INFLUENCE OF THE SPACER LOCATION AND THE DIRECT HEATING IN THE SINGLE AND TWO PHASE REGION ON THE DEVELOPMENT OF IN-PHASE INSTABILITIES OF BWR

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ABSTRACT

The dynamic of boiling flows can be considered as a complex problem where generally more than one instability mechanism is present. This problem of two-phase flow instabilities is found in a big variety of energy and chemical engineering systems such as the channels of Boiling Water Reactors (BWR), conventional steam boilers, and phase change heat exchangers used in the chemical industry. Among the different instability types, one of the most important for the nuclear engineering field is the in-phase instability that appears in boiling water reactors (BWR). In this instability type a synchronized oscillation of the power and the thermal-hydraulic variables is produced in all the channels of the reactor. This paper study the influence of the spacer location and the direct heating i.e. the heating of the boiling channels by neutron and gamma rays on the in-phase instabilities BWRs.

INTRODUCTION

The spacers in a BWR reactor play a triple role, first they serve as supporting structures of the fuel rods limiting its vibrations, second the spacers located at different heights in the fuel channels promote the mixing of the coolant flow, and in this way support a more uniform heating of the coolant. Also there is a third aspect that must be considered and that it is studied in this paper, and it is the pressure drop friction losses that take place at the spacers, and its influence on the reactor stability depending on its location. The other issue studied in this paper is the influence of the direct heating, by neutrons and gamma rays, on the reactor stability, this amount of heat instead to be released directly to the fuel and then transferred by conduction and convection to the coolant, is directly deposed on the coolant by the neutron collisions, mainly with the hydrogen of the water, and also by some of the gamma rays that can be absorbed in the coolant, in this way we have a source of direct heating that goes directly to the coolant instead to go to the fuel rods and then by conduction and convection to the coolant. This amount of direct heating, although small in magnitude, because is only about 3% of the total amount of heat released in the reactor, has a big influence on the reactor stability and it deserves being studied more deeply.

In modern commercial BWRs the predominant mode of interest for the reactor stability is the density wave oscillation mechanism [1, 2]. If the pressure drop across the boiling channel remains approximately constant during the oscillation, as happens in BWR coolant channels subject to a large recirculation flow rate. Then a perturbation in the inlet mass flow rate will cause an immediate change in the channel outlet mass flow rate in the opposite direction [1]. In addition to this momentum feedback effect, the perturbations in the inlet mass flow rate also produce perturbations in the steam generation rate, the boiling boundary and the void fraction with a delay due to the propagation of the perturbation along the channel with the fluid velocity. These delayed effects affect the momentum feedback because the effect produced on the two phase multiplier by the void perturbations that travel upward is to increase the pressure drop in the upper part of the channel when the flow is reduced in the lower part. Therefore a reduction in the inlet mass flow rate reduce the friction at the channel inlet but the void fraction perturbation increase the pressure drop in the upper part of the channel with a certain time delay. These delayed effect are the root of the in-phase instabilities [3].

This paper studies the effect on the reactor stability of the spacer location and the direct heating by neutrons and gamma rays. The paper has been organized as follows, first we study the thermal-hydraulic model equations with especial emphasis on the modelling of the direct heating by neutron and gamma rays and the pressure drop due to the spacers. Second we study the effect on the stability of these issues.

