

Bridging the gap between metallurgy and fatigue reliability of hydraulic turbine runners

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Abstract. The failure of hydraulic turbine runners is a very rare event. Hence, in order to assess the reliability of these components, one cannot rely on statistical models based on the number of failures in a given population. However, as there is a limited number of degradation mechanisms involved, it is possible to use physically-based reliability models. Such models are more complicated but have the advantage of being able to account for physical parameters in the prediction of the evolution of runner degradation. They can therefore propose solutions to help improve reliability. With the use of such models, the effect of materials properties on runner reliability can easily be illustrated. This paper will present a brief review of the Kitagawa-Takahashi diagram that links the damage tolerance approach, based on fracture mechanics, to the stress or strain-life approaches. This diagram is at the centre of the reliability model used in this study. Using simplified response spectra obtained from on-site runner stress measurements, the paper will show how fatigue reliability is impacted by materials fatigue properties, namely fatigue crack propagation behaviour and fatigue limit obtained on S-N curves. It will also present a review of the most important microstructural features of 13%Cr-4%Ni stainless steels used for runner manufacturing and will review how they influence fatigue properties in an effort to bridge the gap between metallurgy and turbine runners reliability.

1. Introduction

Fatigue of turbine runners has received a lot of attention in the recent years. The renewed interest in this phenomenon is due to a combination of factors. First, power plants owners are asking for turbines having a longer lifespan. As blade fatigue cracking is one of the predominant degradation mechanisms, a rigorous exercise needs be done at the design stage to make sure the runner will endure its expected lifespan without cracking. Secondly, the improvements in numerical tools and the continuous demand for better turbine performances tend to push the materials to their limits or, in other words, ask for a better understanding of materials behaviour. Thirdly, the importance of cracking events is exacerbated by the pressure on the plant operator to increase the availability of turbine-generator units: in the past, repairs could be done recurrently during scheduled downtime, but as this downtime is now more and more limited, there is less “free” time to repair cracks. Other factors explaining the efforts put forth to better understand runner fatigue also include the change in turbine operation (e.g. increasing number of starts-stops and of power output variations) combined with the



possibility of prolonging the lifespan of the important fleet of turbines installed in the 70's and in the 80's that are now approaching their expected lifespan.

To assess turbine runners fatigue reliability, the starting point is to define a failure criterion. Because long cracks can develop in runners without incurring safety issues, the main concerns for turbine operators are repair cost and downtime. This lead us to define the failure criterion as the point where high frequency/low amplitude stress loads combine with low frequency/high amplitude stress loads to drive crack propagation [1]. Those high frequency/low amplitude loads are referred to as high cycle fatigue loads (HCF) as opposed to low cycle fatigue loads (LCF) with contain the low frequency/high amplitude loads. Given this failure criterion or limit state, we use the Kitagawa-Takahashi diagram [2] to quantify the probability of a given defect to cross the limit-state i.e. to propagate rapidly. This model was presented in details in previous publications [1, 3-5] and is briefly introduced in the following sections.

To correctly assess fatigue reliability with this physically-based model, three inputs are needed:

- the highest expected HCF load and its uncertainty;
- the largest expected flaw size and its uncertainty;
- the limit-state parameters and their uncertainties.

The limit state is in fact defined by two parameters: the fatigue limit found on the well-known S-N curves ($\Delta\sigma_0$) and the fatigue crack growth threshold as defined by linear elastic fracture mechanics (normally referred to as ΔK_{th}).

These last inputs i.e. $\Delta\sigma_0$ and ΔK_{th} are influenced by loading (stress ratio, overload, loading mode), environment, temperature, residual stress and by material properties. The first three parameters can seldom be controlled during manufacturing, while the last two (residual stress and material properties) can be improved by choosing an optimized alloy for runner fabrication, by improving manufacturing processes and even by an improved maintenance and repair strategy. This raises two questions:

- Can we actually improve fatigue reliability?
- Can we improve it through materials?

