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Prediction of Flow Induced Damping of a PWR Fuel Assembly in Case of Seismic and Loca Load Case

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ABSTRACT

The fuel assembly mechanical strength must be justified under accidental conditions with respect to the different lateral loads, and in particular seismic loads. This justification is done by means of time-history analyses using dynamic models of an assembly row in the core, allowing for assembly deformations, impacts at the grid locations and reactor coolant effects.

One key parameter of such analyses is the damping coefficient of the first mode of the assembly used to compute its dynamic response. This damping coefficient may be very important due to core coolant flow velocity (up to 50 % of critical).

Many research and development works have been done on such a topic. These studies show that flow induced damping comes from a lift phenomenon due to the relative motion between (axial) flow and lateral displacement of the fuel assembly.

The objective of this paper is to predict the flow-induced damping of a fuel assembly using the MEFISTEAU model developed by EDF. This analytical model, based on a perturbation method, predicts the fluidelastic forces applied to the assembly subjected to the coolant axial uniform flow.

The modal characteristics of the fuel assembly (i.e. modal frequencies and mode shapes) used as an input by the model have first to be evaluated. These characteristics are computed using the *Code_Aster* finite element code developed by EDF. This code is in particular able to take into account the effect of irradiation on the mechanical properties of the fuel assembly and especially the softening of the fuel assembly due to relaxation effects. The MEFISTEAU model is then used to predict the flow-induced damping from fuel assembly beginning of life to end of life conditions.

This paper (i) describes the method used to predict the flow induced damping and its validation and (ii) shows the evolution of flow induced damping along the life of the assembly, for different designs (12ft, 14ft especially) and flow conditions for in core situation. This paper finally proposes some recommendations concerning flow induced damping coefficients that should be used for design studies and margin assessments.

1 INTRODUCTION

The analysis of the seismic (or LOCA) behaviour of a PWR core is mainly performed to check the integrity of the mixing grids. For this purpose, finite elements models are built. Non-linear computations are performed because of the gaps between the different sub-structures which cause impacts between each other. In addition, Interaction between the Fluid (primary coolant) and the Structures (FSI) must be taken into account.

Since the early 90's, EDF has carried out a large research and development program, mainly in association with French CEA (Commissariat à l'Energie Atomique) and FRAMATOME-ANP, in order to increase the efficiency of the methods and models used to predict the seismic behaviour of the PWR cores and to quantify the margin in the design methodology.

In that field, the flow induced damping (which is a key parameter) was studied in detail based on semi-analytical reduced scale tests, full scale test and analytical developments.

The objective of this study is (i) to present the methodology used by EDF to predict flow induced damping of a fuel assembly, (ii) to describe its validation and (iii) to predict the evolution of the flow induced damping for 12" and 14" design from beginning of life to end of live fuel assembly in core conditions.

2 DAMPING OF A FUEL ASSEMBLY

The fuel assembly damping is due to two phenomena. The first one is the structural damping and the second one is the damping induced by the fluid (primary coolant).

2.1 Structural damping

Structural damping of the fuel assembly mainly results from friction between the fuel rod and the grid springs and dimples contacts.

This friction contact leads to a non-linear behaviour of the fuel assembly :

- its stiffness decreases with the amplitude of motion,
- its damping increases with the amplitude of motion,

Consequently, the fuel assembly reduced damping coefficient is relatively low for low amplitude of (lateral) motion (about 1 %) and can reach values of about 20% of critical for seismic amplitude of motion (~ 20 mm).

2.2 Fluid induced damping

2.2.1 Flow induced damping

Flow induced damping may be very high (up to 50% of critical). Many research and development works were undertaken in order to evaluate and understand the mechanisms associated with this phenomenon [1], [2], [3].

It has been shown that flow induced damping is induced by a lift phenomenon. The general expression of the lift forces opposed to the lateral motion of the structure is given in Figure 1.This expression shows that the lift force comes from the incidence $\alpha \sim \dot{X} / U$ due to the relative motion between (i) the fluid (axial motion : U) and the structure (lateral motion : $\dot{X}$).

$$F \approx -\frac{1}{2}.\rho.f^{t}_{conf}.C_L.S.\left(\frac{\dot{X}}{U}\right).U^2 = -\frac{1}{2}.\rho.f^{t}_{conf}.C_L.S.U.\dot{X}$$

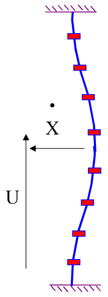


Figure 1 : General expression of the flow induced damping force

Where :

ρ : Density of the fluid.

f^{t}_{conf} : This term illustrates that the lift force depends on the confinement (i.e. hydraulic and geometrical environment of the fuel assembly). This can be taken into account by different ways, depending on the prediction model used.

