seconds, and in the other two SGs, it was going down correspondingly, keeping the total inventory of all the four SGs constant. This was attributed to low-driving forces encountered during natural circulation at low pressure in the

Figure 17: SG level case-steam line isolation.

PHTs, SGs, and PDHR, resulting in an inventory transfer through steam lines. To support these explanations, another hypothetical station blackout with complete isolation of the steam lines was carried out. Here, it was observed that the SG levels (see Figure 17) do not diverge and remain almost at the same value for all the four SGs, though the levels are not exactly same, but they follow a similar trend (variation around 9 m). For most of the other parameters, the trend is almost similar as compared to the previous blackout analysis without isolation of the steam lines. It can be concluded that the alternative parallel path provided by the steam lines leads to inventory transfer. This leads to drying out of one SG in each loop, but the decay heat removal is unaffected as the other SG in the loop with its own inventory and the transferred coolant inventory can carry on the decay heat removal effectively for the complete loop.

5.3. 3 PDHRs with/without primay loop isolation

In these cases, one PDHR connected to one of the SG in loop-1 was kept isolated during the transient. As there is no heat removal from one of the bank, the primary coolant at higher temperature enters in the core through the return pass. After some time as the two phases appear in the return pass, there is a drastic reduction in core flow leading to a stagnation phase at $t > 4200$ seconds (see Figure 18). Since the affected loop pressure was slightly higher as compared to healthy loop (cooling unaffected), there is a continuous transfer of primary inventory from loop-1 to loop-2 through the ROH connection though the pressuriser is isolated, but the PHT loops are connected.

To avoid this inter-loop PHT inventory transfer, a case study with the isolation of these two primary loops was also carried out. With the arrest of primary inventory removal from the affected loop-1 (with one working and another

Figure 18: Core flow case-3PDHRs with no loop isolation.

Figure 19: Clad temperature case-3PDHRs with no loop isolation.

Figure 20: Core flow case-3PDHRs with loop isolation.

Figure 21: Clad temperature case-3PDHRs with loop isolation.

nonworking PDHR), the core flow (see Figure 20) through the two passes of both affected loop-1 and healthy loop-2 remains at a higher-positive value for a considerable period of time (~21 000 seconds), and core cool ability is maintained. The PDHR isolation valve failure leads to the failure of the corresponding PDHR, probability of this event cannot be neglected. Based on this study, it is highly recommended that the two primary loops should be isolated when there is an unsymmetrical mode of PDHR operation.

5.3.1. *Station blackout (SBO) analysis with only 3 PDHRs available and with PHT loops isolation*

In this SBO transient analysis, it is assumed that, one passive decay heat removal (PDHR) heat exchanger fails, as a result only 3 PDHRs are available to cool the steam from all the four SGs. It was also assumed that the pressuriser and the 2 PHT loops are isolated from each other on pressuriser low level. The results obtained are similar to the all PDHRs available

case for the initial period, but the decay heat removal is hampered within 7 hours, whereas for SBO with all PDHRs available case, the decay heat removal is not affected for more than 17 hours.

For the PHT loop isolation case, the nonfunctional PDHR inventory remains at 121 ton throughout. For the other only working PDHR, in the affected PHT loop-1, the PDHR inventory falls to 26.7 ton in 8.3 hours, whereas in the healthy PHT loop-2, with both PDHRs working, the PDHR inventory falls to 16.5 ton in the corresponding period. After 7 hours, the affected loop clad temperature (see Figure 21) shoots up sharply following core flow (see Figure 20) reduction and exposure of the PDHR heat exchanger tubes in the only working PDHR in that loop. For the SG without a working PDHR, the SG-inventory falls to almost one ton within 1.1 hour, after this it remains around this value and does not fall to zero. For the SG in the affected PHT loop-1 with only working PDHR, the SG-inventory comes down below one ton after 5.8 hours, that is, both the SGs in the affected PHT loop-1 dryout due to inventory transfer to the SGs in healthy PHT loop-2.

This unfavorable situation is caused by inventory transfer from the SGs of the affected loops to the other loops where both PDHRs are operational. Steam flow from all the four SGs to 4 PDHRs is guided based on the differential pressure between these components. More steam flow goes from the SG drum with the highest pressure (i.e., low-PHT flow) to the PDHRs, based on the pressure, all the SGs send steam to the PDHRs. The SG receiving maximum steam/condensate will accumulate more inventory. This phenomenon was observed for all the SBO cases analysed (except for the SBO without steam lines). It leads to total SG inventory transfer from loop with one PDHRs working to the SGs of the loop with 2 PDHRs working, as a result both the SGs in the one PDHR available loop-1 dryout after 5.8 hours for the PHT loop isolation case, and the clad temperature (see Figure 20) increases rapidly.

5.3.2. SBO analysis with only 3 PDHRs available and without PHT loops isolation

Here, only 3 PDHRs are available following SBO, and it was also assumed that only the pressuriser isolates from the 2 PHT loops, and the 2 PHT loops remain hydraulically connected through the surge lines as the pressuriser level falls below 1.7 m. For this case, the primary flow reduces to almost zero in the one of the core passes in the affected loop, leading to an increase in clad surface temperature (see Figure 19, more than 1000 C) after ~1.5 hours. The primary coolant flow (see Figure 18) in this core path is hampered by the inter-loop inventory transfer, as the affected loop which is at higher pressure and temperature tries to equalise pressure, forcing flow out from this core path. As the flow reduces and stagnates (see Figure 18), the core exit quality increases sharply (>1.0). Though a lot of coolant inventory is available in the SGs and also in the PDHRs (89 ton), the clad temperature (see Figure 19)shoots up due to core flow stagnation. For the SG without PDHR working, the SG level comes down almost to zero, in one hour. For the other SG in the healthy loop-2 with working PDHRs, it shows an inventory corresponding to 12 m level at about 1.5 hours. Here, also the phenomenon of SGs inventory transfer from the affected loop to the healthy loop is observed, but the clad temperature shoots up far ahead of dryout due to PHT flow stagnation following PHT inventory transfer from affected loop to healthy loop.

The results obtained with RELAP5 model show a similar behavior for natural circulation as reported in the literature [1–4]. It can be concluded that the 2 PHT loops should be isolated following an SBO to avoid inter-loop inventory transfer through the surge lines, which leads to stagnation of core flow in the affected loop due to unfavorable pressure distribution. This undesirable situation is further aggravated by the inventory transfer from the SGs of the affected loops to another loop with both PDHRs operational.

CONCLUDING REMARKS

(1) It can be concluded that the 4 PDHRs, with an initial inventory of 121 ton each at 40 C, can remove the core decay heat without any increase in the clad temperature for about 17 hours without the help of make up system, if all the 4 PDHRs are available.

(2) No delay in valving in of the PDHRs is recommended after confirmation of a station blackout situation, as a higher inventory in the SG leads to more stable natural circulation in the secondary and the primary heat transport system. The rate of change of primary and secondary coolants structure temperature is also moderate.

(3) The secondary inventory transfer from SGs in one loop to SGs in another occurs due to parallel paths interconnected steam lines. Following the SBO and cooldown with natural circulation at low pressure and low-driving forces in the PHT, SGs, and PDHRS, this phenomenon cannot be avoided.

(4) For the SBO with three PDHRs available case, the PHT system inventory transfer takes place from the affected loop (cooling affected due to inventory transfer to other loop) to healthy loop (cooling unhindered), due to pressure imbalance and parallel paths inter-connected surge lines available. In this case, the isolation of the two PHT system loops is helpful in mitigating the consequences of failure of one of the 4 PDHRs, and without the loop interconnection the decay heat removal is not hampered for 7 hours.

(5) The designer feedbacks generated from the analysis, and critical examination of performance analysis results for the added passive system to existing generation II and III reactors will help ascertaining that the these safety systems/inventories in fact perform in sustaining decay heat removal and augmenting safety.

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Research Article

Deterministic Analysis of Natural Circulation Events at the Ignalina NPP

Algirdas Kaliatka, Eugenijus Uspuras, and Virginijus Vileiniskis

Laboratory of Nuclear Installation Safety, Lithuanian Energy Institute, Breslaujos street 3, 44403 Kaunas, Lithuania

Correspondence should be addressed to Algirdas Kaliatka, algis@mail.lei.lt

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Eight main circulation pumps (MCPs) are employed for the cooling of water forced circulation through the RBMK-1500 reactor at the Ignalina nuclear power plant (NPP). These pumps are joined into groups of four pumps each (three for normal operation and one on standby). In the case of all MCPs trip, the reactor shutdown system is activated due to decrease of coolant flow rate. At the same time, after the pump trip, the coolant to the reactor fuel channels during the first few seconds is supplied by pump coastdown. Later, the reactor is cooled by natural circulation. The main question arises whether this coolant flow rate is sufficient to remove the decay heat from the reactor core. This paper presents the investigation of all MCPs trip events at the Ignalina NPP by employing best estimate code RELAP5 and methodology of uncertainty and sensitivity analysis.

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1. INTRODUCTION

The Ignalina nuclear power plant is a twin unit with two RBMK-1500, graphite moderated, boiling water, and multichannel reactors. Unit 1 was permanently shutdown in 2004. A schematic representation of one main circulation circuit (MCC) loop is given in Figure 1. The MCC is divided into two halves: the left and the right loops. The MCPs (5) are joined in groups of four pumps each (three for normal operation and one on standby). The MCPs feed a common pressure header (PH) (8) on each side of the reactor. Each pressure header provides coolant to 20 group distribution headers (GDH) (9), each of which in turn feeds from 38 to 43 fuel channels (11). The coolant flow rate through the individual fuel channel (FC) is regulated by isolation and control valves (ICV) (10), which are mounted in the lower water communication lines. Coolant passing through FCs is boiled and part of the water is evaporated. Steam-water mixture through steam-water communication lines (12) flows to drum separators (DS). The steam, which is separated in the DS, is supplied to turbines through steam lines (13). A detailed description of the Ignalina NPP can be found in [1].

