Safety Evaluation Report

Quasi-laminar flaw indications in the Doel 3 and Tihange 2 reactor pressure vessels

EVALUATION OF THE IMPACT OF THE HYDROGEN FLAKING DAMAGE ON THE SERVICEABILITY OF THE DOEL 3 AND TIHANGE 2 REACTOR PRESSURE VESSELS

Executive Summary

The implementation of the mid-term action plan launched by Electrabel in response to the requirements set up by the Belgian Federal Nuclear Agency for Nuclear Control (FANC) for authorizing the restart of the Doel 3 and Tihange 2 nuclear power plants in May 2013 resulted in significant outputs and developments that made invalid the assessment for continued operation provided in the 2012 Safety Case. A re-assessment by Electrabel of the condition of the Doel 3 and Tihange 2 reactor pressure vessels (RPV) was therefore deemed necessary. That re-assessment is documented in the 2015 Safety Case.

Bel V has reviewed the justification file supporting the 2015 Safety Case. The objective of the review was to evaluate whether and to which extent the justification file provided by Electrabel was sufficiently comprehensive and technically sound and included the sufficient conservatism to demonstrate with the required high degree of confidence that the hydrogen flaking, considered by Bel V as a major deviation from the requirement of having a RPV material of the highest quality, did not affect unacceptably the serviceability of the Doel 3 and Tihange 2 RPVs.

Bel V concludes that hydrogen flaking damage has been demonstrated satisfactorily to have an acceptable impact on the serviceability of the Doel 3 and Tihange 2 RPVs during normal, abnormal and accident service conditions.

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1. Introduction

In May 2013, the Belgian Federal Agency for Nuclear Control (FANC) authorized the restart of the Doel 3 and Tihange 2 nuclear power plants which had been shut down in the summer of 2012 following the detection of large numbers of quasi-laminar indications in the core shells of the reactor pressure vessels (RPV). Those indications were identified by the Licensee (Electrabel) as hydrogen flakes that developed during the manufacturing process. The authorization to restart was accompanied by some requirements that Electrabel had to meet before the next refuelling outage. Those requirements had the main objective of confirming the adequacy of the UT inspection procedure used in 2012 to detect the flakes and the appropriateness of the assumptions made by Electrabel in the 2012 Safety Case to estimate the behaviour of the flaked material under irradiation conditions. Those requirements were translated into actions of a mid-term plan launched by Electrabel.

The implementation of the mid-term plan resulted in significant outputs and developments which made invalid the assessment for continued operation provided in the 2012 Safety Case. Both Doel 3 and Tihange 2 plants were shut down in March 2014. A significant output was the unexpected enhanced irradiation embrittlement evidenced on irradiated specimens taken from the representative VB 395 flaked material that was much higher than the value assumed in the 2012 Safety Case. A significant development was the necessary updating of the UT examination procedure mainly by revising the sizing procedure of the flaw indications in order to avoid potential undersizing for some of them and by lowering the reporting thresholds for ensuring detection of flakes with tilt angle up to 16°.

In July 2015 Electrabel provided two assessment reports, one for each unit, summarizing the justification file documenting the assessment of the structural integrity of the Doel 3 and Tihange 2 RPVs. A revision of those reports was issued in the fall of 2015. As such, the Electrabel assessments reports include the reactor pressure vessel assessments reports (one per unit) issued by Electrabel and the reports on independent analysis and advice regarding the safety case 2015 (one per unit) issued by the *Service de Contrôle Physique* (SCP) which, as an entity independent from the Electrabel operational / functional line, exercises an INSO (Independent Nuclear Safety Oversight) function.

The Electrabel assessment reports, and consequently the present safety evaluation report, are not limited to the assessment of the outputs of the mid-term program. They are a re-assessment of the condition of the Doel 3 and Tihange 2 RPVs, taking into account the outputs and developments of the actions of the mid-term plan.

The present report documents the safety evaluation by Bel V of the condition of the Doel 3 and Tihange 2 RPVs. It is not strictly an evaluation following step after step the Electrabel assessment reports but it is rather a safety evaluation of the condition of the RPVs on the basis of the information provided in the Electrabel assessment reports and their supporting analysis reports and supplemented by the additional information provided by Electrabel following the numerous discussions or exchanges with Bel V.

In order to make the report shorter, most of the information provided in the Electrabel assessment reports has not been reproduced. The reader is therefore assumed to be fully aware of those reports.

2. Basis of the safety evaluation by Bel V

The updated condition of the Doel 3 and Tihange 2 RPV core shells as revealed by the outputs of the 2014 UT inspection using the qualified (updated) procedure, together with the predicted irradiation embrittlement of the RPV material (for use in the structural integrity assessment) higher than initially assumed in 2012, result in a situation which, to Bel V opinion, may not be described as a slight deviation from the situation assessed by Electrabel in the 2012 Safety Case reports (and the Addenda thereto of April 2013). Accordingly, although he recognized that the re-assessment by Electrabel of the Doel 3 and Tihange 2 RPVs is built upon and has been influenced by the 2012 assessment, Bel V considers the re-assessment as a stand-alone assessment. However, when evaluating the technical file documenting the re-assessment, Bel V relied on the same principles as those he used for the evaluation of the 2012 assessment. Those principles are briefly summarized below.

The defense-in-depth philosophy has traditionally been applied in reactor design and operation. In a defense-in-depth approach, the greatest emphasis should be placed on the first level of defense that requires a superior quality in design, construction and operation. The second level of defense is also of prime importance by requiring, amongst others, that in-service measures are taken to ensure that no alterations to materials appear compromising the prevention of the failure modes. As far as the RPV is concerned, the importance of the first two levels of defense is still enhanced by the absence of any measures under higher levels of defense which could ensure accident mitigation in the case where RPV failure would occur. Otherwise stated, in the safety demonstration of the nuclear power plant, the failure of the RPV is not assumed. The application of the assumption of break exclusion to the RPV requires therefore to ensure the very low probability of RPV failure by strengthening the first two levels. In particular, the highest quality is required for the fabrication of the RPV, i.e., the best that the industry can offer.

Even if there is no specific provision in Section III of the ASME B&PV Code regarding hydrogen flaking in RPV forging and even if the condition of the Doel 3 and Tihange 2 RPV forgings might be interpreted as acceptable per the UT rejection criteria of Section III of the ASME B&PV Code under which the RPVs were built, material specification SA 508 for Class 1 vessels requires the molten steel be vacuum treated in order to remove objectionable gases, particularly hydrogen and also the forging to be free of cracks. In any case, to Bel V opinion, the presence of hydrogen flakes, which are crack-like defects, does not comply with the highest quality level expected for a material to be used in a component the failure of which is assumed to be excluded.

Prevention of brittle or ductile failure is ensured at the first defense level by the application of the most stringent manufacturing criteria including: (i) absence of crack-like defects at the end of the manufacturing process, as confirmed by examination during manufacture and (ii) sufficient material toughness to ensure good resistance to propagation of crack-like defects. The presence of hydrogen flaking is therefore to be considered as a major deviation from the requirement of having RPV material meeting the highest quality standards and being in particular as defect-free as possible within the limitations of the best available manufacturing technology. More precisely, it should be emphasized that the issue does not concern the confidence in the quality of the material, but the quality of the material itself.

To Bel V opinion, the presence of hydrogen flaking affects the first level of defense. There is also no way to restore the required highest quality level of the fabrication that constitutes the first level of defense.

The approach used by Electrabel to justify the safe operation of the RPVs, referred to as the structural integrity assessment, consists in assessing analytically the structural strength of the Doel 3 and Tihange 2 RPVs during normal, abnormal and accident service conditions. To Bel V opinion, that approach aims at demonstrating the *serviceability* (fitness-for-service) of the Doel 3 and Tihange 2 RPVs affected by hydrogen flaking but is in no way a substitute to the required highest quality of the fabrication. The issue is therefore to evaluate whether and to which extent the fabrication problem affects the serviceability of the Doel 3 and Tihange 2 RPVs. So, to Bel V opinion, the objective of the assessment of the condition of the RPVs should go further than flaw acceptance analysis of each individual flaw. It requires to demonstrate that the presence of thousands of quasi-laminar crack-like flaws has an *acceptable impact* on the serviceability of the RPVs. With regard to that, the assessment of the Doel 3 and Tihange 2 RPVs shall not be limited to the verification that all the Code and regulatory requirements are strictly met but it should also make use of the best available engineering methods and criteria to evaluate the condition of the RPVs subject to the major deviation created by the flaking damage.

The demonstration provided by Electrabel uses an analytical approach, which to Bel V opinion is the only possible approach, in particular for the prevention of failure from the flakes. Taking into account the high significance of the assessment, a very high confidence in the supporting analyses is necessary. To this end, since deterministic analyses are performed, a conservative approach is to be used to handle in a deterministic way the uncertainties associated to the issue. Classically, in a deterministic approach which makes use of models as well as assumptions and data, conservatism in the approach means that appropriate extreme values of the data, appropriate assumptions and appropriate models and calculation procedures are defined in such a way that the analysis leads to a result that estimates the analysed parameter with a high confidence level (high percentile of the distribution). Of course the concept of sufficient or adequate conservatism is not quantified in this approach. The validity of the approach is justified from experience: if the approach resulted in satisfactory results in the past, it is justified in current practice. As the assessment of the extensive flaking damage of the Doel 3 and Tihange 2 RPVs is a first-of-a-kind issue, there are neither codified procedures to handle it nor standard procedures justified by past experience. Although not specifically defined, enhanced conservatism is therefore required because, for instance, the models and methods could be too simplified or some phenomena affecting the final result could have been neglected. Furthermore, as the demonstration requires several steps, including the definition of models, assumptions and data, conservatism is required to be present at each step of the demonstration. More specifically, an insufficient level of conservatism in one step may not be compensated by an excessive level of conservatism in another step.

When discussing the conservatism associated to analyses, the following notions as used in the present report have also to be clarified.
Safety coefficient (safety factor). In an assessment using an analytical approach, an analysis parameter (e.g., the maximum applied stress intensity factor) is compared to a characteristic value (the fracture toughness of the material). In the acceptance criteria defined in the Codes / regulatory requirements or adopted in specific procedures, the characteristic value is multiplied by a *safety coefficient* (in Section XI of the ASME B&PV code, the material fracture toughness is multiplied by a safety coefficient of $1/10^{1/2}$ for normal conditions; the *safety factor* is $10^{1/2}$). Safety coefficients are traditionally used in construction codes

as a measure of the confidence in the representation of the real physical system by an idealized analytical model. Note that the use of appropriate safety coefficients should not serve as a substitute for the necessity of adopting a conservative approach in the deterministic analysis.
Safety provisions (safety reserve). In addition to the provisions adopted in the analysis to cope with the identified uncertainties, additional provisions, i.e., the *safety provisions*, may be included in the analysis, for instance to cover the foreseen but not quantified variations of the input data or the incompleteness in the knowledge of the value of some input data and the variability thereof.
Safety margin. *Safety margin* is the ratio between the threshold value of the acceptance criterion (*characteristic value* multiplied by the *safety coefficient*) and the value of the analysis parameter. Generally, a safety margin is not a target to be achieved but is obtained once the safety demonstration is finished.

3. Significant changes from the 2012 Safety Case

When compared to the condition of the Doel 3 and Tihange 2 RPVs as known at their restart in 2013 and to the assessment thereof by Electrabel in the 2012 Safety Case reports (and the Addenda thereto of April 2013), the condition of the RPVs as known following the UT inspection in 2014 with the qualified procedure and the assessment thereof in the 2015 Safety Case reports show some significant differences, most of them being due to the outputs of Actions 7 (qualification of the UT inspection procedure) and 11 (irradiation embrittlement of flaked material) of the mid-term action plan and their developments. The main differences and the consequences thereof are identified and clarified below.

3.1 Updated condition of the Doel 3 and Tihange 2 RPV core shells following the qualification of the UT inspection procedure

In comparison to the “historical” UT inspection procedure used in 2012, the qualified procedure for the UT inspection of the RPV core shells is characterized by the following changes:
(i) improved sizing procedure using 6dB amplitude drop sizing technique based on echodynamics to avoid potential undersizing of some flaw indications,
(ii) lowering of the reporting levels of the UT signals to ensure the detection of the flakes having a tilt angle up to 16°, and
(iii) use of the OL 0° MER transducer instead of the OL 0° EAR transducer to improve the detectability of the flakes in the depth range closest to the cladding. In addition, the depth range covered by the transducers has also been modified.

The results of the 2014 UT inspection of the Doel 3 and Tihange 2 RPV core shells using the qualified procedure showed a significantly larger number of detected flaw indications than the one obtained in 2012 using the historical procedure (close to 70% more flaw indications for the Doel 3 RPV and about 60% for Tihange 2 RPV). In particular, for the first 10 mm thick layer beneath the cladding of the Doel 3 RPV lower core shell, using the qualified inspection procedure leads to identify a number of “newly reported” flaw indications nearly equal to the number of “originally reported” flaw indications.

Although the larger number of flaw indications attests to the fact that the flaking damage of the RPVs is more severe than estimated in 2012, there are other parameters that characterize the severity (i.e., structural significance) of the damage. It is indeed known that the potential to grow for a quasi-laminar flaw and fail the vessel as quantified by the stress intensity factor depends on the size, tilt angle and interaction with the neighbouring flaws. The following provides some information about the comparison between the 2012 and the 2014 UT inspection results for the RPV core shells.

The average sizes of the indications have been increased by a factor of about 1.5 while the standard deviations are increased by a factor of about 2.0. As an illustration, considering the size of a flaw indication as the larger of its two dimensions (in the axial (X) and azimuthal (Y) directions), the average flaw sizes for the 2012 and 2014 inspections of the lower core shell of the Doel 3 RPV are 10.1mm and 17.0mm respectively while the standard deviations are 5.0mm and 10.8mm.

No exhaustive information has been made available allowing to compare the statistical parameters characterizing the distribution of the tilt angle of the flaw indications. For the Doel 3 lower core shell, the tilt angle distribution curves for the 2012 and 2014 inspection have been made available and they do not evidence any significant change of the average value and standard deviation.

As the spatial distribution of the flaw indications within the RPV shells appears to remain essentially unchanged between the two inspections, the higher number of flaw indications detected in 2014 makes that the density of the indications in the most affected zones of the RPV is necessarily increased. At Bel V request, some quantitative figures have been determined for the Doel 3 lower core shell. The average density of flaws in the affected zones, expressed as the number of indications per liter, was found to slightly increase from 5.0 to 6.4 while the maximum value increased from 25.8 to 32.4. However, those figures are not indicative of the local density of flaw indications as the flaw indication density is obtained by subdividing the zones affected by flaking in nearly cube-shaped cells having a volume of about one liter and by determining the number of indications in the cells. A figure that would be more indicative of the local density of flaw indication is the distribution of the size of the shorter ligament of sound material between each flaw indication and its neighbouring indications. A conservative estimate of that figure can be obtained by calculating the shortest distance separating the 'boxes' containing the indications, as determined by the UT inspection results. At Bel V request, Electrabel provided the distribution of the ligaments for the shallow indications of the Doel 3 lower core shell (i.e., in the first 10 mm thick layer beneath the cladding). It is seen that the peak of the distribution curve which was between 10 and 15mm in 2012 moved to the 5 – 10 mm range in 2014.

While recognizing the value of the figures given above, Bel V also considered that they were not able to provide an overall picture of the increased severity of the damage. Such a picture could be obtained by providing the distribution curves and the cumulative distribution curves of the $2a/2a_{acc}$ ratios (see Section 9) for Doel 3/Tihange 2 core shells calculated from the results of the 2012 and 2014 inspections and using the same assessment methodology. Recognizing that the objective of the request was only to provide a qualitative picture of the updated condition, it was agreed with Electrabel that the comparison to be made using the assumptions of the 2012 assessment would be carried out considering all the flaws as individual penny-shaped (i.e., circular) flaws and without taking into account the interaction effects. It was indeed shown by Electrabel when performing the 3-D multi-flaw analyses of flaw groups that the $2a/2a_{acc}$ ratios of the flaws making part of a group and modelled as ellipses (contained in the 'boxes') were lower than the $2a/2a_{acc}$ ratios of the same flaws modelled as circles (enveloping the 'boxes') and calculated as individual flaws (i.e., with no consideration of the interaction effects). The results of the comparison show that the impact of the updated condition of the RPV is greater for Doel 3 than for Tihange 2. Hereafter are some results of the comparison made for the Doel 3 RPV. Out of the 13047 flaw indications detected in 2014 (8062 in 2012), about 80% have a $2a/2a_{acc}$ ratio comprised between 0.0 and 0.1 (about 90% in 2012) while about 17% have a $2a/2a_{acc}$ ratio comprised between 0.1 and 0.2 (about 7 % in 2012). A number of 17 flaws detected in 2014 (4 in 2012) have a $2a/2a_{acc}$ ratio comprised between 0.5 and 1.0 while 2 flaws detected in 2014 (0 in 2012) have a $2a/2a_{acc}$ ratio exceeding 1.0.