C_L : This term is the lift coefficient. Its value depends on the surface and the shape of the structure (i.e. bundle or grid).

S : This term is a reference surface. The choice of the reference surface depends on the considered structure: grid or fuel rod.

$\dot{X}$: Lateral velocity of the fuel assembly (more precisely, relative lateral velocity between the assembly an the fluid).

U : Axial velocity of the fluid

2.2.2 Damping in still water

Relatively high values of damping have also been observed in case of still water. In this situation, the damping is due to the lateral motion of the structure in a quiet fluid which induces a drag effect. This drag force has to be formulated differently than in case of flow induced damping. The damping force in still water, in case of full confinement (i.e. low gap between the fuel assembly and its environment, consistent with in core configuration) can be expressed in a general form as follows [4] :

$$F \approx -\frac{1}{2}.\rho.f^{t}_{conf}.C_D.S.\dot{X}^2$$

3 DESCRIPTION OF THE METHOD

The method applied to predict flow induced damping of the fuel assembly is divided in two steps :

1) Calculation of the modal characteristics of the fuel assembly (eigenfrequencies, mode shapes …) based on a finite element model

2) Calculation of the modal flow-induced damping based on a fluid-structure interaction analytical model called MEFISTEAU

All these analyses are performed using the finite element code developed by EDF : *Code_Aster*.

3.1 Fuel Assembly modelling

The finite element model used by EDF is made of 2 beams. The first one (Guide Tubes beam) accounts for the 24 guide tubes and the instrumentation tube. The second one (Fuel Rods beam) accounts for the 264 fuel rods. The characteristics of the model are determined as follows (see Figure 2 a).

- The geometrical and mass characteristics of the beams are directly calculated from the real characteristics of the guide tubes and fuel rods (including Uranium pellets),
- Nozzles are rigid bodies,
- In addition, a rocking stiffness is introduced between two adjacent grids in order to account for the Huyghens term of the inertial characteristics of the tubes which is not taken into account in the guide tubes and fuel rod elements,
- Finally, two springs are introduced in the model between the grid and the fuel rod beam. These springs which account for the internal stiffness of the grid are composed of 2 parameters :
  * A translation stiffness Kt determined based on the spring and dimple characteristics, which enables the lateral impact behaviour of the fuel assembly to be reproduced,
  * A rocking stiffness K_R which enables the lateral behaviour (i. e. natural frequencies) of the assembly to be reproduced: $K_R = q. C1 + (1 - q). C2$. The q coefficient is scaled to 1 for beginning of life situation and low amplitude of motion and 0 for end of life and high amplitude. C1 and C2 coefficients can be calculated based on the springs and dimples characteristics (see Figure 2 b).

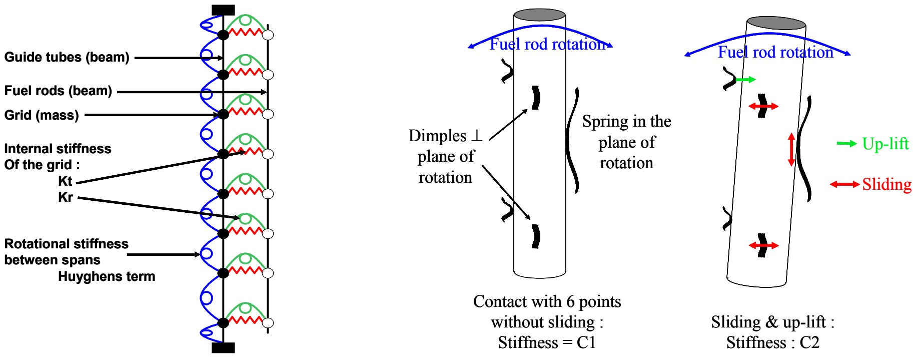


Figure 2 : a) Description of EDF FA model **b) Rocking stiffness of the grid – C1 et C2 coefficients**

3.2 Fluid-Structure Interaction modelling

The model used to compute the FSI is described in this section. This model, developed by EDF is called MEFISTEAU.