NOMENCLATURE

A	[-]	Channel transversal area
D_h	[m]	Hydraulic diameter
f	[-]	Friction factor
G	[kg/m^{2}s]	Coolant mass flux in the channel
h_{gap}	[W/m^{2}K]	Heat transfer coefficient of the gap
h_{fg}	[J/kg]	Enthalpy of phase change
K	[-]	Form loss coefficient
k	[W/m K]	Thermal conductivity
M'_f	[kg/m]	Fuel mass per unit length in the fuel rods
M'_{cl}	[kg/m]	Clad mas per unit length in the fuel rods
p	[N/m^2]	Pressure
Q'	[W/m^3]	Heat rate per unit length
r	[m]	Radius
T	[K]	Temperature
x	[-]	Dynamic quality of the steam
z	[m]	Cartesian axis coordinate

Special characters

α	[-]	Void fraction of the steam
Φ^2	[-]	Two-phase multiplier
ρ	[kg/m^3]	Density

Ω	[-]	Jones multiplier

Subscripts

c	Coolant
cl	clad
fu	fuel
f	Liquid at saturation conditions
g	Gas phase
l	Liquid phase
in	Channel inlet
exit	Channel exit

MODEL EQUATIONS

Thermal-Hydraulics of the boiling channels

The model equations for the two phase flow in the boiling channels when the direct heating and the spacers are included is based on the integration of the mass, energy, and momentum equations in the single phase and two-phase regions. Inside the two-phase region, we distinguish between the sub-cooled boiling region and the bulk-boiling region. The amount of heat transfer to the channel fluid by unit length and time is composed of the heat received by the coolant from the fuel by conduction denoted by Q_C', and the amount of heat released directly to the coolant by neutrons and gamma rays denoted by Q_{DH}'. It is assumed that the direct heating is transferred to the liquid phase in the sub-cooled and bulk boiling regions. So that in the subcooled region the direct heating is employed in heating the subcooled liquid phase, while in the bulk-boiling region the direct heating is used in steam production. The conservation equation are:

Mass conservation of the steam in the subcooled-boiling region:

$$\frac{\partial}{\partial t}\left(\alpha\,\rho_g\right)=-\lambda_s\,\rho_g\,\alpha-\frac{1}{A}\frac{\partial}{\partial z}\left(A\,x\,G\right)+\frac{F_s Q_c'}{A\,h_{fg}} \tag{1}$$

We notice that the heat invested in steam production in the subcooled boiling region is a fraction F_s of the heat transferred from the fuel to the coolant due to the fact that a part is invested in heating the subcooled liquid.

Mass Conservation of the steam in the bulk boiling region:

$$\frac{\partial}{\partial t}\left(\alpha\,\rho_g\right)=-\frac{1}{A}\frac{\partial}{\partial z}\left(A\,x\,G\right)+\frac{Q_c'+Q_{DH,c}'}{A\,h_{fg}} \tag{2}$$

We must say that the last term in equation (2) takes into account the fact that the direct heat, and the heat transferred from the fuel to the coolant are both invested in steam production in the bulk boiling region.

The conservation equation for the total mass, is the same one in the subcooled and the bulk boiling regions and is given by:

$$\frac{\partial}{\partial t}\left(\alpha\,\rho_g+(1-\alpha)\rho_l\right)=-\frac{1}{A}\frac{\partial}{\partial z}\left(A\,G\right) \tag{3}$$

Finally we must say that the direct heating contributes to the total energy equation of the two-phase flow, in the sub-cooled and the bulk-boiling regions. Neglecting acoustic phenomena i.e. neglecting the term dp/dt in the energy balance, the total energy equation of the mixture is:

$$\frac{\partial}{\partial t}\left(\alpha\,\rho_g\,h_g+(1-\alpha)\rho_l\,h_l\right)=-\frac{1}{A}\frac{\partial}{\partial z}\left(A\left[(1-x)h_l+x\,h_g\right]G\right)+\frac{Q_c'+Q_{DH,c}'}{A} \tag{4}$$

The approximation of neglect the acoustic phenomena simplifies greatly the problem of solving the channel dynamic behaviour, because uncouples the momentum equation from the energy and mass balances.