The objective of the present paper is to answer these two questions. To tackle the first question, a sensitivity study will be presented using the reliability model previously introduced. A simplified load spectrum will be used as an input and the limit state parameters will be modified to observe their effects are on the runner fatigue reliability. The second question will be addressed from a metallurgical standpoint. A review of the different microstructural features of the most commonly used alloy for runner fabrication, 13%Cr-4%Ni soft martensitic stainless steel CA6NM, its wrought equivalent AISI 415 and filler material AWS 410NiMo will be made. An insight on how these different features can affect fatigue properties will be discussed based on current work and past literature.

2. The relation between materials properties and fatigue reliability

2.1. The Kitagawa-Takahashi diagram and how it is related to materials properties

The Kitagawa-Takahashi diagram was first introduced in 1976 [2] to illustrate how the ΔK_{th} in high strength steel tends to decrease when crack length decreases. Plotting their results of short fatigue crack growth testing on a stress range ($\Delta\sigma$) versus crack length (a) diagram the authors found that the stress range threshold decreased when crack length was lower than 0.13mm and then reached a plateau (see Figure 1). This plateau corresponds to the fatigue limit ($\Delta\sigma_0$) determined by S-N curves.

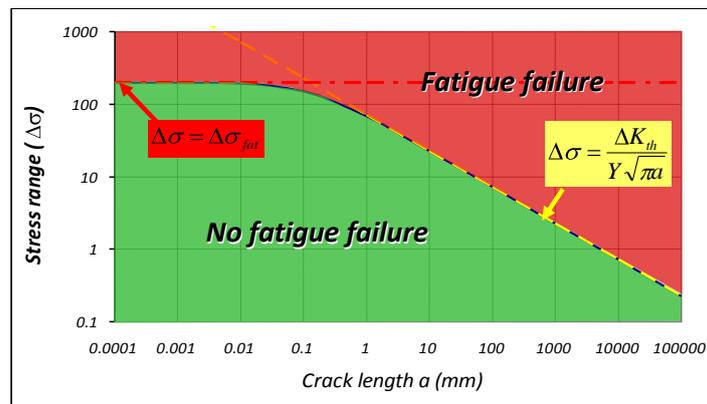


Figure 1. Kitagawa-Takahashi diagram example

This diagram clearly illustrates how classical approaches based on S-N curves can be linked to damage tolerance approaches based on fracture mechanics. It also shows what material properties have to be studied to correctly assess fatigue behaviour of structures: the fatigue resistance ($\Delta\sigma_0$) and most importantly the crack growth threshold (ΔK_{th}). This diagram can also be extended to include other parameters such as notch effect [6], multi-axial criteria [7] and residual stresses [8, 9]. In the following section we will use the probabilistic extension that we presented in previous publications [1, 3-5] and that is illustrated in Figure 2.

It is relevant to note that the point representing a giving defect and a given stress illustrated on the graph of Figure 2 evolves toward the right-hand side of the graph as low-cycle fatigue loads drive crack growth. The results crack propagation under LCF loads is that defects tend to increase over time. This is due to the fact that the LCF load amplitude are above the crack growth threshold and located in the Paris regime of the crack propagation curve i.e. in the stable crack growth regime. Hence, as the runner operates, its fatigue reliability will decrease as the probability distribution depicted in Figure 2 approaches the failure boundary. One conclusion that has to be underlined is that the reliability decreasing rate is mostly function of the LCF loads. The $\Delta\sigma_0$ also decreases with the number of HCF cycles which might also influence fatigue reliability decreasing rate in some instances. However, $\Delta\sigma_0$ will tend to stabilise after $1e7$ cycles.