3.2.1 MEFISTEAU model description

The MEFISTEAU model developed by EDF [5] from ideas first proposed by Païdoussis [10] predicts the fluidelastic forces in bundles of slender circular cylinders subjected to an axial uniform turbulent flow. It is based on a perturbation method, assuming quasi-steady viscous fluid forces and using the slender body theory approximation. Fluidelastic pressure forces are calculated from potential theory, with hypotheses that the boundary layers are very thin and remain attached to the cylinders. The determination of the fluidelastic viscous forces amounts to considering the effect of a flow over an inclined cylinder at a very small angle of incidence. This model deals with circular flow channels as well as rectangular ones by applying the method of images. This model was extended to account for the grids [6].

This model is described in detail in [5] and [6]. In this paper, we will focus on the main parameters.

3.2.2 Lift and drag forces modelling

For small amplitudes of vibration $\left(\frac{\dot{x}}{U} = \alpha_x \ll 1\right)$, drag and lift forces on the grid and the bundle in the direction of motion of the fuel assembly can be written in the as follows :

$$\begin{cases} D_x = -\frac{1}{2}.\rho.A.|U|.U.C_D.\alpha_x \\ L_x = \frac{1}{2}.\rho.A.|U|.U.C_L \end{cases}$$

where A is the projected frontal area of the structure (grid or bundle), C_D and C_L are the dimensionless drag and lift forces coefficients function of the incidence α_x. As the angle of incidence α_x is of order one, an expansion of these forces around zero may be carried out and yields :

$$\begin{cases} C_D = C_{D0} + \left(\dfrac{\partial C_D}{\partial \alpha_x}\right)_{\alpha_x=0} \times \alpha_x = C_{D0} + C'_D \times \alpha_x \\ C_L = C_{L0} + \left(\dfrac{\partial C_L}{\partial \alpha_x}\right)_{\alpha_x=0} \times \alpha_x = C_{L0} + C'_L \times \alpha_x \end{cases}$$

For symmetry reasons, C_{L0} must be zero. In addition, C'_L is theoretically the derivation of the lift coefficient at zero incidence. Finally, it can be shown that the parameters which have to be known (or fixed) are :

C_{Db} : Drag coefficient of the bundle at low incidence

C'_{Lb} : Derivation of the lift coefficient of the bundle at low incidence

C_{Dg} : Drag coefficient of the grid at low incidence

C'_{L_g} : Derivation of the lift coefficient of the grid at low incidence

3.2.2.1 Drag coefficient of the bundle : C_{Db}

This coefficient is directly deduced from the rod bundle pressure loss coefficient. This coefficient is well known for fuel rods bundle and not significantly different from a design to another.

In addition, it has been shown in [5] that this parameter has not a significant effect on the flow induced damping.

Consequently, we consider this parameter as well known : $C_{Db} = 0.005$ (single rod)

3.2.2.2 Lift coefficient of the bundle at low incidence : C'_{Lb}

This coefficient can not be calculated analytically. It should therefore be evaluated based on experimental results.

It has been shown in [5] that the value deduced from EDF internal tests was fully in accordance with other results [1] & [2] and properly fitted experimental data in a wide range of cases.

Consequently, we consider this parameter as known without major uncertainties : $C'_{Lb} = 0.08$ (single rod)

3.2.2.3 Drag coefficient of the grid : C_{Dg}

This coefficient is directly deduced from the grid pressure loss coefficient [6]. For compatibility reasons, pressure loss coefficient K_{DP} can not be very different from one grid design to another ($K_{DP} \sim 1$). A value of $K_{DP} = 1$ leads to a drag coefficient of the grid of approximately 7.

Consequently, we consider this parameter as known with a low variability : $C_{Dg} = 7$.

3.2.2.4 Lift coefficient of the grid at low incidence : C'_{L_g}

A grid is made of strips put together as a frame. Each trip comes from a Zircaloy plate. In the case of a plate placed in a two-dimensional flow, this parameter has a theoretical value of 2.π. Nevertheless, a real strip which is composed of springs, dimples and holes, is obviously different from the theoretical case. Based on test results, reference [7] shows that this parameter could be significantly lower than the theoretical value in case of a real grid.

Consequently, we consider this parameter as unknown. The value of this parameter will thus be adjusted on test results. For the purpose of this study, the value of this parameter will be expressed relatively to the theoretical value of 2.π (for one strip).