At the Ignalina NPP type CVN-8, MCPs are employed for the forced circulation of cooling water through the RBMK-1500 reactor. These pumps belong to the "wet" stator pump group. The CVN-8 type is a centrifugal, vertical, single-stage pump with a sealed shaft. To increase the rotary inertia in order to prolong the rotation of the shaft in the event the electric motor fails, the massive 0.2 m outside diameter and the 0.195 m thick steel flywheel are mounted on the motor shaft. In the all-pumps-trip case, the coolant, due to high inertia of pump flywheel during the first 40–60 seconds to the reactor fuel channels, is supplied by pumps coastdown. Later, the natural circulation through the core is established. The reactor shutdown system is activated due to the decrease of coolant flow rate. All 211 rods of the control and protection system (CPS) are inserted into the reactor core within approximately 8 seconds. The main question that arises is whether this coolant flow rate is sufficient to remove the decay heat from the reactor core.

Best estimate model of RELAP5 Ignalina NPP RBMK-1500 reactor cooling circuit was developed by Lithuanian Energy Institute (LEI) for the investigation of MCP trip events. This model includes forced circulation circuit, steam lines, and safety systems necessary for transient and accident process analyses. Detailed description of RELAP5 nodalization scheme can be found in [2]. The obtained results were compared with the measurements of Ignalina NPP.

Figure 1: Schematic representation of one loop of the RBMK-1500 MCC: 1-DS, 2-downcomers, 3-MCPs suction header, 4-MCP suction piping, 5-MCPs, 6-MCP discharge piping, 7-bypass line, 8-MCPs pressure header, 9-GDHs, 10-lower water communication line, 11-FC, 12-steam-water communication line, 13-steam lines.

Figure 2: Loss-of-all-MCPs event. Coolant flow rate through individual channels.

Figure 3: Loss-of-all-MCPs event. Coolant flow rate through one MCC loop.

Figure 4: Loss-of-all-MCPs event. Comparison of behaviour of coolant flow rate through one MCC loop and heat flux in maximum loaded fuel channel.

Initial and boundary conditions (coolant pressure, flow rate, feed water temperature, amount of steam for in-house needs, reactor power, and flow energy loss in different MCC components) and RELAP5 code models, assumptions, and correlations may impact the uncertainty of calculation results. The GRS method SUSA 3.5 [3] was used for the sensitivity and uncertainty analysis. The parameters, the initial values of which may have the greatest impact on the simulation results, were used for the analysis on the basis of earlier performed benchmarking calculations. The selection of these parameters is described in [4, 5].

2. BEST-ESTIMATE ANALYSIS OF LOSS-OF-ALL-MCPs EVENT

Loss-of-all-MCPs event occurred at Ignalina NPP on March 26, 1986. During this event, all the six operating MCPs at Ignalina Unit 1 were tripped simultaneously. Before this event, the reactor operated at thermal power level of 4650 mW. In response to multiple pump trips, an emergency protection signal AZ-1 was generated and the reactor was shut down. The MCC flow decreased in response to the MCPs coastdown. Long-term flow was due to natural circulation in the MCC.

RELAP5 analysis results were compared with the plant data. The uncertainty and sensitivity analysis was performed using a two-sided tolerance limit (with 0.95 of probability and 0.95 of confidence); 100 runs were performed.

The agreement of the calculation results, obtained using RELAP5 code with the real plant data, was evaluated using the adequacy standard, presented in the Guideline for performing code validation, and issued by DOE International Nuclear Safety Center [6]. The agreement is judged to be excellent, when the code exhibits no deficiencies in the modelling of a given behaviour; major and minor phenomena and trends are predicted correctly; calculation results are judged to agree closely with the real plant data. The agreement is judged to be reasonable when the code exhibits minor deficiencies, although it provides an acceptable prediction; all major trends and phenomena are correctly

Figure 5: Loss-of-all-MCPs event. Pressure in the MCC.

Figure 6: Loss-of-all-MCPs event. The impact of parameters no.1–7 on the coolant flow rate through one MCC loop. The numbering of parameters is the same as in Table 1.

Figure 7: Loss-of-all-MCPs event. The impact of parameters no.8–13 on the coolant flow rate through one MCC loop. The numbering of parameters is the same as in Table 1.

predicted, but differences between the calculation and data are greater than deemed acceptable for excellent agreement. According to the standard, both excellent and reasonable agreements of the calculation results and the real plant data are considered as being acceptable.

Data of the flow rate in twelve individual channels during natural circulation regime were available and are presented in Figure 2. For the comparison of calculated and measured flow rates, calculated maximum flow rate through maximum loaded FC and calculated minimum flow rate through minimum loaded FC were used. These calculated flow rates represent the whole interval (from minimal up to maximal) of flow rates through the core channels. The calculated maximum flow rate through maximum loaded FC, the calculated minimum flow rate through minimum loaded FC, and real plant data showed reasonable agreement. Coolant flow rate through one MCC loop is presented in Figure 3. As it is seen from the figure, measured values are available only in a limited range, and become equal to 0 after approximately 115 seconds from the beginning of the accident. The comparison of measured coolant flow rate through the fuel channels and through one MCC loop at the flow $< 7000\,\mathrm{m}^3/\mathrm{h}$ shows that the last measures are unreliable. This is because the throttling devices, which are not designed for the measurement of low coolant, are employed. The flow rates show a coastdown associated with the loss of forced circulation by the MCPs. The coastdown continues during the first 40 seconds from the beginning of the transient. Later, a natural circulation of the coolant was established at a flow rate equal to approximately 15% of the initial flow.

Figure 8: Loss-of-all-MCPs event. The impact of parameters no.1–7 on the CHFR. The numbering of parameters is the same as in Table 1.

Figure 9: Loss-of-all-MCPs event. The impact of parameters no.8–13 on the CHFR. The numbering of parameters is the same as in Table 1.

After reactor shutdown, the reactor is reliably cooled by natural circulation because heat flux decreases faster than coolant flow rate due to pumps coastdown (see Figure 4).

Figure 5 shows pressure in the MCC. Pressure in the pressure header decreases immediately after MCP trip because the MCP head is decreasing. During the first seconds, before the reactor shutdown, the coolant flow rate decrease through the core causes the short-term increase of steam generation. The increase of steam generation causes the short-term pressure increase in the DS. Steam generation in the core decreases and pressure in the MCC also decreases after the reactor shutdown. The reloading process of turbines is starting immediately after the reactor shutdown. When the turbine control valves are closed, the pressure starts to increase again.

Figure 10: Loss-of-all-MCPs event. CHFR in case of all MCPs trip.

Furthest pressure changes depend on the amount of removed steam for in-house needs. As could be seen from the presented comparison, pressure losses in different parts of the MCC are predicted correctly using the developed model.

Uncertainty and sensitivity analyses were performed for the following.

(i) Coolant flow rate through one MCC loop—one of the important technological parameters which allows identifying the existence of natural circulation.

(ii) Calculated critical heat flux ratio (CHFR) from the side of fuel assembly to coolant. CHFR is defined as a relationship between the calculated critical and real heat transfer fluxes. If critical heat flux ratio is greater than one, no critical heat flux will be observed in any fuel channel segment, and drying of FC will not occur.

Parameters, which may impact the calculation results, are presented in Table 1. Selected RELAP5 code parameters are varied in the area where two-phase flow conditions might occur: in the vertical section before the heated channels, in the heated channels, above the heated channels, and in the steam water communications. The areas with single-phase conditions are excluded due to the fact that these parameters do not have impact on the results in such region. Additionally, one parameter, which might impact the coolant flow regime

Figure 11: Three MCPs sequential trip. Pressure in the MCC.

Figure 12: Three MCPs sequential trip. MCPs throughput in the affected MCC loop.

Figure 13: Three MCPs sequential trip. MCPs throughput in the intact MCC loop.

in the reactor fuel channels, was selected—the 13th position of ICV, what affects coolant flow rate through fuel channels.

Results of the sensitivity analysis are presented using plots with the parameters impact on the results. Higher absolute value of impact on the results means higher parameter influence on the result. The positive impact means that when the higher value of the parameter is selected, the higher coolant flow rate is obtained; the negative impact means that when the higher value of the parameter is selected, the lower coolant flow rate in the affected MCC loop is obtained.

The performed analysis shows that the selection of homogeneous or nonhomogeneous models (see Figures 6, 7, 9, and 10) has the largest impact on the calculation results. Homogeneous model selection is a nonphysical conservative assumption and it is not recommended for best estimate codes.

The parameter-dependent sensitivity analysis shows that initial plant conditions (coolant flow rate, pressure in the DS, feed water temperature, and reactor power) and ICV position have only insignificant influence on coolant flow rate through the pumps (natural circulation regime) (see Figures 6 and 7).

Reactor core is reliably cooled because the CHFR is greater than 1 in all 100 calculations (see Figure 10).

Table 1: Parameters which may impact the uncertainty of calculation results in case of all MCPs trip.