From the above, Bel V concludes that the updated condition of the Doel 3 and Tihange 2 RPV core shells as revealed by the examination performed in 2014 using the qualified UT inspection procedure is to be considered as having a substantially increased structural significance when compared to the condition determined in 2012.

It should be noted that the description of the updated condition of the Doel 3 and Tihange 2 RPVs as provided hereabove does not account for the indications classified as *clad interface imperfections* (in French: *défauts technologiques de revêtement* or *DTR*). The clad interface imperfections which are not crack-like flaws do not present a concern for RPV integrity. Their number which is only significant for the Doel 3 RPV (about 300) was found to be nearly identical in the 2012 and 2014 UT inspections.

3.2 Irradiation embrittlement of VB-395 shell material exceeding significantly expectations

In the reactor vessel material surveillance program of the Doel 3 and Tihange 2 RPVs, use is made of the French FIS formula to predict the irradiation embrittlement (upper bound) of the RPV core shell material. Indeed, in addition to the fact that the material of the RPV core shells is similar to the material used in France in the 900 MWe reactors, it was also found that the RT_{NDT} shift data as obtained from the surveillance specimens was best fitted by the FIS formula.

In the 2012 Safety Case reports, Electrabel considered that the potential effect of the flakes on the material fracture toughness would be adequately taken into account by an additional shift in RT_{NDT}. A value of 50°C (in addition of the shift calculated by the French predictive equation for the nominal content of the RPV forgings in embrittling elements) was selected by Electrabel. That additional shift comprised of :
(i) a term of 11°C that accounts for the possible lower crack initiation fracture toughness (under unirradiated condition) of the material in the macro-segregated areas of the forgings where the hydrogen flakes have been detected when compared to the unsegregated areas,
(ii) a term of 14°C that accounts for the possible lower crack initiation fracture toughness for the flakes under unirradiated condition when compared to the crack initiation fracture toughness of the material in the ligaments between the flakes,
(iii) a term that accounts for the possible higher sensitivity to irradiation embrittlement of the macro-segregated areas of the forgings due to their content in embrittling elements higher than the nominal values ; for the core shell having the highest nominal content in embrittling elements, the term is calculated to be 17°C for the highest fluence (assuming the design life of 40 years), and
(iv) a term of 8°C minimum that should be considered as a safety provision.

The values of the first two terms were determined from fracture toughness tests using unirradiated 1/2 CT specimens taken from the VB 395 shell material.

The conservatism of the additional RT_{NDT} shift was to be confirmed by performing fracture toughness tests on specimens taken from the VB 395 shell and irradiated in the Belgian BR2 test reactor (Action 11 of the mid-term action plan).

In that test campaign (Chivas 9), fracture toughness test specimens taken from the flaked region of the VB 395 shell material were irradiated under high flux to a level of fluence corresponding to more than 40 years of operation. Irradiation was performed in the Callisto loop that simulates PWR conditions. The results showed an increase of the Master Curve transition temperature *To* due to irradiation embrittlement significantly higher than the predicted value, thereby invalidating the additional shift in RT_{NDT} of 50°C assumed in the 2012 Safety Case reports.

3.3 Use of KS02 material

In the late seventies and the early eighties, in the frame of an extensive research program (Forschungsvorhaben Komponentensicherheit - FKS) initiated in Germany, investigations were performed related to manufacturing defects in heavy components as well as to special heats of materials used in those components. The KS02 head flange material, made of 22NiMoCr3-7 (similar to SA 508 Cl 2), was a part of the research program. That material, a forged half ring made from a solid ingot without piercing, had been rejected following the detection of numerous indications in the central part of the forging by UT examination. In 2012, Electrabel stated that those indications had been identified as solidification voids stretched during forging and had a crack-like appearance. Within the FKS program, the KS02 material was used to investigate the differences in fracture toughness between the segregated and the non-segregated materials as well in unirradiated and in irradiated conditions. No significant influence of the segregations on the fracture toughness in unirradiated conditions and on the transition temperature shift was found.

In 2014 additional archive research and examination of spare material allowed Electrabel to conclude that the indications detected in the KS02 material were actually hydrogen flakes. Considering that both the KS02 material and the VB395 material are representative of quenched and tempered low alloy steel forging for RPV, Electrabel concluded that the KS02 material was therefore the second RPV material affected by hydrogen flaking (in addition to the VB395 material) for which fracture toughness data were available as well in unirradiated as in irradiated conditions.

The main information drawn from the research program made on the KS02 material is that, contrary to what was identified on the VB395 material, a RPV material affected by hydrogen flaking can exhibit no enhanced irradiation embrittlement.

3.4 Improved understanding of the mechanical behaviour of flaked material

In the 2012 Safety Case, the issue of the possible impact of the hydrogen flaking on the material properties was raised and investigated. To Bel V opinion, this issue was of prime importance. Indeed if this impact was confirmed, this would mean that the hydrogen flaking would have a double detrimental effect on the structural behaviour of the RPVs: (i) on one side, the flakes are crack-like flaws which could potentially grow and lead to RPV failure, particularly when the vessel is subjected to large thermal transients and (ii) on the other side, the deterioration of the mechanical properties of the flaked material could compromise the required ductile behaviour of the material but also decrease the crack initiation fracture toughness for the flakes well below the fracture toughness of the material in the non-affected regions. In 2013, tensile tests performed on specimens taken from the VB 395 shell in the ligaments between the flakes showed that the ductility of the material was not affected. The good ductile behaviour of the flaked material was confirmed by the structural tests performed on large-scale dia 25mm specimens (Action 15 of the mid-term action plan). However the potential impact of the flaking damage on the fracture toughness remained a matter of controversy.

Although the phenomena governing the occurrence of hydrogen flaking are multiple, not fully understood, and possibly interacting, hydrogen flaking in heavy forgings appears to occur when the content in hydrogen exceeds 0.8 ppm. The flakes are known to appear at temperatures lower than 200°C during (or even after) the cooling of the forging to room temperature following the final quality heat treatment (austenization and quenching followed by tempering). The flakes are therefore formed in a material having its final microstructure and mechanical properties. Otherwise stated, the presence of flakes is not expected to affect mechanical properties of the material and the fracture toughness.

Hydrogen flakes appear in the zones that are the most prone to hydrogen accumulation, i.e., the zones which are the last ones to transform from the austenitic phase to the ferrite phase, namely the most enriched ghost lines in the most macro-segregated zones. Due to their higher content in alloying elements (e.g., C, Mn, P, Mo) that are quenching elements, the ghost lines are more sensitive to quenching, which promotes the formation of a martensitic structure .This is confirmed by the examinations performed by Electrabel on VB395 shell material which have shown that the flakes are located in ghost lines having mostly a tempered martensitic structure. The ghost lines are therefore expected to be more brittle than the adjacent macro-segregated material.

Pre-cracked Charpy specimens taken from the Doel 3 H1 cut-out were fracture tested to assess whether there was an impact of the ghost lines on the material toughness. Two types of specimens were tested, specimens taken in the segregated zone out of the ghost lines and specimens with the notch located in a ghost line and perpendicular to it. The results showed that the difference in the Master Curve transition temperature *To* was less than 5°C, which allowed Electrabel to conclude that the tests performed on the ghost lines in the Doel 3 H1 cut-out do not show any significant impact of the ghost lines on the fracture toughness. Nevertheless this result was surprisingly not consistent with results obtained in a French research program, which showed a difference in the Charpy V transition temperature of several tens of degrees between the material in the ghost lines and outside the ghost lines.

However, the concern of the possibly lower fracture toughness of the ghost lines when compared to the fracture toughness of the macro-segregated material in the close vicinity of the ghost lines would not be relevant to the problem discussed here if it could be shown that fracture initiation at a flake does not occur in the ghost line but in the neighbouring macro-segregated material. Otherwise stated, the crack initiation fracture toughness for a flake depends on the location of the tips of the flakes and it would be governed by the fracture toughness of the adjacent material if it could be shown that the tips of the flakes are located in the material adjacent to the ghost line where the flake is located.

First, there are theoretical arguments based on the assumption that the flaking process is governed by the lower fracture toughness of the material in the ghost lines to support the statement that the flake extension encompasses at least the full length of the ghost line. Then, the investigations performed by Electrabel on the VB 395 shell material have shown that the flakes are located in the ghost lines and their extension is generally limited to the ghost lines but it also happens that the flakes extend slightly beyond the ghost lines. Moreover, other investigation also performed by Electrabel on the VB 395 shell material have shown that the tilt angle of the flakes is systematically smaller than the maximum tilt angle of the hosting ghost line, which suggests that other factors than the fracture toughness, e.g., the residual and transformation stresses, could govern the flaking process. In any case, both arguments lead to consider very likely the assumption that the tips of the flakes are not located in the hosting ghost line but well in the adjacent (macro-segregated) material.

Experimental verification of that assumption has been made possible by performing fracture toughness tests on two types of non-irradiated 1/2 CT specimens taken from the VB 395 shell: conventional pre-cracked specimens taken from the ligaments between the flakes and specimens with a flake as a surrogate for the fatigue pre-crack. The Master Curve transition temperature *To* of the material between the flakes as determined from the standard CT specimens was found to be lower by about 14°C than the *To* temperature as determined from the CT specimens with a flake. This result was used in the original Safety Case to quantify the potential lower crack initiation fracture toughness for the flakes under unirradiated condition (see 3.2 above). However, due to the heterogeneity of the material between flakes and also due to the non-regular crack front in the CT specimens with a flake, the difference in temperature *To* should be evaluated cautiously. So it is reasonable to conclude from those results that, under unirradiated conditions, the crack initiation fracture toughness for a flake does not differ significantly from the fracture toughness of the surrounding (macro-segregated) material.

That conclusion was also confirmed for irradiated conditions. In the Chivas 9 test campaign, fracture tests were performed on irradiated pre-cracked Charpy V fracture toughness test specimens taken from the VB 395 shell material between the flakes as well as Charpy specimens with a flake as a surrogate for the fatigue pre-crack. In addition to having evidenced an irradiation embrittlement significantly higher than predicted, the tests have also shown that there was no significant difference between the Master Curve transition temperatures *To* of both types of test specimens.

From the developments summarized above, some of which being initiated by Electrabel, Bel V concludes that the hydrogen flaking as a damage has no effect on the fracture toughness of the material and the fracture resistance of the flakes is governed by the fracture toughness of the macro-segregated material where the flakes are located. The flakes may therefore be evaluated as any other crack in a sound material, the latter being in the present case the macro-segregated material where the flakes are located. This conclusion assumes that the stability of the flakes under single loading and their growth under repeated loadings may be assessed using the same methods as those currently used for mechanically-induced cracks (e.g., fatigue cracks).

3.5 Revised proximity rules

Proximity rules suited to quasi-laminar flaws and the associated flaw grouping method are used by Electrabel to determine the flaws that need to be grouped for the flaw acceptability assessment. Proximity rules had been defined by Electrabel for the 2012 Safety Case. Those rules were based on the assessment of pairs of planar cracks in a sector of a RPV subject to uniaxial loading (2D flaw calculations). Electrabel developed new proximity rules for the 2015 Safety Case. The new rules are based on the assessment of pairs of planar flaws in a finite volume of a RPV under biaxial loading (3D flaw calculations).

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The objective pursued by Electrabel when defining new proximity rules was to eliminate excessive conservatism in the flaw acceptability assessment. Since the updated condition of the RPV core shells following the 2014 examination using the qualified UT inspection procedure is characterized amongst others by the increased density of indications, using the 2012 proximity rules would have led to a larger number of flaw groups, some of the initial groups having possibly more flaws. As a result, in a flaw acceptability assessment in conformity with the 2012 methodology, there would have been more groups with a $2a/2a_{acc}$ ratio comprised between 0.5 and 1.0, which would lead to a larger number of groups to be assessed using 3-D calculations. Some groups would also likely have had a $2a/2a_{acc}$ ratio exceeding 1.0. Such a situation would not have been found acceptable using strictly the flaw acceptability assessment procedure of 2012, independently of the application of the screening approach initiated by Bel V.

Modifying the flaw assessment methodology by decreasing the conservatism of the proximity rules could be considered as acceptable under the condition that no account is made of the screening criterion approach. If the screening criterion approach is considered, decreasing the conservatism of the proximity rules would in principle not be permitted without further evaluation. It is reminded that the application of the screening criterion required, as a necessary condition, the availability of a “robust” procedure for performing the flaw acceptability assessment. In 2012, the procedure developed by Electrabel in 2012 was found by Bel V sufficiently “robust” for its intended use in the screening criterion approach.

Bel V emphasizes that the issue is not the definition of revised proximity rules based on 3D calculations but the use of those rules in the screening process. Bel V recognizes that 3-D analyses are a state-of-the-art calculation method for analysing groups of indications. However, as reminded here above, the application of the screening process defined in 2012 requires the availability of a robust methodology for the flaw acceptability assessment in order to fulfil its objective of discriminating the indications having a non-significant impact on the structural strength of the RPV from the other ones.

To Bel V opinion, applying the revised proximity rules requires therefore to revisit the screening approach, while maintaining its objective (See section 5).

3.6 Extensive use of X-FEM method for assessing flaws and groups of flaws

In the years 2012 and 2013, a limited number of flaws and groups of flaws were assessed by detailed 3D calculations using the X-FEM method implemented in the MORFEO CRACK finite element computer code. Those calculations were performed by a subcontractor of Electrabel. Starting from 2014, the detailed 3D calculations were performed by Electrabel who had decided in the meantime to buy the license for the MORFEO CRACK computer code. Although it could appear not significant, that decision had an important impact as it permitted Electrabel to perform detailed 3D calculations on a routine basis, which allowed to increase significantly the number of detailed 3D calculations.

4. Qualification of the UT inspection procedure and updated flaw distribution

In 2012 and 2013, the UT inspection of the Doel 3 and Tihange 2 RPV core shells to detect quasi-laminar flaw indications was performed using a UT inspection procedure not qualified for those indications. However, the capability of the straight beam technique to detect and size the hydrogen flakes had been shown through a validation process. Under Action 7 of the mid-term action plan, the UT inspection had to be formally qualified. The qualification was performed by AREVA (Intercontrôle) in conformity with the ENIQ (European Network for Inspection Qualification) methodology. Some modifications to the original (2012) inspection procedure were found necessary (See section 3).

4.1 Role of Bel V in the qualification of the UT inspection procedure

The usual practice for Bel V is to rely on the AIB-Vinçotte expertise for the pure technical issues related to the inservice inspection. However, considering that the best available methods are required for the assessment of the condition of the RPVs, Bel V recognized the prime importance of the qualification of the UT inspection procedure for the hydrogen flakes. Bel V decided therefore to become more involved than usually in the qualification process. By doing so, Bel V pursued the objective of getting a better understanding of the justifications provided by AREVA and Electrabel to support the qualification, of being more aware of the concerns raised by AIB-Vinçotte related to the qualification and the resolution thereof by AREVA and Electrabel, and ultimately of increasing his confidence in the satisfactory performance of the inspection procedure. These monitoring activities were complemented by an in-depth review of the results of the inspection of the UT inspection of Doel 3 and Tihange 2 RPV core shells performed in 2014 using the qualified procedure.

4.2 Assessment of the increased number of reported flaw indications

Compared to the 2012 inspection, the results of the 2014 UT inspection showed a significantly increased number of detected flaw indications, much higher than what was expected by Bel V (see section 3). In particular, for the shallow indications in the Doel 3 lower core shell (i.e., in the first 10 mm thick layer beneath the cladding), the highest increase in number of indications has been recorded (about 100%). In the 2012 inspection procedure, this layer was examined using the OL 0°EAR transducer with a reporting threshold set to -18dB. In the qualified inspection procedure, this layer is examined using the OL 0° MER transducer with a reporting threshold set to -24dB. In a first analysis, based on the response of the EAR and MER transducers to 6mm dia flakes in the first 10 mm thick layer beneath the cladding, it may be reasonably assumed that, in this layer, the detection capability of the MER transducer is similar to the detection capability of the EAR transducer. Furthermore, according to the qualification file, when inspecting block VB395/2A with the OL 0° EAR transducer, lowering by -6dB the reporting level (initially set at -18 dB) increases the number of indications by one indication only (9 indications with a reporting level of -18 dB and 10 indications with a reporting level of -24 dB). Based on that result, it was expected by Bel V that the number of newly reported shallow indications in the Doel 3 lower core shell would be low.

Bel V asked therefore Electrabel to provide the necessary additional information allowing to better characterize the population of the newly reported flaw indications and to better identify the effects of the modifications brought by the qualified UT inspection procedure. As the numbering of the flaw indications detected in 2014 is independent from the numbering used in 2012, the identification of the newly reported flaw indications cannot be performed without major effort. Moreover the three modifications brought to the inspection procedure, i.e., transducers, depth ranges and reporting levels, interact with each other, which makes difficult the assessment of the effects of each modification. Bel V therefore asked Electrabel to focus the analysis on the shallow indications in the Doel 3 lower core shell.

