

Hot Corrosion Damage Modeling in Aero-Engines Based on Performance and Flight Mission

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Hot corrosion is a form of chemical damage that causes surface degradation, sound material loss and reduced component life. A lifing analysis in aero engines without considering hot corrosion can lead to unexpected damage findings and increased scrap rates due to blade thickness loss beyond repair. This paper presents a novel methodology to predict hot corrosion damage based on aero-engine performance and flight mission analysis while taking into account environmental exposure, fuel quality and material factors. The participating mechanisms, from salt and sulfur ingestion to deposition and hot corrosion attack, are discussed to explain the phenomenon in aero-engine components. In the investigated engine type, the first stage of the low-pressure turbine is the most affected. The application of the new methodology provides insights into the damage progression during the flight, the most affected components and the importance of capturing variations in the fuel quality, environmental exposure at the flight region and the thrust derate policy. For a representative 1500nmi mission, the variations in environmental exposure, fuel quality and derate policy within typical limits can result in up to +350% damage. The outputs of the new framework can inform the decision-making for Maintenance, Repair and Overhaul (MRO) contract costing and scheduling.

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Nomenclature

$\dot{m}$	=	mass flow rate [kg/s]
alt	=	altitude [ft]
C	=	concentration [$\mu\text{g}/\text{m}^3$]
d	=	damage [μm]
DPR	=	deposition rate [$\mu\text{g}/\text{cm}^2/\text{s}$]
h	=	heat transfer coefficient [$\text{W}/\text{m}^2/\text{K}$]
HCR	=	hot corrosion rate [$\text{mg}/\text{cm}^2/\text{s}$]
k_{ev}	=	evaporation rate [$\mu\text{g}/\text{cm}^2/\text{s}$]
m	=	mass [kg]
P	=	pressure [atm]
S_{f}	=	sulfur content in fuel
T	=	temperature [K]
t	=	time [sec]
ΔH	=	enthalpy change [cal/gr]
ρ	=	density [kg/m^3]

Subscripts

amb	=	ambient
b	=	blade
dep	=	deposit
g	=	gas
GL	=	ground level
s	=	surface
sat	=	saturation
SS	=	sea salt
stg	=	stage

I. Introduction

A. Motivation

Engine maintenance is essential for safety, component integrity and performance restoration, and it contributes significantly to life cycle costs. An informed maintenance schedule based on a combination of predictive methods and condition monitoring [1] can optimize fleet management by optimizing Time-On-Wing (TOW) and operating costs [1], [2]. Earlier maintenance philosophies were based on a “hard-time” engine removal which frequently lead to under-usage of component life, and increased ground time and costs. Disks and blades were designed using simple rules and had certified lives up to the first crack initiation. More recent practices have shifted towards maintenance actions based on condition monitoring and the predicted state of the engine components [3]. The state of the art is moving towards predictive digital twins that use a combination of real-time engine parameter monitoring, probabilistic analysis and deterministic physics-based models to predict engine condition and remaining life of each component based on the usage, flight profile and operating conditions of each engine number [4]. Modern fleet management takes into account not only safe operation, minimizing maintenance costs and grounding time, but also the fuel cost penalty due to engine degradation. State-of-the-art predictive lifing capabilities such as digital twins can benefit from damage models that go beyond creep, fatigue and oxidation and can capture more complex chemically induced mechanisms i.e. hot corrosion which is driven by the combination of the operating region, environmental exposure, fuel quality, flight path and operating conditions that are specific to each fleet or engine serial number for an even more customized prediction. Neglecting the effect of hot corrosion may lead to less accurate operational severity estimation of aero-engines and fleets that may have similar predicted levels of creep, fatigue and oxidation, but different hot corrosion exposure.

B. Hot Corrosion Background

Hot Corrosion (HC) is often defined as the accelerated oxidation/sulphidation in the presence of alkali salts in a sulfur-contaminated gas environment [5], and it is observed at high temperatures between 600°C and 1000°C. It is a form of environmental attack, usually in the presence of liquid salt deposits and is distinguished from erosion, which is a mechanical process that involves material removal by impingement of solid or liquid particles [6]. Gas-phase-induced hot corrosion [7] may also take place if no deposit can be condensed, but the most catastrophic form of hot corrosion happens in the presence of molten salt deposits. Two main types of hot corrosion have been identified, Type II (Low-Temperature Hot Corrosion) and Type I (High-Temperature Hot Corrosion).

Type II (Low-Temperature Hot Corrosion) usually takes place in the temperature range of 650°C - 800°C and Type I (High-Temperature Hot Corrosion) in the temperature range of 800°C - 950°C [8], although these boundaries should not be treated as hard limits. It is reported that the most catastrophic form of hot corrosion is when molten alkali sulfates are deposited, but a previous study by Lillerud et al. [9] detected sulphidation when corrosive species are in the gas phase and interact with the metal surface. Type I (High-Temperature Hot Corrosion) requires molten salt deposits that dissolve the protective oxide scale and then attack the substrate material. The mechanism can be acidic fluxing or basic fluxing, depending on high SO_3 partial pressure or high Na_2O activity in the melt, respectively. The reactions of Type II HC are activated by high SO_3 partial pressure and the lower-melting-point eutectics of Na_2SO_4 - NiSO_4 for the nickel phase [6] and Na_2SO_4 - CoSO_4 for the cobalt phase [8], with NiSO_4 (or CoSO_4 respectively) being a corrosion product [10].

Hot corrosion theory [11], [12], [13], [14], [15] describes the hot corrosion rate (HCR) over temperature qualitatively with diagrams similar to Fig. 1 with characteristic peaks near 700°C and 850°C. Sumner et al. [15] explained that the peaks of this curve derive from the balance between increasing reaction rates with increasing temperature and destabilization/evaporation of the deposits.

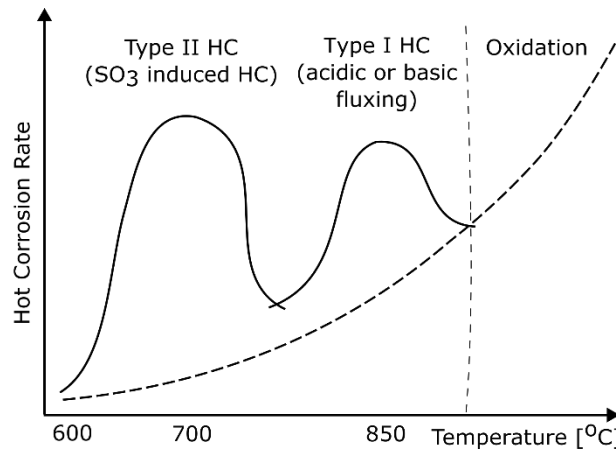


Fig. 1 Hot corrosion rate trend over temperature (qualitative)

C. Hot Corrosion Material Science Studies

There are several types of hot corrosion experimental studies in the literature on various materials, including steels, cobalt alloys, nickel alloys that may or may not have a coating and are used in gas turbine applications (e.g. power generation, marine) or other applications, e.g. boilers. Hot corrosion experimental studies can be focused on analyzing the composition of the exposed surface and the microstructure [6], [16], [17], [18], [19], correlating hot corrosion damage after exposure under a range of conditions in the form of a parabolic rate constant [20], [21], [22] or on

extracting semi-empirical hot corrosion models [23], [24], [25], [26], [27], [28] which fit the experimental results under a given set of conditions. In the context of this research, the most relevant type of experimental study is the latter because formulas that can be used to correlate engine operating conditions with the hot corrosion rate are required to perform a performance-based hot corrosion analysis under the highly varying conditions of aircraft engines.

The available experimental data in material science literature offer significant information about the hot corrosion performance of a specimen as a material itself but do not have sufficient links to aero-engine performance, operational aspects and overall engine lifing. In addition, each of the reviewed experimental models exhibits variability only in a part of the possible parameters. No experimental model offers generalization and sensitivity over the complete set of conditions (temperature, pressure, salt deposition rate (DPR), gas composition). Consequently, some modeling limitations are faced in the present work.

D. State of the Art in Hot Corrosion Life Prediction Based on Operational Parameters

Apart from the hot corrosion experiments that modeled the phenomenon from the perspective of material science using some typical boundary conditions, there are very few studies in the literature that connected hot corrosion damage with operational parameters in the highly varying aero-engine conditions. Lacaille et al. [29] developed a probabilistic framework using neural networks to predict the probability of cumulative hot corrosion wear on the turbine stators of a turbofan engine. They used real flight information (duration, environmental exposure, number of flights) for each Engine Serial Number along with accumulative wear based on inspection findings. This approach incorporates a few macroscopic operational parameters and is mainly data-driven with no physical interpretation of the damage based on engine/aircraft characteristics and operating conditions. DARWIN [30] is a probabilistic life prediction code for fatigue crack growth and has incorporated HOTPITS, a hot corrosion prediction methodology [31], in order to include the effect of hot corrosion pits nucleation on crack coalescence and ultimately calculate a probabilistic fatigue life. The hot corrosion methodology of HOTPITS consists of a deposition rate formula which is a function of Reynolds number, fuel flow, time, a rate constant and the concentrations of Na and K impurities in the fuel and air. It was assumed that there is always excess of sulfur to allow full sulphation of Na and K and full conversion is always completed. The applicability was limited to stationary, industrial gas turbines and conditions were provided as boundary conditions to the model.

Hot corrosion is broadly believed to affect industrial gas turbines more than aero-engines [32] because of their continuous exposure to an industrial, contaminated environment, so there have been more efforts in literature to capture industrial gas turbine hot corrosion than aero-engine hot corrosion, however, the challenges for predicting aircraft engine hot corrosion may be greater due to the highly varying operating conditions, missions and regions.

E. Performance-Based Gas Turbine Lifting Studies

Tinga et al. [33] presented an integrated life modeling approach by using monitored flight data to inform an offline engine performance model, which is then coupled with a thermal model and a stress analysis. They investigated the effect of High-Pressure Turbine (HPT) deterioration on the Turbine Entry Temperature (TET) and creep strain at the first stage (stg1) blade of the Low-Pressure Turbine (LPT). Mohamed et al. [34] correlated power demand with HPT blade creep life through the effect of TET on the Creep Factor. Eshati et al. [35] studied the effect of humidity on the remaining creep life of HPT blades under a range of TETs by considering the change in heat transfer to the blade due to the water presence in the flow and they identified that a higher Water to Air Ratio (WAR) is beneficial to creep life. Hanumanthan et al. [36] estimated the operational severity factor of HPT blades for different take-off and climb derates, as well as ambient temperature. The considered damage mechanisms included low cycle fatigue, creep and oxidation. Abu et al. [37] applied the Neu/Sehitoglu model for thermo-mechanical fatigue life prediction of a high-pressure turbine blade. Mishra et al. [38] investigated engine life consumption using HPT blade and disc severity factor, which consisted of creep, oxidation and low cycle fatigue. Ogiriki et al. [39] modeled the effect of the Thermal Barrier Coating (TBC) failure due to oxidation on the creep life of an aero-derivative HPT by considering the increase in blade metal temperature after the TBC failure. In another study, the same authors [40] also investigated the combined interaction of fouling, TBC degradation and cooling hole blockage with the creep life of HPT blades. Haslam et al. [41] calculated the operational severity for an HPT blade during a short-haul flight based on thermo-mechanical fatigue using the Neu/Sehitoglu model for different take-off derates and ambient temperatures. Shi et al. [42] performed fatigue testing and projected the experimental findings on the remaining flights until failure due to creep and fatigue interaction. Zhou et al. [43] calculated the combined effect of creep and fatigue on the first turbine stage of a power-generation gas turbine engine using the Larson-Miller Parameter and stress-life curve for the two damage mechanisms, respectively. Reyhani et al. [44] performed a sensitivity analysis of the relative life factor of an industrial gas turbine blade to changes in the coating thickness, power load and coolant inlet temperature and pressure, and considered the effect of creep and fatigue using the Larson-Miller Parameter and Coffin-Manson equation

respectively. Alozie et al. [45] translated the effect of fouling and erosion on an HPT to an equivalent time that the engine would have to operate in a reference mission to suffer the same degradation as when exposed to desert sand.

Severity is a measure of engine life consumption [36], and it is defined as the ratio of total damage due to a given flight profile to the total damage of a reference profile [41]. The concept of expressing damage due to one or more damage mechanisms among fatigue, oxidation and creep in a relative factor has been used in many studies ([36], [38], [41], [44], [46], [47], [48]) for different combinations of damage mechanisms but none of them considered hot corrosion.

F. Research Gap

The studies reviewed in Section E are deterministic lifing methods based on gas turbine performance, the experimental studies reviewed in Section C tested material specimens under hot corrosion conditions in a controlled lab environment disconnected from the aircraft operation while frameworks in Section D focus on probabilistic and data-driven approaches to capture hot corrosion effect or considered only stationary gas turbines which have less variability in the operation, environment and fuel quality than aero-engines. The present work offers a deterministic, physics-based hot corrosion framework that aims to describe and interpret the phenomenon from the perspective of aero-engine performance and lifing, while incorporating information from experimental material science that predicts the damage with boundary conditions and inputs at material level which were previously isolated from the wider ecosystem of aircraft/engine operation. The presented methodology does not replace experimental hot corrosion testing, but informs its design and conditions, makes use of its outcome and connects it to the wider ecosystem of aero-engine operation and performance. The aim of this methodology is to predict and explain hot corrosion damage accumulation while capturing the effect of different thrust settings, ambient conditions, take-off and cruise altitudes, payloads, ranges, degraded engine conditions, environments, fuel qualities etc.

G. Heat Transfer and Thermal Analysis Requirements for Aero-Engine Lifing

Material lifetime models focus on the material itself and are based on testing performed on specimens to understand the material behaviour and characterize it under controlled lab conditions. Therefore, material models such as hot corrosion experimental models are typically produced under thermal equilibrium in isolation from the wider ecosystem of aero-engine operation. To move from material-level models to the complex operating environment and life

consumption mechanism of an aero-engine component a series of thermodynamic and aerothermal effects need to be accounted for:

- **Flight mission and aircraft performance**

The thermal conditions of the aero engine components continuously change across the flight phases based on the thrust requirement which is linked with the aircraft weight, and the ambient conditions. The altitude and weather conditions, usually defined as a deviation from the International Standard Atmosphere conditions [49], affect the air temperature and, hence, the air density. Hotter ambient temperature and lower air density reduce the drag of the aircraft but also deteriorate the performance of the gas turbine resulting in higher operating temperatures and increased fuel consumption.

- **Thermodynamic cycle and gas turbine performance**

Aero gas turbine engines operate on an open Brayton cycle. Aero gas turbines need to deliver the thrust required by the aircraft at each segment of the flight mission. The thrust's main component is the change in the momentum of the air passing through the engine which creates a propulsive force, and the secondary component is due to the pressure difference between the exhaust air and ambient air. To calculate the air mass flow and the air properties at each component and at the exhaust nozzle a combination of thermodynamic processes, mass conservation and energy conservation equations needs to be resolved over the flight segments [49], [50], [51], [52].

- **Heat transfer and cooling effects**

The thermodynamic cycle calculation only provides the gas path properties at each component but does not give direct information about the components performing the work or the state of their material. For this reason, a thermal analysis is required to model the heat transfer effects from the gas to the component and within the component, and ultimately estimate the temperature of the component's material. In the case of turbine blades with internal cooling, there is one more interface i.e. between the internal blade wall and the cooling channel, and the heat transfer analysis can be transformed into a blade cooling model. In summary, there is convective heat transfer between the gas and the blade, conductive heat transfer through the thermal barrier coating and blade material and, in the case of internally cooled blade, convective heat transfer from the blade to the coolant air in the cooling passages to reject part of the heat. The heat transfer and blade temperature problem has been presented with solutions of various levels of fidelity and geometric detail for different purposes. Thermal analysis methods can be classified based on four main criteria:

1. Fidelity: full 3D numerical analysis [53], [54], [55], [56], analysis with reduced directions of heat transfer (that can be either numerical or analytical) [57], [58], [59], [60], [61], [62], and analytical methods [61], [63]
2. Timescale: steady-state or transient [64], [65], [66]
3. Geometric complexity: full 3D geometry [33], [44] or generic, simplified representation of a component [37], [62], [67] or local area (ex. cooling hole, cooling channel ribs) [53], [54], [55], [56]
4. Solid-fluid interaction: conjugate heat transfer with coupled gas-solid interaction [44], [56], [68], [69], [70], [71], [72], [73], or isolated gas/solid [33], [74], [75] thermal analysis while providing boundary conditions at the interface.