No.	Parameter	Width of parameter distribution Min.	Max.	Value of the parameter in the basic case calculation	Mean deviation and error[%]	Probability distribution	Explanation
			Initial conditions				
Par. 1	Pressure in DS, Pa	$6.35 \cdot 10^6$	$7.035 \cdot 10^6$	$6.70 \cdot 10^6$	$1.675 \cdot 10^5$ [5%]	Normal	Measurement error
Par. 2	Coolant flow rate through single MCP, m^3/h	7938	8262	8100	81 [2%]	Normal	Measurement error
Par. 3	Feed water temperature, K	458.52	467.78	463.15	2.32 [1%]	Normal	Measurement error
Par. 4	Coefficients which define steam flow rate to the in-house needs header	0.98	1.02	1.0	0.01 [2%]	Normal	Measurement error
Par. 5	Reactor thermal power, W	$4.510 \cdot 10^9$	$4.790 \cdot 10^9$	$4.650 \cdot 10^9$	$7.0 \cdot 10^7$ [3%]	Normal	Measurement error
			Modelling parameters in the core				
Par. 6	Water packing	0 (on)	1 (off)	1 (off)	—	Nonparametric	Model assumption
Par. 7	Stratification	0 (on)	1 (off)	0 (on)	—	Nonparametric	Model assumption
Par. 8	PV term	0 (off)	1	0 (off)	—	Nonparametric	Model assumption
Par. 9	CCFL	0 (off)	1 (on)	0 (off)	—	Nonparametric	Model assumption
Par. 10	Thermal front tracking	0 (off)	1 (on)	0 (off)	—	Nonparametric	Model assumption
Par. 11	Mixture level tracking	0 (off)	1 (on)	1 (on)	—	Nonparametric	Model assumption
Par. 12	Nonhomogeneous	0 (nonhomogeneous model)	1 (homogeneous model)	0 (nonhomogeneous model)	—	Nonparametric	Model assumption
			Flow energy loss				
Par. 13	Coefficients which define ICV position	0.9	1.1	1.0	0.05 [10%]	Normal	Model assumption

The parameter-dependent sensitivity analysis (see Figures 8 and 9) shows that reactor thermal power (Par. 5) and pressure in DS (Par. 1) have the greatest impact on the reactor cooling conditions if the selection of homogeneous or nonhomogeneous models is not taken into account.

Thus, the performed uncertainty and sensitivity analysis shows that in presented all MCPs trip case, reactor core is reliably cooled due to natural circulation regime.

3. BEST-ESTIMATE ANALYSIS OF SEQUENTIAL MCP TRIP

A three-MCPs sequential trip event occurred at the Ignalina NPP on August 23, 2000. Before this event, the reactor operated at 2300 mW thermal power level. At 11 : 17, due to the short circuit into the control cable, fire-prevention signal of Ignalina NPP was activated by mistake. This caused the fire-prevention pump to provide a foam mixture into the MCPs compartments of one MCC loop. The foam was found on the cabinets of MCP electric motors control. The short circuit protections were activated. At 11 : 23, the first MCP was switched off. As the core thermal power was less than 2860 mW, AZ-4 signal was not generated. Still after three minutes, the second MCP of the same MCC loop was switched off. According to two MCPs trip in one loop of the MCC, AZ-1 signal was generated. According to this signal, all CPS rods were inserted within 12–14 seconds, and the reactor was shut down. Approximately 20 seconds after AZ-1 initiation, the steam supply for turbine was suspended. At 11 : 29, the last operating MCP in the affected loop of the MCC was switched off. In order to decrease flow rate differences in both loops, operators stopped one pump in intact loop of the MCC.

Figure 14: Three MCPs sequential trip. Comparison of behaviour of coolant flow rate through one MCC loop and heat flux in average loaded fuel channel.

Table 2: Parameters which may impact the uncertainty of calculation results in sequential MCPs trip case.

No.	Parameter	Width of parameter distribution Min.	Max.	Value of the parameter in the basic case calculation	Mean deviation and error[%]	Probability distribution	Explanation
			Initial conditions				
Par. 1	Pressure in DS, Pa	$6.517 \cdot 10^6$	$7.203 \cdot 10^6$	$6.86 \cdot 10^6$	$1.715 \cdot 10^5$ [5%]	Normal	Measurement error
Par. 2	Coolant flow rate through single MCP, m^3/h	7154	7446	7300	73 [2%]	Normal	Measurement error
Par. 3	Feed water temperature, K	458.52	467.78	463.15	2.32 [1%]	Normal	Measurement error
Par. 4	Steam flow rate to the in-house needs header, kg/s	81.34	84.66	83	0.83 [2%]	Normal	Measurement error
Par. 5	Reactor thermal power, W	$2.247 \cdot 10^9$	$2.387 \cdot 10^9$	$2.317 \cdot 10^9$	$3.48 \cdot 10^7$ [3%]	Normal	Measurement error
			Modelling parameters in the core				
Par. 6	Water packing	0 (on)	1 (off)	1 (off)	—	Nonparametric	Model assumption
Par. 7	Stratification	0 (on)	1 (off)	0 (on)	—	Nonparametric	Model assumption
Par. 8	PV term	0 (off)	1	0 (off)	—	Nonparametric	Model assumption
Par. 9	CCFL	0 (off)	1 (on)	0 (off)	—	Nonparametric	Model assumption
Par.10	Thermal front tracking	0 (off)	1 (on)	0 (off)	—	Nonparametric	Model assumption
Par.11	Mixture level tracking	0 (off)	1 (on)	1 (on)	—	Nonparametric	Model assumption

Results of the analysis are presented in Figures 11–14. The calculated and measured pressure in the DS, SH, and PH agrees well (see Figure 11). Switching off of one MCP leads to the increase of coolant flow rate through other MCPs of the same loop due to the lower pressure drop in the downstream system caused by cutting off the flow rate of a single pump (see Figure 12). The second MCP of the affected MCC loop was switched off after approximately 180 seconds. Output of the only operating pump increases up to 10500 m^3/h. Approximately 400 seconds after the first MCP was tripped,

Figure 15: Three MCPs sequential trip. Critical heat flux ratio.

Figure 16: Three MCPs sequential trip. The impact of parameters No.1–No.6 on the coolant flow rate through the affected MCC loop. The numbering of parameters is the same as in Table 2.

Figure 17: Three MCPs sequential trip. The impact of parameters No.7–No.11 on the coolant flow rate through the affected MCC loop. The numbering of parameters is the same as in Table 2.

the last MCP was switched off. The calculated maximum-minimum values of pressures and coolant flow rates are in reasonable agreement with real plant data (Figures 11–13).

Calculations showed that after MCP trip, the coolant flow rate through it decreases smoothly due to high inertia of flywheel, pump, and motor rotors. In approximately 60 seconds after the last MCP trip, the coolant natural circulation starts in the affected loop of the MCC (see Figure 12). It is necessary to note that coolant flow rate through the first two tripped pumps is also re-established in natural circulation node. Unfortunately, due to insensibility of measuring devices to low coolant flow rates at the Ignalina NPP, the coolant natural circulation was not identified. Coolant flow rate through MCPs of intact loop is presented in Figure 13. In the three-MCPs sequential trip in one MCC loop event, the reactor core is also reliably cooled by natural circulation because heat flux in the core decreases faster than coolant flow rate through the MCPs (see Figure 14), and CHFR is greater than 1 (see Figure 15).

In this case, only coolant flow rate through the affected MCC loop was selected for the uncertainty and sensitivity analysis because CHFR in base case calculations (see Figure 15) is greater than in all MCPs trip case (see Figure 10).

Parameters, which may impact the calculation results, are presented in Table 2. The analysis presented in the previous chapter shows that the selection of the homogeneous model has the biggest impact on the calculation results. However, the homogeneous model is not proper for two-phase flow modelling; it presents too conservative results. Thus, only the nonhomogeneous model was used in the accident analysis described in this chapter.

The performed analysis shows that the initial coolant flow rate through MCP (Par. 2) has the largest positive impact on the calculation results, whereas pressure in DS (Par. 1) has the largest negative impact. Parameter number 11—mixture level tracking model usage (see Figures 16 and17)—has the largest influence in the core. Mixture level tracking model has negative impact on the coolant flow rate in the

affected MCC loop, that is, at the switching off of this model, calculated values of flow rate will be lower.

4. CONCLUSIONS

The uncertainty and sensitivity analysis was performed for the simultaneous trip of all MCPs and sequential trip of three MCPs events.

The performed analysis of all MCPs trip shows that the selection ofthe homogeneous model has the biggest impact on the calculated flow rate through one MCC loop and CHFR. Homogeneous model selection is a nonphysical conservative assumption and it is not recommended for best estimate codes. Calculation results also show that reactor thermal power and pressure in DS have the biggest impact on the reactor cooling conditions.

In the sequential MCPs trip case, initial coolant flow rate through MCP, pressure in DS, and selection of mixture level tracking model have the biggest impact on the reactor cooling conditions. Switch off of the mixture level tracking model decreases the calculated values of flow rate.

Performed benchmark analysis of MCPs trip events showed that the initial reactor thermal power has insignificant influence on cooling conditions during natural circulation regime. Even at an initial maximal thermal power level (loss-of-all-MCPs case in Ignalina NPP), the reactor core is reliably cooled with 0.95 of probability and 0.95 confidence level.