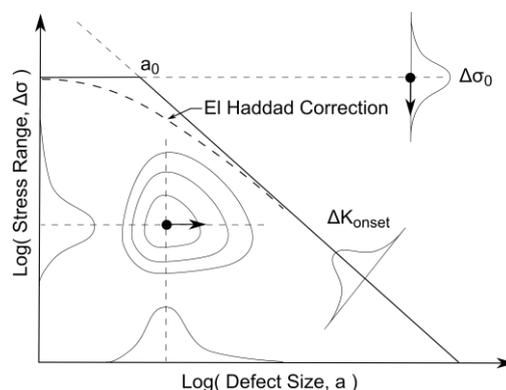


Figure 2. Probabilistic Kitagawa-Takahashi diagram

2.2. Sensitivity study

To illustrate the effect of $\Delta\sigma_0$ and ΔK_{th} on runner fatigue reliability, a simplified load spectrum representative of allowable stresses on a low head Francis turbine was used. This simplified spectrum

consists of superimposed high frequency HCF loads on a low frequency LCF load (see Figure 3). The inputs parameters used for this sensitivity study are detailed in Table 1.

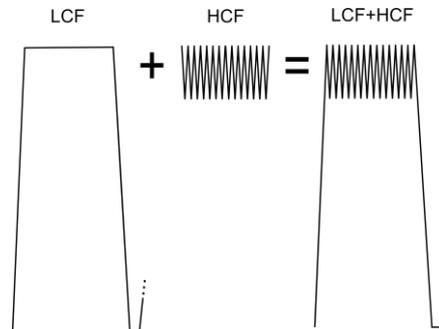


Figure 3. Simplified load spectrum

Table 1 Sensitivity study initial parameters

	Location	Scale	Distribution	Units
ΔK_{th}	2.0 ^a	-	-	MPa m ^{1/2}
$\Delta\sigma_0(N = 10^7)$	50	-	-	MPa
a	1.5 ^b	0.5	Gumbel	mm
σ_{LCF}	200.0	-	-	MPa
σ_{HCF}	20.0	1.0	Gumbel	MPa
N_{LCF}	1	-	-	day-1

^aCrack propagation data are taken from the British standard BS7910 [10]
^bThe defect is taken as a corner flaw

To quantify the effect of material properties on reliability, we first looked at the influence of ΔK_{th} . We used an initial value of $2 \text{ MPa}\sqrt{\text{m}}$, representing the proposed value in the British Standard BS7910 [10]. This lower bound value was then increased to $3 \text{ MPa}\sqrt{\text{m}}$ and then to $4 \text{ MPa}\sqrt{\text{m}}$, those two values representing realistic threshold values for 13%Cr-4%Ni steels [3, 11-14]. The resulting effect on reliability can be seen in Figure 4a. In this graph, the reliability index is the Hasofer-Lind reliability index (β) as described in [1]. The probability of failure (P_f) represents the probability of one blade having a crack. The overall probability (R) of a runner having no cracked blades can be approximated by:

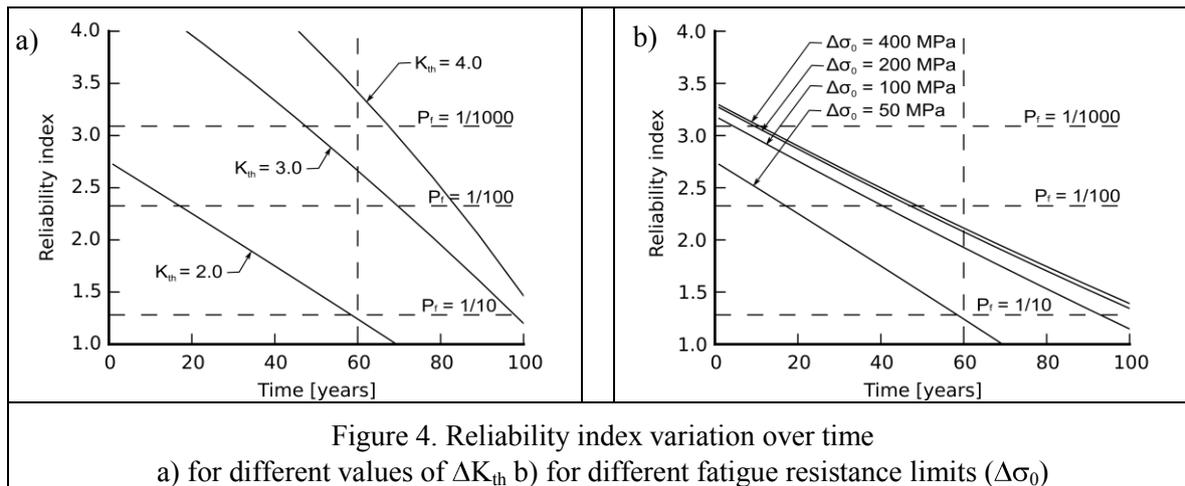
$$R = (1 - P_f)^n$$

$$P_f = \Phi(-\beta)$$

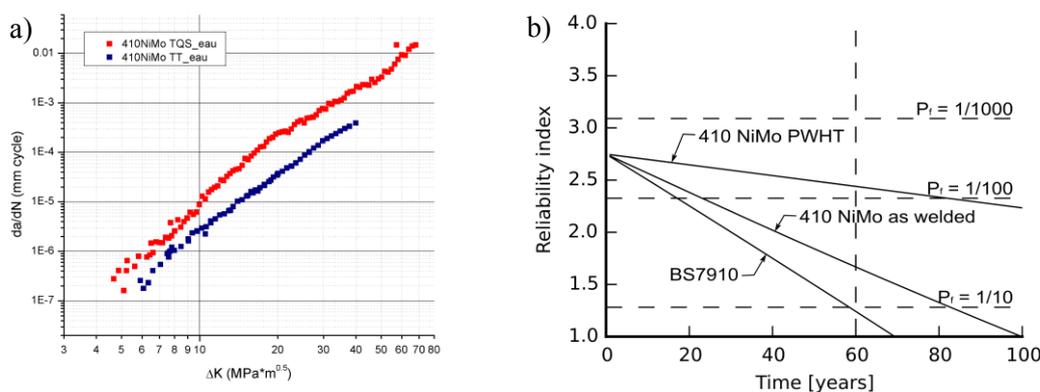
Where n is the number of blades of the runner and Φ is the standard cumulative function. We thus observe on Figure 4a that modifying the ΔK_{th} has an enormous importance on the initial reliability index i.e. the index determined at turbine commissioning (or whenever the first assessment is made). This index decreases with time, or more precisely with each LCF load. In this case, given enough time, the indexes for the different ΔK_{th} tend to merge. This however is only true after the expected lifespan of the turbine is spent (50 to 70 years). To quantify the effect of different ΔK_{th} on reliability over time, the probability of failure at 60 years can be computed and compared. We find that the probability of finding a crack on one blade (P_f) after 60 years is respectively around 1/10, 1/200 and 1/3000 when the ΔK_{th} is increased from $2 \text{ MPa}\sqrt{\text{m}}$ to $3 \text{ MPa}\sqrt{\text{m}}$ and to $4 \text{ MPa}\sqrt{\text{m}}$.

The same exercise was made for the fatigue endurance limit $\Delta\sigma_0$. The limit was changed from the initial value of 50MPa up to 400MPa to illustrate its effect on the reliability over time. These values

represent a realistic range of fatigue limits depending on the conditions and the security factor used for calculations [3]. The results are presented in Figure 4b. It can be seen that the curves are almost superimposed for the highest values of $\Delta\sigma_0$ namely 400MPa, 200MPa and 100MPa. Differences are however seen for 100MPa and the lowest value of 50MPa. If we compare the probability of failure after 60 years in operation we find P_f values between 1/30 and 1/60 for the three highest values of $\Delta\sigma_0$ and around 1/10 for $\Delta\sigma_0 = 50$ MPa. These results stress the importance of understanding the real conditions in which turbine runners operate to correctly set a realistic fatigue limit values.