The momentum equation for the two-phase mixture takes into account the momentum changes due to acceleration, pressure, gravity forces, friction forces, and pressure losses at the channel inlet and the spacers. It also considers the pressure gain due to the expansion at the channel outlet:

$$\begin{aligned}\frac{\partial}{\partial t}G=&-\frac{1}{A}\frac{\partial}{\partial z}\left[A\left\{\frac{(1-x)^2}{(1-\alpha)\,\rho_l}+\frac{x^2}{\alpha\,\rho_g}\right\}G^2\right]-\frac{\partial p}{\partial z}-\left((1-\alpha)\rho_l+\alpha\rho_g\right)g\\&-f\,\Phi^2\Omega\frac{G^2}{2\rho_l D_h}-K_{in}\frac{G^2}{2\rho_l}\delta(z-z_0)-\sum_i K_i\Phi_i^2\Omega_i\frac{G^2}{2\rho_l}\delta(z-z_i)\\&-K_{exit}\Phi^2\Omega\frac{G^2}{2\rho_l}\delta(z-z_{exit})\end{aligned} \tag{5}$$

In equation (5) K_i denotes the form factor at the i-th spacer.

Equations (1) to (5) plus the closure equations to be explained later and the equations in the single-phase are the set of equations that define the thermal-hydraulic variable evolution in the boiling channel.

Heat transfer in the fuel

In this subsection we set the equations of a lumped parameter model that define the temperature evolution in the fuel and the clad. These equations are [4]:

$$M_{fu}'\,c_{fu}\frac{d\tilde{T}_{fu}}{dt}=Q_F'(t)-Q_g'(t) \tag{6}$$

$$M_{cl}'\,c_{cl}\frac{d\tilde{T}_{cl}}{dt}=Q_g'(t)-Q_c'(t) \tag{7}$$

Where, Q_F' is the heat generation rate in the fuel per unit length; Q_g' is the heat transfer rate per unit length from the fuel to the clad; Q_c' is the heat transfer rate per unit length from the clad to the coolant. Finally $\tilde{T}_f$, and $\tilde{T}_{cl}$ are the average temperatures in the fuel and the clad respectively.

In Eq. (6) the heat transfer rate per unit length from the fuel to the clad, for a given number of rods, n_{rods}, is given by the following expression [4]:

$$Q_g'(t)=\frac{n_{rods}}{R_g'}(\tilde{T}_f-\tilde{T}_{cl}) \tag{8}$$

Where R_g' is the thermal resistance per unit length from the fuel to the clad when using volumetric average temperatures. This thermal resistance per unit length is given by:

$$R'_g = \frac{1}{8\pi k_{fu}} + \frac{1}{2\pi r_f h_{gap}} + \frac{1}{2\pi k_{cl}} \left[\frac{r_{cl}^2}{r_{cl}^2 - r_{fu}^2} ln \frac{r_{cl}}{r_{fu}} - \frac{1}{2} \right] \quad (9)$$

The heat transfer rate per unit length from the clad to the coolant is given by the expression:

$$Q'_c(t) = \frac{n_{rods}}{R'_c} \left(\tilde{T}_{cl} - \tilde{T}_c \right) \quad (10)$$

When R'_c is the thermal resistance per unit length from the clad to the coolant when using volumetric average temperatures. This thermal resistance per unit length is given by:

$$R'_c = \frac{1}{2\pi r_{cl} h_c} + \frac{1}{2\pi k_{cl}} \left[\frac{1}{2} - \frac{r_f^2}{r_{cl}^2 - r_{fu}^2} ln \frac{r_{cl}}{r_{fu}} \right] \quad (11)$$

Also we need to compute the heat released from the axial nodes to the coolant at different heights z. Because the heat production rate in the fuel changes axially, we have assumed that the amount of heat transferred to the coolant per unit length and time t and at a given height z is given by:

$$Q'_c(z,t) = \frac{n_{rods}}{R'_c} f_{pow}(z)(\tilde{T}_{clad} - \tilde{T}_c) = U'(z)(\tilde{T}_{clad} - \tilde{T}_c) \quad (12)$$

Where $f_{power}(z)$ is the power profile and $U'(z)$ the global heat transfer coefficient per unit length from the clad to coolant at a given height z.