NOMENCLATURE

AZ-1: Emergency protection
AZ-4: Emergency protection
CHFR: Critical heat flux ratio
CPS: Control and protection system
DS: Drum separator
FC: Fuel channel
GDH: Group distribution header
ICV: Isolating and control valve
MCC: Main circulation circuit
MCP: Main circulation pump
NPP: Nuclear power plant
RBMK: Russian acronym for "Channelled Large Power Reactor"

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Research Article

Reliability Assessment of 2400 MWth Gas-Cooled Fast Reactor Natural Circulation Decay Heat Removal in Pressurized Situations

C. Bassi and M. Marques

Commissariat à l'Energie Atomique, CEA Cadarache, DER/SESI, 13108 Saint-Paul-les-Durance, Cedex, France

Correspondence should be addressed to C. Bassi, christophe.bassi@cea.fr

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As the 2400 MWth gas-cooled fast reactor concept makes use of passive safety features in combination with active safety systems, the question of natural circulation decay heat removal (NCDHR) reliability and performance assessment into the ongoing probabilistic safety assessment in support to the reactor design, named "probabilistic engineering assessment" (PEA), constitutes a challenge. Within the 5th Framework Program for Research and Development (FPRD) of the European Community, a methodology has been developed to evaluate the reliability of passive systems characterized by a moving fluid and whose operation is based on physical principles, such as the natural circulation. This reliability method for passive systems (RMPSs) is based on uncertainties propagation into thermal-hydraulic (T-H) calculations. The aim of this exercise is finally to determine the performance reliability of the DHR system operating in a "passive" mode, taking into account the uncertainties of parameters retained for thermal-hydraulical calculations performed with the CATHARE 2 code. According to the PEA preliminary results, exhibiting the weight of pressurized scenarios (i.e., with intact primary circuit boundary) for the core damage frequency (CDF), the RMPS exercise is first focusing on the NCDHR performance at these T-H conditions.

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1. INTRODUCTION

The trend for safety demonstration of new reactor concepts is, nowadays, based on the combined application of deterministic and probabilistic tools (i.e., "risk-informed design guidance" [1]). In general, the main benefits of the use of probabilistic safety assessment (PSA) are to provide a risk measurement, which is useful to improve the concept. It is performed through the identification of plant vulnerabilities, of intersystem dependencies, and of potential common cause failures (CCFs). During conceptual design studies, the probabilistic engineering assessment (PEA), a simplified PSA in support of the design, provides useful insights about the examination of risk benefits from different design options and helps designers to improve the concept from safety and economical points of view. Finally, it anticipates design justifications during the licensing process and the deterministic safety demonstration.

The gas-cooled fast reactor is considered as a promising concept and has a high priority in CEA R&D work program on future nuclear energy systems [2, 3]. The main specifications of the 2400 MWth GFR concept are driven by the internationally agreed Generation IV objectives, which have been "translated" into the main features of the concept:

(i) a fast neutron core with a zero breeding gain (without fertile blankets) and characterized by an initial plutonium inventory allowing for the deployment of a GFR fleet near 2040 (sustainability and proliferation resistance concerns);
(ii) a helium-cooled primary circuit (7 MPa, 850°C at core outlet) connected to a Brayton secondary circuit allowing for a high-thermodynamic efficiency (economics concern);
(iii) a decay heat removal (DHR) system based on dedicated loops allowing for forced or natural circulation (safety concern).

According to the strategy proposed by the GFR design team [4], the natural circulation (NC) of gas, through the (DHR) system (dedicated loops for forced or natural circulation of gas, with passive water-filled secondary circuit) is foreseen:

(i) for pressurized situations (i.e., with intact helium pressure boundary);
(ii) for the transient termination (here called "late phase," starting around one day after the initiating event) of depressurized situations.

For the later, owing to the poor NC capability of gaseous coolant, a pressure level, evaluated at around 1.0 MPa and provided by a closed-guard containment, should be maintained to guarantee that the temperatures in the core vessel are kept at acceptable values.

As the GFR reactor concept makes use of passive safety features in combination with active safety systems, the question of natural circulation decay heat removal (NCDHR) reliability and performance assessment into the ongoing PEA constitutes a challenge. Within the 5th Framework Program for Research and Development (FPRD), a methodology has been developed to evaluate the reliability of passive systems characterized by a moving fluid and whose operation is based on physical principles, such as the natural circulation. This reliability method for passive systems (RMPSs) [5] is based, in particular, on uncertainties propagation into thermal-hydraulic (T-H) calculations. The aim of this RMPS exercise is finally to determine the performance reliability of the DHR system operating in a "passive" mode, taking into account the uncertainties of parameters retained for T-H calculations (distributions attached to critical parameters and uncertainties propagation). According to the PEA preliminary results, exhibiting the importance of pressurized scenarios (i.e., with intact primary circuit boundary) for the core damage frequency (see Section 2), the RMPS exercise is first focusing on the NCDHR perfomance at high-primary pressure and decay heat. At this point, some publications which discuss this topic, for depressurized scenarios, are referred in [6, 7].

2. THE GFR DECAY HEAT REMOVAL STRATEGY

The DHR system has a strong impact on the overall reactor architecture. On the contrary with other gas-cooled reactor concepts (like high-temperature reactors), solutions based on core thermal inertia plus conduction and radiation to remove heat were not applicable to the GFR due to the high core power density (100 MW/m^3 compared to 5–10 MW/m^3 in HTRs). Therefore, the decision was taken to study with more details systems relying on helium circulation, based on natural circulation as far as possible. This explains some design criteria and options:

(i) an upward core cooling flow and a location of the exchangers (DHR and IHX) above the core level;
(ii) a core design criterion based on a low-pressure drop (which eases the He circulation);
(iii) the use of a guard containment in order to limit the loss of pressure (sufficient backup pressure) after a primary circuit depressurization;

Figure 1: Schematic of the DHR system.

(iv) the use of dedicated DHR loops, allowing to increase the difference of elevation between the DHR heat exchanger and the core, as shown in Figure 1. If needed, the primary DHR circuit can operate with a blower, generating a forced circulation in addition to the natural circulation;
(v) the use of three loops (3 × 100% redundancy) assuming that one could be lost due to the accident initiating event (e.g., break) and that another one must be supposed unavailable (single-failure criterion).

The strategy for the depressurization/backup pressure of the GFR 2400 MWth has been decided already. The most attractive option is the "medium" backup pressure strategy: the pressure is kept "high enough" in order to obtain sufficient margin and to significantly ease the safety demonstration. In fact, for laminar flow conditions and for a given mass flow, the pumping power is almost inversely proportional to the square of the gas pressure, this means that the pumping power decreases very significantly when the pressure increases. At 1.0 MPa, about 15 to 20 kW are required few minutes after the reactor SCRAM instead of more than 1 MW at 0.1 MPa. In addition, for scenarios characterized by a loss of primary circuit integrity (e.g., LOCA), keeping some "backup" pressure at 1.0 MPa allows the possibility to use natural circulation after some time delay (typically 24 hours) due to the decay heat decrease. Finally, such medium backup pressure could be coherent with the use of a metallic containment, also simpler and smaller systems could be used thus limiting investment, operation, and maintenance costs.

Therefore, the DHR system consists of the following:

(i) a metallic guard containment enclosing the primary system, not pressurized in normal operation, and having a free volume such as the fast primary helium expansion is leading to an equilibrium pressure of 1.0 MPa;

Figure 2: Schematic of the DHR strategy based on mixed natural and forced mechanisms [4].

(ii) three dedicated DHR loops (redundancy) with secondary loops connected to an external water pool (the ultimate heat sink).

This strategy and the means (systems, phenomena) to achieve their intended mission are depicted in Figure 2.

3. SOURCES OF UNCERTAINTIES IN THE GFR PROBABILISTIC ENGINEERING ASSESSMENT [8]

Until now, a focus is held in the PEA on the two first families (loss of coolant accidents and loss of offsite power) in order to check the consistency of the DHR systems layout (two DHR loops operating in a "dual" circulation scheme: forced circulations (FCs) and natural circulations, and two DHR loops for forced circulation only and designed according to the systems diversification principle). The choice of these two families is driven by the aforementioned DHR strategy (see Section 2) depending on the primary circuit integrity status (LOCAs are representative of depressurized situations and LOOP of pressurized ones). In Figure 3 is reported the event tree for the LOOP initiating event.

It seems important to notice that the two time periods are distinguished according to the systems or mechanisms potentially involved for DHR ("early phase" during the first 24 hours, and "late phase" from 24 to 168 hours). Many reasons have led to this choice: the first is design dependant (batteries or DHR ternary pools autonomy, systems or mechanisms involved during the two time periods), and the second is relative to some deterministic practices leading to combine some "frequent IEs" with short-duration LOOP or blackout. Therefore, this PEA will furnish some relevant results for the design basis events classification. An iterative process through sensitivity studies (importance measures assessment, sensitivity indices) and cost-benefit evaluation (systems redundancy level) would finally help to optimize the concept without safety weakening.

Usually, uncertainties are categorized as "aleatory" (i.e., inability to predict random observable events) or as epistemic [9] (i.e., our confidence in the knowledge of a model or in some data). These epistemic uncertainties are roughly related to the input parameters, the structure of the model, and finally to its completeness.

For input parameters, lognormal probability distribution functions are systematically attached to components-failure rates, with error factor (i.e., the square root of the 95 to 5 percentile ratio) set to the following:

(i) three for components with operational feedback (e.g., water circuits);
(ii) ten for components in a gaseous environment (e.g,. blowers, valves, etc.).

The uncertainty propagation is performed thanks to the Risk Spectrum [10] software.

Modeluncertainties are studied through parametric studies to analyze the effects of simplifying assumptions on the model output (core bypass assumption weight, e.g., in GFR PEA).