Three-dimensional thermal analysis using numerical methods considers the heat transfer in three directions through finite element faces and is usually used for the detailed design and analysis of specific geometric characteristics of the system (ex. internal cooling passages, ribs, holes for film cooling injection, thermal barrier coating) and when component geometric details are available or need to be defined as an output to inform the design. Moving towards two and one dimensions for the direction of the heat transfer and simplified blade and channel representations reduces the complexity and computational time while allowing to capture the relative effect of changing the operating conditions and flow properties. Transient or pseudo-steady state thermal analyses are adopted based on the phenomena and timescales that need to be captured. Common uses of transient thermal analysis are to predict thermal stresses [65], the heat soakage and the thermal expansion effect on tip clearances and during transient operation [66], [67]. Based on the objective and available information, the approaches for the heat transfer analysis can have a different combination of the above criteria.

- **Degradation**

Over thousands of hours of service, surface quality deterioration (roughness), geometry deformation and increase in tip clearances degrade the component efficiencies [76], [77]. This performance degradation results in an increase in the operating temperatures to produce an equal amount of thrust augmenting the heat transfer and thermally-driven damage mechanisms which further increases the operating temperatures and therefore the blade temperature.

- **Saturation point**

In the case of hot corrosion damage which is driven by the presence of corrosive species and is augmented by liquid deposit formation on the component surface, the phase change effect and, therefore, the saturation point of the corrosive species become of interest. The need for calculating the dew points of sodium sulphate and potassium

sulphate under varying pressures has also been highlighted by Sumner et al. [37]. The saturation point is defined by a temperature and pressure, and for a given pressure the temperature is often called dew point. Jacobson [78] calculated the effect of pressure, Na concentration and sulphur concentration on the dew point of Na_2SO_4 using NASA's chemical equilibrium code and proposed that the upper bound for hot corrosion to occur is the dew point. Within the investigated range of conditions, the variation in the dew point could be as high as $\sim 500\text{K}$. An alternative approach for calculating the saturation temperature is to use the Antoine equation [79] which is an extension of the Clausius-Clapeyron equation [80].

- **Aircraft and fleet level**

Material models do not account for the above thermal and thermodynamic effects, therefore, the present work provides a methodology so that propulsion engineers can take advantage of material models to draw conclusions from engine performance and flight data about the hot corrosion damage rate. Lifting methodologies, such as the presented hot corrosion methodology, usually aim to capture aircraft operation and whole engine effects with the ultimate goal of being applied to fleets to estimate the maintenance requirements. For such purposes, lower fidelity heat transfer and blade temperature methods are usually preferred as they can be coupled with the complete mission analysis and gas turbine performance for thousands of flight hours and multiple engines without excessively increasing the computational time and complexity.

II. Methodology

A. Considerations for Aero-Engine Hot Corrosion Analysis

To be able to capture hot corrosion in the context of aero-engines, the knowledge of a material's behavior under lab-controlled conditions must be linked with aircraft engine operation:

- Flight mission specifications (e.g., payload and aircraft weight, ambient conditions, thrust derate) indirectly influence hot corrosion by altering the take-off time and RoC which in turn affect the time that each component is exposed to specific temperatures, pressures, Fuel to Air Ratio (FAR) and air quality.
- The combination of ambient conditions and thrust level at each time step influences the operating temperatures and pressures at component level as well as the FAR which is associated with the fuel-induced sulfur impurities.

- The local component conditions which are derived from the gas turbine performance analysis (P, T, V, FAR) affect the gas composition, the deposition rate, and the hot corrosion rate.
- The operating region and the regional environment determine the level of exposure to airborne sea salt and pollution. The level of environmental exposure is also linked with the amount of time the engine spends at each altitude which depends on the take-off derate, climb derate, range etc.

B. Hot Corrosion Modeling Framework

A modular framework was developed (Fig. 2) to capture the complexity and multidisciplinary nature of hot corrosion in aircraft engines and model the sequence of processes. The framework is divided into five modules, each dedicated to modeling a specific aspect of the hot corrosion phenomenon.

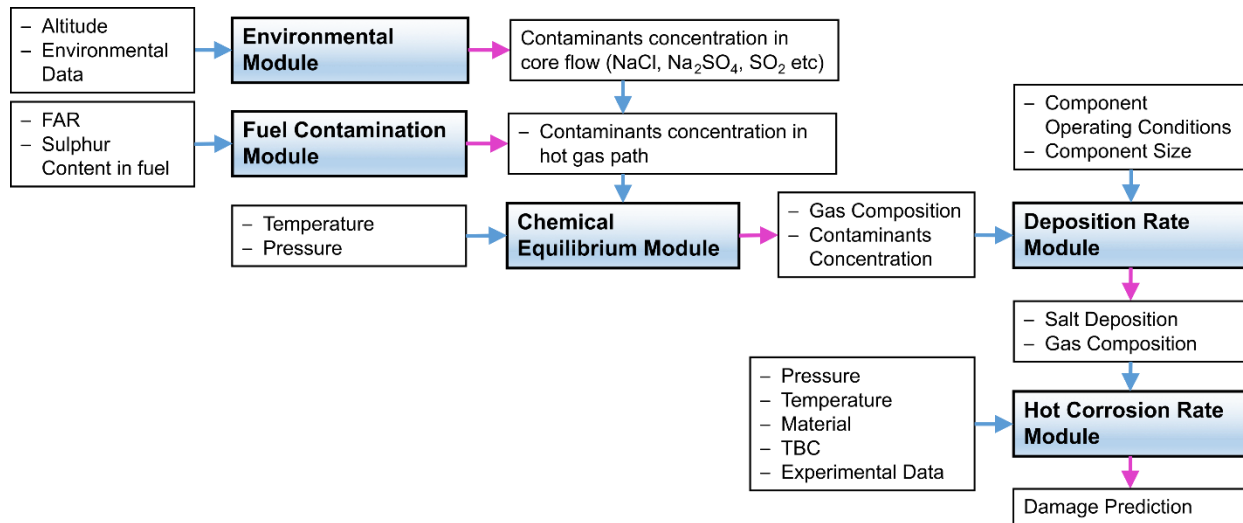


Fig. 2 Hot corrosion framework outline

The methodology describes and captures the sequence of acting mechanisms, from the aircraft flight path and ingestion of contaminants to fuel injection, chemical transformations, engine operating conditions, deposition/condensation of salt and hot corrosion rate at the component based on experimental data. The environmental module receives inputs from the flight mission analysis (altitude, air mass flow, ambient temperature) and air quality data at ground level to calculate the concentration of the contaminants in the core flow at each operating point. The considered air-induced contaminants are airborne sea salt, sulfur dioxide and particulate matter. The fuel module adds to the core flow the impurities that enter the flow in the combustion chamber due to the fuel injection and change the species concentrations in the flow. Subsequently, the chemical equilibrium module addresses the chemical transformations that the initially induced species undergo. Under the effect of high temperatures and

pressures, the impurities react with each other or with the air compounds to form new species. The dominant chemical reaction mechanisms that provide the species associated with hot corrosion are the formation of SO_x during the combustion of the sulfur-containing jet fuel and the formation of Na₂SO₄ through the reaction of NaCl with SO_x [81]. In addition, the formation of HCl and H₂O is included in the chemical modeling as they are also inputs in the selected hot corrosion rate model. In this framework, it will be considered that local equilibrium is achieved in order to reduce the complexity of fully simulating the reaction kinetics.

After calculating the gas composition with the chemical equilibrium module, the deposition rate module uses the concentration of Na₂SO₄ and pressure and checks if the temperature near the component surface is above or below the saturation temperature of Na₂SO₄. If the temperature is below the saturation temperature, a deposition rate model from literature based on the diffusion coefficient calculates the Na₂SO₄ mass transfer onto the component surface. Furthermore, if the temperature is above the saturation temperature and a salt layer is already formed, an evaporation model is applied to calculate the remaining salt deposit on the surface. Regarding the HPT, which is coated with a TBC, the porosity factor is also considered in the calculation. Apart from offering thermal insulation, the ceramic top layer of the TBC also acts as a physical barrier and is expected to prevent some of the deposited salt to reach the bond coat and the substrate.

Finally, the calculated deposition rate and the gas composition serve as input to the hot corrosion rate module where the selected experimental hot corrosion rate model is used. The original experimental model is sensitive to deposition rate, HCl, SO_x and H₂O concentration, but it did not demonstrate any sensitivity to varying temperature because this experiment was performed under the constant temperature of 700°C. In order to compensate for the lack of sensitivity to temperature changes, a temperature scaling factor was introduced, based on another experiment on the same material but with varying temperatures. After the hot corrosion rate is obtained at each operating point, the accumulative damage at the end of the flight cycle is calculated.

C. Engine Performance and Flight Mission Analysis

The engine performance is calculated using Turbomatch, an in-house gas turbine performance simulation tool and the flight mission analysis is Hermes, an in-house aircraft performance tool which is described in [82]. Turbomatch is a 0-D performance simulation code, featuring off-design and transient simulation as well [50]. It comprises several pre-programmed modules, known as “Bricks”, which correspond to models of individual gas turbine components. They can be called up to simulate the action of the different components of the engine, resulting finally in outputs of

engine thrust or power, specific fuel consumption, etc. Its modularity, which is supported by the implementation of generic component maps, enables the detailed design of any gas turbine configuration [83].

A typical single-aisle medium-haul aircraft model and a two-spool turbofan engine model rated at 118.8kN with 1 HPT stage and 4 LPT stages are used in the flight mission analysis to demonstrate the framework and the hot corrosion effects that can be captured. The mission used to demonstrate the new framework is a typical 1500nmi mission with a 12000 kg payload and cruise at 35000ft.

III. Hot Corrosion Modeling

In this chapter, a description of the modeling process is provided, as organized in the five modules of Fig. 2.

A. Environmental Module

In this framework, the primary species participating in the hot corrosion mechanism are modeled: sea salt, particulate matter (sulfate), and sulfur dioxide. It should be noted that there might be an overlap between particulate matter and sea salt in the data in the literature. However, when there is not much detailed information, sea salt and sulfate in the particulate matter will be treated independently. Concentration is usually defined in air quality literature as $\mu\text{g}_{\text{pollutant}}/\text{m}^3$. Ground-level concentration is usually known from air quality data sources because it is measured by instrumentation at measurement stations.

At higher altitudes and further away from the emission sources, the concentration gradually drops as a result of vertical dispersion and mixing phenomena [84], [85]. Sophisticated atmospheric models in literature can take into account wind and dispersion in all directions, starting from a source of pollution. However, for the purposes of aircraft engine lifing, in the context of which whole fleets need to be assessed for thousands of flights, such complex and detailed atmospheric modeling methods could be too expensive computationally and out of scope. A more macroscopic approach is selected in the present framework, although the modular framework design can integrate inputs from more detailed atmospheric models and databases, if available.

An empirical exponential formula (Eq. (1)) is proposed for the concentration drop with an increase in altitude to represent the exponential trend found in atmospheric studies [86], [87].

$$C_i(\text{alt}) = C_{i\text{GL}} \cdot (1 - a \cdot [\text{alt} - \text{alt}_{\text{GL}}])^n [\mu\text{g}/\text{m}^3] \quad (1)$$

alt is the altitude where the concentration needs to be calculated, $C_{i,GL}$ is the concentration at the ground level where the concentration is known, $C_{i,GL}$ is the species i concentration at the ground level, a and n are the exponent factors, which depend on the region. In this study, a and n factors were calibrated using public air quality data.

Aircraft engines ingest air during operation and by extension the contaminants contained in the air. Intake air mass flow at every time step is known from the engine performance simulation. Based on each operating point's ambient conditions and altitude, concentration is expressed as a mass fraction by dividing with air density at the given ambient conditions.

Following the assumption that the pollutant concentration is equal in the core and bypass duct, $C_{m,i}$ is the pollutant concentration both in the core and bypass flow. Regarding gas SO_2 and sea salt particles of small diameters, this assumption does not deviate from reality significantly. On the other side, particles of large size are expected to interact with the fan blades and deviate from the streamline trajectories after impact with the fan blades, so in reality, we expect a higher concentration of the larger particles in the bypass duct and less in the core. Therefore, a factor $f_{c,reduction}$ may be introduced to account for the reduction of the core concentration.

$$C_{m,i,core} = C_{m,i} \cdot f_{c,reduction} \quad (2)$$

This reduction factor is assumed as equal to one since a more detailed particle tracking investigation of particle-fan interactions is out of scope. After the sea salt and the particulate matter in the core flow are calculated, they are analyzed to their constituents using some representative percentages [88], [89]. NaCl concentration was modeled as 58.45% of the sea salt, Na_2SO_4 as 9.75% of the sea salt and sulfates as 17% of the particulate matter.

B. Fuel Module

The input of the fuel module is the FAR from the engine performance analysis and the fuel quality in terms of sulfur content, which is captured with a surrogate jet fuel model. A new surrogate jet fuel model is considered by combining n-decane, a simple surrogate jet fuel model from literature [90], [91], with hydrogen sulfide as the representative species for sulfur impurities [92], [93]. A more thorough representation of the hydrocarbon mixture in the jet fuel, which may be more relevant to combustion studies, was not required for the hot corrosion analysis. Developing a more sophisticated surrogate jet fuel that meets some target properties is out of scope. This work aims to use an acceptable surrogate jet fuel model and combine it with a representative sulfur impurity so that it can be

implemented in the hot corrosion framework. Sulfur is a tiny fraction of jet fuel and has been excluded from current surrogate models in literature; however, even this small amount must be considered in a hot corrosion lifing analysis.

The sulfur content in the fuel is one of the framework inputs and determines the fractions of $n\text{-C}_{10}\text{H}_{22}$ and H_2S in the fuel surrogate. The average sulfur concentration in modern jet fuel was reported between 0.05-0.1% in [94]. In 2006, the average sulfur content in UK Jet A1 and US Jet A was reported between 0.055-0.075% [95]. The surrogate fuel mixture for the average sulfur content (0.1%) and the maximum allowed value by the specifications (0.3% [94], [96]) are presented in Table 1.

Table 1 Surrogate fuel composition for $S_f=0.1\%$ (average) and $S_f=0.3\%$ (maximum allowed by the specifications)

	$S_f = 0.1\%$	$S_f=0.3\%$
Species	Mass fraction	
$n\text{-C}_{10}\text{H}_{22}$	99.9%	99.7%
H_2S	0.1%	0.3%

Since sulfur constitutes a small fraction of the surrogate fuel model (max 0.3%), in this context, it is assumed that it does not vary the surrogate fuel properties significantly. Similarly, in a future version of the framework, vanadium impurities may be considered. Furthermore, the modularity allows the substitution of the current fuel model with a more sophisticated surrogate model of jet fuel or with models that are representative of the chemistry of alternative fuels.

C. Chemical Equilibrium Module

The role of the chemical equilibrium module is to calculate the local gas composition in terms of corrosive species concentration. Local equilibrium composition is assumed to avoid the computational time and complexity required by full reaction kinetics modeling at each component. The inputs are the initial concentrations of the species as they enter the core flow, the temperature and pressure, and the outputs are the equilibrium concentrations. The main assumption is that the chemical reactions participating in the $\text{Na}_2\text{SO}_4/\text{NaCl}/\text{SO}_x$ mechanism are completed fast, and equilibrium is reached locally.

In every chemical system, the reactions tend to bring the system to the state that minimizes the Gibbs energy of the mixture [97]. Therefore, chemical equilibrium composition can be calculated based on the minimization of Gibb's free energy. In this work, the Gibbs energy minimization problem was solved in MATLAB using the built-in `fmincon` solver for constrained minimization to enable integration with the flight mission analysis and the hot corrosion code,

and the chemical equilibrium code was validated against equilibrium calculations in Chemkin software. However, to further reduce the computational time when multiple flights and engines of a fleet must be assessed, the chemical equilibrium compositions were calculated over a wide range of conditions and were expressed as conversion ratios of the available element moles.

The mole conversion ratios for the temperature range of hot corrosion and pressure ratios typical of gas turbine operation are stored in matrices. In addition, two cases are distinguished because they affected the mole conversion ratios:

- Sulfur is in excess so that all sodium moles can potentially be converted to sodium sulfate (most frequent case due to the sufficient amount of sulfur in jet fuel on average)
- Sulfur is in deficit so that full sulphation of sodium cannot happen

A few representative datasets for one fixed pressure are demonstrated in Fig. 3, which includes the distribution of the S mole consumption between SO_2 , SO_3 and Na_2SO_4 , the distribution of the Na moles consumption between Na_2SO_4 , NaCl and NaOH and the distribution of the Cl moles consumption between HCl and NaCl under 1 [atm] pressure. An interesting observation is that the temperature range with higher SO_3 formation coincides with the temperature range of Type II Hot Corrosion, which is attributed to high SO_3 (Fig. 1).