The evolution equation of the power generated in the reactor core is not the goal of this paper and is obtained as explained in reference [2], in this reference is explained how to proceed to obtain the evolution of the neutron population $N(t)$ versus time or the excess of neutron population normalized $n(t) = (N(t) - N_0)/N_0$ that are directly related with the power evolution. The coupling of the fuel temperature equation with the point kinetic equations for the neutron population is performed through the following relationship that gives the amount of direct heating per unit length in the coolant [2]:

$$Q'_{DH,c}(z,t) == P_0 f_{Pow}(z)(1 + n(t)) f_{DH} (1 - f_{DH,Byp}) \quad (13)$$

Where P_0 is the reactor power at steady state, f_{DH} the fraction of direct heating by neutrons and gamma rays and $f_{DH,Byp}$ the fraction of direct heating that goes to the bypass and not to the boiling channels.

Finally the amount of power per unit length generated and deposed on the fuel is:

$$Q'_F(z,t) = \frac{P_0}{H} (1 + n(t))(1. - f_{DH}) \quad (14)$$

Where H is the active length of the reactor core. Notice that $1. - f_{DH}$ denotes the fraction of the reactor power that is deposited in the fuel.

Closure relations and properties

In this section we explain the main closure relation that have been used in the model**.** We start with the void quality relation that is needed to solve the set of equations (1) to (5). This relation is given by the following equation [5]:

$$\alpha = \frac{x}{x + s \frac{\rho_g}{\rho_l}(1 - x)} \quad (15)$$

where s is the slip ratio or ratio between the steam and liquid velocities. To compute s we use the modified Bankoff empirical correlation as determined by Jones given in the subcooled boiling region by[6,7]:

$$s = \frac{1 - \alpha}{k_s - \alpha + (1 - k_s)\alpha^r} \left(1 - \exp\left(-\frac{\alpha H_{z1}}{\alpha_\gamma H_l} \right) \right) \quad (16)$$

Where k_s, and r, are functions that depend on the operating pressure. At an operating pressure of 1000 psi the values of these constants are 0.8 and 3.97 respectively. H_{z1} is the degree of sub-cooling at the inception point z_1 and, $H_l = (h_f - h_l)/h_{fg}$. We must remark that in the bulk boiling region the degree of sub-cooling is zero and the term between brackets in equation (16) is 1.

The other closure relation, that has a strong influence on the channel pressure drop and therefore on the reactor stability is the two-phase multiplier. In this case we have used for the local losses produced by the spacers the multiplier expression obtained by Jones [6, 7], which can be used in a broad range of pressure conditions and dynamic qualities. A fit valid for x< 0.7 is given by:

$$\Phi^2 = exp\left\{ \sum_{i=1}^{4} a_i \left[log(1 + 100x) \right]^i \right\} \quad (17)$$

where the a_i are pressure dependent fitted parameters given by:

$$a_i = \sum_{j=1}^{8} b_{ij} p_j \quad (18)$$

being b_{ij} fitting coefficients, see reference [6, 7]; $p_1 = 1$; $p_2 = 1.42234 \times 10^{-2} p$, for j=3,…6, and finally p is the pressure in kg/cm2.

The heat transfer coefficients in the single-phase and two-phase regions have been obtained using the Dittus-Boelter and the Jens-Lottes heat transfer coefficients respectively [5].