Figure 3: Event tree for LOOP IE (representative of the pressurized situation) in GFR PEA.

Regarding the uncertainties associated with the model completeness, until now, the PEA model is focusing on representative situations according to the various means and systems retained in the DHR strategy; the model is then partial and the PEA results should be affected by a large amount of uncertainties. The next PEA model, for which initiating events and scenarios coverage will be enlarged, may lead to a reduction of model uncertainties.

In addition to these common sources of uncertainties for probabilistic models, the GFR PEA model introduces technological risk uncertainties (i.e., ability to develop an innovative component or system, and uncertainties related to the ability of an innovative system to perform the mission it was designed for). Another significant feature of this probabilistic assessment representation of the 2400 MWth GFR will be the combination of systems and T-H reliability models.

4. IDENTIFICATION AND QUANTIFICATION OF THE SOURCES OF UNCERTAINTIES FOR THE NATURAL CIRCULATION PERFORMANCE ASSESSMENT

4.1. Influence of the time limit before the loss of the forced circulation decay heat removal (FCDHR) process

In order to define the most relevant case for the RMPS exercise, sensitivity studies were performed with the CATHARE 2 code. Due to its relatively high-occurrence frequency in the GFR PEA (i.e., 10^{-2}/reactor · year), the short duration loss of offsite power transient (LOOP < 2 hours) is considered as reference scenario for the pressurized situation depiction. As stated here above, the forced circulation of primary helium is foreseen in the first stage to remove the core decay heat. In the LOOP situation, the electrical supply will be provided by emergency diesel generators (EDGs) or batteries (1 unit per electrical train) to the dedicated circulators. The combination of components losses leading to the complete loss of FCDHR at initiating event (e.g., EDGs fail to start or DHR circulators failures) provides the most stringent case in terms of decay power.

For the 2400 MWth GFR, neutronic calculations were performed with the Darwin code [11] to estimate the decay heat curve for several minor actinides contents. As reported in Figure 4, the core power decreases to around 15% of nominal value just after the control rods full insertion, and decreases rapidly to 3% of nominal power five minutes after scram.

Despite the weak occurrence probability of such failures combination, it seems clear that this situation would provide the most stringent case for NCDHR performance and reliability assessment (maximum core power). On the other hand, just after the reactor scram, the pressure level in the primary circuit and the density difference between hot and cold parts of the DHR loop are maximum, providing potential high-engaged forces for natural circulation. Sensitivity

Figure 4: GFR 2400 MWth core decay heat evolution with time.

Figure 5: Influence of the time limit before NC actuation on the primary pressure evolutions (1 DHR loop available).

Figure 6: Influence of the time limit before NC actuation on the core inlet mass flow evolutions (1 DHR loop available).

studies were performed with the CATHARE 2 code to assess the influence of the T-H system parameters (core decay heat included) at NCDHR actuation, for example, after the FCDHR phase. Several times before NC, actuation was investigated in comparison with the a priori most severe case (NCDHR actuation at initiating event, representative of a blackout transient).

In Figure 5 are reported the primary pressure evolutions with time for time limits before FC loss ranging from 1 to 24 hours. As a result, one should notice that the primary circuit pressure evolutions are similar, monotonic, and ranging from 7.0 to 5.4 MPa during the first 24 hours.

Correlatively with the primary pressure evolutions, the mass flow at core inlet ranges from 23 to 30 kg/s in an FC operating mode (due to a DHR blower rotational speed control scheme, so that DHR circuit pressure drop and blower pressure rise would be matched for an unchanged mass flow rate when pressure decreases) and from 32 to 25 kg/s in natural circulation (see Figure 6). Indeed, according to the circulator performance map depiction in the CATHARE 2 model, the mass flow at DHR loop outlet is inversely proportional to the primary pressure level when the circulator(s) is in operation. When they fail, the natural circulation is established at a mass directly depending on the pressure level in the circuit.

In Figure 7 are reported the maximum temperature evolutions in the core with time. In line with the mass flow at core inlet (between 32 and 20 kg/s), and the decay heat to extract (between 3 and 0,6%, resp., 5 minutes and 24 hours after scram), the maximum temperature in the core is monotonically decreasing. As a result, one should notice that results do not depend on the time limit before FCDHR loss, indicating that the heat extraction potential by the secondary and ternary DHR circuits is largely enough to cope with these situations.

In summary, the core decay heat and primary pressure levels at NCDHR actuation do not appear as critical parameters for the reliability and performance assessment of this passive system. Moreover, as the time delay before FC failure is not influencing the NCDHR performance, it validates the event representation in a Boolean model for the GFR PEA, as all active system failures during the first 24 hours are aggregated as they occur at initiating event ("static" probabilistic model).

Figure 7: Influence of the time delay before NC actuation on the core temperatures evolutions (1 DHR loop available).

4.2. Other elements related to the NCDHR question

Owing to the DHR system features design, it is worth noticing that primary gaseous coolant NC performance is directly related to the natural circulation capability in the secondary side pressurized water circuit (around 1.0 MPa) through the heat exchange coefficient in the DHX. Then, the frame of RMPS exercise for the GFR DHR system is involving a "two-in-one" exercise: one for gaseous coolant, the second for pressurized water natural circulation.

Regarding the NC loop in the primary circuit (from the core to the dedicated heat exchanger for DHR), the natural circulation of a gaseous coolant may be subject to several and diverse "disruptions" potentially challenging its capability to achieve the function it was designed for. One important feature of gas is owing to the fact that viscosity is increasing with temperature. Then, since the viscosity increases as the temperature of the gas increase, the Reynolds number decreases. For the DHR operating conditions, the flow regime is laminar and friction losses are proportional to the inverse the Reynolds number (eventually with an exponent, named m, i.e., $f \propto \mathrm{Re}^{-m}$). Thus as the temperature increases along the channel, friction losses rise leading potentially to the flow blockage. For a single gaseous heated channel (i.e., one single core channel), the laminar flow instability may lead to flow blockage that could be expressed in terms of temperature difference between the inlet and the outlet of this channel. These results may be obtained with a simplified development of the momentum equation in a steady-state approach.

For the GFR core, assuming two enrichments core zones and a radial power profile of about 1.2 (i.e., "parallel" heated channel characterized by different generated power levels), flow redistribution due to viscosity effects in the hottest core channels (increased pressure drop compared to other parallel channels) could occur. Within this frame, simplified methods could not be employed and the use of the CATHARE 2 code is required.

Regarding the DHR secondary circuit, the natural circulation phenomena are driven by the density (i.e., typically temperature for pressurized water) change between hot (i.e., secondary side of the primary to secondary HX) and cold (i.e., primary side of the secondary to pool HX) parts of the loop, then by the exchange at the interfaces between circuits.

4.3. Identification of critical parameters

4.3.1. Mission of the system

As reported in Section 2, the major mission of the DHR system is to remove the core decay heat during the mission time defined for the two representative scenarios:

(i) from nearly 0 to 24 hours for pressurized situations (with intact helium pressure boundary);
(ii) from 24 to 168 hours for depressurized ones (this mission time is related to the choice made for the simplified PSA application for GFR); in a first approach, the mission success will be checked at 48, 72, and 168 hours after the initiating event, assuming the electrical supplies recovery at these successive time delays.

For both situations, it is implicitly assumed that additional measures will be taken in order to guarantee a safe termination of the transient (mobile active systems linked to the guard containment, for example, recovery of electrical supplies allowing for a forced circulation transient termination).

4.3.2. Failure criteria

The DHR system failure criteria are defined as the failure to maintain a decay heat removal rate that is required to limit vital component temperatures (vessel boundary, core plates) to the values of the "IVth category" design safety limits defined, to date, for GFR safety studies:

(i) 1600°C for fissile core (cladding temperature),
(ii) 1250°C for He temperature in upper plenum (vessel structures).

By now, these failure criteria are based on preliminary values used for GFR design and safety studies. In parallel, some R&D effort is provided by CEA in order to increase our confidence for future studies.

At this point, it is worth to notice the close interaction between deterministic and probabilistic studies in support to the safety demonstration. Uncertainties taken into account through the RMPS exercise will furnish and quantify safety margins reservations for the deterministic analysis.

4.3.3. Failure modes and critical parameters identification

It seems not always obvious to associate a failure mode to the mission of the system due to the complexity of T-H

Figure 8: Deductive approach for RMPS critical parameters determination.

phenomena and interaction between the passive system and components present in the fluid potential pathway. As proposed in [5], a qualitative analysis as the failure mode and effect analysis (FMEA) could be performed (with a "virtual component" for NC process simulation) to help defining critical parameters.

The proposal is to determine the critical parameters according to "engineering judgment" based upon a simplified "deductive analysis" (or cause consequence analysis). This approach is chosen due to the complexity of the involved phenomena reported here above, an FMEA starting from components failure mode and ending at consequences, as the "deductive analysis" is more attached to physical phenomena. Furthermore, the method employed for the critical parameters determination is linked to the level of detail of the systems. As yet, the GFR concept is still under development and, therefore, the design is not strictly fixed.