4 COMPARISON BETWEEN TEST RESULTS AND MODEL PREDICTION

The validation of the MEFISTEAU model was based on many experimental results (especially semi-analytical test results). In this paper we selected test results obtained on full scale fuel assemblies in the frame of a R&D program performed in France in association with CEA (Commissariat à l'Energie Atomique) and FRAMATOME-ANP.

Two different configurations have been selected :

1) 14ft fuel assembly – Sinus excitation – 0.5 to 10 mm amplitude of motion,

2) 12ft fuel assembly – Flow induced excitation – Low (~10 µm) amplitude of motion.

4.1 First step – Best estimate input parameters

The objective of this first step is to determine the best estimate value of the lift coefficient of the grid at low incidence: C'_{L_g} .

4.1.1 14ft FA – Sinus excitation – 0.5 to 10 mm amplitude of motion

These tests were performed in the Hermes-T loop of the CEA [3].

The objective of these tests was to identify the flow induced damping under motion representative of the seismic motion of the fuel assembly. For this reason, it has been decided to excite the fuel assembly on its first mode with a shaker.

For high amplitude of motion, the structural damping of the fuel assembly is significant. In order to compare directly the results of the test and the prediction of the model, the following procedure has been applied:

1) Determination of the q parameter of the fuel assembly model to get the first eigenfrequency in air (which depends on the amplitude of motion),

2) Computation of the predicted flow induced damping with MEFISTEAU model,

3) Determination of the experimental value of flow induced damping by subtracting the structural damping (measured in air) from the total damping under flow,

The results are presented in Figure 3.

One can observe that for a lift coefficient fixed to $C'_{L_g} \sim 2.\pi / 5$, the model is very well in accordance with the experimentation, especially in case of a moderate amplitude of motion (less or equal to 3 mm).

For higher amplitudes, the model reproduces experimental tendencies. In that situation, one can notice that reduced damping coefficients are very high (over 50 % of critical).

Figure 3: Flow induced damping – Sinus excitation – Seismic amplitude of motion - $C'_{L_g} = 2.\pi / 5$

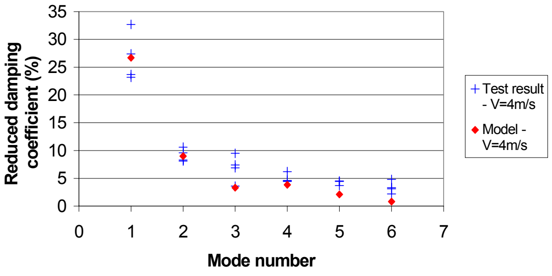
4.1.2 12ft FA – Flow induced excitation – Low (~10 µm) amplitude of motion

These tests were performed in the Hermes-T loop of the CEA [12].

The objective of these tests was to identify the flow induced damping under very low amplitude of motion in order to confirm the theory which predicts high damping even in that case. For this reason, low amplitude of motion was generated only by the turbulence of the flow.

The methodology used to identify damping was based on the numerical modal identification model IMENE [11] which can be used for identifying the flow induced damping of the different fuel assembly modes (modes n°1 to 6 for a 12ft fuel assembly).

The results are presented in Figure 4.



In this situation, test results and prediction are very well in accordance, for mode 1 to 6.

As for the previous test, the lift coefficient has been fixed to $C'_{L_g} \sim 2.\pi / 5$.

This confirms that flow induced damping is high, even for low amplitudes of motion.

Figure 4 : Flow induced damping – Flow induced excitation – Low amplitude of motion - $C'_{L_g} = 2.\pi / 5$

4.1.3 Conclusion

These results show that the model is able to predict the flow induced damping, not only for mode n°1 but also for higher modes, for low and high amplitudes of motion, and for different designs (12ft and 14ft), with the same values of the input parameters, which gives a high level of confidence on the theory developed in the model.

Consequently, we consider the value of $C'_{L_g} \sim 2.\pi / 5$ as the best estimate value of this parameter.

4.2 Second step - Sensitivity to input data

In this section, we give some elements to evaluate the sensitivity of the flow induced damping to some key parameters.

4.2.1 Temperature and Flow velocity

Due to the linear theory on which the model is based, the predicted flow induced damping is proportional to the flow velocity and to the density of the fluid (i.e. temperature).

These tendencies have been confirmed experimentally [1], [2] and [3].

4.2.2 Confinement

FSI is dependent on the lateral confinement of the fuel assembly (in test-loops as well as in cores) [8].