Another property that can be modified is the fatigue crack growth rate in the Paris region i.e. in the stable crack growth regime of the crack growth curve. Changing this growth rate will not change the initial reliability index but will change the effect of the LCF loads, that is to say the rate at which this index decreases over time. Taking two values corresponding to actual crack propagation rates measured in labs for as-welded and post-weld heat treated (PWHT) 410NiMo, we can compare their effect on reliability to the initial curve calculated using the propagation curve found in the British Standards [10]. The propagation curves used for this comparison are presented in Figure 5a. The results of the reliability comparison are presented in Figure 5b. It can be seen that the observed differences in crack growth behaviour before and after post-weld heat treatment have an important effect on long time runners reliability. If we again compare the probability of finding a crack in one blade after 60 years of operation, we find a P_f value around 1/100 for the tempered alloy, around 1/20 for the as-welded alloy and 1/10 if the curve from the BS7910 standard is used. As can be seen in Figure 5b, these disparities will tend to increase over time.



3. The microstructural features of 13%Cr-4%Ni martensitic stainless steel and their potential effect on fatigue

3.1. Generalities

CA6NM has been used extensively for turbine runner manufacturing since its development in the sixties [15]. This low-carbon soft martensitic stainless steel has a high strength, good corrosion and cavitation resistance, and a high toughness. This cast alloy has replaced CA15 in many applications because it is easier to process in the foundry and because it has a better weldability [16]. This improved weldability is mainly due to the lower carbon content (0.06% instead of 0.15%). This lower carbon content is compensated by a higher nickel content to keep the Cr_{eq}/Ni_{eq} constant. The phase transformations occurring in this alloy when it solidifies and cools down can be illustrated by a pseudo-binary phase diagram (Cr-Ni)-Fe in which a Cr/Ni ratio of 3 is kept constant. The AISI 415 is essentially the same alloy as CA6NM but is produced by hot-rolling in the austenite stage. Most of the time, CA6NM and AISI 415 are welded using AWS 410NiMo which is a filler material having similar chemical composition and similar microstructural features.

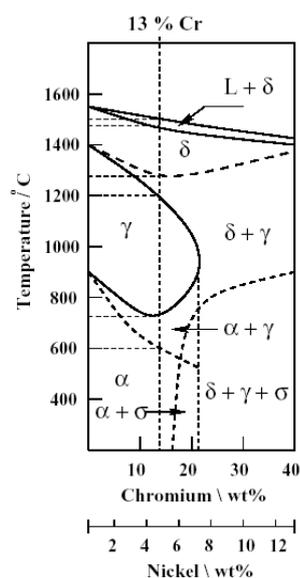


Figure 6. Pseudo-binary (Cr-Ni)-Fe phase diagram ($Cr/Ni = 3$). The dashed line at 13%Cr corresponds to the CA6NM, AISI415 and 410NiMo composition (from Folkhard [17])

At room temperature, the as-quenched alloy consists essentially of laths martensite but can also contain a small amount of δ -ferrite [18]. The cooling rate does not play an important role as it normally does with martensitic steel: only one phase forms for cooling time ranging from 20 seconds to 24 hours [19], so that thick sections can be air-cooled and still be fully martensitic. CA6NM is normally used in the tempered state. During tempering between 565°C and 620°C, some of the martensite transforms back to austenite. This austenite gets enriched in nickel and is thus stable when the alloy is cooled back to room temperature. Depending on tempering time and temperature and depending on the exact chemical composition of the alloy, as much as 25% reformed austenite can be found after tempering [18, 20, 21].