The cause-consequence diagrams for this deductive analysis are developed upon simplified principles:

(i) T-H parameters like pressure, temperature, and gas densities and velocities are calculated by the CATHARE 2 code; therefore, they are not appearing in block diagrams introduced in the development of trees;

(ii) according to the former point, the analysis is then based on the development of diagrams in a "quasi" steady-state approach (a time- and space-dependent analysis being to complicate without the use of a detailed T-H code); at each stage, the cause potentially leading to the consequence placed above is developed until "critical parameters" like code user parameters (e.g., valves opening fraction, i.e., related singular pressure drop) or specific T-H features characterized by a lack of knowledge (e.g., N_2 fraction reentering in the primary circuit through the break) are reached;

(iii) in most cases, the analysis is developed in consistency with the foreseen flow circulation, for example, starting from the core region going into a dedicated DHR loop, passing through the DHX, the stopped blower (or the ejector in an alternative design) and the isolation valve (potentially not fully closed) exiting above the downcomer and reentering in the core.

The first step for the determination of critical parameters based on this deductive analysis is represented in Figure 8. Starting from the defined mission of the system (decay heat removal during the mission time), referenced as Stage 0, the success/failure criteria (see Section 4.3.2) are developed just under this "top goal" (Stage 1). For the first success/failure criteria (keeping the maximum fuel temperature under 1600°C in every core channels), two potential failure modes during the transient phase are defined. Concerning the second success/failure criteria (keeping the maximum coolant temperature at core outlet below 1250°C), we implicitly assume that the gas flow (and temperature) harmonization at core channels outlet is sufficient to consider the reduction of mass flow at core inlet as the only mean for leading to unacceptable temperature for upper head internal structures (assumption schematized by a red arrow in Figure 8). In complement to the former point, it is worth to notice the following:

(i) first, the gas temperature at each representative channel output could be a meaningful indicator for this success/failure criteria, if needed;

(ii) secondly, the precise calculation of heat exchange between the coolant and the upper head structures

Figure 9: Development of the deductive approach for critical parameters determination (NCDHR pressure drops "1").

necessitate a multidimensional code due to the probable internal circulation of gas in the upper plenum.

The next step is then to develop the success/failure criteria into the potential failure modes (Stage 2). Two ways were determined leading to the increase of the maximum fuel temperature in a core channel, which are as follows:

(i) the apparition of a "sharp distortion of mass flow between core channels;"

(ii) an "insufficient mass flow at core inlet."

At this point, it seems important to remark that the first failure mode addresses exclusively to the core region, as the second is more related to the gas flow path from the core boundaries (through the DHR system and eventually through the main loops if they are not fully closed).

The distortion of mass flow between different core channels could be associated with the following:

(i) a "spurious closure of an inlet flow gag or flow blockage by foreign object:" it is assumed that inlet flow restrictions located at assemblies entrance are designed for a gas outlet temperature equalization at nominal full power (avoiding hot spots for upper head internal structures and providing an optimized fuel consumption during the batch cycle); the "inadvertent" closure would then be assimilated as an "additional event" following the initiating event. This point is also applicable to the potential flow blockage by a foreign object (or eventually by accumulation of foreign materials) at core inlet;

(ii) the "temperature viscosity-induced laminar instabilities" phenomena for single heated and, by consequence, for parallel core channels.

Concerning the insufficient mass flow at core inlet, this consequence is associated with the following:

(i) a DHR system-related cause (linked to the "NCDHR" capability);

(ii) a main circuit-related cause ("core bypass through the IHX vessel").

The development of the cause-consequence diagram for the first point is depicted in Figure 9 (Stage 3). The steady-state approach is employed in order to assess the critical parameters for the NC phenomenon with the dedicated decay heat exchanger. The mass flow decrease at core inlet is linked either to a "decrease of the NC driving term" or to an "increase of the whole loop friction terms" (starting from the core inlet, passing through the DHR specific components, flowing in core vessel, and down to the core), referenced as Stage 4.

The "driving term" is decomposed into the gas densities difference and the driving height for NCDHR. It is assumed that thermal dilation of materials (i.e., variation of the driving height) is negligible compared to the potential variation of gas densities difference. Therefore, the "driving term decrease" is only related to the gas densities difference variation, which decomposes in a "decrease of the mean gas density at

Figure 10: Development of the deductive approach for critical parameters determination (NCDHR pressure drops "2").

DHX" and/or an "increase of the gas density in the core region" (Stage 5). This increase is related to the critical parameter ECPLAQ , which corresponds to a multiplicative factor for core materials to fluid heat transfer coefficient.

At DHX primary side, the perfect gas law is employed for defining the causes of a mean gas density decrease, for example, a "decrease of the mean gas pressure at DHX" and/or an "increase of the mean gas temperature at DHX." Concerning the pressure decrease, we talk about a pressure decrease not due to a pressure drop increase until the DHX is reached (this effect being reviewed during the development of the "increase of the whole loop friction terms" block), but consecutive to a whole circuit pressure decrease. This pressure dropoff in the primary circuit is assumed to be driven by the "primary circuit natural leakage" (critical parameter, named FUITE). For the gas mean temperature at DHX primary side, its increase is attributed to the "decrease of the gas convective heat transfer in DHX" and/or to the "decrease of the water convective heat transfer in DHX" (parameter, named ECDHX1 hereafter).

Concerning the gaseous side, the convective heat transfer is proportional to the gas mean heat conductivity, to the hydraulic diameter in the DHX primary side, and to the inverse of the Nusselt number of the flow. As for the driving height variation with temperature (thermal dilatation), the hydraulic diameter decrease is not taken into account for the determination of critical parameters. This means that the material accumulation (i.e., fouling) of the DHX at primary side is not judged as critical as variations of the gas T-H properties (driving term concern), or as singular pressure drop along the flow path (stopped blower, isolating valve).

The development of the block entitled "decrease of the water convective heat transfer in the DHX" emphasizes the so-called "two-in-one" RMPS exercise. Indeed, as the DHR system secondary side is made of pressurized water (1.0 MPa) set in motion by natural circulation with an above-positioned immerged heat exchanger (with a dedicated pool), one should extend the RMPS exercise frame to the DHR secondary side. According to previous work performed for potential passive systems designed for pressurized water reactors, the critical parameters for this part of the RMPS application are directly issued from [5]. Then, in the DHR secondary side, the characteristic parameters will be as follows:

(i) the initial temperature of water in the pool (named T2DHR);

(ii) the mean temperature of pressurized water in DHR system (i.e., the pool temperature T2DHR due to secondary-to-ternary thermal equilibrium);

(iii) the pressure in DHR system secondary side (named P2DHR);

(iv) the rate of incondensable gases at the inlet of the pool HX (simulating a potential leakage through the DHX);

(v) the fouling of the pool HX;

(vi) the number of broken tubes in the pool HX.

The last three critical parameters are jointly described by a corrective factor of the exchange coefficient between secondary and ternary "circuits", that is, ECDHX2.

The decrease of NC driving term (Stage 4) could also be a result of a spurious "increase of the mean gas density in the core region." Assuming the gas gaseous coolant as a perfect gas, this density increase is attributed to a pressure increase and/or to a mean temperature decrease. Assuming that the pressure increase will solely be caused by a lack of heat extraction (i.e., a DHR loop inadvertent closure, or a lack of extraction by secondary circuit, which is the main issue of the RMPS exercise), the gas mixture increase in the core region is only attributed to the mean gas temperature decrease. The mean temperature decrease is, therefore, consecutive to a gas temperature decrease at core inlet and then to increased exchange terms in the core vessel (not considered for this first analysis) or at DHX (critical parameter ECDHX1).

As for the determination of the critical parameters related to the "driving term" concern, the same approach is followed for the "increase of the whole loop friction terms." The analysis focuses on the regular and singular pressure drop on the flow path of the gaseous mixture (in the representative "loop" starting from the core inlet). In Figure 10 is depicted the development of the block diagram for the potential increase of "whole loop" pressure drops. In dashed lines are represented two groups characterized by "structural" (singular pressure drops) and "T-H" (regular pressure drops) concerns.

It is worth to notice that only four "major components" are taken into account at the flow path for pressure drop analysis: the core assemblies (including for each representative one the inlet flow gag, the lower and upper neutron shielding zone, and the fissile part), the DHX, the DHR blower (singular pressure drop when stopped), and the isolation valve. This choice is performed according to the meaning that they will be the most important contributors for the whole "loop" pressure drops. Moreover, this choice is linked to the present complexity of the CATHARE 2 model for the GFR concept whose design is still in progress.

According to this development, and to the sources of known uncertainties in the CATHARE 2 model, critical parameters potentially challenging the engaged forces are related to the plate-type core regular pressure drop in laminar conditions (FRPLAQ), the turbulent-to-laminar transition Reynolds number (REPLAQ) for exchange (cf., ECPLAQ above) and regular pressure drop assessment in the core region, and the singular pressure drop provided by the stopped DHR blower (DPSOUF). In a first step, the singular and regular pressure drop at DHR heat exchanger are not taken into account, owing to the fact that correlations for circular tubes are less subject to large uncertainties. In anyway, a sensitivity study is reported at the end of this document and related to the impact of pressure drop reservations on margins to failure/success criteria. This work is performed through the DHR loop isolating valves partial opening and is finally translated into additional pressure drops in the loops. Therefore, they should also be investigated as a combination of other singular pressure drops in the loop (for DHR HX, e.g.).

5. RELIABILITY EVALUATION OF PASSIVE SYSTEMS WITH THE CATHARE 2 CODE

5.1. The CATHARE 2 model for GFR

The complete CATHARE 2 [12, 13] model for 2400 MWth GFR is composed of the following:

(i) the primary circuit with helium as coolant, includingthe main reactor vessel comprising the core, three principal-independent loops each containing a finned plate intermediate heat exchanger (IHX) and a blower, three tanks for helium supply (for guard containment pressure), and three dedicated DHR loops, each with its own water-filled secondary and pool-type ternary circuits;

(ii) each principal loop has its own secondary circuit for power conversion, includingthe second part of IHX, a single shaft-mounted turbomachinery, and a steam generator; the working fluid in the secondary circuit is a mixture of helium and nitrogen with mass fractions, respectively, equal to 20% and 80%;

(iii) a ternary steam-water circuit (second part of the once-through counter-current plate-shape steam generator).