To evaluate the importance of this parameter on the flow induced damping, two different confinement have been studied:

1) In-core nominal confinement : 215 mm

2) Larger confinement considering all the gaps in a row of assembly concentrated around one assembly: ~ 245 mm

For in-core thermal-hydraulic conditions, situation 1) gives a reduced flow induced damping coefficient of 28 % of critical whether situation 2) gives a reduced flow induced damping coefficient of 26 % of critical.

Our conclusion is that confinement has a moderate effect on the flow induced damping (within a realistic range) and the nominal value of in core confinement (215 mm) should be used. Anyway, additional studies are in progress on that point [9].

4.2.3 Drag coefficient of the grid

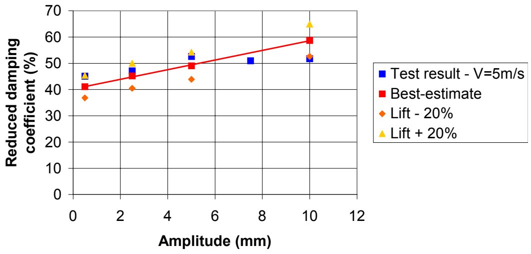
As it was mentioned previously, this grid drag coefficient is directly deduced from the grid pressure loss coefficient K_{DP}. For compatibility reasons, the pressure loss coefficient K_{DP} cannot be very different from one grid design to another ($K_{DP} \sim 1$).

Based on a realistic value of the resultant grid drag coefficient ($C_{D_g} = 7$, cf. § 3.2.2.3), we performed another calculation increasing this value of ~50% ($C_{D_g} = 10$) which is much higher than expected for this parameter (due to necessarily compatibility requirements). In that situation, the relative increase of the flow-induced damping is 2.5% (damping increases from 41% to 42% of critical).

Our conclusion is that drag coefficient (i.e. pressure loss coefficient) of the grid has no significant effect on the flow induced damping.

4.2.4 Lift coefficient of the grid

Based on the same test results than in § 4.1.1, a sensitivity study on the value of C'_{L_g} has been performed around the best estimate value $C'_{L_g} = 2.\pi / 5$. The result of the sensitivity study is presented in Figure 5.



We can observe that a variation of +/- 20% of the lift coefficient of the grid ($C'_{L_g} \approx \frac{2.\pi}{6}$; $C'_{L_g} \approx \frac{2.\pi}{4}$) leads to a variation of +/- 10% on the flow induced damping.

Our conclusion is that the lift induced by the grids leads to half of the flow-induced damping (consequently, lift induced by the bundle also corresponds also to half of the total damping).

Figure 5 : Flow induced damping – Sensitivity to lift coefficient of the grid

Finally, it is possible to conclude that a value of $C'_{L_g} \approx \frac{2.\pi}{}$ gives a best estimate value of the lift effect due to the grid and a value of $C'_{L_g} \approx \frac{2.\pi}{}$ gives a conservative evaluation of the lift effect due to the grid.

4.2.5 Height of the grid

According to the theory, grid height should have a direct impact on the damping because the lift force is proportional to the surface of the strips (cf. § 3.2.2.4 & [7]). The model has been used to predict the flow induced damping of two comparable fuel assemblies but one with ~ 35 mm high grids and the other equipped with 60 % higher grids (about 55 mm high, which is a realistic upper value).

In that situation, flow induced damping increase from 41% to 54% of critical, which corresponds to a relative increase of approximately 30%. This result, which is a consequence of the theory, confirms that lift effect due to grids corresponds to approximately half of the total flow-induced damping (an increase of height of 60% lead to an increase of damping of 30 %).

Nevertheless, this result should be confirmed by experimental results.

5 PREDICTION OF FLOW INDUCED DAMPING OF A F.A. – IN CORE SITUATION

Based on the previous results, calculations were performed with the model to predict the flow induced damping of typical fuel assembly designs used for in-core conditions. The prediction was done under the following conditions:

1) Two different designs of fuel assembly were studied

- one 12ft – 8 grids representative of a French 900 MWe PWR

- one 14ft – 10 grids representative of a French 1300 MWe PWR

- In both cases, the height of the grid is approximately 35 mm, which is a relatively low value (i.e. conservative one).

2) The flow temperature was fixed to 300°C. The flow velocity was about 5 m/s for 900 MWe conditions and 5.5 m/s for 1300 MWe conditions.