Electrabel reported the following information regarding the shallow indications in the Doel 3 lower core shell. The newly reported shallow indications are scattered over the shell without any evidence of

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formation of clusters. Their average dimensions in the axial and azimuthal direction are about 50% less than the indications reported in 2012 and their maximum dimensions are significantly smaller than those reported in 2012. Otherwise stated, the newly reported shallow indications are on the average smaller than those identified in 2012 with most of them in the range of 6 to 15mm and there is no newly reported indication having a dimension larger than 24mm. In two typical sectors of the RPV considered by Electrabel as representative of the whole layer, 104 out of the newly reported indications were found to have an amplitude larger or equal to -18dB (reporting threshold of the EAR transducer in the 2012 inspection procedure), which allows to conclude that their detection was due to the replacement of the EAR transducer by the MER transducer. That conclusion was confirmed by comparing the amplitude distributions for the indications detected at the -18dB reporting level by the EAR (2012 inspection data) and MER (2014 inspection data) transducers. The comparison was made for the indications in the layer covering the first 15mm in depth. The reporting level of -18dB was considered for both transducers to allow direct comparison between the EAR and MER transducers, so excluding the effect of the lowered reporting level (-24dB) of the MER transducer in the qualified procedure. That comparison showed that, at the identical reporting threshold of -18dB, the MER transducer detects much more indications than the EAR transducer, which is attributed by Electrabel to the higher sensitivity of the MER transducer. In particular, the sensitivity of the MER transducer at very low amplitudes is much higher, which can be attributed, according to Electrabel, to the much better capability of the MER transducer to detect very small flakes, which are out of the limits of the qualification domain since the qualification ensures a detection of flakes with a minimum size of 6mm and inclined at 16° maximum with a specified high confidence level.

From the above information, Bel V concludes that the better sensitivity of the MER transducer is responsible for the detection of a significant part of the newly reported shallow indications, most of them having an amplitude larger or equal to -18dB. It is likely that those indications have a size close to the beam dimension, even if the sizing procedure leads to overestimate their actual size.

4.3 Analysis of large flaw indications

Another important feature of the 2014 UT inspection is the identification of some flaw indications having a size significantly larger than those identified in 2012. In the Doel 3 lower core shell, the maximum axial dimension for a flaw indication was reported to be 179mm in 2014 and 68mm in 2012. Those flaw indications with large size were identified at locations deeper than 50mm. The identification of indications with large size (> 25 to 30mm) raises an important issue because "elementary" flakes of such a size are practically excluded due to metallurgical considerations. The hypothesis to be rejected is that those large indications result from the growth of smaller flakes. Bel V asked therefore Electrabel to provide a detailed analysis of the large flaw indications identified in 2014. In a first answer, Electrabel provided the requested analysis for the largest indications that had already been reported in 2012. It was shown that, as a consequence of the lowered detection threshold of the OL 0° T1 transducer (-24dB instead of -12dB), some small close flaw indications reported in 2012 could not be discriminated. According to Electrabel, those indications are reported as a single large indications although the size, shape and contour variation with amplitude are more typical of a cluster than of an allegedly large flake.

While acknowledging the outputs of that analysis, Bel V remarked that the amplitude of these large indications should not be low since the smaller indications that make the large indications were detected with the historical inspection procedure. Bel V asked therefore to complete the analysis of the large indications by the analysis of large indications of low amplitude with the objective of showing that these indications were also clusters of smaller indications. According to Electrabel, the maximum dimension of the 2014 flaw indications not reported in 2012 was 55mm and the largest 2014 indications that could not be surimposed on 2012 indications also featured multiple local amplitude peaks that were most likely attributable to distinct neighbouring flakes.

Bel V found satisfactory the analysis of the large flaw indications by Electrabel and agreed with him that the larger maximum dimensions of the flaw indications reported in 2014 resulted from the merging of small neighbouring indications that could not be discriminated when using the qualified inspection procedure.

To Bel V opinion, the fact that the large indications are clusters of small indications with sound material between them is of prime importance since, as mentioned above, individual flakes of large size are practically excluded due to metallurgical considerations. Moreover, the methodology for evaluating the crack growth analysis of the flaw indications with large size (see section 10) has therefore an inherent conservatism.

To Bel V opinion, another contribution to the rejection of the assumption of flake growth would also be the confirmation that the UT inspection of the large indications has allowed to evidence the “facetted” morphology of the large indications on their entire surface. Indeed, as agreed by Electrabel experts, the part of a flake that would have grown in service would not evidence the facetted appearance of the original flake but a rather flat appearance. As a consequence, the UT response of this part would have an amplitude much higher than the one generated by a flake of equivalent dimensions. Electrabel confirmed that very high and uniformly distributed UT amplitude response on the border of a given UT indication was not observed in the Doel 3 and Tihange 2 RPV core shells. To Bel V opinion, this strengthens the rejection of the growth of the flakes by any mechanism. The conclusion relies nevertheless on the assumption that the fatigue crack growth of a flake is similar to the fatigue crack growth of other flaws.

4.4 High efficiency of the detection performance

Bel V recognizes that the reporting levels of the UT inspection procedure are very low. According to Electrabel, confidence in the high efficiency of the detection performance is obtained by the amplitude distribution curves which show “the vanishing left end of each Gaussian curve”. It is true that, for low values of the amplitude, the relationship between the number of indications and the amplitude (dB) is approximately linear. So even if drawing the slope of the straight line from the available data could present some uncertainty, the potential number of missed indications should not be very high. However, some additional information could also be obtained by taking profit of the overlapping zones between the layers inspected with different transducers or with different reporting thresholds. For instance, the first inspection layer (8-35mm) is inspected with the OL 0° MER transducer with a reporting level set at -24 dB while the inspection of the second layer (35-50mm) with the same transducer is performed with the reporting threshold of -30dB. Bel V asked therefore Electrabel to investigate whether some additional flaw indications would be detected in the first inspection layer if the -30dB threshold was used. According to Electrabel, a small percentage of additional indications would be reported at -30dB, an unknown part of them being likely artefacts. However, Electrabel also emphasized that the qualified inspection procedure ensured the detection of hydrogen flakes with a specified confidence level and the number of additional indications detected at -30dB in the first inspection layer was not inconsistent with the probability of missing indications.

Bel V concluded that the satisfactory efficiency of the qualified UT inspection procedure for detecting hydrogen flakes was ensured.

Furthermore, for the 2014 inspection of the Doel 3 and Tihange 2 RPV core shells, use was also made of an additional 15° transducer in order to detect any highly tilted flake. Bel V acknowledged that no such indications were detected.

4.5 Absence of radial connection between flakes

The UT inspection of the RPV core shells with straight beam transducers does not allow to identify any hypothetical radial connection between flakes located at slightly different depths. In order to reject that assumption, the data recorded by the eight 45° transducers installed on the UT inspection tool were analysed in order to detect any such connections. Bel V acknowledged that no radial connections between flakes were detected. That conclusion is of prime importance in particular for the assessment of the prevention of the instantaneous failure under single load application (see section 10).

4.6 Investigation of the potential in-service growth of the flakes

The UT inspection of the Doel 3 and Tihange 2 core shells performed in 2014 also allowed to investigate whether flakes had experienced in-service growth since the first inspection in 2012 (less than one operating cycle). To allow the comparison with the 2012 inspection, the data acquired during the 2014 inspection were analysed using the same settings as those used in 2012. For each of the indications, the amplitude and size were compared according to the criteria set forth in the (French) RSE-M Code.

According to Electrabel, the comparison led to conclude that no new indication was detected in 2014 and no in-service growth of the indication was identified.

To Bel V opinion, considering that the time elapsed between the restart in 2013 and the shutdown in 2014 is less than one year, the results of the comparison do not allow to claim that there is an experimental evidence of no in-service growth. However, they should be considered as positive results.

5. Revised proximity rules and consequences on the screening criterion

5.1 Revised proximity rules

It is known that the driving force acting on a given crack (stress intensity factor) can be significantly affected by the presence of one or more cracks in the close neighborhood. Depending on the relative position and orientation of the neighboring cracks, this interaction effect can either increase or decrease the stress intensity factor. When assessing the fracture strength of structures affected by multiple cracks, the classical procedure used by the fitness-for-service Codes for avoiding the calculation of the interaction effect between neighboring cracks is to replace the closely-spaced interacting flaws by one single larger flaw that envelopes those flaws. Such a procedure is acceptable subject to the condition that the fracture potential of each interacting flaw is conservatively assessed by the fracture potential of the larger enveloping flaw. Otherwise stated, the stress intensity factor of the enveloping flaw shall be larger than the stress intensity factor of each individual flaw taking into account the interaction effect. The usual practice is to define interaction criteria or proximity rules that are used to determine which flaws are not to be assessed separately as isolated flaws but are to be merged.

The damage of the Doel 3 and Tihange 2 RPV core shells is characterized by a high number of closely-spaced flaw indications. Due to the presence of multiple closely-spaced flaws, the local stress field in the zone containing the flaws may be significantly affected. As the stress intensity factors are governed by the stresses in the vicinity of the cracks, the perturbation to the local stress field is the underlying cause of the interaction effect. An adequate and conservative assessment of the interaction effect between multiple flaws is therefore a key step in the evaluation of the fracture behavior of the RPV core shells.

The definition of the new proximity rules using 3D flaw calculations is based, as for the ones originally defined in 2012, on the results of a research work performed by K. Hasegawa showing that two non-aligned thru-wall cracks in a flat plate will connect to each other once the brittle fracture is initiated at the condition that the interaction factor is greater than 1.06. The interaction factor quantifies the increase of the stress intensity factor for a flaw due to the interaction with another flaw. While recognizing that those results have provided the technical basis for the definition of the revised grouping criteria of Section XI of the ASME B&PV Code, Bel V remains reluctant to the basic idea of requiring grouping of two flaws when coalescence of these two flaws is expected in the brittle fracture process. To Bel V understanding, the objective of calculating the stress intensity factor and comparing it to the fracture toughness is to prevent the crack initiation. An accurate estimate of the value of the stress intensity factor is therefore required. Due to the interaction effect, calculating the stress intensity factor of a crack as an isolated crack while being in the neighborhood of other cracks may be non-conservative. For practical purpose however, it may be found necessary to define a threshold below which the interaction effect should not be considered in fracture assessment. The threshold of the interaction factor should not be too low to account for the accuracy in the evaluation of the stress intensity factor but also not too high. A threshold value of 1.06 for the interaction factor appeared acceptable to Bel V.

One of the main concerns raised by Bel V in his evaluation of the revised proximity rules was the assumption made by Electrabel that the interaction between two neighbouring flaws was not affected by the presence of other flaws in the close neighbourhood. By doing so, Electrabel assumed that the interaction between two neighboring flaws in a cluster was not affected by the presence of the other flaws in the cluster. Otherwise stated, Electrabel assumed that the local stress field around two neighboring flaws in a cluster was not affected by the other flaws in the cluster. A potential consequence of taking into account the impact of other flaws in the neighbourhood would be the enlargement of the interaction domain, the latter being defined as the limiting distances between two flaws for which interaction has to be considered. Bel V recognized that the boundaries of the interaction domain for two flaws had been enlarged by 20% to define the proximity rules but he wondered whether that arbitrary enlargement was sufficient to cover the potential enlargement of the interaction domain when other flaws in the close neighbourhood of the two analyzed flaws were considered.

Electrabel and Bel V agreed that an acceptable answer to that concern was to show by using multi-flaw 3D analyses of a few typical flaw configurations from the Doel 3 RPV lower core shell that the enlargement by

20% was actually not necessary. In other words, in regions affected by closely-spaced flaws, some groups of flaws defined by using the proposed proximity rules are expected to contain more flaws than the groups that would be defined by using proximity rules without the 20% enlargement. For those groups, the enlargement could be considered as not necessary if the maximum equivalent stress intensity factor K_{eq} (see section 9) in the group defined using the proposed proximity rules was not significantly higher than the maximum equivalent intensity factor K_{eq} in the group defined without 20% enlargement. Otherwise stated, the consideration of additional neighbouring flaws did not increase significantly the maximum equivalent intensity factor K_{eq}. Two groups of flaws belonging to the Doel 3 lower core shell were considered as typical flaws for illustrating the non-necessity of enlarging by 20% the interaction domain. When using the proposed proximity rules, the first group contained 3 flaws and the second group contained 9 flaws. When using proximity rules without enlargement of the interaction domain by 20%, the number of flaws in those groups was reduced to 2 and 5 flaws respectively. However, it was found that the value of the maximum equivalent intensity factor K_{eq} was not significantly changed. Adding one flaw in the first group increased the maximum equivalent intensity factor K_{eq} by 0.01% and in the second group, adding 4 flaws increased the maximum equivalent intensity factor K_{eq} by 0.34%. Bel V concluded that for the flaw configurations in the Doel 3 and Tihange 2 RPVs, the interaction between two neighbouring flaws was not significantly affected by the presence of other flaws in the close neighbourhood.

Another concern raised by Bel V was related to the use of the proximity rules defined from the principles of the linear elastic fracture mechanics for assessing the necessity of grouping flaws in the 20mm thick-layer beneath the cladding where the fracture behaviour of the flaws under small-LOCA loading conditions is analysed using the elastic-plastic fracture mechanics. More precisely the concern is the possible enlargement of the interaction domain due to the yielding of the material at the crack tip.

In order to illustrate the non-necessity of enlarging the interaction domain for accounting for the possible yield at the crack tip, Electrabel took the example of the most critical group of flaws in the 20mm thick-layer beneath the cladding of the Doel 3 RPV lower core shell. Electrabel used the results of the multi-flaw 3D analysis under small-LOCA loading conditions to calculate the flaw interaction in that group. For that group that contains 3 flaws, Electrabel showed that the most severe flaw saw its maximum equivalent stress intensity factor increased by 2.5% due to its interaction with the other two flaws. According to Electrabel, this example illustrated the conservatism of the proximity rules even when yielding of the material is expected. Indeed, although the interaction could theoretically exceed 6% since the flaws are grouped, the actual interaction factor was lower (2.5%), which justifies the non-necessity of enlarging the interaction domain. Moreover Electrabel emphasized that for the flaws detected in the 20mm thick-layer beneath the cladding of the Doel 3 and Tihange 2 RPVs, the extent of the plastic zone at the crack tip under small-LOCA loading conditions was very limited (maximum value of 0.4mm). Bel V concluded that for the flaw configurations of the Doel 3 and Tihange 2 RPVs in the 20mm thick-layer beneath the cladding, the proximity rules did not need modification to account for the yielding of the material at the crack tip under small-LOCA loading conditions.

Bel V concluded that the use of the revised proximity rules for quasi-laminar flaws was acceptable but he also emphasized that the conclusion only applied to the flaw configurations detected in the Doel 3 and Tihange 2 RPV core shells.

5.2 Revaluation of the role of the screening criterion

Bel V recognizes that the revised proximity rules and the associated grouping rules are conservative for the flaw configurations detected in the Doel 3 and Tihange 2 RPV core shells, as illustrated by the following example. The group of nine flaws taken from the Doel 3 RPV lower core and discussed above is considered again. The group assumed to be loaded by pressure only is first assessed by 3D detailed analysis and then the flaw with the highest stress intensity factor is assessed by 3D detailed analysis as a single isolated flaw, which allows to determine the interaction factor (about 1%). This shows that using the revised proximity rules can lead to combine flaws that have actually an interaction factor smaller than 6%. If the nine flaws are modelled as a single elliptical flaw with the largest size and tilt angle that can be included in the box containing the nine flaws, the maximum stress intensity factor of that flaw, as calculated by the 3D analysis is more than twice higher than the maximum stress intensity factor of any of the nine flaws with consideration of the interaction.

However, the revised proximity rules are less conservative than those used in the 2012 Safety Cases, which were defined by 2D analysis. A 2D approach is indeed known to include a proven conservatism since it considers the cracks as infinitely long. That conservatism was accounted for by Bel V when defining the screening criterion approach in his evaluation of the 2012 Safety Case.