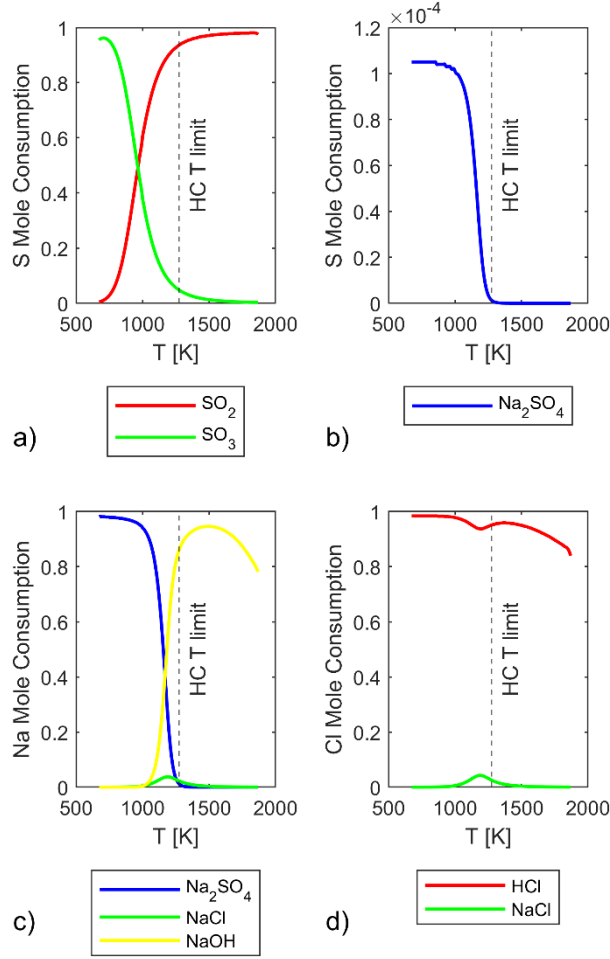


Fig. 3 Mole consumption ratios over temperature

D. Deposition Rate Module

The deposition module incorporates a condition check (above or below the saturation point of sodium sulfate) and two mechanisms for mass transfer onto the component surface (deposition through vapor diffusion or evaporation).

1. Saturation Point

Catastrophic hot corrosion occurs in the presence of liquid salt deposits, so one of the most important aspects that should be considered is the contaminants' phase transformation and whether the conditions allow the formation of stable deposits. Consequently, the effect of pressure variation and species concentration in the gas turbine components on the sodium sulfate saturation point should be addressed. The need for calculating the dew points of sodium sulfate and potassium sulfate under varying pressures has also been addressed by Sumner et al. [15].

The saturation point of sodium sulfate defines the applicability of the deposition rate models under specific conditions. The sodium sulfate saturation point is calculated using the Antoine Eq. [79], [98] and ultimately it is a

unique combination of pressure, temperature and sodium sulfate concentration. In this framework, the saturation point is calculated with temperature as the unknown term.

For the specific pair of pressure and Na_2SO_4 concentration [99] at every operating point, saturation temperature is calculated, and the following condition has to be checked:

- If $T > T_{\text{sat}} \rightarrow \text{evaporation}$
- If $T < T_{\text{sat}} \rightarrow \text{condensation}$

The above is the general condition that applies to all evaporation/condensation problems. However, studying deposition and hot corrosion in cooled or thermally transient gas turbine components is more complicated. Ahluwalia et al. [100] distinguished three different cases regarding cooled gas turbine components and sodium sulfate deposition:

- If $T_s < T_{\text{sat}}$ and $T_g \gg T_{\text{sat}} \rightarrow \text{evaporation}$
- If $T_s < T_{\text{sat}}$ and $T_g \geq T_{\text{sat}} \rightarrow \text{condensation}$
- If $T_s < T_{\text{sat}}$ and $T_g < T_{\text{sat}} \rightarrow \text{condensation}$

T_s is the metal surface temperature, T_g is the gas temperature, and T_{sat} is the sodium sulfate saturation temperature.

However, Ahluwalia et al. [100] do not clarify how much higher gas temperature has to be than the saturation temperature for evaporation to occur when the surface temperature is below the saturation point. Consequently, a decision had to be made in the framework regarding the condensation/evaporation threshold.

In the modeling of the LPT, which is not cooled, the thermally transient state of the blade in response to gas temperature changes is ignored in the present study, and it is assumed that at every operating point, the surface temperature equals the gas temperature. Therefore, the selection of the condensation/evaporation conditions becomes straightforward for non-cooled components, e.g. LPT stages:

- If $T_s / T_g < T_{\text{sat}} \rightarrow \text{condensation}$

If the surface/gas temperature is below the saturation point of Na_2SO_4 , condensation shall take place. Regarding HPT modeling, the chosen condition is to check whether the gas temperature is higher than the saturation temperature.

- If $T_s \leq T_{\text{sat}}$ and $T_g > T_{\text{sat}} \rightarrow \text{evaporation}$
- If $T_s > T_{\text{sat}}$ and $T_g > T_{\text{sat}} \rightarrow \text{evaporation}$
- If $T_s \leq T_{\text{sat}}$ and $T_g \leq T_{\text{sat}} \rightarrow \text{condensation}$

2. Deposition Rate

In [98], the authors reviewed candidate deposition rate models, analyzed them and assessed their applicability to hot corrosion lifing. In the present paper, the analytical model based on Rosner's work [101], [102] is used.

3. Evaporation Rate

There are many evaporation studies in the literature, but their main interest has been water droplets, fuel droplets, and semi-empirical correlations for specific materials [103] and applications. A physics-based model for potassium sulfate evaporation is provided in [104], and a semi-empirical model to calculate the chromium evaporation rate is provided in [103]. Nettleton et al. [104] described the theory previously developed by Maxwell and Langmuir for droplet evaporation in a stationary gaseous medium based on the vapor pressure difference between the surface and the surrounding environment, diffusivity and temperature. Finally, a generic model, based on the principle that the required enthalpy of evaporation is provided by heat transfer from the gas to the surface, is described in [105].

The evaporation model from [105] is implemented in the framework and is based on the concept that the required enthalpy of evaporation is provided with heat transfer from the hotter gas to the condensed salt. The rate of evaporation is a function of the difference between the gas temperature and the deposit temperature. When the salt condenses and before it evaporates back to the flow, it is assumed that it has the temperature of the saturated vapor to avoid the complexity of performing heat transfer calculation on the salt deposits, which would be beyond the scope of this study.

$$k_{\text{evap}} = \frac{h \cdot (T_g - T_{\text{dep}})}{\Delta H_{\text{evap,Na}_2\text{SO}_4}} \quad (3)$$

Where h is the heat transfer coefficient between the gas and the salt deposit. $\Delta H_{\text{evap,Na}_2\text{SO}_4}$ is the enthalpy of evaporation and the typical value $\Delta H_{\text{evap,Na}_2\text{SO}_4} = 71$ [cal/gr] is used [106].

4. Salt Built-Up

The thickness of the salt deposit per area at each operating point is calculated by applying the deposition rate model or evaporation model based on whether the operating conditions are below or above the Na_2SO_4 saturation point.

- If $T_g(t_i) > T_{\text{sat}}(t_i)$ & $m_{\text{dep}}(t_{i-1}) > 0 \rightarrow$ evaporation:

$$m_{\text{dep}}(t_i) = m_{\text{dep}}(t_{i-1}) - k_{\text{ev}}(t_i) \cdot (t_i - t_{i-1}) \quad (4)$$

- If $T_g(t_i) \leq T_{\text{sat}}(t_i) \rightarrow$ condensation:

$$m_{\text{dep}}(t_i) = m_{\text{dep}}(t_{i-1}) + \dot{m}_{\text{dep}}(t_i) \cdot (t_i - t_{i-1}) \quad (5)$$

m_{dep} is the deposited salt layer at each time segment, $\dot{m}_{\text{dep}}$ is the instantaneous deposition rate at time t_i , k_{ev} is the evaporation rate.

5. Infiltration through the Thermal Barrier Coating

One of the differences that need to be considered between modeling hot corrosion on the HPT and LPT is that the HPT is typically coated with a TBC. Apart from offering thermal insulation, the ceramic top layer acts as a physical barrier, which is expected to prevent a part of the deposited salt from reaching the substrate. A typical porosity of an EB-PVD thermal barrier coating for high-pressure turbines is 24%-22% [107], although sintering effects after many hours of operation may reduce the porosity [108]. In the present study, to account for the fact that HPT is coated with a TBC and that the ceramic layer may act as a partial physical barrier between the salt and the bond coat, the porosity factor is multiplied with the deposition rate in order to approximate the amount of salt reaching the bond coat from the top surface.

In reality, the infiltration of liquid deposits is complex and influenced by capillary pressure and material permeability - a function of the channel's geometrical details [109], [110]. Previous studies [109], [110] have investigated CMAS infiltration through the TBC pores using a 1D form of Darcy's Law, and such an approach could potentially be used for liquid Na_2SO_4 infiltration in a future study dedicated to this aspect. In the present framework, more detailed modeling of the infiltration process is out of scope, so the porosity factor is used to consider the TBC presence on HPT blades. Nevertheless, in the type of engine investigated in this work, LPT stages suffer more from hot corrosion than the TBC-coated HPT blades.

E. Hot Corrosion Rate Module

1. Selected Baseline Model

The baseline hot corrosion model for nickel superalloy is provided by Nicholls et al. in [26]. The baseline Eq. is :

$$\text{Hot Corrosion depth after 500h} = x_0 + A \cdot \ln \left[1 + \frac{\dot{m}}{\dot{m}_0} \right] \quad (6)$$

Where A is the parameter that depends on the partial pressures of the contaminants:

$$A = A_0 ([P_{\text{SO}_x}] + 1)^k \cdot ([P_{\text{HCl}}] + 1)^m \cdot ([P_{\text{H}_2\text{O}}] + 1)^n \quad (7)$$

$\dot{m}_0$ is the threshold salt deposition rate for flux-induced hot corrosion initiation and is calculated with Eq. (8).

$$\dot{m}_0 = F_0 ([P_{\text{SO}_x}] + 1)^r \cdot ([P_{\text{HCl}}] + 1)^s \cdot ([P_{\text{H}_2\text{O}}] + 1)^t \quad (8)$$

where k, n, m, r, s, t are experimentally tuned parameters found in [26] and P_i is the partial pressure of a species i.

2. Introduction of a Temperature Correction Factor

One of the most important limitations of the selected nickel superalloy model is that it was developed under constant temperature and consequently is not sensitive to temperature inputs. Based on the experimental data from Haight et al. [76] (Fig. 4) who mapped the effect of temperature on the median metal loss, a temperature correction factor was extracted with the damage at 700°C acting as a reference point (Eq. (9)) because it is the temperature under which the baseline nickel superalloy hot corrosion model was developed.

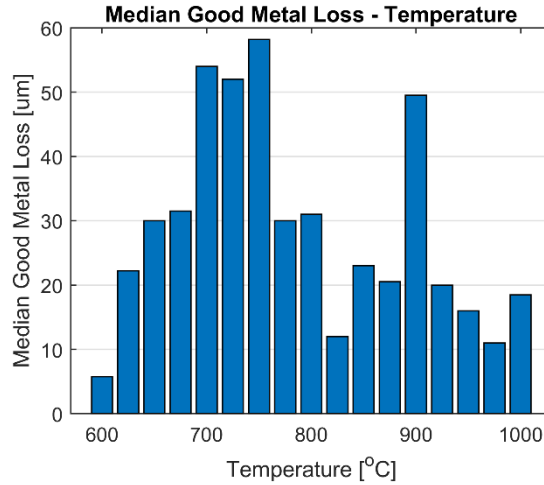


Fig. 4 Median good metal loss after 200h of hot corrosion exposure (redrawn based on [111])

$$f_c(T) = \frac{\text{Median Loss @ } T}{\text{Median Loss @ } 700^\circ\text{C}} \quad (9)$$

The variable-temperature experiment [111], on which the temperature correction factor is based, was performed on the same material (CMSX4) as the baseline model by Nicholls et al. [26], but the deposit had a different composition. Until more relevant hot corrosion experimental data become available, it is assumed that the damage trend over temperature is similar for both mixtures. The hot corrosion rate over temperature trend obtained by Haight et al. [111] is in agreement with the hot corrosion theory (Fig. 1). Using the experimental results in [111] as representative was the only available way to quantify the theoretical trend due to the lack of a more generalized experimental hot corrosion model. The data extracted from Haight et al. [111] (Fig. 4) are normalized and interpolated using the cubic convolution method resulting in the temperature correction factor of Fig. 5.

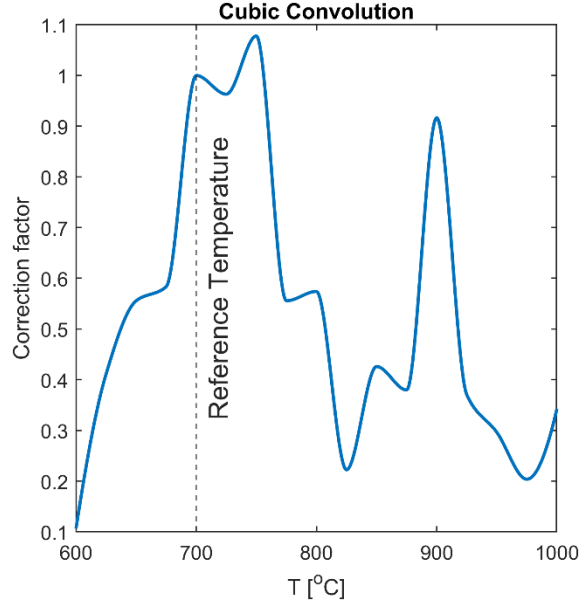


Fig. 5 Temperature correction factor fitted with the cubic convolution method over the hot corrosion temperature range

Nicholls et al. [26] expressed hot corrosion rate as the depth of attack after 500h of exposure. The initial Eq. (6) is transformed to (10) with the addition of the normalized temperature correction factor of Fig. 5 and by converting the exposure time to seconds so that it is comparable to the time scale of the flight mission analysis, assuming linear damage distribution over time. Therefore, the implemented hot corrosion damage model transforms into Eq. (10):

$$\text{HCR}_{\text{depth}} = f_c(T) \cdot \frac{\left[x_0 + A \cdot \ln \left(1 + \frac{\dot{m}_{\text{Na}_2\text{SO}_4}}{\dot{m}_0} \right) \right]}{500 \cdot 3600} \left[\frac{\mu\text{m}}{\text{s}} \right] \quad (10)$$

The hot corrosion rate (HCR) in Eq. (10) is expressed as the depth of attack into the metal over time, but it can also be expressed as the good metal mass loss over time by using the density of CMSX4:

$$\rho_{\text{CMSX4}} = [786 - 1.23 \cdot (T - 1654)] [\text{kg}/\text{m}^3] \quad (11)$$

HCR (as depth) can be converted to mass loss using Eq. (12)

$$\text{HCR}_{\text{mass}} = \text{HCR}_{\text{depth}} \cdot \rho_{\text{CMSX4}} \cdot 10^3 \left[\frac{\mu\text{g}}{\text{cm}^2/\text{s}} \right] \quad (12)$$

3. Hot Corrosion Damage Accumulation

After the instantaneous hot corrosion rate has been calculated at each operating point, damage accumulation at the end of the flight cycle is approximated linearly by adding the damage caused during each time segment for which the hot corrosion rate is calculated:

$$d_{\text{cycle}} = \sum_{i=1}^N \text{HCR}(i) \cdot [t(i) - t(i-1)] \quad (13)$$

d is the damage in thickness loss or mass loss, HCR is the calculated hot corrosion rate, $i=1 \dots N$ are the time segments of the flight mission analysis, and t is the mission time.

Damage accumulation is assumed to be linear to reduce complexity since there are not enough experimental data to account for non-linear damage accumulation. Consequently, the hot corrosion damage of each mission is added linearly using Eq. (14):

$$d_{\text{total}} = \sum_{\text{cycle}=1}^K d_{\text{cycle}} \quad (14)$$

If only one representative flight mission or the average flight mission is available for an engine type of a fleet, then Eq. (14) converts to (15):

$$d_{\text{total}} = K \cdot d_{\text{cycle}} \quad (15)$$

K is the total number of operated flights since new or since the last full component restoration.

IV. Hot Corrosion Analysis

A. Hot Corrosion Analysis over Flight Time

1. Temperature Assessment

In this section, the analysis focuses on the flight mission, and the contamination, deposition and hot corrosion rates are calculated over the flight time before proceeding to more macroscopic analyses of whole missions. The discussion of the intermediate results of each module contributes to the understanding of the final outputs and explains the progress of the mechanism.

Hot corrosion mechanisms are typically activated between 600°C-1000°C (although these boundaries should not be treated as absolute). Therefore, a first step in the hot corrosion analysis is to assess which components are within the temperature range of hot corrosion over the flight time. The averaged component conditions are used as a metric in the present analysis to explain the methodology and establish an approach for aero-engine hot corrosion analysis, but other levels of resolution can be selected for different purposes.