Each dedicated DHR loop is composed of the following:

(i) a primary loop (crossduct connected to the core vessel), with a driving height of 10 m between core and DHX midplan;

(ii) a secondary circuit filled with pressurized water at 1.0 MPa (driving height for NCDHR of 5 m);

(iii) a ternary pool, initially at 50°C, whose volume is determined to handle one-day heat extraction (after this time delay, additional measures are foreseen to fill up the pool).

In addition, a large free volume filled with nitrogen is described for guard containment simulation in case of a depressurization accident. This free volume is hydraulically and energetically linked to the primary circuit by specific functionalities implemented in the CATHARE 2.5 version.

A special care was put on the core model, taking into account the heterogeneous plate structure, the core radial dimension and power profiles (6 hydraulics channels with singular pressure drop in order to equalize the outlet temperature in nominal conditions, etc.).

In Figure 11 are reported the main features of one dedicated DHR loop modeled in the CATHARE 2 input data deck for 2400 MWth GFR [14].

5.2. Critical parameters distributions and sampling

The first class of parameters addresses the specific uncertainties associated with factors on which the magnitude of the engaged forces and counter forces depends (values of heat transfer coefficients and pressure losses) on the following:

(i) *in the core region:* correlations used to calculate the Nusselt number and the friction factor have different functional forms depending on the geometry, fluid

Figure 11: DHR loop features and nodalization in the CATHARE 2 code.

characteristics, and flow regime (forced, natural, or mixed); as natural and mixed convection correlations are not supported by extensive experimental results, the resulting correlations in natural convection laminar regime are subject to large uncertainties; for the GFR core (plate-type core), an R&D program is underway in CEA to establish specific correlations for these geometry and T-H conditions [15]; however, multiplicative factors are retained in the present CATHARE 2 input data deck for exchanging coefficients and regular friction factors in laminar regime (the transition regime Reynolds number being also a critical parameter);

(ii) *in the DHR loop:* exchange coefficients multiplicative factors for primary to secondary, and from secondary to ternary pool heat exchangers.

The second class of parameters addresses the uncertainties associated with the potential discrepancy of T-H parameters at transient initiation:

(i) a first subclass is related to the ternary and secondary DHR circuits initial temperature and pressure; by this way, we implicitly assume that small deviations are possible within the allowable range defined by the protection system; therefore, these parameters are characterized by lower and upper bounds between −15% and +15% around their nominal values;

(ii) the second subclass concerns the primary circuit natural leakage (named FUITE), and is very concept dependant (number of loops, nozzles, sealing systems for rotating machines, etc.).

Table 1: Critical parameters distributions.

	Mean	*Distribution*	*Min. value*	*Max. value*
T2DHR	50°C	Normal	42.5°C	57.5°C
P2DHR	1.0 MPa	Normal	0.85 MPa	1.15 MPa
FRPLAQ	15%	Normal	0	30%
ECPLAQ	5%	Normal	0	10%
REPLAQ	5000	Normal	4000	6000
ECDHX1	5%	Normal	0	10%
ECDHX2	5%	Normal	0	10%
DPSOUF	15%	Normal	0	30%
	Max.	*Distribution*	*Lower bound*	*Upper bound*
FUITE	2e − 04	Triangular	2e − 5	2e − 3

Table 2: Mean, standard deviation (SD), and upper and lower bounds for critical parameters after LHS (100 values).

	Mean	*SD*	*Min. value*	*Max. value*
T2DHR	50.009	3.308e + 00	4.293e + 01	5.720e + 01
P2DHR	9.998e + 05	6.625e + 04	8.583e + 05	1.144e + 06
FUITE	7.40e − 04	4.487e − 04	6.287e − 05	1.812e − 03
FRPLAQ	1.50e − 01	6.639e − 02	5.444e − 03	2.972e − 01
ECPLAQ	4.99e − 02	2.212e − 02	4.632e − 05	9.773e − 02
REPLAQ	4.99e + 03	4.411e + 02	4.063e + 03	5.930e + 03
ECDHX1	4.99e − 02	2.211e − 02	1.423e − 03	9.634e − 02
ECDHX2	4.99e − 02	2.213e − 02	2.564e − 03	9.888e − 02
DPSOUF	1.50e − 01	6.614e − 02	6.816e − 03	2.895e − 01

The critical parameters and their distribution features are given in Table 1, which have been established by engineering judgment.

Owing to the precision required for this first NCDHR performance and reliability assessment, and compared to the magnitude of other sources of uncertainties in the PEA model, the number of calculations is first limited at 100. An optimized Latin hypercube sampling (LHS) is then performed according to the distributions and bounds selected reported in Table 1 and furnishes a set of values as input parameters for the CATHARE 2 calculations. In Table 2 are given the mean, standard deviation, and minimum and maximum values for each critical parameter after LHS.

In Figures 12, 13 are, respectively, plotted the 100 values and the final distribution for the first critical parameter, for example, T2DHR, in order to check the consistency of the sampling.

5.3. RMPS exercise results

The process followed for this RMPS exercise implies to combine the steady state (due to the secondary circuit pressure

Figure 12: Optimized LHS (100 values for T2DHR).

Figure 13: Verification of the T2DHR distribution after sampling.

and ternary pool temperature initialization) and the transient calculations with CATHARE 2. Each calculation takes around 5000 seconds CPU on a personal computer under LINUX operating system.

The results obtained with the CATHARE 2 code are plotted in Figure 14 showing the core maximum temperature evolution with time.

As shown by Figure 14, a sharp decrease of the core temperature occurs during the first minute of the transient due to the reactor scram and the main blower rundown on their inertia. Until the NCDHR is established, the temperatures are increasing up to around 1030°C and then decrease owing to the large amount of energy exchanged with the secondary (and ternary) DHR circuit. As a result, one should notice the weak discrepancy of the temperature evolutions with time and of the maximum level reached. In Figure 15 is reported the maximum temperature distribution for these 100 calculations. At this stage, it is worth mentioning the robustness of the CATHARE 2 code as around 94% calculations performed provide results (6 calculations exiting without results).

This temperature distribution is characterized by a mean value of 1029°C and a standard deviation of 4.3°C (see Table 3).

Figure 14: RMPS reference case—maximum temperature in the core region evolutions.

Figure 15: RMPS reference case—maximum temperature in the core region distribution.

Table 3: Results for the RMPS reference case.

	Mean	SD	Lower	Upper
TMAX (°C)	1029	4.32	1019	1037
TUPPER (°C)	907	2.35	903	913

Compared to the failure/success criteria fixed at 1600°C as mentioned earlier in this document, the results obtained with the CATHARE 2 code are exhibiting the safety margin conferred by the DHR loop structural and thermodynamical design. For the ongoing PEA, it seems that the reliability of the natural circulation in pressurized situation is close to unity. In order to determine a more precise value of the NCDHR unreliability (if necessary, according to the magnitude of the other sources of uncertainties in the PEA model),

Figure 16: Real versus simulated maximum temperature in the core region.

Table 4: Linear correlation standard regression coefficient.

	TMAX		TUPPER	
	SRC	Rank	SRC	Rank
T2DHR	**0.23**	**3**	**0.30**	**2**
P2DHR	0.02	7	−0.08	7
FUITE	−0.05	5	0.08	5
FRPLAQ	**0.96**	**1**	0.01	9
ECPLAQ	−0.09	4	**−0.08**	**3**
REPLAQ	**−0.24**	**2**	**0.80**	**1**
ECDHX1	0.03	6	−0.01	8
ECDHX2	0.01	8	−0.08	4
DPSOUF	−0.01	9	0.08	6

the percentile corresponding to 1600°C can be calculated. Another way to evaluate the NCDHR unreliability consists in the research of linear correlation between the response result and the input parameters, in order to increase the number of calculations with a low CPU cost.

5.3.1. *Correlations between input parameters and temperature results*

A linear correlation between TMAX (maximum temperature in the core region) and input parameters has been determined:

$$\begin{aligned}\text{TMAX} = {} & 1016.5 + (\text{T2DHR}*0.30211) - (\text{FUITE}*449.71) \\ & + (\text{FRPLAQ}*63.027) - (\text{ECPLAQ}*18.222) \\ & - (\text{REPLAQ}*2.4157*10^{-3}) + (\text{ECDHX1}*5.5575).\end{aligned} \tag{1}$$

It should be mentioned that this linear correlation is based only on 6 input parameters (the last three ones having a negligible influence for the response). The R^2 coefficient of determination for this linear model is equal to 97%. Figure 16 compares TMAX calculated by the CATHARE 2 code and simulated by the linear model.

According to this result, the utilization of linear standard regression coefficients is justified in order to rank the most influent parameters (see Table 4).

The ranking is based on absolute values, as the sign is indicating the effect of variation of the input parameter for the response. For TMAX, the most influent parameters are, according to importance, FRPLAQ (multiplicative factor for laminar NC pressure drop in the core region), REPLAQ (Reynolds number for turbulent to laminar transition in the core region), and T2DHR (ternary pool and secondary circuit temperature level at DHR initiation).