In the subcooled boiling region we need to evaluate the fraction of the heat transfer to the coolant that is invested in steam production, this fraction denoted as F_s depends mainly on the ratio of the heat flux q''_{evap} invested in steam production, and the energy flux q''_{pump} associated to the pumping of the liquid mass out of the control volume by the expanding action of the steam bubble formation. Therefore it is possible to express F_s as follows [2, 5]:

$$F_s \approx \frac{q''_{evap}}{q''_{evap} + q''_{pump}} = \frac{1}{1 + \frac{q''_{pump}}{q''_{evap}}} \quad (19)$$

If j_b denotes the steam volumetric flux due to the bubbles created at the fuel rod wall, then this evaporation heat flux is obviously given by $q''_{evap} = \rho_g j_b h_{fg}$. The energy flux

q''_{pump} during the pumping process of the liquid mass which is being expelled out of the control volume by the expanding action of the steam bubble formation is given by $q''_b = \rho_f \, j_b (h_f - h_l)$. Therefore equation (19) can be expressed in the form:

$$F_s = \frac{1}{1 + f_p H_l} \tag{20}$$

Being f_p a correction factor to better correlate the predictions with the experimental data and H_l that has been defined previously.

The time decay constant of the bubbles in the subcooled liquid depends on the channel conditions at the axial position being considered. Obviously if the liquid is very subcooled then the decay rate of the bubbles will be bigger than if the sub-cooling of the liquid is small, because the heat transfer between the phases increase with the degree of sub-cooling. This means that the decay rate of the bubbles should depend on the degree of sub-cooling i.e. $\lambda_s \propto H_l^n$, where n should be obtained from experiments. An expression obtained by Jones [6,7] and that gives results close to the RELAP code is:

$$\lambda_s = c \, \lambda_0 \, \phi \, H_l^2 \tag{21}$$

being ϕ and λ_0 given by the following expressions:

$$\phi = \left(\frac{h_{fg}}{c_{pf} (T_{c0} - T_{sat})} \right)^2 , \quad \lambda_0 = \frac{H_w^2}{k_f \rho_l \, c_{pf}} \tag{22}$$

The recommended value for constant c in equation (21) is 0.125. T_{c0} is the clad temperature at the inception of the sub-cooled boiling; H_w is the single-phase heat transfer coefficient; k_f is the liquid conductivity at saturation conditions; and, finally c_{pf} is the water specific heat at saturation conditions.

Recirculation loop dynamics

The recirculation loop closes the hydraulic circuit of the BWR considered in in-phase oscillations and is formed by: the upper plenum, the steam separators, the down-comer, the jet pumps and the lower plenum. The recirculation loop dynamics determines the mass flux to the boiling channels. To simplify the calculations, the recirculation loop is considered as a single path of uncompressible fluid and without boiling. We use the same recirculation loop dynamics of references [3, 4]:

$$\frac{dG(z=0,t)}{dt} = \frac{\delta p_{iL}(t) - \delta p_{0L}(t)}{\Gamma A} + \frac{\Delta p_{f,nc,0}}{\Gamma A} \left(1 - \frac{G^2(z=0,t)}{G_0^2(z=0)} \right) \tag{23}$$

Being $\Delta p_{f,nc,0}$, the pressure drop in the boiling channel at steady state and natural circulation conditions; $G(z=0,t)$ the mass flux at the channel inlet; Γ the length to area ratio in the recirculation loop. Finally $\delta p_{iL}(t), \delta p_{0L}(t)$ are the pressure oscillations at the inlet and outlet of the recirculation loop (RCL) with respect to the steady state pressures at natural circulation conditions. It is assumed as proved by Prassad et al [8], that the pressure oscillations at the inlet of the recirculation loop (upper plenum) can be neglected so that $\delta p_{iL}(t) = 0$.

NUMERICAL METHOD AND VALIDATION

The numerical method only will be outlined and can be found with more detail in reference [2].

Discretization of the conservation equations

The set of all the conservation equations forms a partial differential equation system. The integration of these equations with respect to the axial coordinate z, between the limits of the different nodes, yields a set of coupled ordinary differential equations for the nodal average variables. To see how this operation is performed the averaging operator $\frac{1}{\Delta} \int_{z_{i-1}}^{z_i} dz$ is applied to the following set of all partial differential equations: mass, energy and momentum conservation equations explained previously. Then, we have defined average nodal thermal-hydraulic variables for an arbitrary magnitude $g(z,t)$ as follows:

$$g_i = \frac{1}{\Delta} \int_{z_{i-1}}^{z_i} g(z,t) dz \tag{24}$$

For simplicity we have assumed that all the nodes have the same length Δ.