In Fig. 6a and Fig. 6b, the average gas and metal temperature of each turbine stage is compared with the theoretical temperature range of hot corrosion and the local saturation point of Na_2SO_4 . For the specific mission and metric of

assessment (averaged component conditions), LPT stg3 is out of the temperature range of hot corrosion, LPT stg2 is in the temperature range of hot corrosion only at take-off and during the activation of the thrust reverser at landing, while LPT stg1 is in the temperature range of hot corrosion at take-off, climb, beginning of cruise and during the activation of the thrust reverser. The average HPT metal temperature is in the temperature range of hot corrosion at take-off, climb, and thrust reverser activation. The metal temperature of the cooled HPT is estimated using a typical cooling effectiveness value for an engine of this technology level.

However, temperature alone is not a sufficient criterion to predict hot corrosion. The availability of corrosive species (i.e. sodium sulfate and sulfur oxides are considered in this work) and their ability to form liquid deposits should also be assessed to predict the severity of hot corrosion damage. Figure 6c compares the sodium sulfate saturation temperature for the given component pressure against the gas temperature. The average metal temperature of the HPT (Fig. 6b) is within the temperature range of hot corrosion at take-off and climb, but the HPT gas temperature is above the saturation point (Fig. 6c) so molten salt deposits cannot be sustained on the surface and they evaporate back to the flow.

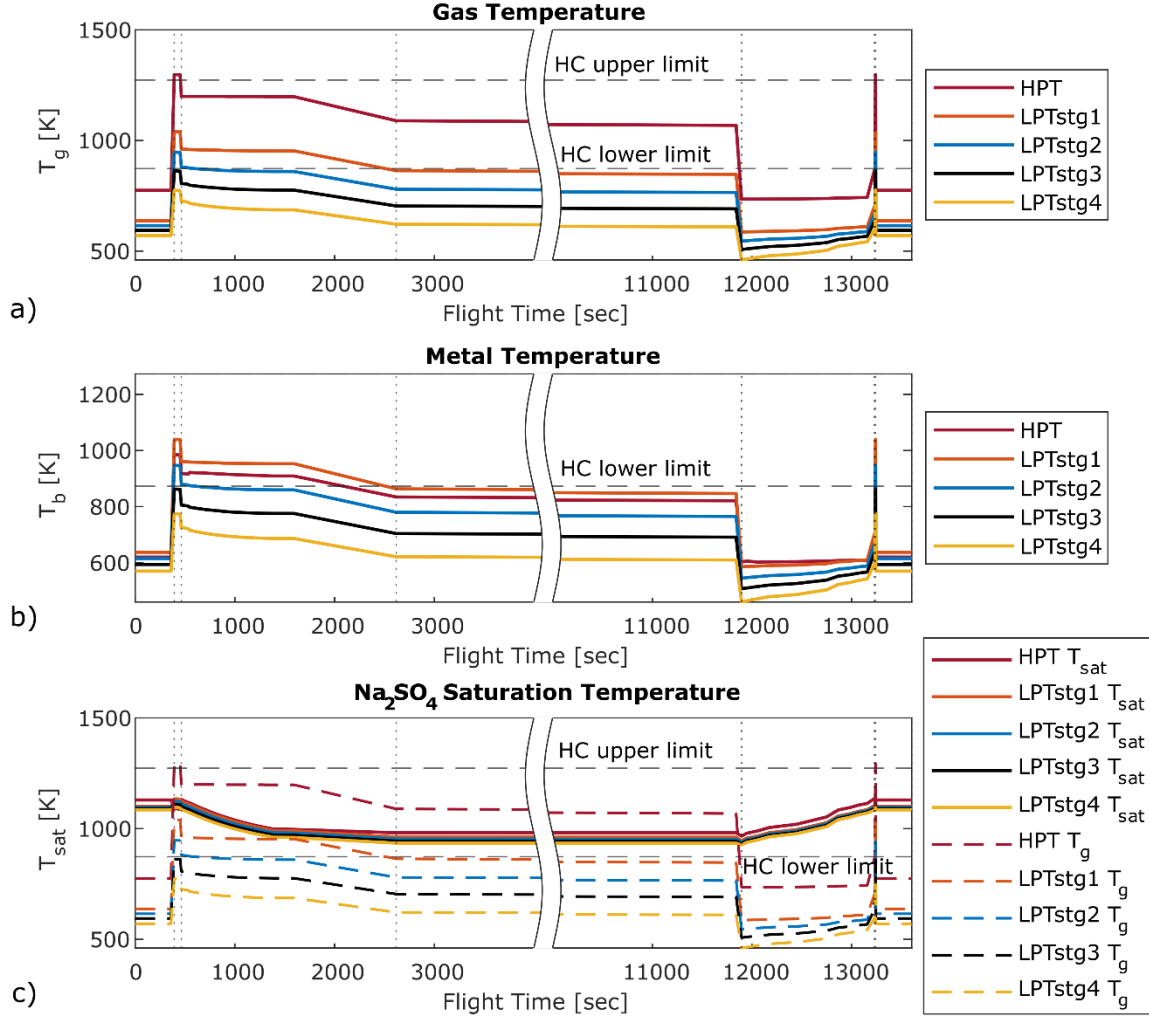


Fig. 6 Average component gas temperature, metal temperature against the Na_2SO_4 saturation point and the theoretical hot corrosion temperature range

2. Environmental Impact

In terms of available corrosive species in the atmosphere, the framework incorporates calculations of SO_2 , sulfate in particulate matter and sea salt ingestion. Sea salt is usually the contaminant with the highest effect among the three, so it is selected for demonstration. Figure 7a demonstrates the sea salt concentration in the atmosphere over altitude, which is then used in conjunction with the flight mission analysis (Fig. 7b) to calculate the sea salt concentration in the core flow at each operating point (Fig. 7c) and the sea salt ingestion rate into the engine core based on the instantaneous core mass flow. The ingested species concentrations are gradually reduced during the climb, which is then gradually restored during descent.

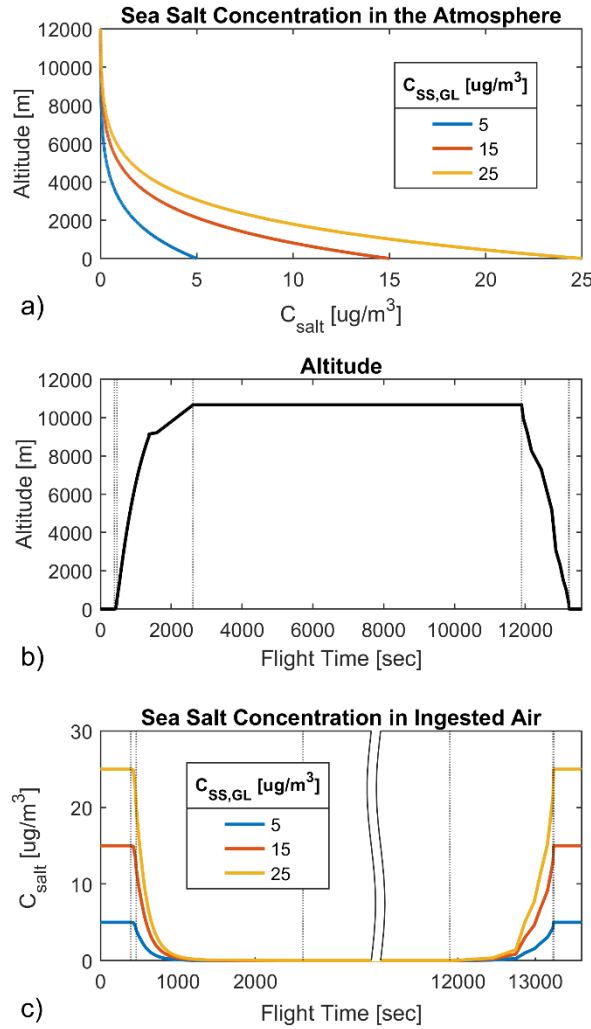


Fig. 7 Effect of ground level contaminant concentration and flight altitude on ingested air quality

3. Fuel Impact

In Fig. 8b, the Fuel to Air Ratio (FAR) is presented against flight time for three thrust settings at take-off and climb. Thrust affecting the FAR is translated into sulfur impurity concentration in the core flow. The hot corrosion rate is a function of sulfur concentration, so the parameter of interest is the FAR, while the absolute fuel flow alone cannot be used for hot corrosion analysis, as the hot corrosion rate in Eq. (6) and (7) is correlated with SOx partial pressure, thus SOx concentration. Figure 8 supports the findings of the SOx concentration (Fig. 9a) and subsequent hot corrosion rate calculations. The phases with the highest FAR are take-off, climb and thrust reverser activation at landing. The fully-rated thrust and climb have the highest FAR (Fig. 8b) up to the washout altitude, which is translated into higher sulfur concentration imposed on the flow. The climb derate is modeled considering altitude washout that begins at 10000ft and is completed at 30000ft according to [112].

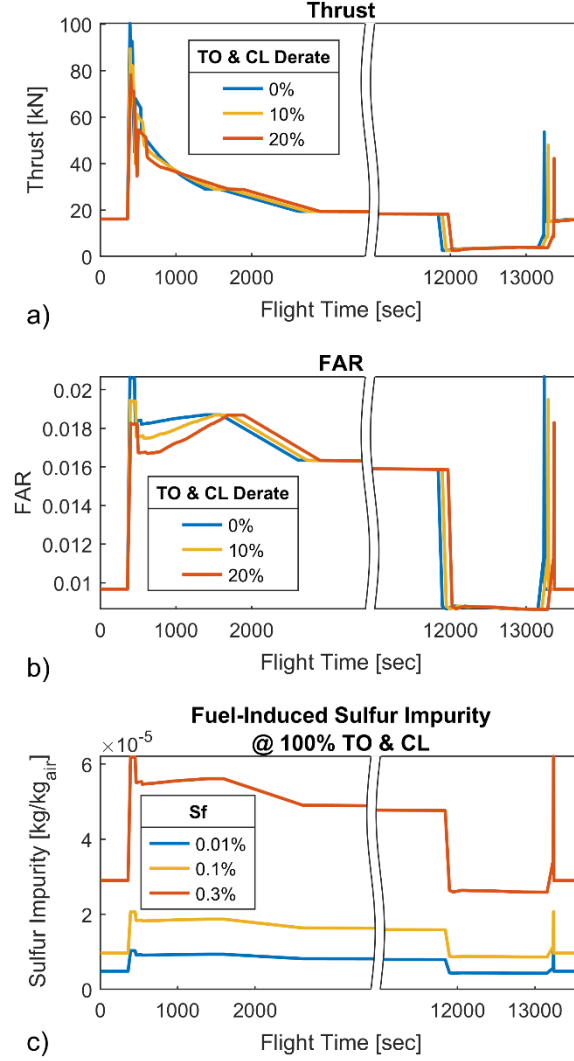


Fig. 8 Thrust, FAR and sulfur impurity over flight time

4. Chemical Equilibrium Impact

Chemical equilibrium calculations are incorporated into the framework for a mechanism with 30 species. The concentration of the most relevant species is demonstrated over the flight time: SO_x, Na₂SO₄ and H₂O. In Fig. 9, the SO_x, Na₂SO₄ and H₂O mole concentrations are presented for each component over the flight time, using the local chemical equilibrium assumption.

The peak of SO_x concentration (Fig. 9a) at take-off and thrust reverser at landing is due to the high FAR (Fig. 8b) and par extension increased fuel-induced sulfur (Fig. 8c). Overall, SO_x and H₂O are both dependent on FAR, as SO_x is mainly formed by sulfur impurities in the fuel and H₂O is a product of fuel combustion. Under the LPT stg1 temperature, Na₂SO₄ is in the stable region of Fig. 3c, in contrast to the HPT which has too high temperature that is in the unstable region of Fig. 3c during take-off.

The sulfur mole consumption by SO/SO₂/SO₃ is varied over temperature as plotted in Fig. 3a, but since the hot corrosion rate is correlated with the partial pressure of total SO_x in Eq. (6), FAR and total sulfur mole concentration become the dominant parameter, while a temperature variation will have nearly no effect on the total SO_x. These observations are valid for the typical condition that S is in excess of Na so that there are always enough available S moles to fully convert Na to Na₂SO₄. However, in the less frequent case that Na is in excess of S, a higher percentage of S moles would have been consumed by Na₂SO₄.

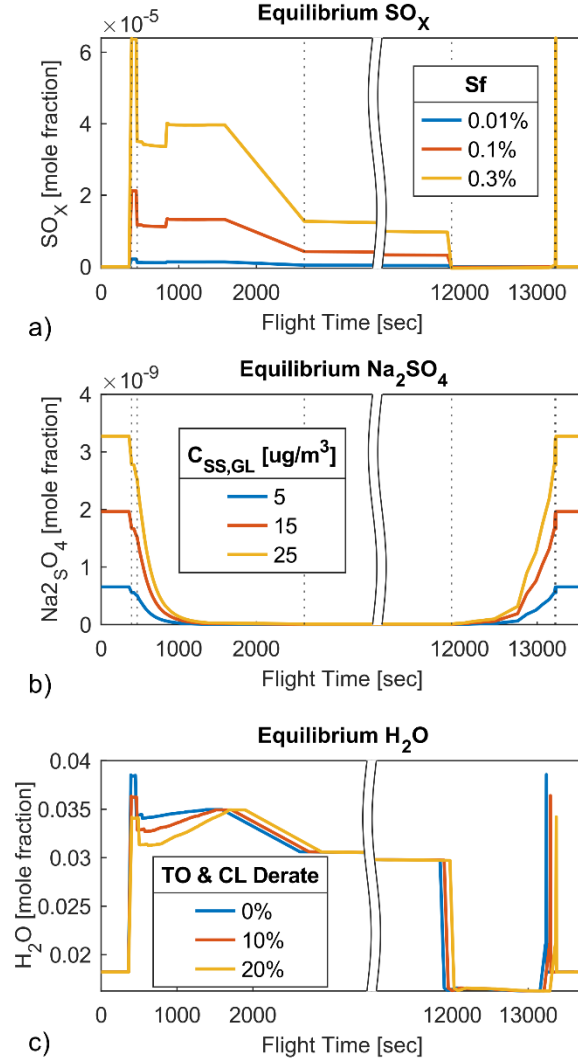


Fig. 9 SO_x, Na₂SO₄ and H₂O mole concentration over flight time

5. Deposition Rate Impact

The LPT stg1 instantaneous deposition rates (Fig. 10) are derived by checking if the conditions are above or below the local saturation point of sodium sulfate and then applying the deposition rate model as appropriate. It should be noted that the deposition results are targeted to be most valid in the temperature range of hot corrosion (not far below

the saturation point). At temperatures far below the saturation point, mechanisms of nucleation are expected to come into effect and cause increased deposition by particle transport and impact on the surface. Consequently, at the operating points below 600°C, ex. descent, there might be an underestimation of the deposition rate, but in the current context and application, it does not affect the hot corrosion rate results because the hot corrosion rate model is only applied in the temperature range of 600-1000°C and is a function of the instantaneous deposition rate, not of the accumulated salt layer.

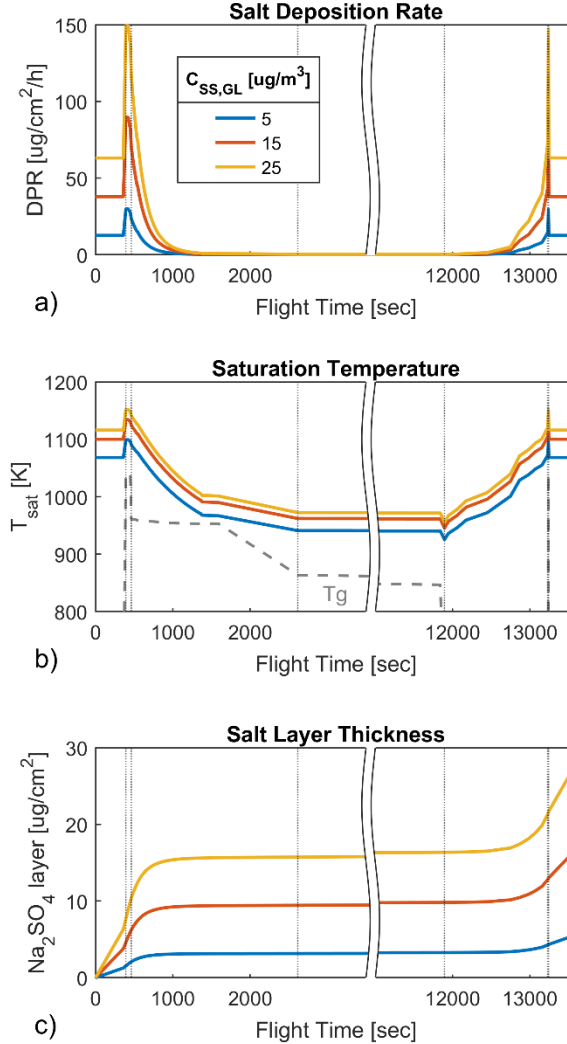


Fig. 10 Deposition rate, saturation temperature and salt layer thickness over flight time ($S_f=0.1\%$, Derate 0%)

A peak in deposition rate is observed at take-off (Fig. 10a) because of the high pressure and high concentration of contaminants which are the two primary driving factors of the deposition rate model [98]. As the sea salt concentration (Fig. 7) gradually reduces during climb, the deposition rate also follows this declining trend. During descent, as the sea salt concentration increases with the altitude drop, the deposition rate increases again but the temperatures are too

low for hot corrosion. Another deposition rate peak occurs at thrust reverser activation at landing but does not contribute significantly to the damage accumulation, because it is activated for a few seconds.