The use of the screening criterion as applied in the 2012 Safety Case should be reminded. To Bel V opinion, demonstrating the acceptability of each individual flaw against the acceptance criteria of IWB-3610 in Section XI of the ASME B&PV Code was not sufficient for demonstrating the required acceptable impact of thousands of flakes on the serviceability of the RPVs. Instead, applying one of the principles he used for the safety evaluation (see section 2), Bel V considered that high confidence in the acceptable impact of flaking on the serviceability of the RPVs would be ensured if almost all of the flaws were evaluated as having a non-significant impact on the structural strength of the RPV. A possible way would have been to calculate with a refined calculation method the stress intensification factor applied to each flaw under the governing loading conditions (heat-up, cooldown or small LOCA) and to show that, for almost all of the flaws, the stress intensification factor was *well below t*he applicable fracture toughness value of the material (with the applicable safety coefficient). Only a few flaws would have been allowed to have a stress intensification factor closer to the applicable fracture toughness value (with the required safety coefficient) while being below. However, accounting for the procedure used by Electrabel for assessing the flaws, Bel V preferred to use an approach based on a screening criterion that may be summarized as follows. All the flaws are assessed using a robust conservative calculation method and in a first step, all of them are required to meet the acceptance criterion of IWB-3610 ($2a/2a_{acc} \leq 1.0$) in Section XI of the ASME B&PV Code. In a second step, it is verified that almost all of the flaws are below the screening criterion ($2a/2a_{acc} < 0.5$). Then, in a third step, a refined calculation method (3D Finite Element analysis) using less conservative input data should show the few flaws exceeding the screening criterion to have a *comfortable* margin against the flaw acceptance criterion of IWB-3610. In summary, in 2012, the key element in the demonstration of the acceptable impact of flaking on the serviceability of the Doel 3 and Tihange 2 RPVs was the demonstration that, for almost all of the indications, the actual driving force for crack initiation (stress intensification factor) under the postulated loading conditions was very low. In fact, from the data obtained from Electrabel, Bel V concluded with a high confidence that, for almost all of the flaws and possibly for all of the flaws, the applied stress intensity factor was within a small interval around the lower shelf fracture toughness. This was found determining by Bel V when evaluating the 2012 Safety Case.

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Although the screening criterion could appear somewhat arbitrary, it was found by Bel V to be a valuable tool to identify the flaws which might be assessed as having a non-significant impact on the structural strength of the RPV without performing refined finite element calculations. As the revised proximity rules decrease somewhat the conservatism of the *robust* calculation method used in 2012 for flaw evaluation, the screening process might no longer fulfil its objective of discriminating the indications having a non-significant impact on the structural strength of the RPV from the other ones.

However, Bel V concluded that the screening criterion approach was still useful in the 2015 Safety Case but its objective had to be changed. Taking into account the availability of the MORFEO CRACK finite element code within Electrabel for performing on a routine basis 3D calculations of flaws or groups of flaws, Bel V considered that the objective of demonstrating the acceptable impact of flaking on the serviceability of the RPVs affected by flaking could be met by showing that most of the flaws have under the governing loading conditions a stress intensity factor lower than the lower shelf fracture toughness divided by the applicable safety coefficient. For the few other flaws, the stress intensity factor should be shown to be acceptable in accordance with the acceptance criteria of Section XI of the ASME B&PV Code.

In a first step (flaw acceptability assessment), the $2a/2a_{acc}$ ratio is calculated for each of the flaws or flaw groups as in the 2012 Safety Cases but using the assumptions and methods of the 2015 Safety Cases (e.g., revised proximity rules). Values of the $2a/2a_{acc}$ ratio higher than 1.0 may be tolerated in that step.
In a second step, the screening criterion is used to determine the flaws or flaw groups having a $2a/2a_{acc}$ ratio higher than 0.5 and for which performing refined 3D calculations is mandatory. For flaws or flaw groups having a $2a/2a_{acc}$ ratio lower than 0.5, some of them identified as being typical and penalizing configurations for having high values of stress intensity factor shall be assessed by refined 3D calculations.

6. Material properties for flaw assessment

A significant part of the activities performed by Electrabel for the 2015 Safety Case is related to the assessment of the unexpected irradiation embrittlement of the VB 395 shell material. That assessment was indeed a prerequisite for determining whether or not a similar behaviour was expected for the Doel 3 and Tihange 2 RPV core shells. The irradiation testing of other available materials was also found necessary.

6.1 Irradiation testing of VB 395 material

The AREVA VB 395 shell made of 18MND5 steel according to the RCC-M Code and manufactured from a hollow ingot was discarded during fabrication after detection of hydrogen flaking in its bottom part. In the 2012 Safety Case, the flaked material of the VB 395 shell was considered as representative of the flaked material of the Doel 3 and Tihange 2 RPV core shells for the qualification of the UT inspection procedure and for the assessment of the mechanical properties of flaked material in unirradiated conditions.

In absence of any data about the irradiation embrittlement of material affected by hydrogen flaking, the 2012 Safety Cases assumed that the irradiation embrittlement of the flaked material could be estimated by the predictive (French) FIM or FIS formulae (considered applicable to the Doel 3 and Tihange 2 RPV, see section 3) where the contents of the macro-segregations in embrittling elements (P, Cu, Ni) were estimated by multiplying their nominal contents in the forgings by conservative enrichment factors. Under action 11 of the mid-term action plan, that assumption had to be verified by performing mechanical testing on specimens taken from the VB 395 shell and irradiated in the Belgian BR2 test reactor.

The results of the first irradiation test campaign (Chivas 9) complemented by those of the second one (Chivas 10) showed that, for the macro-segregated material between the flakes, the shift in RT_{NDT} (as measured by ΔT_{41J}) at a fluence representative of the 40 year lifetime of the RPV was significantly above the value predicted by the FIM or FIS formula. As an example, for a fluence of about 6.5 10^{19} n/cm², the measured ΔT_{41J} exceeds the FIM predicted value by about 50°C. When applying the FIM or FIS formulae, the contents in embrittling elements were the mean values of the composition measurements made in the macro-segregations where the test specimens were taken.

Additional tests were performed during the successive irradiation campaigns at lower fluence levels. Their results showed that the values of the shift in RT_{NDT} for macro-segregated material between the flakes were not distributed randomly but could be fitted by the RSE-M predictive formula (see section 7) with an increased (fictive) chemistry factor.

For some reasons including the irradiation-induced hardening conform to the expectations and the decrease of the microcleavage fracture stress with fluence, non-hardening embrittlement was suspected by Electrabel to be responsible of the (unexpected) enhanced irradiation embrittlement of the VB 395 material in the macro-segregated material between the flakes. However the main feature characterizing the non-hardening embrittlement, i.e., intergranular fracture (P segregation at grain boundaries), was not evidenced.

It should also be pointed out that the material close to the flaked area as well as the material of the top end of the VB 395 shell (free from flakes and far from the flaked region) did not exhibit apparent enhanced irradiation embrittlement (as measured by ΔT_{41J}) but showed nevertheless decrease of the microcleavage fracture with fluence. That decrease was found to be consistent with the decrease evidenced in the material between the flakes.

6.2 KS02 material

Additional search in the documentation file of the FKS program as well as examination of some test specimens still available in Germany allowed Electrabel to conclude that the laminar indications detected in the macro-segregated zone of Segment B of the KS02 forging were actually flakes (see section 3). As a part of the FKS research program, test specimens had been taken from that zone, irradiated in a test reactor and then submitted to mechanical testing. The shifts in RT_{NDT} (ΔT_{41J}) measured at different fluence levels might therefore be considered as representative of the irradiation embrittlement of a RPV material affected by hydrogen flaking other than the VB 395 material. Shifts in RT_{NDT} (ΔT_{41J}) were also measured in the non-segregated area of segment B. The results showed that the shifts in RT_{NDT} as well for the material taken from the segregated area as from the non-segregated area are within the confidence interval of the RSE-M predictive formula, although that formula is not strictly applicable to the 22NiMoCr3-7 material. No decrease of the microcleavage fracture stress with fluence was evidenced. Confirmatory tests were performed on material of segment M made available to Electrabel and also containing flakes. After irradiation in the BR2 test reactor (Chivas-12), mechanical testing was performed, the results of which are consistent with the results obtained by the FKS program.

6.3 Irradiation testing of materials from the Doel 3 RPV forgings.

Some materials of the Doel 3 RPV forging were found by Electrabel to be still available for irradiation testing: the Doel 3 nozzle shell cut-out D3H1 (material with macro-segregations) and spare material of the Doel 3 surveillance program taken from the upper end of the upper core shell (material with no macro-segregations). Test specimens taken from both materials were irradiated in the BR2 test reactor (Chivas 10 program).

The surveillance program also permits to have data related to the irradiation embrittlement of materials taken from the upper end of the upper core shell of the Doel 3 and Tihange 2 RPVs.
All those materials are not affected by hydrogen flaking.

The shifts in RT_{NDT} (ΔT_{41J}) of the D3H1 material (inside and outside macro-segregations) and Doel 3 upper shell material (forging upper end) as determined by Chivas 10 program are within the confidence interval of the RSE-M prediction as well as the shift in RT_{NDT} of the material of the Doel 3 and Tihange 2 RPV upper core shells (surveillance program). No decrease of the microcleavage fracture stress is evidenced in the material of the Doel 3 D3H1 nozzle cut-out and upper core shell. For the material of the Tihange 2 RPV upper core shell, no data is available.

6.4 Investigations on the enhanced irradiation embrittlement of the VB 395 shell material

Numerous investigations comprising of literature search and experimental program have been conducted by Electrabel to assess the enhanced irradiation embrittlement of the VB 395 material. To Bel V opinion, a sufficient understanding of the responsible mechanism(s) and of the factor(s) triggering it (them) is necessary to assess
(i) whether the irradiation embrittlement of the VB 395 shell material is representative of the expected embrittlement of the Doel 3 and Tihange 2 core shell materials under irradiation and if so,
(ii) under which conditions the shift in RT_{NDT} (ΔT_{41J}) determined for the VB 395 material may be transferred to the Doel 3 and Tihange 2 core shell materials.

The main results of the root cause analysis performed by Electrabel are:
(i) hydrogen flaking can be excluded as the root cause of the enhanced irradiation embrittlement and
(ii) two mechanisms have been identified that could contribute to the occurrence of the enhanced irradiation embrittlement.

The exclusion of the hydrogen flaking as the root cause of the enhanced irradiation embrittlement is, to Bel V opinion, of a prime importance. Two arguments are provided by Electrabel to support that statement.

First, the flakes themselves are not the cause of the enhanced irradiation embrittlement since similar Master Curve transition temperatures *To* after irradiation were determined from Charpy pre-crack specimens taken from the VB 395 material between the flakes and from test specimens with a flake as a surrogate for the pre-crack.
Then, hydrogen flaking is not the cause of the enhanced irradiation embrittlement since the KS02 material is not affected by some enhanced irradiation embrittlement although it is also affected by hydrogen flaking.

To Bel V opinion, the first argument that was also used to justify that the crack initiation fracture toughness for a flake is governed by the fracture toughness of the surrounding macro-segregated material is poorly convincing. Indeed, it may not be excluded that some mechanisms or factors contributing to the formation of flakes could also contribute to the enhanced irradiation embrittlement. Otherwise stated, flakes could preferentially appear in materials susceptible to enhanced irradiation embrittlement. For instance, in the VB 395 shell, the flakes are located in ghost lines having mostly a tempered martensitic structure, and loss of strength of the segregation network due to non-hardening embrittlement of the tempered martensite is recognized by Electrabel as a mechanism that could contribute to the occurrence of the enhanced irradiation embrittlement.

The second argument appears to be more convincing to demonstrate that hydrogen flaking is not the root cause of the enhanced irradiation embrittlement. However, to Bel V opinion, the argument should be interpreted with caution. A difference should be made between two cases: (i) hydrogen flaking is the root cause of enhanced irradiation embrittlement, i.e., enhanced irradiation embrittlement occurs in any steel material affected by hydrogen flaking and (ii) hydrogen flaking makes the steel susceptible to enhanced irradiation embrittlement, i.e., some mechanism responsible for the enhanced embrittlement requires as a prerequisite for being activated that hydrogen flaking be present. The existence of the KS02 material may be used successfully to reject the hypothesis that hydrogen flaking is the root cause of enhanced irradiation embrittlement. However the argument is not sufficient to reject the hypothesis that hydrogen flaking makes the steel susceptible to enhanced irradiation embrittlement. Rejection of that hypothesis would require in addition the existence of a material exhibiting enhanced irradiation embrittlement without being affected by hydrogen flaking. To Bel V knowledge, there are no identified cases with similar enhanced irradiation embrittlement without being associated to specific causes, e.g., high content in Ni. With regard to that, the behaviour under irradiation of the top part of the VB 395 shell needs to be carefully assessed. If the decrease of the microcleavage fracture stress is considered as the criterion for determining whether or not a material is susceptible to enhanced irradiation embrittlement, the material of the VB 395 top part is susceptible to enhanced irradiation embrittlement although it is not affected by hydrogen flaking.

From the above, Bel V concludes that there is likely no link between enhanced irradiation embrittlement and hydrogen flaking but the demonstration might appear as exhibiting certain weaknesses.

Bel V has no fundamental objection against the identification of two mechanisms as mechanisms that contribute to the occurrence of the enhanced irradiation embrittlement. Bel V also shares the cautiousness shown by Electrabel when establishing the possible link between both mechanisms and specific aspects of the manufacturing history of the VB 395 shell.

7. Predictive formula for the irradiation embrittlement

7.1 Predictive formula initially proposed by Electrabel

The approach used by Electrabel to estimate the fracture toughness values of the Doel 3 and Tihange 2 RPV core shell materials for use in the structural integrity assessment is based on the standard practice provided in Section XI of the ASME B&PV Code. That standard practice relies on the ASME reference

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fracture toughness curve indexed to the reference temperature in irradiated conditions ($RT_{NDT,irr}$), as given in Appendix G to Section XI of the ASME B&PV Code.

The reference temperature in irradiated conditions, i.e., $RT_{NDT,\,irr}$, to which the ASME reference fracture toughness curve is indexed is given classically by the following expression

$$RT_{NDT,irr} = RT_{NDT,init} + \Delta RT_{NDT} + Margin\ (M)$$

where

$RT_{NDT,init}$ is the initial RT_{NDT}, i.e., the nil-ductility reference temperature for the unirradiated steel, which is determined by a procedure set forth in NB-2330 of the ASME B&PV Code Subsection NB and based on either Charpy or drop weight tests. For heavy forgings, the initial RT_{NDT} is required to be measured from test coupons removed from one of the discarded forging ends and taken at a quarter of thickness from any surface.

ΔRT_{NDT} is the mean value of the shift of the reference temperature due to irradiation embrittlement. It is obtained from predictive equations based on a large collection of surveillance data. ΔRT_{NDT} is controlled by the fluence and the content in embrittling elements (copper, phosphorus, nickel).

M, improperly referred to as a margin, is a term that is added to obtain a two-sigma upper bound value of $RT_{NDT,irr}$. The standard deviation 'sigma' combines quadratically the standard deviation for $RT_{NDT,init}$ and the standard deviation for ΔRT_{NDT}.

For the structural integrity assessment performed in the 2015 Safety Case, the following expression of $RT_{NDT,irr}$ was initially proposed by Electrabel

$$RT_{NDT,irr} = RT_{NDT,init} + \Delta RT_{NDT}(RSE - M) + \Delta RT_{NDT}(VB\ 395) + Margin$$

where

$RT_{NDT,init}$ is defined as above.

$\Delta RT_{NDT}(RSE - M)$ is the shift of the reference temperature as predicted by the more recent formula adopted by EDF for the assessment of the irradiation embrittlement of the RPV core shells of the French PWRs and included in the 2010 Edition of the RSE-M Code. Contrary to the FIS equation that had been developed in 1983 from data obtained in experimental reactors, the new formula has been developed from a data base including mainly results from the French surveillance program. The new formula intends to remedy a lack of consistency between some results of the French surveillance program and their FIS predictions, especially for high fluences. The formula adopted by Electrabel for use in the equation of $RT_{NDT,irr}$ is the one providing the mean value of the shift. The use of the $\Delta RT_{NDT}(RSE - M)$ equation and its associated confidence interval was shown to provide a better consistency with the Doel 3 and Tihange 2 surveillance program than the FIM/FIS formulae.

The $\Delta RT_{NDT}(RSE - M)$ formula is expressed as the product of a (chemistry) factor by the fluence Φ raised to the power of 0.59, i.e.,

$$\Delta RT_{NDT}(RSE - M) = factor_{RSE-M} * \Phi^{0.59}$$

The chemistry factor $factor_{RSE-M}$ is a function of the nominal content of the forging in copper, phosphorus and nickel, as given by the product analysis of the forging. For use in the flaw evaluation in the Doel 3 and Tihange 2 RPV core shells, the content in copper, phosphorus and nickel is the nominal content in those elements increased by the same postulated enrichment factor as the one defined in the 2012 Safety Case ($Cu/Cu = 1.25$, $\Delta P/P = 1.35$) and $\Delta Ni/Ni = 1.08$).

$\Delta RT_{NDT}(VB\ 395)$ is an additional term that accounts for the anomalous irradiation embrittlement affecting the VB 395 material in addition to the hardening embrittlement. It is expressed as

$$\Delta RT_{NDT}(VB\ 395) = factor_{VB\ 395} * \Phi^{0.59}$$

where $factor_{VB\ 395}$ is determined from the difference between (1) the (best estimate) curve $\Delta RT_{NDT} - \Phi$ determined by least squares fitting with a power-law with exponent 0.59 the experimental shift data (ΔT_{41J}) obtained on specimens taken from the bottom of the VB 395 shell (between flakes) and irradiated in the BR2 test reactor at different fluences and (2) the predictive mean curve obtained by determining the $\Delta RT_{NDT}(RSE-M)$ equation for the content in embrittling elements (copper, phosphorus and nickel) measured in the macro-segregated zone of the VB 395 shell where the flakes are located.