Proceeding in this way and after some manipulation it is obtained a set of coupled ordinary differential equations for the dynamic variables of the system at the nodes[2]. For instance, for the evolution of the average value of the void fraction at the i-th node in the subcooled boiling region:

$$\frac{d\alpha_i}{dt} = \frac{\dfrac{F_{si} Q'_{c,i}}{A \, h_{fg}} - \lambda_{si} \rho_g \alpha_i - \dfrac{1}{\Delta} G(z_{i-1},t) [x(z_i,t) - x(z_{i-1},t)]}{[\rho_g + (\rho_l - \rho_g) x(z_i,t)]} \tag{25}$$

And in the bulk boiling region:

$$\frac{d\alpha_i}{dt} = \frac{ (\dfrac{Q'_{c,i} + Q'_{DH,c,i}}{A}) + \dfrac{1}{\Delta} \{ [x(z_i,t) - x(z_{i-1},t)] (h_f - h_g) G(z_{i-1},t) \} }{\rho_g h_g - \rho_f h_f + [h_f (1 - x(z_i,t)) + h_g x(z_i,t)] \; (\rho_f - \rho_g)} \tag{26}$$

The evolution of the pressure in the different nodes is obtained applying the averaging operator to the momentum equation (5), this calculation yields:

$$\begin{aligned} p(z_{i-1},t) = {} & p(z_i,t) + \Delta \frac{dG_i(t)}{dt} + [MF(z_i) - MF(z_{i-1})] + \\ & \Delta ((1-\alpha_i)\rho_l + \alpha_i \rho_g) g + \Delta \, FR_i + K_0 \frac{G_0^2}{2\rho_l} \delta_{i,1} + \\ & \sum_j K_j \Phi_j^2 \Omega_j \frac{G(z_{sp,j})}{2\rho_l} \delta_{i,j} + K_{exit} \Phi^2 (z_{exit}) \Omega_{exit} \frac{G(z_{exit})}{2\rho_l} \end{aligned} \tag{27}$$

Where $MF(z_i)$ is the momentum flux at $z=z_i$, and FRi are the friction losses at the i-th node per unit length. The discretized mass and energy equations are the same ones that in reference

[2]. For instance from the total mass equations it is obtained:

$$G(z_i,t) = G(z_{i-1},t) + \Delta(\rho_l - \rho_g)\frac{d\alpha_i}{dt} \qquad (28)$$

Normalization of the equations and integration

Therefore solving the steady state equations i.e. setting the time derivatives to zero and iterating until convergence, one obtains the initial values for the thermal-hydraulic variables of the problem at time 0, at the nodes $i = 1,2,...,nd$.

Then we subtract from the dynamic equations the steady state equations, and we obtain a set of coupled differential equations for the variations of the magnitudes around its steady state values. If we denote by $\vec{y}$ the magnitude variations at all the nodes, except the mass flux variations denoted by $\vec{G}$, and the pressure variations in all the nodes denoted by $\vec{p}$, it is obtained a set of coupled differential equations of the form:

$$\frac{d\vec{y}}{dt} = \vec{f}(\vec{y}, \vec{p}, \vec{G}, t) \qquad (29)$$

Plus two additional set of equations formed by equations (27) and (28) that provide the pressures and the mass fluxes at the node boundaries. All these equations have been solved by a modified fourth-order Runge-Kutta (RK) method. During one time step first we integrate equation (29) by standard four order RK method using the pressures and mass fluxes of the previous step. Then at the end of the time step we update the pressures and the mass fluxes using equation (28) and (29).