According to the above-mentioned linear regression, 10^6 simulations are performed to increase the number of calculations (compared to the 100 ones performed with CATHARE 2) and then to improve the probability value of the NC reliability assessment in the PEA model.

The results are leading to a value of NCDHR unreliability performance less than $5 \cdot 10^{-6}$ with a confidence level above 99%. It is worth to notice that this unreliability value is surely overestimated as the upper value of TMAX (1041°C) estimated by 10^6 simulations is far from the 1600°C limit. This result will be incorporated in the next PEA model.

5.3.2. *Sensitivity study upon the DHR isolating valve opening fraction*

Concerning the isolation valves opening fraction, the CATHARE 2 code requires an evolution law of the valve flow coefficient (Cv) with the valve's stem position (Pu). The assumption made in the input data deck is considering a linear evolution of Cv with Pu ranging from 10^5 (for Pu = 0) and 0 (for pu = 1). The law valve expression in CATHARE 2 code is

$$\text{Cv} = \frac{3600 \cdot q}{27,3 \cdot Y \cdot \sqrt{x \cdot P_a \cdot \rho_a \cdot 10^{-5}}}, \tag{2}$$

where $Y = 1 - x/(3 \cdot F_\gamma \cdot x_t)$, $x = \Delta P/P_a$, $F_\gamma = \gamma/1.4 \approx 1,67/1.4$, $x_t = 0.6804, q$ is the mass flow in sonic condition (in kg/s), P_a is the upstream pressure (in Pa), ΔP is the pressure drop (in Pa), ρ_a is the upstream gas density (in kg/m^3).

According to this expression and the bounding values of Cv, it seems more efficient to translate the stem position in terms of additional singular pressure drop in the NC loop (and here provided by the DHR isolating valve). As reported in Figure 17, the pressure drop stays under 1000 Pa for a valve opening fraction above 25%, and increases rapidly for a stem position less than 10% (around 10000 Pa for a stem position close to 7%).

Sensitivity calculations were performed with the CATHARE 2 code in order to feel the impact of this additional pressure loss related to the valve depiction in the code (see Figure 18).

If we consider a maximum opened fraction for the DHR isolating valve of 10%, the maximum temperature in the core is around 1400°C. However, for this opened fraction (or additional pressure drop in the primary NC loop, evaluated at 5200 Pa according to the Cv versus Pu valve law), the gas temperature at core outlet reaches 1286°C, that is, just above the 1250°C criteria defined in Section 4.3.2. For a valve opening fraction close to 7%, which is giving an additional pressure drop in the NC loop of about 10000 Pa, the 1600°C criterion is approximately reached. In Figure 19 are reported the maximum temperatures in the core region and of the gas at core

Figure 17: Flow coefficient and related pressure drop versus stem position for DHR isolating valves.

Figure 18: Maximum fuel temperature evolutions for different values of DHR valve stem position (1 DHR loop available).

outlet versus the additional singular pressure drop in the NC loop.

According to the results obtained in this sensitivity study, it was decided to perform another set of calculations for an additional singular pressure drop in the NC loop of about 10000 Pa (i.e., valve opening fraction close to 7%). The RMPS methodology was applied with the same set of values for critical parameters as generated by LHS in the RMPS reference case. The temperature evolutions with time are reported in Figure 20.

In Table 5 are given the statistical results of the responses provided by the CATHARE 2 calculations.

Figure 19: Fuel and gas temperature evolutions versus additional pressure drop in the DHR loop.

Figure 20: RMPS alternative case (1 DHR loop, valve opening fraction = 7%).

Table 5: Results for the RMPS alternative case.

	Mean	SD	Lower	Upper
TMAX (°C)	1575	10.09	1547	1595
TUPPER (°C)	1451	6.81	1434	1465

As in the reference case, a linear correlation has been determined in order to increase the number of calculations (up to 10^6) and correlatively, the statistics precision for the NC reliability evaluation. Considering a pressure loss penalty of about 0.1 MPa, the unreliability of the NC process is evaluated at about $2, 3, 10^{-3}$ considering only the TMAX criterion. The most influent parameters for the NC performance and reliability are the same as in the reference case (i.e., T2DHR, FRPLAQ, and REPLAQ).

6. CONCLUSIONS AND IMPACT FOR THE GFR DESIGN AND PROBABILISTIC ENGINEERING ASSESSMENT

In the frame of the ongoing probabilistic safety assessment in support to the 2400 MWth GFR design (called "probabilistic engineering assessment"), the RMPS methodology has been applied in order to assess the performance and the reliability of the natural circulation DHR in pressurized situations (i.e., for intact helium pressure boundary). So far, the gaseous NC process is foreseen:

(i) for pressurized situations in the early phase (i.e., first 24 hours) of transients characterized by the integrity of primary circuit;

(ii) for the transient termination (here called "late phase," starting around one day after the initiating event) of depressurized situations.

Owing to the DHR system features (dedicated loops for forced or natural circulation of gas, with passive water-filled secondary circuit), the primary gaseous coolant NC performance is directly related to the natural circulation capability in the secondary side pressurized water circuit, which is thermally linked to a ternary pool. Then, the frame of RMPS exercise for the GFR DHR system is involving a "two-in-one" exercise: one for gaseous coolant, the second for pressurized water natural circulation. This interdependency of physical processes had lead to a methodology proposal in order to define the critical parameters for the RMPS exercise. This proposal is to determine the critical parameters according to "engineering judgment" based on a simplified "deductive analysis" (or cause-consequence analysis). Two classes of critical parameters were determined for the pressurized situation: the first addresses the specific uncertainties associated with factors on which the magnitude of the engaged forces and counter forces depends (values of heat transfer coefficients and pressure losses), where the second relates to uncertainties associated with the potential discrepancy of T-H parameters at "transient" initiation.

Correlatively, sensitivity studies were performed with the CATHARE 2 code to check the influence of the time delay before the forced circulation is lost (FC being the first line of defense) on T-H conditions at NC actuation. The results obtained are exhibiting that the core decay heat and primary pressure levels at NCDHR actuation do not appear as critical parameters for the reliability and performance assessment of this passive system. Translated for the GFR PEA, it validates the event representation in a Boolean model, as all active systems failures during the first 24 hours are aggregated as they occur at initiating event ("static" probabilistic model).

Owing to the precision required for this first NCDHR performance and reliability assessment and compared to the magnitude of other sources of uncertainties in the PEA model, the number of calculations is first limited at 100. However, a linear correlation between TMAX and input parameters, and based only on 6 input parameters, has been determined. The linear standard regression coefficients were applied in order to rank the most influent parameters. For the maximum temperature of materials in the core region, the most influent parameters are according to importance:

(i) FRPLAQ (multiplicative factor for laminar NC pressure drop in the core region);

(ii) REPLAQ (Reynolds number for turbulent to laminar transition in the core region);

(iii) T2DHR (ternary pool and secondary circuit temperature level at DHR initiation).

Compared to the failure/success criteria fixed at 1600°C as mentioned earlier in this document, the results obtained with the CATHARE 2 code are exhibiting the safety margin conferred by the DHR loop structural and thermodynamical design. According to the above-mentioned linear regression, 10^6 simulations were performed to increase the number of calculations (compared to the 100 ones performed with CATHARE 2) and then to improve the probability value of the NC reliability assessment in the PEA model. The results are providing an NCDHR unreliability upper bounded by $5 \cdot 10^{-6}$ for the reference case. This result should be regarded knowing that previous parametric studies on the NCDHR reliability value in the GFR PEA model (from 10^{-1}, as reference value, to 0) were leading to a weak reduction of the core damage frequency of about 3% (for the initiating event families retained for this first model and according to the assumption that the same level of NCDHR reliability will be obtained for depressurized situations).

On the other hand, the sensitivity study performed upon the additional singular pressure drop in the NC loop (through the DHR system isolating valve opening fraction) exhibits that the process unreliability ranges from $5 \cdot 10^{-6}$ (reference) to $2,3 \cdot 10^{-3}$ ($\Delta P \approx 0.1$ MPa) in pressurized situations. At this stage, it should be mentioned that the valve opening fraction was not retained as critical parameter (i.e., with distribution attached on this parameter) due first to its influence on temperature results (potentially masking the effects of others parameters) and also in relation with the common practice in PSA, in which partially opened valves are not depicted. These results should be weighted against the mean reliability value of an active system which is near 10^{-3}.

Nevertheless, the effect of additional pressure drop in the NC loop may be translated to insights for reactor design team and is potentially combining a broad range of uncertainties. Indeed, as the GFR design is still in progress, the improvements of the concept and of specific components (DHR heat exchanger, core) may lead to a reduction of margin compared to the present design. In addition, the CATHARE 2 model for GFR, even detailed, is not representing all the sources of counter forces potentially challenging the NC process. Then, for the design team, it defines margin for overall pressure drop increase in the primary NC loop. Another way to interpret this result could be linked to a reliability allocation for the NCDHR process in order to guaranty a defined core damage frequency. Then, it fixes the overall NC loop pressure drop potential in order to respect the, to date, success/failure criteria.

The pursuit of this work is now related to the study of depressurized situations where the primary circuit is linked

to the guard containment and to T-H involved in it (natural leakage, exchange with a large amount of structures, etc.).

NOMENCLATURE

f: Friction factor;
Re: Reynolds number;
Nu: Nusselt number;
Pr: Prandtl number;
Cv: Valve flow Coefficient;
Pu: Stem position for a valve;
q: Mass flow in kg/s;
P_a: Upstream pressure in Pa;
ρ_a: Upstream gas density.

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