The so-called $Margin$ equal to two times the standard deviation is added to obtain the upper bound formula (97.5% fractile). The standard deviation is obtained by quadratically combining the standard deviation associated to the shift ΔRT_{NDT} and the standard deviation associated to the initial reference temperature $RT_{NDT,init}$. In turn, the standard deviation associated to the shift ΔRT_{NDT} is obtained by quadratically combining the standard deviation associated to the prediction of the hardening embrittlement by the RSE-M formula ($\Delta RT_{NDT}(RSE-M)$) and the standard deviation associated to the prediction of the anomalous hardening of the VB 395 material ($\Delta RT_{NDT}(VB\ 395)$). The standard deviation associated to the shift $\Delta RT_{NDT}(RSE-M)$ is equal to 9.3°C as mentioned in the RSE-M. As the shift $\Delta RT_{NDT}(VB\ 395)$ is expressed as the difference between two (independent) variables, its standard deviation is equal to the quadratic combination of the standard deviations associated to each of both variables. Knowing that the standard deviation associated to the least squares fitting of the VB 395 experimental data is assumed by Electrabel to also be equal to 9.3°C (without considering the actual scattering of the experimental data but not inconsistent with them), the standard deviation associated to the shift $\Delta RT_{NDT}(VB\ 395)$ is equal to 13.15°C. Then, the standard deviation associated to the shift ΔRT_{NDT} is equal to 16.1°C. The standard deviation associated to $RT_{NDT,init}$ is taken equal to 8.3°C, value traditionally used for the assessment of the irradiation embrittlement of the Doel 3 and Tihange 2 RPV. The resulting standard deviation on $RT_{NDT,irr}$ is therefore equal to 18.1°C and the $Margin$ is equal to 36.2°C.

Following the discussions with Bel V, the predicting equation of $RT_{NDT,irr}$ was revised (see Section 7.8).

7.2 Use of the fracture toughness value of the macro-segregated material

The implicit assumption underlying the predictive formula proposed by Electrabel is that the irradiation embrittlement to be considered for the assessment of the flakes is that of the macro-segregated material where the flakes are located. This is conform to the understanding (see Section 3) that the hydrogen flaking as a damage has no effect on the fracture toughness of the material and the fracture resistance of the flakes is governed by the fracture toughness of the macro-segregated material.

7.3 Consideration of a two-component irradiation embrittlement

Electrabel assumes that the irradiation embrittlement is the sum of two components, i.e., the (usual) hardening component and an additional component responsible for the enhanced irradiation embrittlement experienced by the VB 395 material.

Bel V agrees with that assumption for the two following reasons.

Firstly, the irradiation embrittlement of the VB 395 material (between flakes) is qualified by Electrabel as “enhanced” by reference to the predicted value given by the RSE-M formula. To Bel V opinion, that qualification needs further substantiation. A clear distinction should be made between an *outlier* and an *extreme value*. An *outlier* is defined as an observation that “appears” to be inconsistent with other observations in the data set. An outlier has a very low probability that it originates from the same statistical distribution as the other observations in the data set. An *extreme value* is an observation that might have a low probability of occurrence but cannot statistically be shown to originate from a different distribution than the rest of the data. Bel V considers that the VB 395 material exhibits *enhanced* irradiation embrittlement at the condition that the measured RT_{NDT} shifts (ΔT_{41J}) of the VB 395 material are outliers in the population of the RT_{NDT} shifts determined in the surveillance program of the French 900 MWe RPV materials and used in the definition of the RSE-M predictive equation. To make the analysis easier, each of the French RT_{NDT} shift data as well as the available RT_{NDT} shifts of the VB 395 material are divided by the specific chemistry factor applicable to the material, which allows to normalize all the shift data. Although no statistical test has been carried out, by inspection of the data in the RT_{NDT} shifts vs. fluence diagram it may be concluded that the VB 395 material (between the flakes) is an outlier for high values of fluence. For low values of fluence, i.e., for fluences lower than about 4 10^{19} n/cm², the RT_{NDT} shifts of the VB 395 material may not be considered neither as outliers nor as extreme values.

Secondly, irradiation hardening is expected to be fully recovered by a post-irradiation annealing at 450°C for 150 hours. After such an annealing, material taken from the VB 395 shell between flakes and irradiated at about 7 10^{19} n/cm² only showed a partial recovery (35%) of the (ΔT_{41J}) Charpy shift (41°C out of 120°C), which corresponds approximately to the expected shift due to irradiation hardening as calculated by the RSE-M formula. The tensile properties were also found to be fully recovered. Annealing at 610°C for one hour was necessary to recover a larger fraction (about 80%) of the (ΔT_{41J}) Charpy shift. Those results suggest that a second embrittlement mechanism is acting in addition to the irradiation hardening mechanism in the VB 395 material.

7.4 Basic principles of the evaluation by Bel V

The definition of the predictive formula is a key step in the demonstration not only because of its role in the structural integrity assessment of the Doel 3 and Tihange 2 RPVs but also because of the unknowns or uncertainties affecting the estimation of the expected irradiation embrittlement of their core shell material.

The predictive equation of the irradiation embrittlement should therefore be defined with the required level of conservatism (see Section 2). In particular, to Bel V opinion, a clear picture of the conservatism of the predictive RT_{NDT} equation requires that each term thereof is estimated with the adequate conservatism: the possible over-conservatism of one term should not be used to balance the under-conservatism of another term.

It should also be emphasized that a clear distinction has to be made between the treatment of identified uncertainties and the consideration of the unknowns.

7.5 Evaluation of the initial RT_{NDT}

In the predictive formula proposed by Electrabel, the beginning-of-life RT_{NDT} , i.e., $RT_{NDT,init}$, is the reference temperature for the unirradiated material, which is determined by a procedure set forth in NB-2330 of Section III in the ASME B&PV Code.

Bel V recognizes that:

(i) according to the applicable regulatory requirements, i.e., Appendix G to Part 50 of 10 CFR, the nil-ductility reference temperature at beginning of life $RT_{NDT,init}$ is determined according to the procedure set forth in paragraph NB-2331 in Section III of the ASME B&PV Code from test coupons removed from one of the discarded forging ends and taken at a quarter of thickness from any surface;
(ii) according to Regulatory Guide 1.99, the equation predicting the reference temperature under irradiated conditions includes, as the initial reference temperature, the reference temperature $RT_{NDT,init}$ as defined above;
(iii) according to the fracture mechanics approach used in Section XI of the ASME B&PV Code for flaw evaluation, the lower bound of the material fracture toughness is obtained from a curve that is indexed to the reference temperature taking into account the irradiation embrittlement, as defined in Regulatory Guide 1.99.

The RSE-M equation, as any other predictive equation, aims first at predicting the embrittlement of the specimens encapsulated in the reactor vessel for the surveillance program. The specimens used for determining the initial RT_{NDT} (as well as those used as surveillance specimens) are taken from a ring at one end of a core shell, i.e., in a zone where the carbon segregation is typically lower than the carbon segregation expected in the material adjacent to the flakes. As a result thereof, the fracture toughness of the macro-segregated material in the areas affected by hydrogen flaking is possibly lower when compared to the fracture toughness of the unsegregated material in the forging end where the value of $RT_{NDT,init}$ has been determined.

For assessing the hydrogen flakes detected in the Doel 3 and Tihange 2 RPV core shell, Bel V considers that the initial reference temperature needs to be the estimated initial temperature of the material in the macro-segregated region of the forging where the flakes have been detected. Bel V recognizes that an uncertainty (1σ) of 8.3°C on the initial RT_{NDT} is considered and combined quadratically with the standard deviation on ΔRT_{NDT}. However, to Bel V opinion, even if this uncertainty may be considered as acceptable to account for the accuracy in measuring the initial RT_{NDT}, it is not adequate to account for the potential lower fracture resistance (and the associated variability) of the material in the highly segregated areas where the flakes are located. It should also be recognized that Electrabel does not exclude that the material in the macro-segregations could have a fracture toughness lower than in the unsegregated zones but he considers that the difference is already covered by the conservatism in the analysis, which is considered unacceptable by Bel V. To Bel V opinion, the $RT_{NDT,init}$ shall characterize the estimated fracture toughness of the macro-segregated areas of the Doel 3 and Tihange 2 RPV core shells where the flakes have been detected.

An additional term, referred to as $\Delta RT_{NDT\ init\ (segregation)}$, should therefore be added to $RT_{NDT,init}$ to account for the possibly lower fracture toughness of the macro-segregations in unirradiated conditions. The additional term should be considered as an average or best-estimate value to which an uncertainty term will be attached to account for the variability about the average value.

Extensive exchanges took place with Electrabel to discuss the issue. First Bel V concurred with Electrabel that the difference in T_{41J} between the segregated and non-segregated areas of the KS02 material under unirradiated conditions could not be used as an acceptable basis to assess $\Delta RT_{NDT\ init\ (segregation)}$. Indeed the significant value of the difference in T_{41J} , 47°C or 31°C depending on which of the two segregated areas where Charpy curve has been determined is selected for the calculation, is likely heavily biased by an effect of sampling location. Then Bel V agreed with Electrabel that the Doel 3 H1 cut-out (D3H1) and the Tihange 2 H2 cut-out (T2H2) are the materials which are the most representative of the Doel 3 and Tihange 2 core shells for the issue. In particular, the predicted carbon enrichments using the correlation developed by Creusot-Loire for large forgings made of solid ingots show that the sensitivity to segregation formation of the Doel 3 and Tihange 2 core shells are bounded by the sensitivity of the D3H1 cut-out (lower bound) and the sensitivity of the T2H2 cut-out (upper bound). Otherwise stated, the D3H1 cut-out may be considered as a representative material to assess on the lower bound side the impact of the macro-segregations on the fracture toughness while the T2H2 cut-out may be considered as a representative material for the upper-bound side.

Based on his own analysis of the available data, Bel V concluded that the estimated difference in RT_{NDT} between the non-segregated and the segregated zones of the Doel 3 and Tihange 2 core shells was in the range of 0°C to 20°C. That is to say that those values of 0°C and 20°C are to be considered respectively as the estimated lower bound and the upper values of the difference in RT_{NDT}. Then, there are some unpublished experimental data which show that the segregation effect increases by about 10°C the RT_{NDT} temperature in the zones with positive segregation when compared to the zones with a carbon segregation equal to zero. So, Bel V concluded that a value of 10°C for $\Delta RT_{NDT\ init\ (segregation)}$ and a value of 5°C for the associated uncertainty (1σ) were acceptable. Those values are identical to the values proposed by Electrabel to solve the issue but they were obtained in a different way.

7.6 Evaluation of the $\Delta RT_{NDT}(RSE\text{-}M)$ term

On the basis of his assessment of the enhanced irradiation embrittlement of the VB 395 shell material, Electrabel concluded that the Doel 3 and Tihange 2 RPV core shells were not expected to suffer from the irradiation embrittlement experienced by the VB 395 material. However, by conservatism, the part of the RT_{NDT} shift exceeding in the VB 395 flaked material the (normal) hardening irradiation embrittlement predicted by the RSE-M formula is transposed to the Doel 3 and Tihange 2 RPV core shells. That part includes the term $\Delta RT_{NDT}(RSE - M)$ and its associated uncertainty.

To Bel V opinion, there are arguments to support the statement that the material of Doel 3 and Tihange 2 RPV core shells is likely not affected by the enhanced irradiation embrittlement. However Bel V considers that the statement does not rely on a sound and well-reasoned demonstration. Indeed, to Bel V understanding, there is nothing in the characterization of the VB 395 and KS02 materials under unirradiated conditions (chemical composition, grain size, microstructure…) as in the fabrication history (particularly the heat treatments) which could suspect enhanced irradiation embrittlement to occur in VB 395 material and not in KS02 material. The only distinctive element between the VB 395 and KS02 materials is the drop in micro-cleavage fracture stress with increasing fluence observed in VB 395 (including the top part of the shell with no segregation) and not in KS02. Based on the available information, the VB 395 material and the Doel 3/Tihange 2 RPV core shells material belong to the same family of material. The fact that enhanced irradiation embrittlement has not been identified in the Doel 3 nozzle shell and upper core shell does not allow to conclude that the occurrence of enhanced irradiation embrittlement may be excluded for the Doel 3 lower core shell and Tihange 2 upper core shells (which are the most affected by hydrogen flaking).

Bel V considers therefore that the term $\Delta RT_{NDT}(RSE-M)$ to account for the potential decrease of the Doel 3/Tihange 2 RPV core shell fracture toughness by enhanced irradiation embrittlement should not be considered as a conservatism but rather as a *safety provision* (see Section 2) to be included in the predictive equation to cover the incompleteness in the knowledge of the fracture toughness of the Doel 3/Tihange 2 RPV core shell materials in irradiated conditions.

Moreover Bel V also considers that the transposal of the enhanced irradiation embrittlement experienced by the flaked regions of the VB 395 shell to the macro-segregated regions of the Doel 3/Tihange 2 RPV core shell is not fully satisfactory. Theoretically, the objective is to define an upper bound of the expected RT_{NDT} shift due to enhanced irradiation embrittlement from the experimental data obtained in the VB 395 shell material, or alternatively to define a mean value of that RT_{NDT} shift and its associated uncertainty (2σ). The latter option was selected by Electrabel but, to Bel V understanding, its implementation raised issues related to the statistical treatment of $factor_{VB\ 395}$. Bel V considers that the material of three regions of the VB 395 shell are sensitive to the enhanced irradiation embrittlement, i.e., the macro-segregated region where the flakes are located, the macro-segregated region in the close neighbourhood of the flaked region, and the unsegregated top part of the VB 395 shell not affected by flaking. That statement relies on the similar downward trend in the micro-cleavage fracture stress with increasing fluence. However, the severity of the enhanced irradiation embrittlement is not identical in the three regions. Those three materials may therefore be considered as the only known elements in the population of materials sensitive to enhanced irradiation embrittlement. There is however no way to determine from those three elements the characteristic values (mean value, standard deviation) of $factor_{VB\ 395}$. To Bel V opinion, the only possible way to define a value to $factor_{VB\ 395}$ is to consider the value calculated from the experimental data obtained in the flaked region of the VB 395 shell as an estimate of the upper bound and to associate therefore no uncertainty to it. Bel V considers that approach as satisfactory for providing an acceptable value to the safety provision $factor_{VB\ 395}$.

7.7 Use of less conservative enrichment factors

When discussing the issues related to the beginning-of-life RT_{NDT} and the $RT_{NDT}(RSE-M)$ term, Electrabel also proposed to use in the term $\Delta RT_{NDT}(RSE-M)$ of the predictive equation enrichment factors (enhancing the content in copper, nickel and phosphorus) smaller than those used in the original Safety Case. The new enrichment factors are those considered in the RSE-M Code. Taking into account that copper, nickel and phosphorus are elements which co-segregate with carbon, the proposed new

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enrichment factors correspond to a maximum carbon segregation of +20%. Bel V has no objection against the use of the proposed new enrichment factors.

7.8 Modifications to the predictive equation following Bel V evaluation

As a conclusion to the discussions with Bel V about the predictive equation, Electrabel determines what would be the predictive equation for the irradiation embrittlement if the modifications initiated by Bel V were considered, i.e.,

(i) addition of an additional term $\Delta RT_{NDT\ init\ (segregation)}$ of 10°C to account for the potential lower fracture toughness of the macro-segregations
(ii) consideration of an uncertainty (σ = 5°C) associated to $\Delta RT_{NDT\ init\ (segregation)}$, to be combined quadratically with the uncertainty (σ = 8.3°C) on $RT_{NDT,init}$
(iii) removal of the uncertainty on $\Delta RT_{NDT}(VB\ 395)$ and
(iv) use of the enrichment factor considered in the RSE-M Code for determining the chemistry factor $factor_{RSE-M}$

Accounting for those modifications, the predictive equation is written as

$$RT_{NDT,irr} = RT_{NDT,init} + \Delta RT_{NDT\ init\ (segregation)} + \Delta RT_{NDT}(RSE-M) + \Delta RT_{NDT}(VB\ 395) + Margin$$

For each of the core shells, the modified predictive equation was found to be enveloped by the initial predictive equation proposed by Electrabel.

The modified predictive equation was adopted by Electrabel in the Safety Case. However, as the initially proposed equation envelopes the modified equation for each of the core shells, the structural integrity assessment calculations that used the initial equation did not need to be revised. That was accepted by Bel V. For the 10CFR50 Appendix G and PTS analyses (see Section 10), the modified predictive equations were used.