6. Hot Corrosion Rate

Fig. 11 presents the instantaneous hot corrosion rate on LPT stg1 with variable contamination inputs and thrust settings. The hot corrosion rate is higher at take-off and climb, explained by the combination of suitable gas and blade temperatures (Fig. 5), high FAR (Fig. 8b) and SO_x concentration (Fig. 9a), and high deposition rate (Fig. 10a).

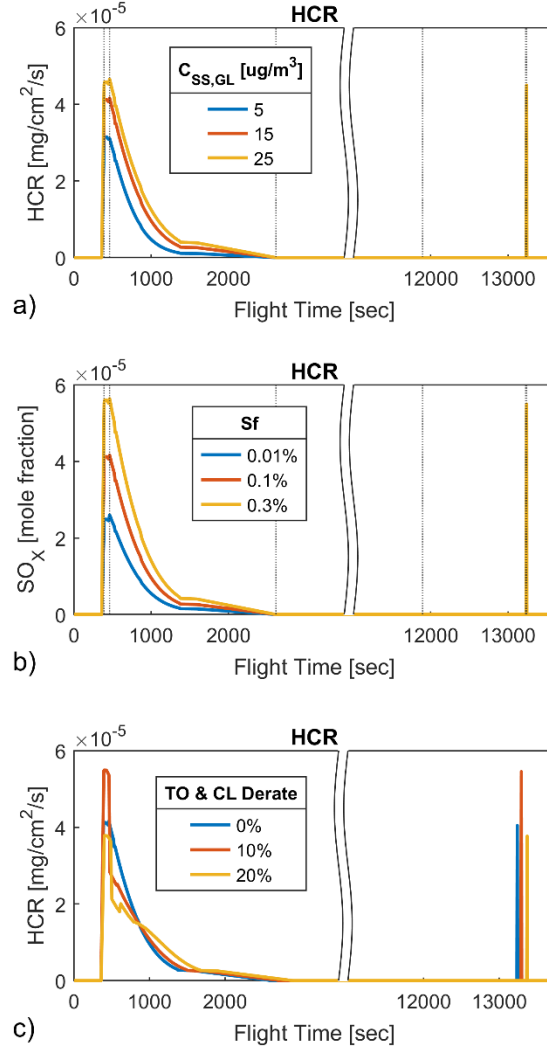


Fig. 11 LPT stg1 hot corrosion rate over flight time a) Sf=0.1%, TO & CL Derate = 0% b) $C_{SS,GL} = 15$ [ug/m³], TO & CL Derate = 0% c) $C_{SS,GL} = 15$ [ug/m³], Sf=0.1%

B. Affected Component

After having presented the analytical mission results for LPT stg1 and variable inputs, Fig. 12 compares the temperatures, deposition rates and hot corrosion rates of all the hot section components under a fixed set of inputs. LPT stg1 is the most affected component in the investigated type of engine and mission and LPT stg2 is the second

affected component. The average blade temperature at the LPT stg1 is within the hot corrosion temperature range, while the gas temperature remains below the sodium sulfate saturation point, which means liquid salt deposits can be formed on the surface. The average component temperature of the HPT may also be in the temperature range of hot corrosion, but the gas temperatures are at most times above the sodium sulfate saturation temperature which means that liquid sodium sulfate deposits cannot be sustained. The LPT stg2 temperature is below the saturation temperature of sodium sulfate at take-off and climb, but after take-off, the average component temperature drops below the lower threshold of hot corrosion.

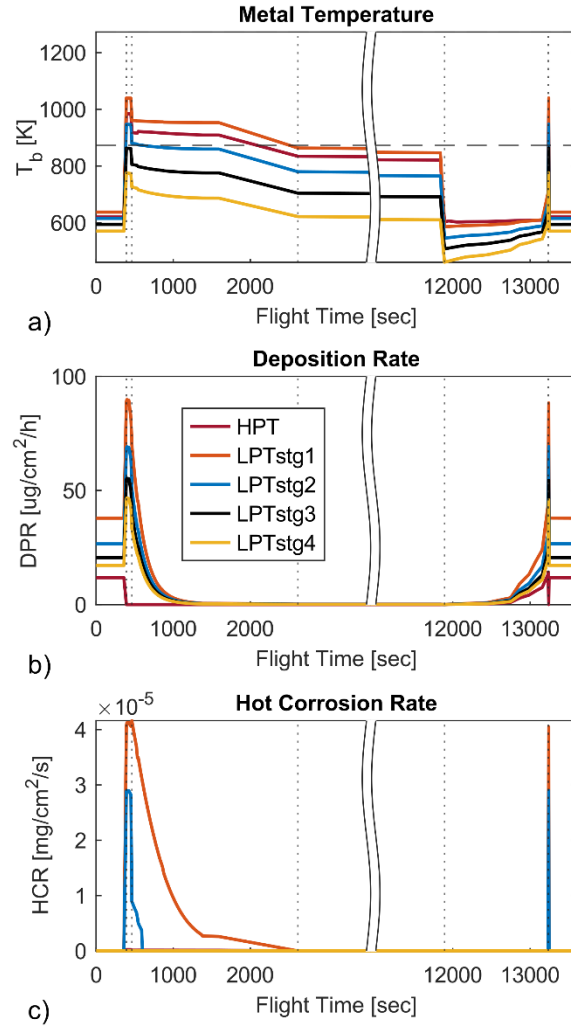


Fig. 12 Temperature, deposition rate and hot corrosion rate at hot section components ($C_{\text{SS,GL}} = 15 \text{ [ug/m}^3\text{]}$, $\text{Sf}=0.1\%$, $\text{Derate } 0\%$)

C. Damage Accumulation

After having calculated the instantaneous hot corrosion rate over flight time, the damage summation can be performed using Eq. (13) which is based on the concept of linear damage accumulation. In reality, hot corrosion does not progress linearly but typically has two stages – incubation and propagation stage. However, to reduce the complexity and uncertainty of the actual progress over time, the linear damage summation is broadly used in engine lifing. The available experimental results are based on hot corrosion damage after 500 hours of exposure and offer a more macroscopic view of the resulting damage after many operating hours. The damage after 500 hours was divided linearly to be able to express the equivalent damage at each time step of an individual mission without the knowledge of how many operating hours have preceded this mission, but when the mission damage is projected to many hours of operation, the macroscopic view is restored and the missions in the incubation and propagation stage may average out.

In Fig. 13 the results of the hot corrosion damage accumulation over flight time are presented. In Fig. 13a-c, two parameters are kept fixed while the third parameter is variable and is presented in multiple curves over the flight time. Most of the damage is accumulated during take-off and climb, where the temperatures of LPT stg1 are within the temperature range of hot corrosion, the FAR (hence SO_x concentration) is higher and there is higher salt concentration which deposits on the component surface. It is observed that at the beginning of climb, the gradient of the damage accumulation is steeper and it starts to flatten out progressively at higher altitudes and before reaching the top of climb.

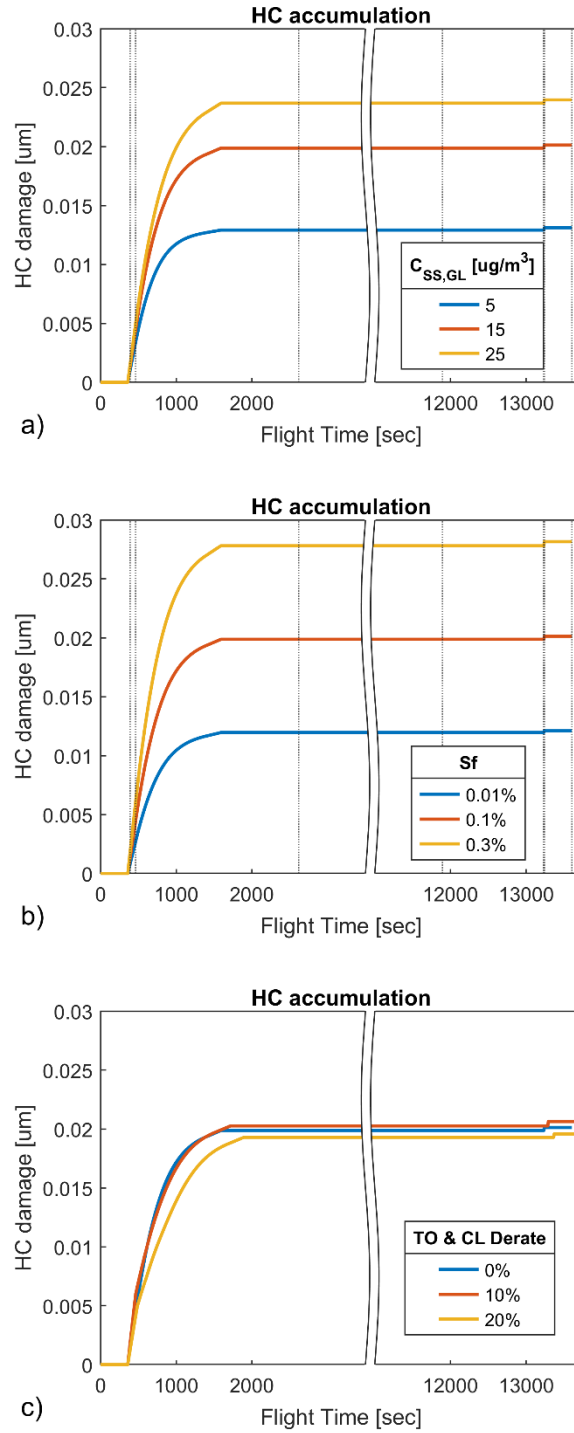


Fig. 13 Hot corrosion damage accumulation over flight time a) $S_f=0.1\%$, TO & CL Derate = 0% b) $C_{SS, GL} = 15 [\mu\text{g}/\text{m}^3]$, TO & CL Derate = 0% c) $C_{SS, GL} = 15 [\mu\text{g}/\text{m}^3]$, $S_f=0.1\%$

D. Classification of Hot Corrosion Factors

The fuel quality, environmental exposure and derate policy are parameterized within a typical range and their impact on hot corrosion is characterized based on the relative hot corrosion damage (Fig. 14) they cause. The relative

changes in the hot corrosion damage accumulation for variable parameters are presented with reference to the mission with 10% derate at take-off and climb, 0.1% sulfur in fuel and 15 [ug/m³] sea salt concentration in the atmosphere at ground level.

The different levels of sulfur in fuel represent refueling at different regions over the world with different quality of fuel supply. All three presented values of sulfur content in fuel are acceptable by the specifications as the upper limit is 0.3%, however, whether the fuel supply is near the specification limit or away from the specification limit has a great effect on hot corrosion damage. For 0.01% sulphur concentration, the hot corrosion damage is reduced by 39% (Fig. 14) compared to the average sulfur content (0.1%) of jet fuel, for the considered 1500nmi mission with sea salt concentration 15 [ug/m³] and 10% derate. In addition, since Sustainable Aviation Fuel (SAF) is expected to have very low to no sulfur, it can be expected that the use of SAF has the potential to reduce the hot corrosion damage by at least 39%.

The different levels of ground-level sea salt concentrations represent different airport locations and flight regions over the world. Seaside airports and flight segments above the sea have higher sea salt concentrations, and additional seasonal and weather variations are also expected to take place [113]. The upper limit for the sulfur in jet fuel can be easily selected on the specification limit (0.3%), but it is harder to define an upper bound in the sea salt concentration. For a typical range of environmental exposure considered in this study (5-25 [ug/m³]), the change in the relative damage is up to +80%, however, more extreme values are expected to be found locally which can further increase the damage.

The selected thrust derate policy is usually a result of considering the available runway length and payload/aircraft weight for the take-off, and a result of payload/aircraft weight, climb time and desired trade-off between fuel burn and engine life for take-off [114]. For a constant derate policy across take-off and climb, the damage variation between 10% and 20% derate is in the order of 5-7% when the sulfur content is 0.1-0.3% and the sea salt concentration is at 25 [ug/m³] (Fig. 14b). However, it should be noted that there is a competing effect between take-off and climb. As shown in Fig. 11c, although the 10% derate at take-off causes the highest hot corrosion rate at take-off among the three derate settings, the 10% climb setting results in lower hot corrosion than the 0% setting at the beginning of climb where there is still high sea salt concentration in the atmosphere. This means that, for the LPT stg1, a 10% derate at take-off is more detrimental at take-off while the 0% derate is the most detrimental at the beginning of climb. Hot corrosion is a non-linear and non-monotonous phenomenon with temperature, consequently, when studying two

phases at the same time, the same setting might have a competing effect when assessing which derate setting is more detrimental. In a future study, the effect of more mission specifications and the effect of individual phases will be mapped, so the effect of the derate at take-off and climb will be distinguished and a higher variation will become visible. Within the typical ranges under consideration, the sulfur in the jet fuel and the sea salt concentration are the most important drivers, and the third driver is the derate policy which will affect the operating temperatures and the time of exposure.

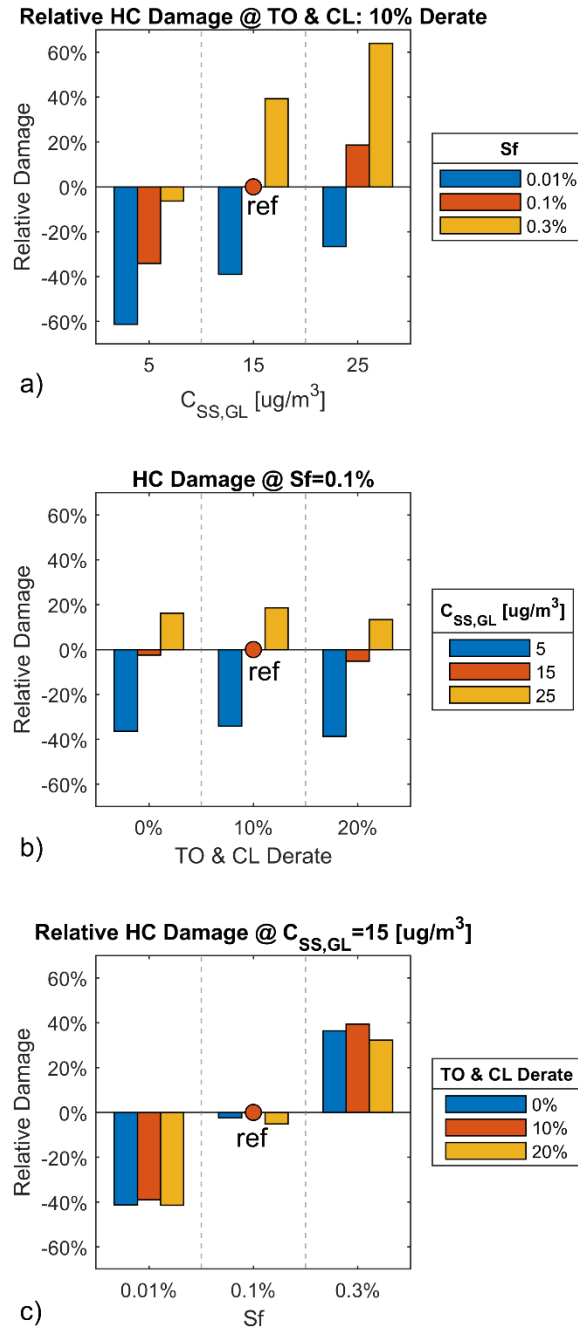


Fig. 14 Relative change in the accumulative hot corrosion damage at the end of the 1500nmi mission

E. Maintenance Aspects

Hot corrosion causes surface degradation, good metal loss and a significant reduction in fatigue life due to the microcracks or pits forming on the corroded surface. Good metal loss also means blade thickness loss and reduction in the load-bearing capability of the component. When the workscope of the shop visit includes the HPT or the LPT module, the vanes/blades are inspected and if the thickness loss is beyond a limit defined by the manufacturer, they

cannot be repaired and must be scrapped. If the damage is within the repairable limits, the blade surface can be cleaned, repaired and re-coated for further use.