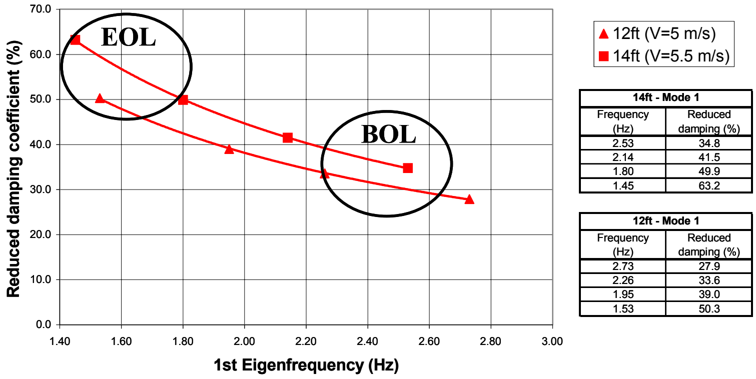
3) Fuel assemblies first eigenfrequency was calculated from beginning of live (BOL) conditions - low amplitude of motion (parameter q=1) to end of live (EOL) conditions - high amplitude of motion (parameter q=0).

4) The lateral confinement is set to 215 mm, which is the nominal value for in-core conditions.

The following parameters were used for the in-core flow-induced damping prediction :

$C_{D_b} = 0.005$	Best estimate value – Well known - No major impact on the flow induced damping
$C'_{L_b} = 0.08$	Best estimate value – Low uncertainties - Significant impact on the flow induced damping
$C_{D_g} = 7$	Best estimate value – Well known – Low variability - No major impact on the damping
$C'_{L_g} \approx \frac{2.\pi}{6}$	Conservative value – High uncertainties - Significant impact on the flow induced damping

The results are shown in Figure 6.



14ft - Mode 1	
Frequency (Hz)	Reduced damping (%)
2.53	34.8
2.14	41.5
1.80	49.9
1.45	63.2

12ft - Mode 1	
Frequency (Hz)	Reduced damping (%)
2.73	27.9
2.26	33.6
1.95	39.0
1.53	50.3

Figure 6: In core flow induced damping of a PWR fuel assembly – BOL to EOL conditions

5.1 Interpretation of the results

The main comments on these results are the following:

Lift and drag effects - Evolution of damping with frequency

It has been shown that the effect of lift is predominant and the effect of drag could be neglected. As a consequence, the reduced damping coefficient is inversely proportional to the frequency. This result is consistent with other studies on this topic.

Evolution of damping from BOL to EOL condition

Due to relaxation effects, the first eigenfrequency of the fuel assembly decreases from BOL to EOL conditions. The reduced damping coefficient will therefore increase from BOL to EOL.

This behaviour implies that conventional reduced damping coefficients, which are usually used for design analyses with assembly BOL conditions, must be adjusted if the frequency of the assembly decreases significantly along its life.

Differences between 12ft and 14ft flow induced damping

We can observe that the damping is significantly different between a 12ft and a 14ft design (12ft damping is about 15 % lower than 14ft damping). This difference is due to two main reasons:

- First, the flow velocity is 10% higher in case of the 14ft design (5.5 m/s instead of 5 m/s)
- Secondly, the spacing of the grids is also ~10% less important for 14ft design (~440 mm span height instead of ~500 mm)

The first effect leads to a 10% increase on damping. The second leads to a ~5% increase on damping (grid lift is only half of the flow induced damping).

5.2 Recommendations for design analyses and margin assessments

We finally recommend to use a reduced damping coefficient of 30% of critical (for FA mode n°1) for design analyses, independent on the FA design, which gives significant margin compared to the realistic total damping of the fuel assembly (structural damping and flow induced damping).

For margin assessments, we recommend to use the values presented in Figure 6. These values remain conservative because the parameters of the model used for the prediction are conservative and structural damping of the fuel assembly is neglected.

6 CONCLUSION

EDF has developed a model that is able to predict the PWR fuel assembly flow induced damping. This model has been validated on the basis of reduced scale semi-analytical test results and full-scale test results.

This model has been used to predict fuel assembly flow induced damping for different fuel assembly designs from beginning of live to end of live conditions.

Based on these conservative results, a reduced damping coefficient of 30% of critical (for FA mode n°1) is recommended for conventional design accident studies (BOL fuel assembly characteristics). For margin assessments, more realistic values are proposed.

This allows us to evaluate precisely the margins in the seismic design methodology of the fuel assembly.

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