8. Basis of structural integrity evaluation by Bel V

This section provides the basis of the evaluation by Bel V of the structural integrity assessment of the Doel 3 and Tihange 2 RPVs. To Bel V opinion (see Section 2), the objective of the structural integrity assessment is to demonstrate that the presence of thousands of quasi-laminar crack-like defects has an acceptable impact on the serviceability of the RPVs. The assessment of the structural integrity should therefore be based on the best engineering practice and sound safety principles, more than on the verification that all the Code and regulatory requirements are strictly met.

8.1 Criteria for the evaluation of flaking damage

The usual procedure for avoiding failure by propagation of pre-existing crack-like flaws in nuclear components is to prevent crack initiation. Otherwise stated, in the justification of the structural integrity of the RPV, the prevention against RPV brittle or ductile failure is ensured by demonstrating the lack of risk for crack initiation. Classically, for ensuring the prevention of crack initiation from a crack-like flaw, it is demonstrated that the crack driving force, i.e., the stress intensity factor, is lower than the resistance of the material to crack initiation, i.e., the fracture toughness of the material. That procedure has been adopted by Section XI of the ASME B&PV code which, for each loading level defines the fracture toughness curve to be used (crack arrest material toughness K_{Ia} or crack initiation material toughness K_{Ic}) and the safety coefficient to be applied.

However applying strictly the procedure of Section XI of the ASME B&PV code, i.e., demonstrating that all the flaws meet the acceptance criteria of Section XI, was not considered sufficient by Bel V. Bel V considered (see Section 5) that in order to achieve the objective of demonstrating the acceptable impact of the hydrogen flaking on the serviceability of the RPVs it should be demonstrated that most of the flaws have under the governing loading conditions a stress intensity factor lower than the lower shelf toughness of the material divided by the applicable safety coefficient in Section XI of the ASME B&PV code. Only a few ones are allowed to have a higher stress intensity factor, but still lower than the ASME acceptance criterion.

8.2 Prevention of other failure modes

The assessment of the structural integrity of the Doel 3 and Tihange 2 RPVs shall not be limited to the prevention of fracture from the flakes. It should also be verified that the condition of the RPVs does not put into question the prevention of the other failure modes. Taking into account that the hydrogen flaking does not affect the mechanical properties of the RPV material, and more specifically the ductility (see Section 3), Bel V considered that no other failure modes than those assumed in the ASME B&PV Code have to be investigated (with the safety coefficients required by the ASME B&PV Code).
Recognizing that the flaking damage only affects the core shells that have a simple geometry with no structural discontinuities, the required effort shall be limited to the assessment of the prevention of the (instantaneous) collapse failure under single load application and incremental collapse by accumulation of plastic deformations under load cycles. Protection against those failure modes is ensured in Section III of the ASME B&PV code (Subsection NB) by limiting the primary stresses and primary plus secondary stress ranges. For assessing the prevention of those failure modes with consideration of the damage, plastic analysis was found by Bel V to be the most adequate method.

8.3 Prevention of crack growth by fatigue

Usual flaw assessment requires also to demonstrate that the potential in-service crack growth by any mechanism does not increase the size of the pre-existing crack up to its critical size. The only suspected mechanism for in-service growth of the flakes is fatigue (see Section 12). However, to Bel V opinion, the demonstration that the increase in size of any flake by fatigue does not lead the flake to exceed its critical size is not sufficient.

Indeed even if the fatigue growth of the flakes for the 40-year lifetime is only a few percent, this indicates that the flakes experience in-service growth, i.e., the loadings have a non-negligible impact on the flakes. Such a crack growth would not be consistent with the required non-significant impact of the loadings on the risk of fracture at the flakes. Moreover it should also be added that, for the RPV that is assumed not to break, no sub-critical crack growth mechanism that could lead to the increase of the pre-existing defects is allowed.

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From above, Bel V concludes that the ranges of applied stress intensity factors must be sufficiently low to practically exclude potential fatigue growth of those flaws. The crack growth analysis should therefore show not only that the calculated crack growth is low but also not significant.

8.4 Protection against brittle failure

The regulations in 10 CFR 50.60 and associated Appendix G to 10 CFR Part 50 describe the conditions that require P-T limits and provide the general basis for these limits. The regulations in 10 CFR 50.61 provide the fracture toughness requirements for protection against pressurized thermal shock (PTS) events.

As the definition of the pressure/temperature and low-temperature overpressure protection limits as well as the PTS screening criterion depend on the shift in the nil-ductility reference temperature due to irradiation embrittlement, updating of the fracture prevention measures is required.

9. Assessment of prevention against crack initiation

9.1 Electrabel approach for flaw acceptability assessment

The approach used by Electrabel to perform the flaw acceptability assessment is a two-step approach (see Section 5) similar to the one used in the 2012 Safety Case regarding its principle.

The first step is an analytical flaw evaluation in accordance with the acceptance criteria of IWB-3600 in Section XI of the ASME B&PV Code. For this step use is made of acceptable flaw size curves ($2a_{acc}$ curves), which allows not to perform the required evaluation for each individual flaw. This step allows to assign a value of the $2a/2a_{acc}$ ratio to each flaw or group of flaws. The main differences with the flaw evaluation performed in the 2012 Safety Case are related to the proximity rules and the acceptable flaw size curves. Updated proximity rules based on 3D calculations have been defined (see Section 5) and the acceptable flaw size curves have been updated due to the revised predictive equation of the irradiation embrittlement (see Section 7) and the limitation of the plant lifetime to 38 years (instead of 40 years). In addition, for the flaw evaluation of the Doel 3 RPV under small LOCA loading, the temperature of the Safety Injection (SI) water is taken equal to 40°C, reflecting so the Electrabel decision to increase by 10°C the SI water temperature set to 30°C in 2013. Bel V had no objection against the approach used by Electrabel for the ASME Section XI flaw evaluation.
In a second step, the flaws or groups of flaws exceeding the screening criterion ($2a/2a_{acc}$ = 0.5) are assessed using refined analysis (3D finite element calculations). That step is a part of the calculations performed to allow the evaluation by Bel V of the acceptable impact of the flaking damage on the serviceability of the RPVs (see Section 9.4).

In response to a concern raised by AIB-Vinçotte on how the clad interface imperfection indications (see Section 3) were considered in the flaw acceptability assessment, Electrabel decided to assess the clad interface imperfection indications as additional hydrogen flakes. That decision was based on the fact that, according to Electrabel, the characterization of those indications located close to the cladding-base metal interface did not allow for some of them to make the distinction between clad interface imperfection and hydrogen flake with the required high confidence. A similar approach was used in the 2013 Addendum to the 2012 Doel 3 Safety Case. A number of 301 and 4 clad interface imperfection indications were detected in 2014 in the Doel 3 and Tihange 2 RPV core shells respectively.

9.2 Mixed-mode fracture criterion

Due to their quasi-laminar configuration, the flakes are not loaded in pure mode I but they are loaded in mixed-mode condition. Mixed-mode fracture and mixed-mode crack growth are therefore to be considered.

In the 2015 Safety Case just like in the 2012 Safety Case, Electrabel makes use of the equivalent stress intensity factor K_{eq}, which is defined as

$$K_{eq} = \left[K_I^2 + K_{II}^2 + \frac{1}{1-\nu} K_{III}^2\right]^{1/2}$$

The equivalent stress intensity factor K_{eq} should be understood as the effective Mode I stress intensity factor.

When evaluating the 2015 Safety Case, Bel V paid more attention to the equivalent stress intensity factor K_{eq} used by Electrabel in the flaw acceptability assessment.

Under pure Mode I loading in a homogeneous isotropic material, propagation of a planar crack occurs in its own plane. Crack initiation occurs when $K_I \geq K_{Ic}$. Under mixed-mode loading, the values of K_I, K_{II}, and K_{IIi} can be calculated along the crack front but the issue is to define the critical combination of K_I, K_{II} and K_{III} that renders the crack locally unstable and forces it to propagate.

The main feature of the mixed-mode fracture is that the crack propagation does not occur in the same manner as in the mode I fracture. There are several fracture criteria defined in the literature, e.g., the *maximum (elastic) strain energy release rate* criterion and the *maximum hoop stress* criterion. Basically those mixed-mode fracture criteria predict propagation in the same direction. For a through-thickness (planar) crack in a plate oriented at a certain angle relative to the applied stress, textbooks provide the predicted crack propagation angle (the kink angle). Whatever the fracture criteria used, it is concluded that the equivalent stress intensity factor K_{eq} as defined by Electrabel corresponds to a situation where the plane in which the crack propagates is the original crack plane and it could be not conservative to prevent crack initiation. Otherwise stated, the combination of the K_I, K_{II} and K_{IIi} as made in the equivalent stress intensity factor K_{eq} may underestimate the crack driving force in mixed-mode condition.

The issue of the potential non-conservatism of the proposed equivalent stress intensity factor K_{eq} to prevent crack initiation of quasi-laminar flaws was discussed with Electrabel as well for the flaws evaluated by the linear-elastic fracture mechanics as for the shallow flaws evaluated by the elastic-plastic fracture mechanics. For the elastic mixed-mode fracture, Electrabel provided arguments justifying the validity of his proposed equivalent stress intensity factor K_{eq}. The most convincing argument is that in triaxial stress conditions the difference between the proposed equivalent stress intensity factor K_{eq} and the equivalent stress intensity factor obtained by applying the available mixed-mode fracture criteria is very low. For elastic-plastic mixed-mode fracture, the use of the proposed equivalent stress intensity factor K_{eq}, which has been determined for elastic mixed-mode fracture, is justified due to the small size of the plastic zone relative to the flake size.

Bel V did not raise any objection against those arguments and found acceptable the use of the proposed equivalent stress intensity factor K_{eq} for the flaw acceptability assessment.

9.3 Refined analyses

Refined analyses refer to those 3D analyses performed using the X-FEM method implemented in the MORFEO CRACK finite element computer code.

The qualification file of the MORFEO CRACK code has been reviewed by Bel V and found satisfactory for the specific application of the code in the Safety Case.

In the refined analyses, the flaws are modelled as elliptical planar flaws with the largest size and tilt angle compatible with the box bounded by the UT measurements. The model is a sector of the core shell containing the flaws to be assessed. Boundary conditions are applied to represent the pressure loading. The transient thermal analysis is not performed by the MORFEO CRACK computer code. Instead, the temperature distribution within the RPV wall thickness during a thermal transient is calculated using a 1-D finite element code and is used as an input for the MORFEO CRACK calculation. The potential effect of the flakes on the temperature distribution is therefore not taken into account.

For circumferential or axial planar flaws that are perpendicular to the RPV surface, it may be assumed without any significant loss of accuracy in the calculation that the temperature distribution is not affected by the presence of the flakes. For quasi-laminar flaws such as the flakes, their orientation with respect to the (radial) heat flux makes that the same conclusion might be invalid. Indeed, depending on their thermal resistance, the quasi-laminar flaws may act as thermal barriers. As a result, the temperature distribution in the wall thickness may depart from the normal one (i.e., without flaw) and the temperature gradient in the vicinity of the flaws may potentially be higher. This concern may be of importance for the 20mm thick zone beneath the cladding where the thermal stresses due to the small break LOCA contribute significantly to the stress intensity factor.

Postulating the thermal conductance of the flakes equal to the thermal conductance of the sound material is an idealization of the actual physical condition because the thermal contact resistance between the contacting surfaces of the flakes should at least be considered. In order to better substantiate the effect of the flakes on the thermal distribution, Bel V asked Electrabel to investigate the consequences of the other bounding assumption, i.e., no heat transfer thru the flakes, on the flaw assessment.

As an answer to the Bel V request, Electrabel provided the results of the finite-element MORFEO-CRACK analysis of a 2D model of a RPV sector. The model includes two closely-spaced flaws located close to the cladding. In addition to the pressure, the boundary conditions of the model include the small-break LOCA temperature transient at the inner surface. The model considers no heat transfer through the flakes by modelling the flakes with a small opening in the mesh. In order to assess the impact of the assumption of no-heat transfer through the flakes, a second analysis is performed on the same model, but assuming perfect heat transfer through the flakes and by using the temperature distribution calculated using a 1-D finite element code as an input for the MORFEO CRACK calculation, just like in the refined analysis. The results given by Electrabel are those corresponding to the time in the transient for which the margin to the acceptance criterion is the lowest. When compared to the case of perfect heat transfer through the flakes, the maximum stress intensity factor at the front of the flaws (assumed to have a tilt angle of 8°) is increased by about 10%. As expected, that increase is due to the local increase of the thermal gradient at the crack front and between the flaws. The impact on the flaw assessment is lower. Indeed, as the flaws act as thermal barriers, the temperature at the crack front is slightly increased and, as a result thereof, the material fracture toughness is slightly increased.

Recognizing (i) that the 2D model overestimates somewhat the perturbation to the temperature field and (ii) that, as a result of the radiation heat transfer, the actual heat transfer through the flakes has definitely a non-null value even if the flakes are open, Bel V concludes that the potential underestimation of the crack driving force due to the lower heat transfer through the flakes should have a low impact of the flaw assessment.

9.4 Results of the analyses

The flaw acceptance assessment shows that for the core shells of the Doel 3 RPV, 28 flaw configurations (0.25%) have a $2a/2a_{acc}$ ratio exceeding the screening criterion of 0.5, five of them having a $2a/2a_{acc}$ ratio exceeding 1.0. For the core shells of the Tihange 2 RPV, 9 flaw configurations (0.3%) have a $2a/2a_{acc}$ ratio exceeding the screening criterion of 0.5 and none of them has $2a/2a_{acc}$ ratio exceeding 1.0. At the exception of two flaw configurations (one in the Doel 3 RPV core shells and one in the Tihange 2 core shells), all flaw configurations with a $2a/2a_{acc}$ ratio exceeding 0.5 are groups of flaws. In the Doel 3 RPV core shells as in the Tihange 2 RPV core shells, the $2a/2a_{acc}$ ratio of the individual flaw with a $2a/2a_{acc}$ ratio exceeding 0.5 is not higher than 1.0. It should be noted that all the clad interface imperfection indications have a $2a/2a_{acc}$ ratio lower than 0.5.

Refined analyses were performed for the flaw configurations having a ratio $2a/2a_{acc}$ exceeding the screening criterion of 0.5. As a first result, those refined analyses show that all the flaws making part of the five group with a ratio $2a/2a_{acc}$ exceeding 1.0 have a ratio $2a/2a_{acc}$ much lower than 1 and are so acceptable according to the acceptance criteria of Section XI of the ASME B&PV Code. The $2a/2a_{acc}$ ratio of the two individual flaws with a $2a/2a_{acc}$ ratio exceeding 0.5 is reduced less significantly, as expected : the individual flaw in the Doel 3 RPV has a $2a/2a_{acc}$ ratio lowered to less than 0.5 while the $2a/2a_{acc}$ ratio of the individual flaw in the Tihange 2 RPV is slightly above 0.5.

Nevertheless the first objective of the refined analyses is to demonstrate (see Section 5) that most of the flaws have under the governing loading conditions a stress intensity factor lower than the lower shelf toughness of the material divided by the applicable safety coefficient in Section XI of the ASME B&PV Code. In order to achieve that objective, refined analysis should not be performed only on the flaws having a ratio $2a/2a_{acc}$ exceeding 0.5. Screening criterion was therefore developed by Electrabel to identify amongst the flaw configurations with ratio a $2a/2a_{acc}$ lower than 0.5 those that are the most penalizing, i.e., those that have the highest crack driving force. Then, for the selected most penalizing flaws, the maximum driving force ($K_{eq\ max}$) is calculated by performing refined analyses.

The results of all the refined analyses may be summarized as follows. All the flaws have a maximum driving force ($K_{eq\ max}$) lower than the lower shelf fracture toughness. For the Doel 3 RPV core shells, only one shallow flaw has a value of $K_{eq\ max}$ that exceeds $K_{Ic,lower\ shelf}/2^{½}$ and 8 non-shallow flaws have a value of $K_{eq\ max}$ that exceeds $K_{Ia,lower\ shelf}/10^{½}$. For the Tihange 2 RPV core shells, there are only two flaws that have a value of $K_{eq\ max}$ that exceeds $K_{Ia,lower\ shelf}/10^{½}$.

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For all those flaws having a value of $K_{eq\ max}$ that exceeds the applicable lower shelf toughness divided by the safety coefficient, Electrabel calculated the margin in RT_{NDT} considering the temperature dependence of K_{eq} and K_{Ic} or K_{Ia}. In the worst case, the margin in RT_{NDT} is equal to more than 100°C.

9.5 Evaluation of the results by Bel V

The calculation of the driving forces of the flaws is, to Bel V opinion, a key element in the demonstration of the acceptable impact of the flaking damage on the serviceability of the Doel 3 and Tihange 2 RPVs. While recognizing that the presence of flakes increases the risk of RPV failure from pre-existing crack-like defects, Bel V also considers that the low value of the stress intensity factors provides a convincing evidence of the low potential of RPV fracture from those defects.