In engine lifing, the absolute damage values are hard to be accurately predicted due to the many uncertainties, modeling limitations, the complex interactions between multiple damage mechanisms, material defects and deviation from idealist or assumed conditions. For this reason, a hot corrosion severity factor is introduced to express the damage relative to a reference mission and set of conditions.

$$SF = \frac{d_{total}}{d_{ref}} \quad (16)$$

An example is demonstrated assuming that the thickness loss for the LPT stg1 blade to be repairable is 400um. If previous experience has shown that a reference mission (1500[nmi], Sf=0.1%, C_{SS,GL}=15[ug/m³], TO derate = 10%, Climb derate = 10%) leads to a damage finding of 350 [um] after 10000 Flight Cycles (FC), then a severity factor of 1.14 relative to the reference mission means that the damage has reached the repairable limit.

Table 2 Hot corrosion severity factor to predict if the hot corrosion damage is beyond repair

d_{ref}	d_{repairable limit}	SF_{repairable limit}
350 um	400 um	1.14

Subsequently, severity factors are calculated for variable inputs as a ratio of the predicted damage by the framework to the predicted damage at the reference mission and compared against the severity factor that corresponds to the repairable limit (Table 2). This way a map is created (Fig. 15) to assist the decision-making.

The synergy between environmental exposure and sea salt concentration strongly affects the hot corrosion severity (Fig. 15a, c and e) and the prediction of whether the blade will be repairable (Fig. 15b, d and f) after a number of flight cycles. The derate policy has a smaller effect, as it shifts the damage up to 5-7% (Fig. 16), but it is the only parameter out of the three that can be controlled (within some constraints). A 10% derate is the most detrimental for LPT stg1 hot corrosion, the second most detrimental is 0% derate and the less detrimental is 20% derate. In theory, a 0% derate may lead to lower hot corrosion damage in the LPT stg1, but it increases the effect of other damage mechanisms, so it would normally be avoided and the 20% derate would make more sense. If a flight is departing from a seaside airport with a fuel supply of low quality, the contamination cannot be avoided but a 20% derate policy across take-off and climb can offer a hot corrosion life benefit up to 7% which may be able to shift the damage and bring it below the damage threshold for the component to be repairable (Fig. 15f). Overall, considering the whole parameter range in

Fig. 15, a change from [$S_f=0.01\%$, $C_{SS,GL}=5[\mu\text{g}/\text{m}^3]$, Derate = 20%] to [$S_f=0.3\%$, $C_{SS,GL}=25[\mu\text{g}/\text{m}^3]$, Derate = 10%] increases the damage by +348%, which shows the importance of predicting hot corrosion in the highly varying aero-engine operation and environment.

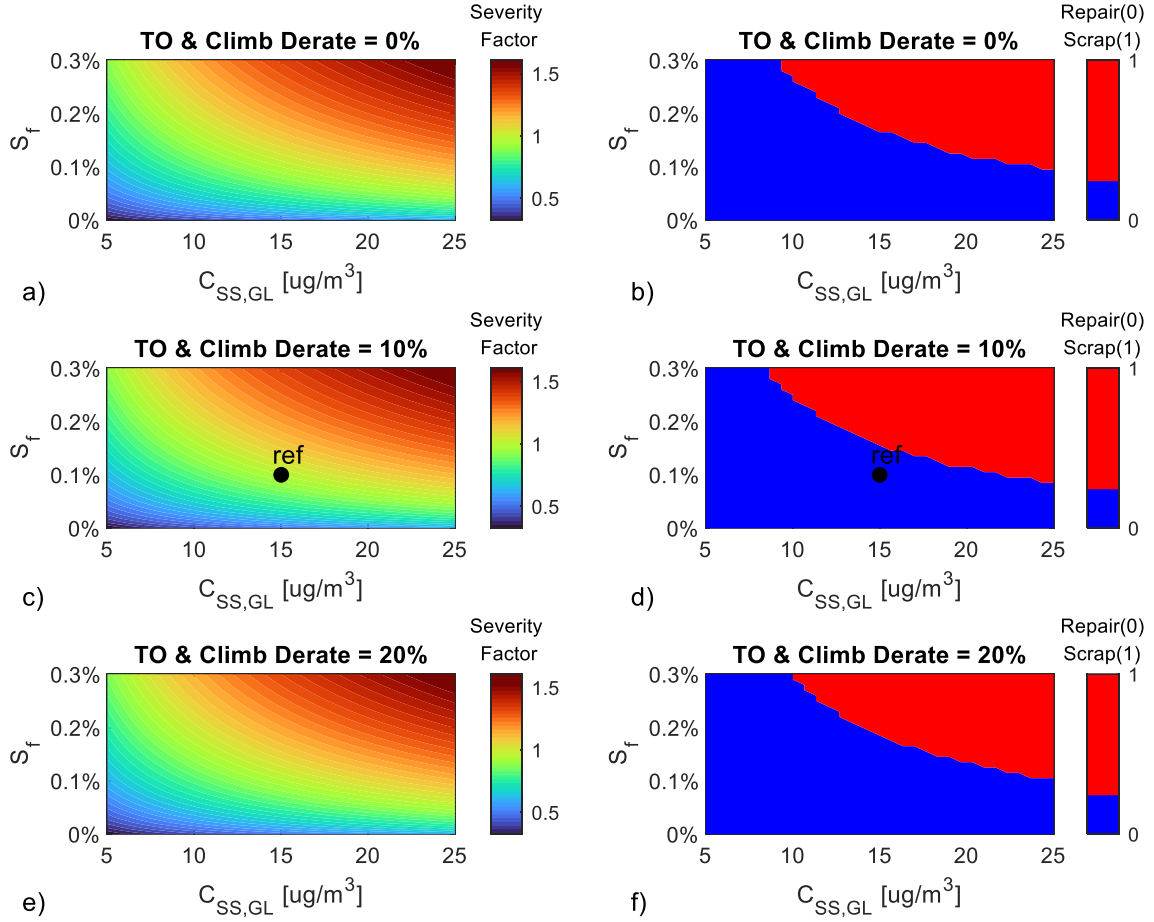


Fig. 15 Hot corrosion severity factor and prediction if the component will be repairable after 10000FC

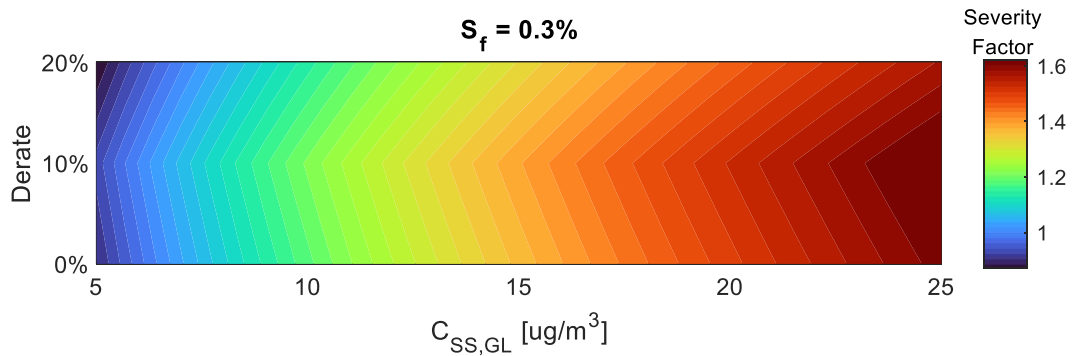


Fig. 16 Impact of derate policy on hot corrosion severity

After the severity of hot corrosion is estimated and related to the repairable limit (Fig. 15), two decisions can be made:

1. The maintenance schedule can be informed by the new hot corrosion framework and be adapted so that the engine is removed before the material loss is beyond repair. In that case, some remaining life of Life-Limited Parts (LLPs) may be sacrificed to prevent blade scrap. This is a trade-off choice that is part of a wider optimization problem that considers multiple component damage, LLP expiry, hardware degradation, fuel efficiency penalty and engine availability.
2. Even if no schedule change is made due to hot corrosion, if increased scrap rates are predicted due to hot corrosion under a given maintenance schedule and shop visit interval, the expected costs can be more accurately be predicted.

V. Discussion

A. Extra considerations

Existing engine lifing approaches based on creep, fatigue and oxidation mainly usually focus on the HPT, and the most significant inputs are the HPT inlet temperature, maximum rotational speed and stress at take-off. While other lifing approaches are looking for the highest temperatures and rotational speeds at take-off, a new philosophy and types of information are required for hot corrosion analysis based on the whole flight mission. The highest temperatures are not the most detrimental in regard to hot corrosion, but intermediate temperatures of a specific range should be identified over the whole flight duration. However, the temperature limits should not be treated as absolute, but they can be slightly different when vanadium eutectics that cause hot corrosion at lower temperatures are present or when the salt saturation point is higher and liquid deposits can be sustained at higher temperatures, the upper temperature-limit can also be higher. Furthermore, the hot corrosion rate does not have a monotonously increasing relationship with temperature, like oxidation, so the hot corrosion predictions become more challenging and more sensitive to any temperature accuracy errors than other temperature-dependent damage mechanisms such as creep and oxidation.

B. Operational & Technical Conclusions

The first and most straightforward observation is that high sea salt concentration and high sulfur concentration (fuel sulfur impurities) are detrimental to hot corrosion, but other better-hidden correlations are also identified. At component level, increased hot corrosion occurs when a favorable combination of high pressure, high sulfur oxide and high salt deposition rate is present under an intermediate temperature range that remains below the saturation

point of sodium sulfate. It should be noted that any change in the component temperatures due to different thrust or ambient temperatures, may bring a component from a peak of the hot corrosion rate curve to a lower point or vice versa, may move the component into or out of the temperature range of hot corrosion or it may move the conditions below or above the saturation threshold of sodium sulfate. High pressure indirectly favors hot corrosion through three mechanisms:

- It increases the saturation point of sodium sulfate allowing liquid deposits to be maintained at higher temperatures [98].
- It increases the partial pressure of sulfur oxides, a function of which is the hot corrosion rate (Eq. (6) and (7)).
- It increases the deposition rate through vapor diffusion [98].

At mission level, take-off and climb, which are associated with high thrust, offer a combination of high SO_x due to the high FAR, high pressure and high salt concentration at lower flight altitudes. Although the HPT has the highest pressures, the gas temperatures are usually too high for salt condensation to occur. The average LPT stg2 conditions are below the saturation point of sodium sulfate both at take-off and climb, but at climb, the temperature drops below the lower temperature limit of hot corrosion. LPT stg1 has higher pressure than LPT stg2 and remains in the hot corrosion temperature region for both at take-off and climb. Consequently, in the investigated type of engine, the first stage of the LPT was found to be the most affected by hot corrosion.

For the engine type that was investigated in this paper and for engines of similar technology level, TET has a typical range of 1550K-1650K at take-off, 1400K-1500K at climb and 1200-1350K cruise. The TET influences the temperature ranges of all the downstream components. Therefore, the hot corrosion conclusions are based on this specific type of engine, but the same rationale and methodology can be used to assess hot corrosion in engines of different technology levels. For example, a typical cruise TET in the investigated engine is 1200-1350K, which means the HPT will be in the range of hot corrosion during cruise; perhaps a small part of LPT stg1 will marginally be in the lower end of hot corrosion range, and LPT stg2 will most likely be out of range. A new engine of higher technology level, which may have a TET of 1500K at cruise will have a decreased risk of HPT hot corrosion progress during cruise, but this means that both LPT stg1 and LPT stg2 will be in the temperature range of hot corrosion at cruise. On the other side, the higher take-off and climb TETs of newer engines are expected to bring both the HPT and LPT stg1 above the temperature range of hot corrosion, and LPT stg2 and stg3 will likely become more affected.

C. Current Limitations & Future Work

This framework considers sodium sulfate and sodium chloride from sea salt, sulfate from particulate matter and sulfur impurities in the fuel. Additional species in lower concentrations that can be considered in the future are the potassium chloride in the sea salt, vanadium traces in heavily polluted areas and vanadium impurities in the fuel. Hot corrosion experimental models that include the effect of all the aforementioned species are currently missing. However, if suitable experimental models become available, the same methodology can incorporate the currently missing species.

In the hot corrosion module, a temperature correction factor, based on experimental results, was combined with the baseline hot corrosion rate model that was developed under constant temperature. This temperature correction factor represents the trend of hot corrosion rate over the temperature. However, this trend hides some effects:

- Reaction rates sensitivity to temperature
- Change in the equilibrium composition of the gas and deposits
- The activation of different chemical reactions due to the change in the surface species
- Evaporation/deposition of species

In a similar manner, the peaks in the hot corrosion rate have also been explained by Sumner et al. [15] with the underlying balance of increasing reaction rates with temperature and destabilization/evaporation of the deposit. The framework predicts changes in the gas composition and the deposition or evaporation of species, so some of the underlying effects of temperature variation with temperature may be already partly accounted for. Consequently, the implementation of this temperature correction factor is suspected of leading to an “overcorrection”.

The framework includes calculations for the salt built-up over flight time based on the combined effect of condensation/evaporation, but the experimental hot corrosion rate models available in the literature do not support this form of input. Their input is usually in mass/area/time (e.g. $\text{mg}/\text{cm}^2/\text{h}$) i.e. deposition rate. This assumption is expected to lead to an underestimation of the hot corrosion damage, as the available experimental hot corrosion rate models are functions of the deposition rate and do not relate to the salt thickness that has already accumulated. The underestimation would be expected especially during cruise because the deposition rate is zero or very low at cruise, but a salt layer may have been formed during take-off and climb when the engine is exposed to a higher amount of sea salt. However, salt-layer thickness is calculated in the framework and can be used to assist the hot corrosion assessment and potentially consider a safety factor to compensate for the damage underestimation during cruise.

Another limitation of the used hot corrosion rate model is that the effect of sulfur oxide partial pressure is only synergistic with the deposition rate. Equation (6) shows that when the deposition rate is zero, the parameter A which includes the sulfur oxide exponential factor stops having any effect, so the variation in gas sulphidation/oxidation when a deposit is not present cannot be captured. Nevertheless, hot corrosion under molten alkali salts is notably more catastrophic; consequently, it is expected that the current limitation in the modeling of pure gas effect does not affect dramatically the conclusions of this work which is focused on sodium sulfate hot corrosion. However, this limitation can be further investigated and improved in a future version of the hot corrosion framework when more relevant experimental models become available.

To summarize, the currently available experimental hot corrosion models were identified to have some limitations regarding the input parameters and the variability of the conditions. This framework should be assisted by the development of more relevant hot corrosion experiments covering a more comprehensive set of conditions that appear during aircraft engine operation and which can be estimated using the developed hot corrosion framework. The conditions of the new experimental set-ups can be specified using the presented hot corrosion framework, and the derived experimental models can be fed back into the framework to assist future predictions as part of the hot corrosion rate module (Fig. 17).