Validation with plant Data

A series of 12 stability tests were performed at the Vermont Yankee Nuclear Power Plant (VY) during cycle 8 in march 1981. Some tests were performed in single loop operation i.e. were conducted with one recirculation drive loop blocked so the decay ratio of these two tests are not computed in this section. Test 2 and 4 are not included in the calculations because the power to flow ratio was very low and we have not information about the reactivity coefficients. On account that Sandoz and Chen [9] provides information about the power and flow conditions for each stability test, and also on the measured decay ratio and resonant frequency. We have performed with the previous model the calculation of the decay ratio (DR) and the oscillation frequency (ω) for the different conditions of these tests that are displayed in table 1. We must remark that for all the tests we use the same conditions: a bypass flow fraction of 0.053, a direct heating fraction close to 0.03 typical of BWR-4 and a subcooling of -11ºC.

EFFECT ON THE STABILITY OF THE DIRECT HEATING BY NEUTRONS AND GAMMAS

The direct coolant heating by neutrons and gamma rays adds heat directly to the liquid in the sub-cooled and boiling regions. Previous studies performed by Van der Hagen with a very simple model [10] concluded that the direct energy deposition appears to have a strong stabilizing effect on BWR reactors, this means that increasing the fraction of direct heating will increase the reactor stability. The reason of this behavior is that the heat added directly to the coolant has not the time delay of the conduction process in the fuel. This time delay produces a destabilizing effect.

To check the influence of the direct deposition heat, we have modified the model parameters for the VY reference case, where we used a direct heating fraction $f_{DH} = 0.032$, and a direct heating fraction to bypass of $f_{DH,Bypass} = 0.52$, these values are typical for BWR. We have varied the direct heating fraction f_{DH} between 0.0 and 0.052 maintaining $f_{DH,Bypass}$ constant, and we display the results obtained for the DR and the maximum amplitude of the limit cycle $[n(t)]_{max}$ in Table 2. We observe that for the reference value of the direct heating $f_{DH} = 0.032$, it is obtained a limit cycle of small amplitude. If the amount of direct heating f_{DH} is increased to 0.042, maintaining constant the fraction that goes to the bypass, then the reactor stabilizes and the decay ratio diminishes to 0.906. However if the fraction of direct heating diminishes to 0.022, then we have a limit cycle of very large amplitude with $[n(t)]_{max} = 1.31$. These calculations confirm Van der Hagen predictions [10].

Table 1 Experimental and computed DR and frequency

Test	Power% rated power	Flow % Rated Flow	DR (test)	DR model	ω test	ω model
1	51.1	38.5	0.36	0.52	0.40	0.50
5	48.1	38.3	0.40	0.49	0.41	0.49
6	57.2	38.5	0.74	0.65	0.44	0.54
7	51.2	32.6	1.00	0.97	0.43	0.50
8	50.9	32.6	< 1	0.93	0.43	0.49
9	48.1	32.4	0.81	0.88	0.42	0.47
11	67.1	38.5	< 1	0.80	0.47	0.59
12	63.1	38.5	0.84	0.74	0.46	0.55

Table 2 Decay Ratio for different values of the direct heating fraction.

f_{DH}	$f_{DH,Bypass}$	DR	$[n(t)]_{max}$
0.000	0.52	1.00	3.13
0.012	0.52	1.00	2.19
0.022	0.52	1.00	1.31
0.032	0.52	1.00	0.14
0.042	0.52	0.906	-
0.052	0.52	0.820	-