10. Assessment of prevention against other failure modes

10.1 Fatigue crack growth evaluation

An update of the crack growth analysis performed in 2012 was deemed necessary to account for the new cartography of the flaw indications following the inspection performed in 2014 in conformity with the qualified UT inspection procedure.

The revised crack growth analysis is performed using the same approach as in 2012. The individual flaws or groups of flaws considered in the analysis are those of the flaw acceptance assessment (including the clad interface imperfection indications). In particular, when compared to the 2012 analysis, the grouping method using the revised proximity rules is used to define the groups of flaws. So, consistency with the flaw acceptance assessment is ensured.

Although the concern is mixed-mode crack growth, no use is made of the equivalent stress intensity factor K_{eq} (see Section 9). Indeed the range of applied stress intensity factor is calculated for the axial projection of the flaw configurations, so that only Mode I loading condition is to be considered. It was shown by Electrabel that using the stress intensity factor of the axial projection of the flaws instead of the equivalent stress intensity factor was conservative for the assessment of the fatigue crack growth.

The analysis shows that the calculated maximum crack growth for a lifetime of 40 years is 3.2% for the Doel 3 RPV core shells and 1.2% for the Tihange 2 RPV core shells, which is considered by Bel V as low but still significant. However, the crack growth analysis includes some identified conservatisms that lead to overestimations of the growth. By eliminating one of those, i.e., the use of the equivalent stress intensity factor of the quasi-laminar flaw instead of that of its axial projection, Electrabel showed that the growth of the flaw having the largest (calculated) growth decreased from 3.2% to 0.2%. This confirms the conclusions drawn in 2012 for the former flaw cartography that the fatigue crack growth under the service loadings for a service life of 40 years is not significant.

It should also be emphasized that the highest values of the calculated fatigue growth were obtained for the flakes of large size and it is known (see section 4) that those large flakes are sets of individual smaller neighbouring flakes either grouped by application of the proximity rules or merged because they could not be discriminated when using the qualified UT procedure. To Bel V opinion, that provides additional confidence in the non-significant fatigue growth of the flakes.

It should finally be mentioned that the non-significant fatigue growth of the flakes justifies the (implicit) assumption of not considering in-service growth of the flakes when applying the proximity rules.

10.2 Prevention of instantaneous failure

Ensuring the structural strength of a pressure component includes the prevention of the potential failure modes. One of the failure modes due to tensile stress considered in Section III of the ASME B&PV Code is the instantaneous failure under a single application of a load. Prevention of that failure mode with a given safety coefficient is ensured in Section III of the ASME B&PV Code by limiting the primary stresses.

To Bel V opinion, ensuring the acceptable serviceability of the Doel 3 and Tihange 2 RPVs requires that the hydrogen flaking damage of the RPV core shells does not affect significantly the capacity of the RPV of avoiding instantaneous failure as initially ensured for a flaw-free RPV or, at least, that prevention of instantaneous failure is ensured with the same safety coefficient as the one required by Section III of the ASME B&PV Code. By requiring the primary stress limits to be met in a pressure component containing flaw(s), IWB-3610(d)(2) in Section XI of the ASME B&PV Code recalls that, in presence of flaw(s), the fracture at a defect is not the only failure mode to be prevented. Even if the efforts required to perform the fracture mechanics evaluation of the Doel 3/Tihange 2 RPVs are much bigger than those for the verification of the primary stress limits, the importance of meeting the requirement of IWB-3610(d)(2) in the safety demonstration should be adequately considered.

Here also, an update of the analysis performed in the 2012 Safety Cases was deemed necessary to account for the new cartography of the flaw indications following the inspection performed in 2014 with the qualified UT inspection procedure. As already mentioned to Electrabel in 2012, Bel V considered that the strict application of IWB-3610(d)(2) was not straightforward due to the difficulties in determining the residual net section. So, to Bel V opinion, the use of a plastic analysis was recommended.

In lieu of the stress analysis (that, to Bel V opinion, is not an adequate tool in this case), Section III of the ASME B&PV Code allows the use of other methods to verify the objective of primary stress limitation, more specifically the limit analysis (NB-3228.1) and the plastic analysis (NB-3228.3). Section III of the ASME B&PV Code considers both analyses as equally acceptable for ensuring prevention of the instantaneous failure instead of verifying the primary stress limits. To Bel V understanding, those alternative methods have been written for component with sound material and their application to the Doel 3/Tihange 2 RPVs should be correctly assessed. In particular, to Bel V opinion, the appropriateness of the method with regard to the objective should be assessed, as well as the correctness of its application.

The primary stress limits of Section III of the ASME B&PV are intended to prevent two types of instantaneous failure: (i) the collapse failure, i.e., the loss of the load-carrying capacity of the pressure component (plastic instability) and (ii) the failure by excessive distortion. To Bel V understanding, the limit analysis is suited to the prevention of the collapse pressure while the plastic analysis is suited to the failure by excessive distortion. Bel V considers that the important issue to be assessed is the impact of the multiple cracking on the structural load carrying capacity of the RPV. So, to Bel V opinion, the limit analysis

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appears as the most suitable method to assess the capability of a cracked component to sustain the applied pressure load and so, to estimate the margin against the maximum load carrying capacity.

Nevertheless Electrabel, just like he did in the 2012 Safety Case, decided to use the plastic 2D analysis in conformity with NB-3213.25 and NB-3228.3 in Section III of the ASME B&PV Code. Despite his preference for the limit analysis, Bel V did not raise any objection against the use of the plastic analysis method since he considered the two alternative analyses should give quite similar results.

Bel V reviewed the plastic analyses and identified some issues that were resolved satisfactorily.

For the Doel 3 RPV, the updated plastic analysis taking into account the updated flaw description considers a 2-D model with 8 flaws having a cumulated projected length of 26.6mm on the Z-(radial) axis. The analysis allows to conclude that the presence of these 8 flaws decreases the plastic-analysis collapse load by 1.9% but the calculated collapse load of 26.1 MPa being 1.6% higher than 1.5 times the design pressure still allows to meet the criterion of NB-3228.3. For the Tihange 2 RPV, the calculated collapse load is 2.9% higher than 1.5 times the design pressure.

From those analyses, Bel V concludes that, although some part of the available margin is being consumed by the presence of flakes, hydrogen flaking has a low impact on the capacity of the RPV to prevent instantaneous failure under a single application of load and that the prevention is ensured with the safety coefficient required by Section III of the ASME B&PV Code. Without putting in question this conclusion, Bel V remarks that, although the required safety coefficient is satisfied, the confidence in the prevention of instantaneous failure with a low margin and no flakes has not the same value as the confidence with a still lower margin and with flakes.

10.3 Prevention of incremental collapse

The second failure mode due to tensile stress considered in Section III of the ASME B&PV code is the incremental failure by accumulation of plastic deformations under variable repeated or cyclic loadings. Indeed it is known that in the case of variable repeated or cyclic loads, not only low-cycle fatigue failure below the collapse load may occur but also an accumulation of plastic deformations may occur resulting in excessive distortion. Prevention of that failure mode is ensured in Section III of the ASME B&PV Code by the so-called $3S_m$ limit on stress range.

Bel V recognizes that the flaw acceptance assessment procedure set forth in Section XI of the ASME B&PV Code does not require the verification of the $3S_m$ limit on stress range. However, to Bel V opinion, the demonstration of the acceptable impact of hydrogen flaking on the serviceability of the Doel 3 and Tihange 2 RPVs requires the demonstration that the prevention against all the failure modes as ensured by the design assuming a defect-free material is not significantly affected by hydrogen flaking. Recognizing that the RPVs are subject to the action of varying mechanical and thermal loading during their whole lifetime, the impact of the flaking damage on the prevention of the incremental failure needs to be assessed. Here also the plastic analysis is a useful tool.

The shakedown behaviour is required by Section III of the ASME B&PV Code for preventing the incremental collapse by accumulation of plastic deformations under cycling loadings that have plastic effects. The shakedown takes place due to the development of (permanent) residual stresses which, imposed to the actual stresses shift them to purely elastic behaviour. The vessel shell exhibits *elastic shakedown* when, during the first cycle the unloading is found to be wholly elastic, i.e., no further plastic deformation occurs when the vessel is depressurized to zero. Shakedown by *adaptation* occurs when the plastic effects are restricted to the initial loading cycles and then they are followed by elastic behaviour.

Demonstration of the prevention of incremental collapse should consider the cyclic primary stresses (pressure transients) and the cyclic secondary stresses (temperature transients). However Bel V found acceptable to limit the analysis to the cyclic primary stresses.

It is known that for a defect free cylindrical shell having the geometry of the RPV core shells (outer to inner radius ratio equal to 1.1) subject to repeated pressure ramps from P=0 to a given pressure and then back to 0, the elastic-shakedown behaviour is ensured for a pressure exceeding the first yield pressure but lower than the plastic collapse pressure. Therefore, to Bel V understanding, the assessment of the potential effect of the flaking damage on the shakedown behaviour of the core shells needs to be made for pressure cycles with a maximum pressure of 1.5 times the design pressure (25.7 MPa), which is slightly lower than the calculated plastic collapse pressure (26.1 MPa). To this end, a plastic analysis using the same 2D models as those used for the verification of the prevention against instantaneous failure but loaded by pressure ramps needs to be performed. The objective of that assessment is twofold: (1) to show that elastic shakedown occurs everywhere in the core shell (at the exception of the local areas in close vicinity of the flakes) and more specifically at the inner radius and (2) to show that, in the close vicinity of the flakes that act as stress raisers, elastic shakedown or shakedown by adaptation occur.

Electrabel performed the analyses as asked for by Bel V for both the Doel 3 and Tihange 2 RPV core shells. The results show that elastic shakedown occurs not only at the inside surface of the vessel but also in the most stressed areas between the flakes.

From those analyses, Bel V concludes that there is no evidence on any impact of hydrogen flaking on the shakedown behaviour of the Doel 3 and Tihange 2 RPV core shells.

10.4 Pressure – temperature limits and overpressure protection

According to Electrabel, the current pressure-temperature limits defined for Doel 3 and Tihange 2 from the RT_{NDT} values determined in 2012 as a part of the former Safety Case are not to be updated, and a result thereof, the overpressure protection reports also remain valid. That conclusion is supported by the fact that the end-of-life RT_{NDT} values calculated at 1/4T and 3/4T in the core shells with the assumptions used in the 2015 Safety Case are lower than the values used in 2012 for the verification of the fracture toughness requirements of Appendix G to 10CFR50. The lower end-of-life RT_{NDT} values obtained in 2015 at the tip of the postulated flaws are lower than the values used in 2012 due to the lower values of the end-of-life fluence (38 years of operation instead of 40 years) but also to the fact that because of the additional shift of 50°C independent of the fluence, the predictive equations used in 2012 provide values of the RT_{NDT} shift higher than the ones obtained by the 2015 predictive equations for fluences lower than about 4 10^{19}n/cm² for Doel 3 and about 4.5 10^{19}n/cm² for Tihange 2.

Bel V agreed with the non-necessity of updating the current pressure-temperature limits.

10.5 Protection against pressurized thermal shocks

According to paragraph 50.61 of 10 CFR "*Fracture toughness requirements for protection against pressurized thermal shock events*" applicable to the Doel 3 and Tihange 2 nuclear power reactors, no assessment is to be performed if the RT_{NDT} evaluated at the end-of-life fluence for the RPV beltline materials does not exceed the PTS screening criterion. The PTS screening criterion is 132°C (270°F) for the forgings. For the Doel 3 RPV, the highest end-of-life RT_{NDT} as calculated by the predictive equation proposed by Electrabel is the one for the upper core shell and is equal to 115.2°C. For the Tihange 2 RPV, the highest end-of-life RT_{NDT} is equal to 116.3°C (upper core shell). As the highest end-of-life RT_{NDT} does not exceed the screening criterion as well for the Doel 3 RPV as for the Tihange 2 RPV, Electrabel concluded that the protection against pressurized thermal shock was satisfied.

Bel V agreed with that conclusion.

11. Action 15

11.1 Context

The demonstration of the structural strength of the RPVs affected by hydrogen flaking is an analytical demonstration. In 2012, in order to get high confidence in the analytical procedure, Bel V asked Electrabel to perform some experimental verification. A usual way for estimating experimentally the load carrying capacity of a structure is to determine the load-deflection diagram of the structure. As full-scale testing of large specimens under conditions representative of the RPV and its actual loading could not practically be conceived, the structural tests consisted in the tensile testing of unirradiated dia 25mm specimens with tilted flakes taken from the VB 395 shell. The tension tests performed on dia 25mm specimens were intended to simulate on "large-scale" specimens the behaviour of the RPV wall (affected by hydrogen flakes) under the circumferential axial loading due to internal pressure.

The "large-scale" test specimens were machined so that the flakes had a tilt angle of about 20° relative to the specimen axis. The "large-scale" testing program had three objectives. Two of those were met satisfactorily in 2013. The third objective under discussion here is related to the fracture behaviour of these specimens. That objective was not to validate the approach used by Electrabel to determine the fracture toughness curve K_{Ic} applicable to the RPV material affected by hydrogen flaking but to verify whether some results of the tests were not in conflict with this approach. More precisely, the objective of the tests was to verify whether the fracture behaviour of the specimens with tilted flakes could be predicted from the (shifted) fracture toughness curve applicable to the material of the AREVA shell VB 395 in the macro-segregated zones affected by hydrogen flaking. The objective would be met if (i) the stress intensification factor K_I at the tip of the governing flake under the applied load at initiation of the fracture exceeded the fracture toughness value given by the (shifted) fracture toughness curve and (ii) the fracture mode was conform to the fracture mode predicted by the shifted fracture toughness curve. Tests were performed at -80°C and 20°C. Only the tests performed at room temperature are discussed here.

By application of the ASME Code Case N-629, the reference temperature RT_{To} may be used as an alternative indexing reference temperature for the K_{Ic} and K_{Ia} curves in Appendix A and Appendix G to

Section XI of the ASME B&PV Code. In conformity with Code Case N-629, the reference temperature RT_{To} is higher than the Master Curve transition temperature T_o by 20°C (35°F). As a result thereof, the reference temperature RT_{To} of the VB 395 AREVA shell material in the ligaments between the flakes is equal to -84.5°C. Then the test temperature of 20°C is greater by about 105°C than the RT_{To} of the VB 395 material in the ligaments between flakes. Bel V concluded that the material of the specimens at the test temperature of 20°C was on the upper shelf of the (shifted) fracture toughness curve and therefore a fully upper shelf behaviour (fully ductile fracture mode) of the specimens was expected. So practically, the objective of the "large scale" test at 20°C would be met if the two following sub-objectives were met: (i) the stress intensification factor K_I at the tip of the governing flake under the applied load at initiation of the fracture exceeded 240 MPa $m^{1/2}$, which is the fracture toughness value in the upper shelf according to Appendix G to Section XI of the ASME B&PV Code and (ii) the fracture mode was fully ductile. To Bel V opinion, the second sub-objective (ii) is as important as the first one.

11.2 Test results

The testing of the dia 25mm specimens with tilted flakes occurred in 2013. The dia 25 mm specimens tested at 20°C did not exhibit pure ductile fracture but rather they experienced a limited crack extension of the governing flake that converted in brittle cleavage fracture. Later in 2014, elastic-plastic analyses using the X-FEM method implemented in the MORFEO CRACK computer code were performed to calculate the J integral along the crack front of the governing flakes of the two specimens for the maximum load preceding fracture initiation. For those calculations, a simplified geometry was used to model the flakes. For both specimens, the calculated maximum value of the stress intensity factor was found to exceed the upper shelf fracture toughness value of 240 MPa $m^{1/2}$.

To Bel V opinion, the observed fracture mode raised a major concern since it could possibly mean that the flaking damage had a weakening effect on the fracture behaviour of the material greater than the one determined by the adopted shift of the RT_{NDT} temperature.

In order to get a better insight into the results of the tests performed at 20°C on dia 25mm specimens with tilted flakes, Bel V recommended to perform the following additional tests:

(1) tensile testing of dia 25mm specimens with tilted flakes at a higher temperature in order to assess whether there was a temperature threshold above which purely ductile fracture occurs;
(2) tensile testing at 20°C of dia 25mm specimens without flakes but with a tilted EDM notch as a surrogate for the flakes. The objective of these tests was to assess whether the fracture mode of the specimens with tilted flakes tested at 20°C was due to the geometry of the specimens and their loading conditions or was due to the condition of the material when affected by hydrogen flaking.

Performing that additional large-scale testing was required under action 15bis. The following additional tests were performed in 2014:

(1) tensile testing at 100°C of two dia 25mm specimens with tilted flakes.
(2) tensile testing of four dia 25mm specimens (with tilted notches) taken from the top part of the AREVA VB 395 shell (not affected by hydrogen flaking), two of them being tested at 20°C and the two others at 100°C.