3.2. Grain size (prior austenite, martensite packets, etc)

As with any metallic alloy, one of the most important microstructural parameter that has to be investigated is the grain size. If the definition of a crystallographic grain is quite clear for austenitic or ferritic microstructure, it is more complex when it comes to martensitic structures. The grain can refer to many different microstructural features that are revealed with chemical or electrochemical etching (see Figure 7). The first one being the prior austenite grain (referred as parent austenite grain) which is

the grain forming from ferrite at high temperature (starting around 1300°C). From this parent grain, martensite laths nucleate and grow heterogeneously with particular orientations. The martensite laths can be regrouped by blocks that represent a group of laths having the same orientation and packets which are a group of laths having almost the same habit plane. So, a prior austenite grain can be divided in different packets that are themselves divided in different blocks of laths. [22, 23]

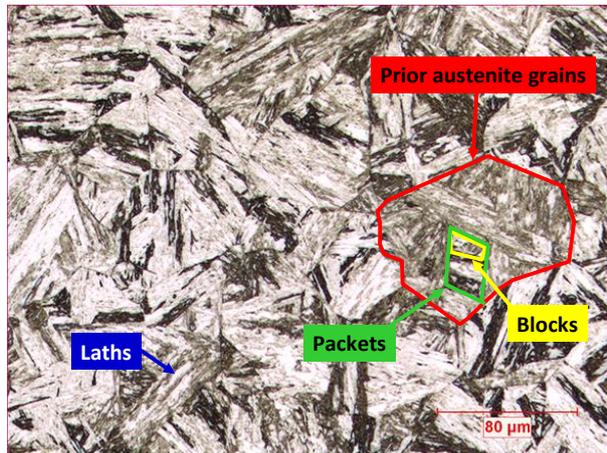


Figure 7. A typical tempered microstructure of 13%Cr-4%Ni stainless steel as seen by optical microscopy (500x)

These features are recognized as being directly related to the mechanical properties of martensitic structures [24-27]. But for fatigue crack growth properties, even if it is usually admitted that for austenitic, ferritic and pearlitic steels, larger grain sizes mean higher values of ΔK_{th} [28-32], this relation is not as clear for martensitic structures as different studies have come to contradictory conclusions [33-36]. For the 13%Cr-4%Ni specifically, Trudel et al. found that differences in crack growth rate between a tempered weld and its base metal at low and mid-range ΔK could partly be explained by crack path tortuosity that was due to the coarser microstructure of the base metal [37]. In a previous study, we observed by fractographic analyses that cracks tend to grow at packet boundaries in cast and in wrought 13%Cr-4%Ni stainless steel near ΔK_{th} [14]. So it seems that for 13%Cr-4%Ni, the martensite packets boundaries play a significant role in crack propagation, a role that needs to be better understood and quantified.

3.3. Reformed austenite

As was noted in the previous section, the as-quenched martensite in 13%Cr-4%Ni alloys transforms back to austenite during tempering. This reformed austenite grows by a diffusional process [38]. It is finely dispersed in the martensitic matrix and cannot be resolved by optical microscopy; it can however be observed by scanning electron microscopy (Figure 8). It is normally recognized that this reformed austenite found in tempered 13%Cr-4%Ni stainless steel helps to increase their Charpy impact resistance [39, 40]. This austenite is richer in Ni than the surrounding matrix [40, 41]. A part of this austenite is mechanically unstable and can transform to martensite during tensile and during impact tests [18, 39, 42]. Recent studies pointed out that the hardness of the martensite matrix could also play an important role in the stability of this austenite and hence on the mechanical properties [41] of these alloys. It has also been showed that this reformed austenite transforms during fatigue crack propagation at near threshold values [43]. The compressive stress resulting from the austenite to martensite transformation and the energy spent by this transformation could be thought to lower crack growth rate but the exact contribution of this transformation to the crack growth rate and to the ΔK_{th} has still to be investigated.