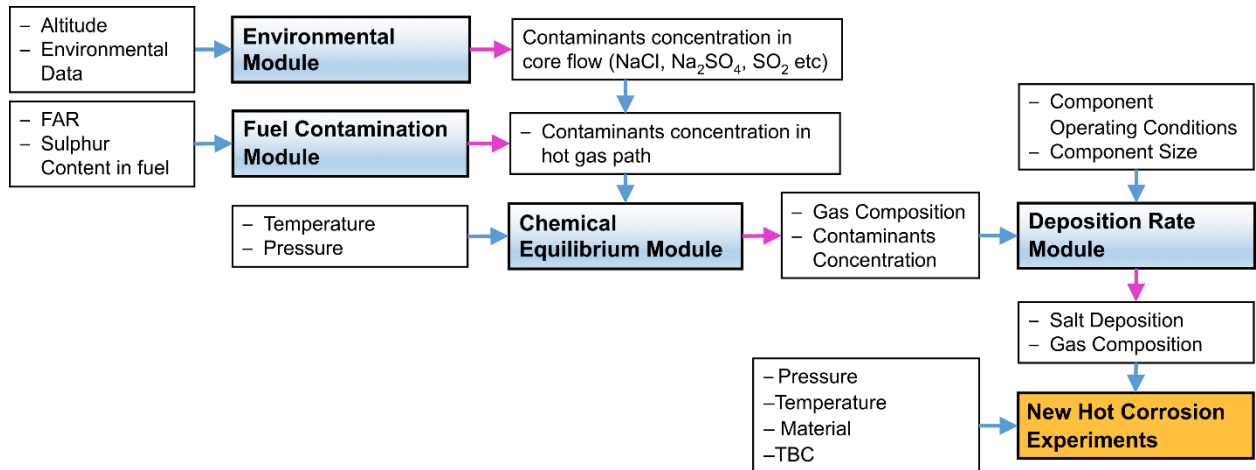


Fig. 17 Mapping of new hot corrosion experiments using the hot corrosion framework

VI. Conclusion

The development of a multidisciplinary framework that predicts and explains hot corrosion damage in aircraft engines based on the flight mission, engine performance, operational policy, flight location and fuel quality was described. The framework was applied to a typical 1500nmi flight mission with variable fuel quality, environmental

exposure and derate policy to provide insights into the damage drivers during the flight mission, the progress of the acting mechanisms, the most detrimental flight phases and the most affected components:

- A 10% derate policy leads to higher LPT stg1 hot corrosion damage than 0% derate, which is contrary to the trend commonly observed for creep, fatigue and oxidation. Hot corrosion has the unique characteristic that it is activated within an intermediate temperature range, where liquid salt deposits can be sustained, and there is a non-monotonous relationship between the rate and temperature with two peaks which have been explained as a result of the balance between increasing reaction rate with increasing temperature and destabilization of the deposits. Hot corrosion is a phenomenon with complex non-linear and non-monotonous mechanisms, and a change in the thrust or operating temperatures due to any reason (degradation, derate policy, altitude, deviation from standard atmosphere conditions) may bring a component from a peak of the hot corrosion rate curve to a point of lower rate and vice versa or may bring a component into or out of the temperature range of hot corrosion or above/below the sodium sulfate saturation point. For standard atmosphere conditions in a 1500nmi mission, a 20% derate can reduce hot corrosion damage by up to 7%.
- A combination of high sulfur oxide concentration, high sea salt concentration and high pressure while the surface temperature is in the intermediate range of 600-1000°C and the gas temperature below the salt saturation point is the main condition for deposit-induced hot corrosion to occur. In the investigated type of engine, this set of conditions appears during take-off and climb for the LPT stg1 and at take-off only for the LPT stg2.
- The LPT stg1 is the most affected component and the LPT stg2 is the second most affected component. The HPT was found to be less affected due to the higher gas temperature far above the sodium sulfate saturation temperature during take-off and climb which prevents the formation of stable molten alkali sulfate deposits.
- As long as the affected components are within the temperature range of hot corrosion and below the sodium sulfate saturation point, take-off and beginning of climb provide a combination of low altitude, therefore, high sea salt concentration in the intake air, and high pressure which increase the deposition rate. The high thrust is related to a high FAR which is translated into high level of sulfur oxides. Climb has a declining profile of salt concentration and pressure as the altitude increases, but the time of the exposure is longer than take-off. Hot corrosion is a mechanism that is temperature, environment and time-dependent.

- Hot corrosion is a chemically induced damage mechanism that requires the presence of corrosive species. In the context of aero engines, the major source of sulphur is the jet fuel and the main source of alkali metal sulphates is the sea salt concentration in the atmosphere. The impact of sodium sulfate deposition rate and sulfur oxides on the hot corrosion rate is synergistic. For a fixed ground-level sea salt concentration of 15 [ug/m³], a fuel quality variation within acceptable limits of the specifications (0.01 – 0.3%) can increase the damage by up to 128.4%. This highlights, first, the necessity for monitoring the fuel quality from various suppliers in different regions and, second, the potential for hot corrosion damage reduction with the use of SAF which are expected to have little to no sulfur impurities as they do not come from the sulfur-containing crude oil. For the average sulfur content in jet fuel of 0.1%, a rather conservative variation in sea salt concentration from 5 to 25 [ug/m³] can increase the damage by up to 80% and even higher concentrations may be found locally. The environmental exposure and sea salt concentration must be traced to the flight region and local air quality, including seasonal variations, with emphasis on the departure airport which is associated with the take-off and climb.
- Finally, the available hot corrosion experimental models are a function of the deposition rate, and not the accumulated salt layer. This leaves some uncertainty in regard to the impact of the flight segments when the deposition rate has dropped, but a salt layer has been formed and is sustained, such as during the end of climb and cruise. Particularly in advanced engines with higher operating temperatures, more components could be at risk of being in the temperature range of hot corrosion during cruise so there is a necessity for experimental models that capture the impact of the accumulated salt layer thickness when the deposition rate is low or zero.

The new framework was used to explain how the hot corrosion mechanism develops during the flight mission and estimate the damage. It also highlighted the importance of collecting additional information for hot corrosion lifing such as fuel quality, and environmental exposure during the whole mission in addition to the derate policy and other mission specifications. This methodology can contribute to a more accurate engine lifing analysis including the effect of hot corrosion and can be integrated with digital twins. Finally, the outputs can be used to inform the decision-making related to MRO scheduling and contract costing based on whether the predicted damage is expected to be below or above the repairable limits of the component.

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References

- [1] S. Ackert, "Engine Maintenance Concepts for Financiers," *Aircraft Monitor*, no. 2, p. 30, 2011.
- [2] J. Kraft and S. Kuntzagk, "Engine fleet-management - The use of digital twins from a MRO perspective," *Proceedings of the ASME Turbo Expo*, vol. 1, 2017, doi: 10.1115/GT2017-63336.
- [3] North Atlantic Treaty Organization, "Recommended Practices for Monitoring Gas Turbine Engine Life Consumption," 2000. Accessed: Mar. 26, 2024. [Online]. Available: <https://apps.dtic.mil/sti/pdfs/ADA380949.pdf>
- [4] M. Xiong, H. Wang, Q. Fu, and Y. Xu, "Digital twin-driven aero-engine intelligent predictive maintenance," *The International Journal of Advanced Manufacturing Technology*, vol. 114, no. 11–12, pp. 3751–3761, Jun. 2021, doi: 10.1007/s00170-021-06976-w.
- [5] D. A. Shifler, "Hot corrosion: a modification of reactants causing degradation," *Materials at High Temperatures*, vol. 35, no. 1–3, pp. 225–235, 2018, doi: 10.1080/09603409.2017.1404692.
- [6] R. Rajendran, "Gas turbine coatings - An overview," *Eng Fail Anal*, vol. 26, pp. 355–369, 2012, doi: 10.1016/j.engfailanal.2012.07.007.
- [7] A. Ghoshal *et al.*, "Effect of Semi-Molten Particulate on Tailored Thermal Barrier Coatings for Gas Turbine Engine," *52nd AIAA/SAE/ASEE Joint Propulsion Conference*, vol. 140, no. February, pp. 1–15, 2016, doi: 10.2514/6.2016-4852.
- [8] J. K. Sahu, B. Ravi Kumar, S. K. Das, N. Paulose, and S. L. Mannan, "Isothermal high temperature low cycle fatigue behavior of Nimonic-263: Influence of type I and type II hot corrosion," *Materials Science and Engineering A*, vol. 622, pp. 131–138, 2015, doi: 10.1016/j.msea.2014.11.016.
- [9] K. P. Lillerud, B. Haflan, and P. Kofstad, "On the reaction mechanism of nickel with SO₂+O₂/SO₃," *Oxidation of Metals*, vol. 21, no. 3–4, pp. 119–134, Apr. 1984, doi: 10.1007/BF00741467.
- [10] R. A. Rapp, "Hot corrosion of materials: A fluxing mechanism?," *Corros Sci*, vol. 44, no. 2, pp. 209–221, 2002, doi: 10.1016/S0010-938X(01)00057-9.
- [11] M. Shourgeshty, M. Aliofkhazraei, and M. M. Alipour, "Introduction to High-Temperature Coatings," in *High Temperature Corrosion*, InTech, 2016, pp. 685–694. doi: 10.5772/64282.
- [12] J. Sumner, A. Encinas-Oropesa, M. Seraffon, N. J. Simms, and J. R. Nicholls, "Lifetime prediction for gas turbine materials in power generation," *International Conference on Sustainable Power Generation and Supply (SUPERGEN 2012)*, pp. 103–103, 2012, doi: 10.1049/cp.2012.1806.
- [13] E. Kosieniak, K. Biesiada, J. Kaczorowski, and M. Innocenti, "Corrosion failures in gas turbine hot components," *Journal of Failure Analysis and Prevention*, vol. 12, no. 3, pp. 330–337, 2012, doi: 10.1007/s11668-012-9571-3.
- [14] S. Gialanella and A. Malandrucolo, *Aerospace Alloys*, 1st ed. in Topics in Mining, Metallurgy and Materials Engineering. Cham, Switzerland: Springer International Publishing, 2020. doi: 10.1007/978-3-030-24440-8.
- [15] J. Sumner, A. Potter, N. J. Simms, and J. E. Oakey, "Hot corrosion resistance of gas turbine materials in combusted syngas environments," *Materials at High Temperatures*, vol. 32, no. 1–2, pp. 177–187, 2015, doi: 10.1179/0960340914Z.00000000098.
- [16] R. Chaharlang, E. Hajjari, S. M. L. Baghal, and M. Siahpoosh, "Premature damage of the second stage nozzle guide vanes of a gas turbine made of Inconel 738LC," *Eng Fail Anal*, vol. 105, no. March, pp. 803–816, 2019, doi: 10.1016/j.engfailanal.2019.07.053.
- [17] C. Leyens, I. G. Wright, and B. A. Pint, "Hot corrosion of an EB-PVD thermal-barrier coating system at 950 °C," *Oxidation of Metals*, vol. 54, no. 5–6, pp. 401–424, 2000, doi: 10.1023/A:1004634400479.
- [18] P. Lortrakul, R. W. Trice, K. P. Trumble, and M. A. Dayananda, "Investigation of the mechanisms of Type-II hot corrosion of superalloy CMSX-4," *Corros Sci*, vol. 80, pp. 408–415, 2014, doi: 10.1016/j.corsci.2013.11.048.
- [19] T. Gheno and B. Gleeson, "On the Reaction Mechanism of MCrAlY Alloys with Oxide-Sulfate Deposits at 1100 °C," *Oxidation of Metals*, vol. 86, no. 5–6, pp. 385–406, 2016, doi: 10.1007/s11085-016-9649-5.
- [20] V. Shukla, R. Jayaganthan, and V. Tewari, "Oxidation and Hot Corrosion Behaviour of Ni-based Superalloy Inconel 718 in Na₂SO₄ - 0.75 V₂O₅ Environment at Elevated Temperature," *J Mater Eng Perform*, vol. 3, no. 1, pp. 20–24, 2003, [Online]. Available: <http://link.springer.com/10.1361/105994906X150858>
- [21] A. Encinas-Oropesa, G. L. Drew, M. C. Hardy, A. J. Leggett, J. R. Nicholls, and N. J. Simms, "Effects of oxidation and hot corrosion in a nickel disc alloy," *Superalloys 2008: 11th International Symposium on Superalloys*, no. September, pp. 609–618, 2008, doi: 10.7449/2008/Superalloys_2008_609_618.

- [22] V. Sreenivasulu and M. Manikandan, "High-temperature corrosion behaviour of air plasma sprayed Cr3C2-25NiCr and NiCrMoNb powder coating on alloy 80A at 900 °C," *Surf Coat Technol*, vol. 337, no. January, pp. 250–259, 2018, doi: 10.1016/j.surfcoat.2018.01.011.
- [23] N. J. Simms, P. J. Smith, A. Encinas-Oropesa, S. Ryder, J. R. Nicholls, and J. E. Oakey, "Development of Type II Hot Corrosion in Solid Fuel Fired Gas Turbines," in *Lifetime Modelling of High Temperature Corrosion Processes: (EFC 34)*, Maney Publishing, 2001, pp. 246–260.
- [24] N. J. Simms, J. R. Nicholls, and J. E. Oakey, "Corrosion and Lifetime Modelling of Components in Coal Fired Combined Cycle Power Systems," in *Lifetime Modelling of High Temperature Corrosion Processes*, 1st ed., CRC Press, 2002, pp. 379–397.
- [25] N. J. Simms *et al.*, "Predicting type II hot corrosion in industrial gas turbines," *Journal of Corrosion Science and Engineering*, vol. 6, pp. 1–25, 2003.
- [26] J. R. Nicholls, N. J. Simms, and A. Encinas-Oropesa, "Modelling hot corrosion in industrial gas turbines," *Materials at High Temperatures*, vol. 24, no. 3, pp. 149–162, 2007, doi: 10.3184/096034007X263587.
- [27] N. J. Simms, A. Encinas-Oropesa, and J. R. Nicholls, "Modelling the Development of Type I Hot Corrosion on Coated and Uncoated Single Crystal Superalloys," *Materials Science Forum*, vol. 595–598, pp. 689–698, 2008, doi: 10.4028/www.scientific.net/MSF.595-598.689.
- [28] J. Sumner, A. Potter, N. J. Simms, and J. E. Oakey, "Modeling gas turbine materials' hot corrosion degradation in combustion environments from H2-rich syngas," *Materials and Corrosion*, vol. 68, no. 2, pp. 205–214, 2017, doi: 10.1002/maco.201508779.
- [29] J. Lacaille and R. Langhendries, "Corrosion Risk Estimation and Cause Analysis on Turbofan Engine," *Annual Conference of the PHM Society*, vol. 14, no. 1, Oct. 2022, doi: 10.36001/phmconf.2022.v14i1.3254.
- [30] K. S. Chan, M. P. Enright, J. Moody, C. Thomas, and A. Fast, "Hotpits: The Darwin approach to assessing risk of hot corrosion-induced fracture in gas turbine components," *ICF 2017 - 14th International Conference on Fracture*, vol. 1, no. September 2019, pp. 429–430, 2017, doi: 10.1016/j.engfracmech.2020.106889.
- [31] K. S. Chan, M. P. Enright, J. P. Moody, and S. H. K. Fitch, "Physics-based modeling tools for predicting Type II hot corrosion in nickel-based superalloys," *Proceedings of the International Symposium on Superalloys*, vol. 2016-Janua, pp. 917–925, 2016, doi: 10.1002/9781119075646.ch98.
- [32] C. A. C. Sequeira, "Hot Corrosion," in *High Temperature Corrosion*, First., Wiley, 2019, pp. 340–360. doi: 10.1002/9781119474371.ch15.
- [33] T. Tinga, W. P. J. Visser, W. B. de Wolf, and M. J. Broomhead, "Integrated Lifting Analysis Tool for Gas Turbine Components," in *ASME Turbo Expo 2000: Power for Land, Sea, and Air*, Munich, Germany: American Society of Mechanical Engineers, May 2000. doi: 10.1115/2000-GT-0646.
- [34] W. Mohamed, S. Eshati, P. Pilidis, S. Ogaji, P. Laskaridis, and A. Nasir, "A method to evaluate the impact of power demand on HPT blade creep life," in *Proceedings of the ASME Turbo Expo*, 2011, pp. 1–9. doi: 10.1115/GT2011-45092.
- [35] S. Eshati, P. Laskaridis, A. Haslam, and P. Pilidis, "The influence of humidity on the creep life of a high pressure gas turbine blade: Part II case study," in *Proceedings of the ASME Turbo Expo*, 2012, pp. 281–288. doi: 10.1115/GT2012-69462.
- [36] H. Hanumanthan, A. Stitt, P. Laskaridis, and R. Singh, "Severity estimation and effect of operational parameters for civil aircraft jet engines," *Proc Inst Mech Eng G J Aerosp Eng*, vol. 226, no. 12, pp. 1544–1561, 2012, doi: 10.1177/0954410011424854.
- [37] A. O. Abu, S. Eshati, P. Laskaridis, and R. Singh, "Aero-engine turbine blade life assessment using the Neu/Sehitoglu damage model," *Int J Fatigue*, vol. 61, pp. 160–169, 2014, doi: 10.1016/j.ijfatigue.2013.11.015.
- [38] R. K. Mishra and C. Beura, "Life consumption assessment of a large jet engine," *Journal of Failure Analysis and Prevention*, vol. 14, no. 4, pp. 519–529, 2014, doi: 10.1007/s11668-014-9842-2.
- [39] E. A. Ogiriki, Y. G. Li, and Th. Nikolaidis, "Prediction and Analysis of Impact of Thermal Barrier Coating Oxidation on Gas Turbine Creep Life," *J Eng Gas Turbine Power*, vol. 138, no. 12, p. 121501, 2016, doi: 10.1115/1.4034020.
- [40] E. A. Ogiriki, Y. G. Li, T. Nikolaidis, T. E. Isaiah, and G. Sule, "Effect of Fouling, Thermal Barrier Coating Degradation and Film Cooling Holes Blockage on Gas Turbine Engine Creep Life," in *Procedia CIRP*, Elsevier B.V., 2015, pp. 228–233. doi: 10.1016/j.procir.2015.07.017.
- [41] A. Haslam, A. Abu, and P. Laskaridis, "A method for the assessment of operational severity for a high pressure turbine blade of an aero-engine," *Open Engineering*, vol. 5, no. 1, pp. 447–456, 2015, doi: 10.1515/eng-2015-0041.