It should be noted that the reference temperature RT_{To} of the VB395 material at the top end of the forging as determined by Compact Tension (CT) toughness specimens was found to be -113.8°C, which is 29.3°C lower than the reference temperature RT_{To} of the VB395 material in the ligaments between the flakes (-84.5°C).

The results of the additional tests are as follows:

(1) the dia 25mm specimens with tilted flakes tested at 100°C experienced unstable ductile fracture;
(2) the dia 25mm specimens with a tilted EDM notch tested at 20°C and 100°C experienced stable ductile crack extension followed by a unstable ductile fracture.

11.3 Evaluation by Bel V

Numerous technical exchanges with Electrabel were hold to further analyse the results obtained in 2013 with due consideration of the additional information provided by the 2014 test program. To Bel V understanding, although recognizing the small number of tests performed at 100°C on specimens with flakes, those tests showed with a high confidence that the temperature of 100°C was beyond the temperature limit from which the fracture of the VB 395 material affected by hydrogen flaking could only occur by a ductile mode. Concerning the mixed-mode fracture of the two specimens with tilted flakes tested at 20°C while the specimens with a notch tested at the same temperature experienced a fully ductile fracture mode, complementary information was provided by Electrabel for supporting the statement that the mixed-mode fracture of the specimens with flakes could not be attributed to a flaking-induced weakening process.

Bel V recognizes that in the upper transition region where the brittle fracture initiation toughness K_{Ic} exceeds, but not excessively, the ductile fracture initiation toughness K_{Jc}, fracture occurs by a mixed-mode mechanism: cracking initiates and grows a small amount by a ductile mechanism (ductile crack extension) and then brittle fracture (cleavage) occurs. This behaviour is in accordance with the present understanding of the cleavage fracture mechanism. Cleavage initiates from the propagation of micro-cracks nucleated from microstructural features such as a carbide or inclusion. However the propagation of these micro-cracks requires not only that the principal stress is sufficiently high in a sufficient volume ahead of the (sharp) crack front (higher than the cleavage stress) but it also requires that the plastic strain is sufficient for allowing the nucleation of micro-cracks from microstructural features and that the stress triaxiality is sufficient for preventing the micro-cracks from blunting. When experiencing ductile extension, the sampled volume of material under stress in front of the crack is increased, which increases the probability of 'finding' microstructural features from which micro-cracks will be nucleated. Ductile crack extension has therefore the effect of increasing the probability of cleavage fracture.

When the temperature further increases, the brittle fracture initiation toughness K_{Ic} reaches so high values that cleavage fracture becomes impossible. The two following questions may then be raised: (1) can mixed-mode fracture occur at temperature exceeding the reference temperature RT_{To} of the material by about 100°C and (2) why do the specimens with a notch experience ductile fracture while the specimens with flakes experience mixed-mode fracture. As an answer to question (1), Electrabel provided results giving experimental evidence that mixed-mode fracture of fracture toughness specimens could occur at temperatures exceeding the Master Curve transition temperature *To* by more than 100°C. In particular, Electrabel reminded the results of a benchmark program performed on a German RPV steel (22NiMoCr37)

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similar to the VB 395 material (18MND5) and the Doel 3-Tihange 2 RPV material (ASME SA 508 Class 3). A large number of compact tension specimens tested at 20°C (i.e., 110°C above the Master Curve transition temperature *To* of the material) exhibited cleavage fracture after significant ductile crack extension.

Detailed 3D elastic-plastic calculations using the X-FEM method implemented in the MORFEO-CRACK finite element computer code were also performed by Electrabel to support the interpretation of the large-scale tests performed at 20°C. Two test specimens were analysed, one with tilted flakes and one with a notch. For each specimen analysed, two configurations of the governing flaw have been analysed: (i) the initial flaw geometry (flake or notch) and (ii) the initial flaw geometry with the addition of the ductile crack extension achieved before the last stage of fracture (cleavage for the flaked specimen and unstable (ductile) shear fracture for the notched specimen). Specific attention has been put to the accurate modelling of the flaws by using optical 3D micro-coordinate measurement of the fracture surface of the test specimens. For each specific flaw configuration the applied loads were the recorded tension load at start of crack extension for the configuration (i) and the recorded tension load at the initiation of the last fracture stage for configuration (ii). The various parameters calculated along the crack front were the principal stress, the triaxiality (calculated in the region neighbouring the crack front), the CTOD value and the J-integral. Although they shed an additional light on the interpretation of the tests, the calculations have not allowed to predict the fracture mode. In particular, for both specimens, the principal stress exceeded the cleavage stress all along the crack front as well for the initial flaw (i.e., before crack extension) as for the flaw configuration before the final fracture stage. Furthermore, assessment of the triaxiality factor before the final stage of rupture could be promising as it is known that triaxiality promotes cleavage. However the results showed that the high level of triaxiality was similar for both specimens. The only difference between the two specimens was that for the notched specimen, the CTOD calculated before the final stage of fracture was more than twice the one calculated for the flaked specimen. To Bel V understanding, since the CTOD measures the blunting of the crack it could be concluded that the flaked specimen was more prone to cleavage than the notched specimen since cleavage initiates ahead of a sharp crack.

An additional distinctive feature of the flaked specimen could explain its cleavage fracture. The examination of the fracture surface of the flaked and notched specimens allowed to identify a difference between their ductile crack front shapes. According to Electrabel, the tunnelling effect in the notched specimen indicated a less constrained crack front than in the flaked specimen. Therefore a larger volume was subjected to very high tensile stress ahead of the ductile crack front of the flaked specimen, which promoted the occurrence of cleavage fracture.

Based on the assessment of the tests performed at 20°C, Bel V concluded that there was no convincing evidence available in support of the possible weakening of the flaking damage on the fracture behaviour. In particular, those tests illustrated that the different result of the brittle versus ductile competition following ductile crack extension might be explained by other reasons than by a weakening of the fracture behaviour due to the flaking damage.

The assessment of the fracture behaviour of the dia 25mm flaked specimens tested at 20°C to determine whether or not it is conform with the expectations should consider not only the temperature difference between the test temperature and the Master Curve transition temperature *To* (or the reference temperature RT_{To}) of the VB 395 material but also the temperature difference between the test temperature and the temperature from which upper shelf behaviour is expected. With regard to that, the two following remarks should be made.

(1) There is a significant uncertainty associated to the determination of the Master Curve transition temperature *To* of the VB 395 material. Indeed the VB 395 material was shown to be macroscopically heterogeneous, which puts into question the validity of the application of the ASTM E1921 standard. Application of the bimodal master curve analysis to the three zones in the VB395 shell considered here (material between flakes, material at the front of the flakes, and material at top of forging) shows that, for the material in ligaments as well as for the material at top of forging, the two sub-populations identified by the analysis exhibit significantly different Master Curve transition temperatures. In particular, for the material in the ligament between the flakes, the two sub-populations are of equal size and their temperatures *To* differ by 35°C.
(2) If the onset of upper shelf temperature is defined as the temperature at which the brittle fracture initiation toughness K_{Ic} exceeds the ductile fracture initiation toughness K_{Jc}, it should be noted that the onset of upper shelf temperature depends not only on the brittle fracture properties of the material but also on the ductile fracture properties. Assuming two materials with the same brittle fracture properties, the material having a low ductile fracture resistance will show a lower onset of upper shelf temperature than the material having a high ductile fracture resistance.
Ductile tearing resistance tests performed at 20°C on the two specimens taken from the VB395 material between the flakes have shown a ductile fracture initiation toughness K_{Jc} exceeding 300 $MPa.m^{1/2}$. Considering that relatively high value of the ductile fracture initiation toughness K_{Jc} and the 50% fracture toughness Master Curve of the VB395 material between flakes, the temperature at which the brittle fracture initiation toughness K_{Ic} exceeds the ductile fracture initiation toughness K_{Jc} should be around -30°C, which is about 75°C higher than the *To* temperature (-104.5°C) or about 55°C higher than the RT_{To} reference temperature (-84.5°C). Considering the 5% fracture toughness Master Curve, that temperature would be about +10°C.

From the above Bel V concluded that for the material of the VB395 material between the flakes, the test temperature of 20°C is likely too low for ensuring at a high confidence level that a fully upper shelf behaviour is expected.

11.4 Conclusion

From the evaluation developed above, Bel V concluded that the large-scale tests performed at 20°C on dia 25mm specimens taken from the VB395 shell did not allow to evidence a fracture behaviour that could be affected by the flaking damage.

12. Mechanisms of in-service crack growth other than fatigue

The demonstration of the acceptable serviceability of the RPVs requires that there is no mechanism leading to in-service growth of the flakes. Indeed for the RPV that is assumed not to break, no in-service growth of the pre-existing crack-like defects is allowed. In the 2012 Safety Case, fatigue crack growth was recognized by Electrabel as the only mechanism that could be responsible for potential in-service growth of the flakes (see Section 10). In particular, growth of the flakes by hydrogen-induced-cracking had been rejected by

Electrabel as a potential in-service growth mechanism. Occurrence of this mechanism relies on the hypothesis that exposure to PWR primary water can lead to accumulation of molecular hydrogen (H_2) in the flakes and to resulting pressure build-up sufficiently high to produce defect growth.

Hydrogen blistering and hydrogen induced cracking are well-known phenomena which can lead to severe damage of a structure or even to its complete failure. It is especially known in the petro-chemical industry, which has to carry very harmful fluids with respect to these phenomena.

The phenomena of material damaging relevant to nuclear reactor pressure vessels have been extensively studied since a while. Up to now, these studies did not consider hydrogen blistering or hydrogen induced cracking as an issue for the reactor pressure vessel base metal. This is, amongst others, supported by the favourable return of experience with regard to these damaging mechanisms.

Nevertheless, the risk of hydrogen accumulation in flakes and their consequent growth has been specifically investigated by the licensee in the 2012 Safety Case. The calculations made by the licensee showed that the hydrogen concentrations coming from the different possible sources during operation were too low to cause any hydrogen damage. Bel V agreed with this conclusion.

More recently, following some public statements suggesting still a potential risk of crack propagation due to accumulation of molecular hydrogen inside the flakes, the FANC decided to set up an expert group (National Scientific Expert Group - NSEG) to investigate this specific question. Bel V participated at the meetings of the NSEG.

The arguments presented by the protagonists of the molecular hydrogen accumulation hypothesis did not convince Bel V, neither did they convince the international experts invited by the NSEG. On the contrary, it was concluded that the Doel 3 and Tihange 2 reactor pressure vessels are unlikely to suffer hydrogen induced cracking. As a matter of fact, the hydrogen pressure in cavities within the material cannot exceed the driving force of the dissolved hydrogen partial pressure from PWR primary water under stationary state conditions at normal operating temperatures, which is insignificant in the context of any hydrogen pressure induced defect growth mechanism. Also, the phenomenon of hydrogen super-saturation due to hydrogen in solution at high temperatures and then quenched rapidly to low temperatures would not be an issue, as the threshold hydrogen concentration for such delayed hydrogen cracking is well in excess of that which could be absorbed from primary water.

13. Compensatory measures

The assessment of a deviation, and even more a major deviation, includes usually the definition of compensatory measures.

At this point, Electrabel has proposed to perform at the end of the next fuel cycle a follow-up inspection of the Doel 3 and Tihange 2 RPVs with the qualified UT inspection procedure.

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14. Conclusion

The updated condition of the Doel 3 and Tihange 2 RPV core shells as revealed by the outputs of the 2014 UT inspection using the qualified procedure, together with the predicted irradiation embrittlement of the RPV material for use in the flaw assessment higher than initially assumed in 2012, result in a situation that requires a re-assessment.

In the safety demonstration of the nuclear power plants, the failure of the RPV is not assumed (break exclusion assumption) since no measures are foreseen at the third level of the Defense-in-Depth which could ensure accident mitigation in the case where RPV failure would occur. The application of the break exclusion assumption requires therefore to ensure the very low probability of PRV failure and this can only be achieved by stringent prevention measures taken at the first and second defense-in-depth levels.

Prevention of RPV failure is ensured by preventing the potential failure modes. One of those failure modes is the propagation of a pre-existing crack-like defect in either a brittle or ductile mode.

Prevention of failure by propagation of crack-like defects is normally based on:
(i) absence of crack-like defects at the end of the manufacturing process, confirmed by examinations during manufacture;
(ii) material toughness offering good resistance to propagation of crack-like defects;
(iii) absence of in-service sub-critical crack growth mechanisms that could lead to the increase in the size of pre-existing defects, confirmed by the in-service examination.

The measures under (i) and (II) above belong to the first level of defense-in-depth while the measure under (iii) belongs to the second level.

The presence of severe hydrogen flaking in the Doel 3 and Tihange 2 RPV core shells, which has not been detected during manufacture, is a major deviation from the requirement of having a material that meets the highest quality standard and affects the first level of defense-in-depth.

To Bel V opinion, the assessment of the RPVs as documented in the Electrabel assessment reports is in no way a substitute to the highest quality of the fabrication required for a component for which break exclusion is assumed but rather it aims at demonstrating the serviceability of the RPV as affected by hydrogen flaking.

The Electrabel assessment reports and their supporting analysis reports were evaluated by Bel V. Basically these assessments conclude that:
(i) the sizes of all the flakes are much lower than their acceptable size, i.e., the size which would cause crack initiation under the governing loading conditions. In particular, the flakes have a very low driving force, lower than the lower shelf fracture toughness of the ASME B&PV Code curve divided by the appropriate safety factor for most of them; the few other ones exhibit a significant margin in terms of RT_{NDT}.
(ii) flaking itself has no detrimental effect on the fracture toughness of the RPV material as well in unirradiated as in irradiated conditions. However the enhanced irradiation embrittlement evidenced in the flaked material of the VB 395 shell, considered as a material representative of the Doel 3 and Tihange 2

core shell flaked material in the 2012 Safety Case, raised a major concern that was not entirely resolved. Although there were some arguments supporting the assumption that the Doel 3 and Tihange 2 RPV core shell material should not experience such enhanced irradiation embrittlement, the predictive equation used in the flaw assessment includes an additional term corresponding to that embrittlement.
(iii) there are no potential in-service growth mechanisms for the flakes, at the exception of fatigue crack growth; the fatigue crack growth evaluation shows that the fatigue crack growth of the flakes under the service loads for the service life is not significant.
Bel V concluded that the Electrabel reports provide a convincing evidence of the low potential of RPV failure from the detected flakes and that the in-service fatigue growth of the flakes was not significant.

Those conclusions are supplemented by the following. Confidence in RPV assessment requires confidence in the capability of the UT examination procedure to detect and characterize the flakes. Bel V monitored the survey activities of AIB-Vinçotte related to the qualification of the procedure but also performed an in-depth review of the results of the UT inspection performed in 2014. Bel V did not find anything that could put into question the high efficiency of the UT examination procedure as demonstrated by the qualification. Confidence in the results of the flaw assessment also requires the availability of qualified calculation tools. In particular, Bel V reviewed the qualification file of the MORFEO CRACK finite element code used by Electrabel to perform the detailed 3D analyses of the flaws and concluded that the code was satisfactory for its intended use in the Safety Case.

Confidence in those conclusions requires also that each step of the assessment includes conservatism. Although it may not be quantified, Bel V recognizes that the analysis procedures include inherent conservatism even if some potential lacks of conservatism have been identified. Moreover the input data also include conservatism or at least consider satisfactorily the uncertainties associated to them or the necessary provisions to account for the limited knowledge of some phenomena.

Ensuring the serviceability of the RPV also requires the prevention of other basic failure modes under tensile stress, i.e., the instantaneous failure by single load application and the incremental failure by repeated loadings. When designing a pressure component assumed to be made from a defect-free material, prevention of those modes is ensured by the requirements of the design code. To Bel V opinion, prevention of those failure modes for a damaged component may only be ensured by performing plastic analyses. The analyses performed by Electrabel showed that, although some part of the available margin is consumed by the presence of flakes, the prevention of the instantaneous failure mode was ensured with the safety coefficient required by Section III of the ASME B&PV Code. It was also shown that the elastic shakedown behaviour of the RPV core shells, and so the prevention of the incremental failure mode, was not affected by the presence of flakes.

Finally it was shown by Electrabel for both the Doel 3 and Tihange 2 RPVs that it was not necessary to update the current pressure-temperature limits and also that the updated predictive equation for the irradiation embrittlement did not lead to exceed the Pressurized Thermal Shock screening criterion of 10 CFR 50.61.

Considering the information made available, in particular the Electrabel assessment reports and the supporting analysis reports, but also the current understanding of the involved phenomena, Bel V concludes that the flaking damage has been demonstrated satisfactorily to have an acceptable impact on the serviceability of the Doel 3 and Tihange 2 RPVs during normal, abnormal and accidental service conditions.

Bel V acknowledges the extensive efforts undertaken by Electrabel and his supporting entities for assessing the condition of the Doel 3 and Tihange 2 RPVs.