EFFECT ON THE STABILITY OF THE SPACER LOCATION AND FORM LOSS COEFFICCIENTS

As we comment in the introduction: if the inlet flow is decreased while the channel power is kept constant, there is a pressure drop reduction at the lower nodes, and also an increase in the voiding in the channel that will travel upward as a packet, forming a propagating density wave. This packet of voids produces a change in the local pressure drop at each axial location, which is delayed axially by the density wave propagation time. In two-phase flow regimes, the local pressure drop is very sensitive to the local void fraction that is very large at the upper region of the channels, where the void fraction is greatest. Therefore, due to the two-phase flow multiplier that is larger in this region and increases with the dynamic quality, we

have a change in the pressure drop over a significant channel length, which is delayed with respect to the original perturbation. Such delay is the basis for the channel instability.
Therefore the spacers can have a significate influence on the DWO and therefore on the reactor stability. The previous cases were computed with typical values of form factors and spacers location typical of BWR-4 cores with GE-5 fuel type used by VY and Peach Bottom in the eighties. The typical values were $K_{spacer,low} = 2.42$, for the two spacers located in the lower part of the core channels, $K_{spacer,middle} = 3.63$, for the three spacers located in the middle, and $K_{spacer,upper} = 2.42$, for the two spacers located in the upper part. The spacer location was the typical one provided by the vendors.
To study the spacer's effect first we reduce the form factor to zero in the two upper spacers, as shown in the second column of table 3, in this case in spite of reducing the channel friction, the reactor stabilizes and the DR drops from 1.00 to 0.523. The opposite effect happens when we reduce to zero the form loss factors at the two lower spacers. In this last case, this is equivalent to reduce the pressure drop in the lower region practically without modifying the pressure drop delay with respect to the inlet perturbations. Obviously the effect in this case is to increase the amplitude of the limit cycle oscillations that attain a large amplitude with $[n(t)]_{max} = 2.46$.

Table 3 Effect on the stability of removing the spacers form loss coefficients at the upper and lower positions

Node	K_{spacer}		K_{spacer}		K_{spacer}	
4	2.42	Limit Cycle $[n(t)]_{max}$ = 0.14	2.42	DR= 0.523	0.0	Limit Cycle $[n(t)]_{max}$ 2.46
7	2.42		2.42		0.0	
10	3.63		3.63		3.63	
13	3.63		3.63		3.63	
16	3.63		3.63		3.63	
19	2.42		0.0		2.42	
22	2.42		0.0		2.42	

CONCLUSIONS

The most common instability for commercial BWRs is the known as density-wave instability (DWO). This instability type as we have mentioned earlier can be described as follows given a flow perturbation at the boiling channel inlet, a "wave" of voids travels upward through the channel producing a pressure drop that is delayed with respect to the original perturbation. An increase in the channel inlet flow typically induces an increase in pressure drop at the channel lower region and a decrease in the pressure drop in the upper nodes that is delayed with respect to the original perturbation. The original perturbation should induce a negative feedback that should tend to reduce the flow perturbation. However, the density-wave phenomenon delays this feedback, and, at some frequency, the delay is equivalent to a 180º phase lag; thus, at this frequency, the pressure drop feedback becomes positive. If the gain is large enough, the channel flow becomes unstable and oscillates at that frequency. Because the thermal-hydraulic of the reactor channels is coupled to the neutronic via the void reactivity feedback, then the reactivity feedback caused by changes in void fraction along the reactor core is delayed as the voids travel upward trough the channel. If this delay is long enough or the void feedback coefficient is strong enough, the reactor configuration becomes unstable, and the neutron flux oscillates in phase with a frequency close to the inverse of the density-wave time constant.
In this paper we have studied first the effect on the reactor stability of the direct heating by neutron and gamma rays. Because the direct heating reduces the delay, then has a stabilizing effect as it is displayed in table (2). However the role and influence played by the spacers on the stability depends on its position and its form loss coefficient values. In general an increase of the form factor in the upper spacers decrease the reactor stability because we increase the phase lag of the pressure drop perturbations, compare columns (3) and (5) of table 3. However an increase of the form factor in the lower spacers increase the reactor stability i.e. diminishes the decay ratio (compare columns 3 and 7 of table 3) as have been proven in this paper.

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