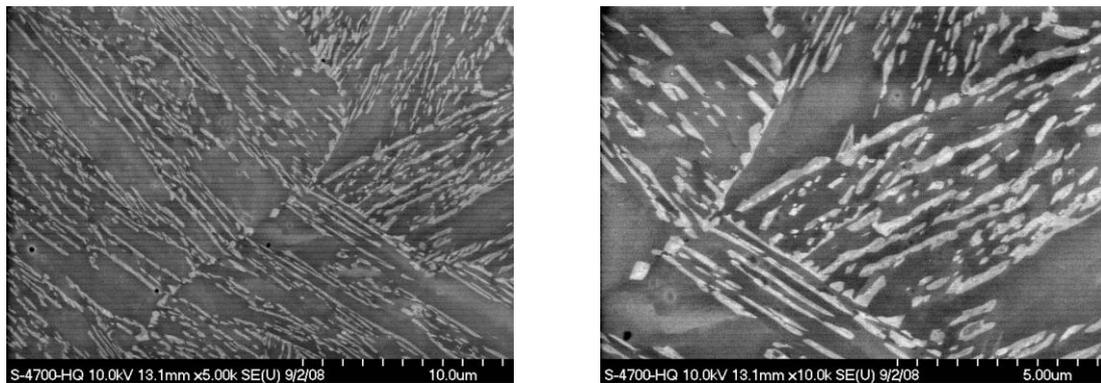


Figure 8. Reformed austenite lamellae as seen by scanning electron microscopy
a) 5000x b) 10 000x

3.4. δ ferrite

As pointed out previously, 13%Cr-4%Ni stainless steel oftentimes contain some ferrite at room temperature. The presence of this phase can be explained by inappropriate chemical compositions or, more frequently, by non-equilibrium solidification conditions. As solidification during welding is far from equilibrium conditions, welds made with 410NiMo often contain δ -ferrite. A recent study has shown that the presence of δ -ferrite can lower impact properties of 13%Cr-4%Ni CA6NM, mostly by increasing the ductile to brittle transition obtained by Charpy V-notch impact testing [44]. Carrouge et al. had previously come to the same conclusion when they tested a supermartensitic steel having a similar microstructure [45]. It seems that the effect of this phase on $\Delta\sigma_0$ or on ΔK_{th} have not yet been investigated in martensitic stainless steels.

3.5. Oxides and other inclusions

Some studies found that oxides and other inclusions have an adverse effect on fatigue crack propagation in different alloys [46]. While others found no effect on the threshold value but a detrimental effect on the fatigue limit [47]. This effect of inclusions on the fatigue limit is in fact generally accepted [48] and have been thoroughly investigated; complete books have been written on the contribution of inclusions on fatigue resistance (e.g [49]). Murakami et al. observed that the fatigue limit of bearing steel tended to increase as the size of inclusions decreased over the years due to better steelmaking practices [50]. For 13%Cr-4%Ni steels, silicon and aluminum complex oxides are found in different proportion and sizes. In 410NiMo welds, the inclusion type and size will depend on the welding process and procedures. The effects of these oxides on fatigue properties of 13%Cr-4%Ni stainless steel have yet to be investigated.

4. Conclusions

From the sensitivity analysis presented in this paper some conclusions can be drawn about the effect of materials properties on fatigue reliability of turbines:

- Both crack growth threshold and fatigue limit have to be considered to correctly assess the turbine runner reliability in fatigue.
- The crack growth rate has a big influence on the reliability decreasing rate over time.
- For the simplified spectrum used in this study, the most influential property is the crack growth threshold.

This exercise shows clearly how improvements on the materials could be beneficial to fatigue reliability. Increasing knowledge on this matter will also help to decrease the uncertainty and directly improve reliability assessment. The brief review of the most influential microstructural features of 13%Cr-4%Ni steels shows that a lot of research work has still to be done on this complex alloy to correctly assess the their relative importance. This has to be done in parallel and in cooperation with

research on load spectrum characterization and calculation and on non-destructive defects characterisation to efficiently progress toward better turbine reliability.

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