- [42] D. Shi, C. Wang, X. Yang, and S. Li, "Failure assessment of the first stage high-pressure turbine blades in an aero-engine turbine," *Fatigue Fract Eng Mater Struct*, vol. 40, no. 12, pp. 2092–2106, 2017, doi: 10.1111/ffe.12630.
- [43] D. Zhou, T. Wei, H. Zhang, S. Ma, and S. Weng, "A Damage Evaluation Model of Turbine Blade for Gas Turbine," *J Eng Gas Turbine Power*, vol. 139, no. 9, pp. 1–9, 2017, doi: 10.1115/1.4036060.
- [44] M. Rezazadeh Reyhani, M. Alizadeh, A. Fathi, and H. Khaledi, "Turbine blade temperature calculation and life estimation - a sensitivity analysis," *Propulsion and Power Research*, vol. 2, no. 2, pp. 148–161, Jun. 2013, doi: 10.1016/j.jprr.2013.04.004.
- [45] O. Alozie, Y. G. Li, M. Diakostefanis, X. Wu, X. Shong, and W. Ren, "Assessment of degradation equivalent operating time for aircraft gas turbine engines," *The Aeronautical Journal*, vol. 124, no. 1274, pp. 549–580, Apr. 2020, doi: 10.1017/aer.2019.153.
- [46] E. Maqueda, "Engine Overall Severity," MSc Thesis, Cranfield University, 2014.
- [47] U. Zuazo, "Investigation into Combined Severity by means of Lifting and Degradation Calculations," MSc Thesis, Cranfield University, 2014.
- [48] A. C. Stitt, "A Physics-Based Maintenance Cost Methodology for Commercial Aircraft Engines," EngD Thesis, Cranfield University, 2014.
- [49] P. P. Walsh and P. Fletcher, *Gas Turbine Performance*, 2nd ed. John Wiley & Sons, 2004. doi: 10.1002/9780470774533.
- [50] W. L. McMillan, "Development of a Modular Type Computer Program for the Calculation of Gas Turbine Off-Design Performance," PhD Thesis, Cranfield Institute of Technology, 1974.
- [51] J. Janikovic, "Gas turbine transient performance modeling for engine flight path cycle analysis," PhD Thesis, Cranfield University, 2010. Accessed: Mar. 26, 2024. [Online]. Available: https://dspace.lib.cranfield.ac.uk/bitstream/handle/1826/7894/Jan_Janikovic_Thesis_2010.pdf
- [52] J. Kurzke and I. Halliwell, *Propulsion and Power*, no. September. Cham: Springer International Publishing, 2018. doi: 10.1007/978-3-319-75979-1.
- [53] C. Saumweber and A. Schulz, "Effect of Geometry Variations on the Cooling Performance of Fan-Shaped Cooling Holes," *J Turbomach*, vol. 134, no. 6, Nov. 2012, doi: 10.1115/1.4006290.
- [54] K.-D. Lee and K.-Y. Kim, "Optimization of a Fan-Shaped Hole for Film Cooling Using a Surrogate Model," in *Volume 3: Heat Transfer, Parts A and B*, ASMEDC, Jan. 2009, pp. 505–514. doi: 10.1115/GT2009-59520.
- [55] W. Jin, Y. X. Jia, J. Lei, W. T. Ji, and J. M. Wu, "Coupled heat transfer analysis of internal and film cooling of turbine blade under medium temperature conditions," *Appl Therm Eng*, vol. 214, Sep. 2022, doi: 10.1016/j.applthermaleng.2022.118792.
- [56] J. Ahn, J. C. Song, and J. S. Lee, "Fully coupled large eddy simulation of conjugate heat transfer in a ribbed channel with a 0.1 blockage ratio," *Energies (Basel)*, vol. 14, no. 8, Apr. 2021, doi: 10.3390/en14082096.
- [57] S. Eshati, P. Laskaridis, A. Haslam, and P. Pilidis, "The Influence of Humidity on the Creep Life of a High Pressure Gas Turbine Blade: Part I Heat Transfer Model," in *Proceedings of ASME Turbo Expo 2012*, Copenhagen, Denmark: American Society of Mechanical Engineers, Jun. 2012, pp. 281–288. doi: 10.1115/GT2012-69462.
- [58] L. Torbidoni and A. F. Massardo, "Analytical Blade Row Cooling Model for Innovative Gas Turbine Cycle Evaluations Supported by Semi-Empirical Air-Cooled Blade Data," *J Eng Gas Turbine Power*, vol. 126, no. 3, p. 498, 2004, doi: 10.1115/1.1707030.
- [59] C. Maravilla Herrera, S. Yepifanov, and I. Loboda, "Improved Turbine Blade Lifetime Prediction," in *Volume 6: Ceramics; Controls, Diagnostics and Instrumentation; Education; Manufacturing Materials and Metallurgy; Honors and Awards*, American Society of Mechanical Engineers, Jun. 2015. doi: 10.1115/GT2015-43046.
- [60] E. Pontika, P. Laskaridis, F. Montana Gonzalez, W. Jacobs, and A. Mills, "Mapping the Effect of Variable HPT Blade Cooling on Fuel Burn, Engine Life and Emissions for Fleet Optimization using Active Control," in *AIAA SCITECH 2023 Forum*, Reston, Virginia: American Institute of Aeronautics and Astronautics, Jan. 2023. doi: 10.2514/6.2023-0706.
- [61] D. G. Ainley, "Internal Air-Cooling for Turbine Blades. A General Design Survey," 1957.
- [62] J. W. Chapman, J. Kratz, T. H. Guo, and J. Litt, "Integrated turbine tip clearance and gas turbine engine simulation," *52nd AIAA/SAE/ASEE Joint Propulsion Conference*, 2016, no. October, 2016, doi: 10.2514/6.2016-5047.
- [63] J. H. Horlock and L. Torbidoni, "Turbine blade cooling: The blade temperature distribution," *Proceedings of the Institution of Mechanical Engineers, Part A: Journal of Power and Energy*, vol. 220, no. 4, pp. 343–353, 2006, doi: 10.1243/09576509JPE177.

- [64] S. Patil and D. Tafti, "Large-eddy simulation with zonal near wall treatment of flow and heat transfer in a ribbed duct for the internal cooling of turbine blades," *J Turbomach*, vol. 135, no. 3, Mar. 2013, doi: 10.1115/1.4006640.
- [65] L. Cai, Y. Hou, F. Li, Y. Li, S. Wang, and J. Mao, "Impacts of Startup, Shutdown and Load Variation on Transient Temperature and Thermal Stress Fields within Blades of Gas Turbines," *Journal of Thermal Science*, vol. 31, no. 3, pp. 727–740, May 2022, doi: 10.1007/s11630-022-1603-z.
- [66] R. Kumar, V. S. Kumar, M. M. Butt, N. A. Sheikh, S. A. Khan, and A. Afzal, "Thermo-mechanical analysis and estimation of turbine blade tip clearance of a small gas turbine engine under transient operating conditions," *Appl Therm Eng*, vol. 179, Oct. 2020, doi: 10.1016/j.applthermaleng.2020.115700.
- [67] Z. Li, Y. G. Li, and S. Sampath, "Aeroengine transient performance simulation integrated with generic heat soakage and tip clearance model," *Aeronautical Journal*, vol. 126, no. 1302, pp. 1265–1287, Aug. 2022, doi: 10.1017/aer.2022.15.
- [68] K. M. Kim, J. S. Park, D. H. Lee, T. W. Lee, and H. H. Cho, "Analysis of conjugated heat transfer, stress and failure in a gas turbine blade with circular cooling passages," *Eng Fail Anal*, vol. 18, no. 4, pp. 1212–1222, 2011, doi: 10.1016/j.engfailanal.2011.03.002.
- [69] K. Kusterer, D. Bohn, T. Sugimoto, and R. Tanaka, "Conjugate Calculations for a Film-Cooled Blade Under Different Operating Conditions," in *Volume 3: Turbo Expo 2004*, ASMEDC, Jan. 2004, pp. 675–684. doi: 10.1115/GT2004-53719.
- [70] M. Alizadeh, A. Izadi, and A. Fathi, "Sensitivity analysis on turbine blade temperature distribution using conjugate heat transfer simulation," *J Turbomach*, vol. 136, no. 1, pp. 1–13, 2013, doi: 10.1115/1.4024637.
- [71] R. Liu, H. Li, R. You, Y. Huang, and Z. Tao, "Conjugate heat transfer study of various cooling structures and sensitivity analysis of overall cooling effectiveness," *Sci Rep*, vol. 12, no. 1, Dec. 2022, doi: 10.1038/s41598-022-23948-6.
- [72] F. Duchaine, A. Corpron, L. Pons, V. Moureau, F. Nicoud, and T. Poinot, "Development and assessment of a coupled strategy for conjugate heat transfer with Large Eddy Simulation: Application to a cooled turbine blade," *Int J Heat Fluid Flow*, vol. 30, no. 6, pp. 1129–1141, Dec. 2009, doi: 10.1016/j.ijheatfluidflow.2009.07.004.
- [73] L. Laveneziana *et al.*, "Conjugate Heat Transfer Analysis of the Aero-Thermal Impact of Different Feeding Geometries for Internal Cooling in Lifetime Extension Processes for Heavy-Duty Gas Turbines," *Energies (Basel)*, vol. 15, no. 9, May 2022, doi: 10.3390/en15093022.
- [74] D. H. Lee, K. M. Kim, S. Shin, and H. H. Cho, "Thermal Analysis in a Film Cooling Hole with Thermal Barrier Coating," *J Thermophys Heat Trans*, vol. 23, no. 4, pp. 843–847, Oct. 2009, doi: 10.2514/1.41341.
- [75] A. O. Abu, P. Laskaridis, and R. Singh, "Integrated approach for stress based lifing of aero gas turbine blades," PhD Thesis, Cranfield University, 2013. [Online]. Available: <http://dspace.lib.cranfield.ac.uk/handle/1826/8405>
- [76] J. Kraft, V. Sethi, and R. Singh, "Optimization of aero gas turbine maintenance using advanced simulation and diagnostic methods," *J Eng Gas Turbine Power*, vol. 136, no. 11, 2014, doi: 10.1115/1.4027356.
- [77] Y. Il Yun, I. Y. Park, and S. J. Song, "Performance Degradation due to Blade Surface Roughness in a Single-Stage Axial Turbine," *J Turbomach*, vol. 127, no. 1, p. 137, 2005, doi: 10.1115/1.1811097.
- [78] N. S. Jacobson, "Sodium sulfate: Deposition and dissolution of silica," *Oxidation of Metals*, vol. 31, no. 1–2, pp. 91–103, 1989, doi: 10.1007/BF00665488.
- [79] NIST, "sodium sulphate." Accessed: Nov. 18, 2019. [Online]. Available: <https://webbook.nist.gov/cgi/cbook.cgi?ID=C7757826&Mask=4#Thermo-Phase>
- [80] J. Wisniak, "Historical development of the vapor pressure equation from Dalton to Antoine," *Journal of Phase Equilibria*, vol. 22, no. 6, pp. 622–630, 2001, doi: 10.1361/105497101770332433.
- [81] G. H. Meier, "Invited Review Paper in Commemoration of Over 50 Years of Oxidation of Metals: Current Aspects of Deposit-Induced Corrosion," *Oxidation of Metals*, Jan. 2021, doi: 10.1007/s11085-020-10015-6.
- [82] P. Laskaridis, P. Pilidis, and P. Kotsiopoulos, "An Integrated Engine-Aircraft Performance Platform for Assessing New Technologies in Aeronautics," in *ISABE 2005*, Munich, 2005, pp. 1–13.
- [83] P. Giannakakis, P. Laskaridis, T. Nikolaidis, and A. I. Kalfas, "Toward a scalable propeller performance map," *J Propuls Power*, vol. 31, no. 4, pp. 1073–1082, 2015, doi: 10.2514/1.B35498.
- [84] D. Vallero, "The Science of Air Pollution," in *Fundamentals of Air Pollution*, 2014, pp. 43–81. doi: 10.1016/B978-0-12-401733-7.00003-7.
- [85] Y. Shinozuka, A. D. Clarke, S. G. Howell, V. N. Kapustin, and B. J. Huebert, "Sea-salt vertical profiles over the Southern and tropical Pacific oceans: Microphysics, optical properties, spatial variability, and variations

- with wind speed,” *Journal of Geophysical Research D: Atmospheres*, vol. 109, no. 24, pp. 1–17, 2004, doi: 10.1029/2004JD004975.
- [86] D. S. Bisht *et al.*, “Tethered balloon-born and ground-based measurements of black carbon and particulate profiles within the lower troposphere during the foggy period in Delhi, India,” *Science of the Total Environment*, vol. 573, pp. 894–905, 2016, doi: 10.1016/j.scitotenv.2016.08.185.
- [87] D. C. BLANCHARD, A. H. WOODCOCK, and R. J. CIPRIANO, “The vertical distribution of the concentration of sea salt in the marine atmosphere near Hawaii,” *Tellus B*, vol. 36 B, no. 2, pp. 118–125, 1984, doi: 10.1111/j.1600-0889.1984.tb00233.x.
- [88] S. Philip *et al.*, “Global chemical composition of ambient fine particulate matter for exposure assessment,” *Environ Sci Technol*, vol. 48, no. 22, pp. 13060–13068, 2014, doi: 10.1021/es502965b.
- [89] Y. Cheng *et al.*, “PM_{2.5} and PM_{10-2.5} chemical composition and source apportionment near a Hong Kong roadway,” *Particuology*, vol. 18, pp. 96–104, 2015, doi: 10.1016/j.partic.2013.10.003.
- [90] Y. Liu, Y. Yan, C. Dai, and J. Li, “A simplified chemical reaction mechanism for surrogate fuel of aviation kerosene,” *Chem Res Chin Univ*, vol. 33, no. 2, pp. 274–281, 2017, doi: 10.1007/s40242-017-6280-1.
- [91] B.-J. Zhong and H.-S. Peng, “Development of a Skeletal Mechanism for Aviation Kerosene Surrogate Fuel,” *J Propuls Power*, pp. 1–7, 2019, doi: 10.2514/1.B37421.
- [92] S. Bonifazi *et al.*, “Handbook of Aviation Fuel Properties,” Atlanta, Georgia, USA, 1983. Accessed: Mar. 26, 2024. [Online]. Available: <https://crao.org/crc-report-no-635/>
- [93] R. Javadli and A. de Klerk, “Desulfurization of heavy oil,” *Appl Petrochem Res*, vol. 1, no. 1–4, pp. 3–19, 2012, doi: 10.1007/s13203-012-0006-6.
- [94] Chevron Products Company, “Aviation Fuels Technical Review,” 2000. Accessed: Mar. 26, 2024. [Online]. Available: <https://www.chevron.com/-/media/chevron/operations/documents/aviation-tech-review.pdf>
- [95] C. K. Gilmore *et al.*, “Environmental Cost-Benefit Analysis of Ultra Low Sulfur Jet Fuel: PARTNER Project 27 Final Report,” 2011. Accessed: Mar. 27, 2024. [Online]. Available: <https://ascent.aero/documents/2020/02/environmental-costbenefit-analysis-of-ultra-low-sulfur-jet-fuel.pdf/>
- [96] ASTM, “Standard Specification for Aviation Turbine Fuels,” *Annual Book of ASTM Standards*, pp. 1–16, 2010, doi: 10.1520/D1655-10.2.
- [97] W. C. Reynolds, “The element potential method for chemical equilibrium analysis: Implementation in the interactive program STANJAN,” 1986.
- [98] E. Pontika, P. Laskaridis, T. Nikolaidis, and M. Koster, “Comparison of sodium sulphate deposition rate models based on operational factors influencing hot corrosion damage in aero-engines,” *Proceedings of the ASME Turbo Expo*, vol. 2E-2020, pp. 1–13, 2020, doi: 10.1115/GT2020-15715.
- [99] D. Sun, J. Xu, and Q. Chen, “Modeling of the evaporation and condensation phase-change problems with FLUENT,” *Numerical Heat Transfer, Part B: Fundamentals*, vol. 66, no. 4, pp. 326–342, 2014, doi: 10.1080/10407790.2014.915681.
- [100] R. K. Ahluwalia, “Correlation Between Sodium Sulfate Mass Transfer and Low-Temperature Hot Corrosion,” *ASME 88-GT*, 1988, doi: 10.1115/88-GT-133.
- [101] J. E. Helt, “Evaluation of Alkali Metal Sulphate Dew Point Measurement for Detection of hot corrosion conditions in PFBC flue gas,” Argonne, Illinois, 1980.
- [102] D. E. Rosner, B.-K. Chen, G. C. Fryburg, and F. J. Kohl, “Chemically frozen multicomponent boundary layer theory of salt and/or ash deposition rates from combustion gases,” *Combustion Science and Technology*, vol. 20, no. 3–4, pp. 87–106, 1979, doi: 10.1080/00102207908946899.
- [103] G. R. Holcomb, “Calculation of reactive-evaporation rates of chromia,” *Oxidation of Metals*, vol. 69, no. 3–4, pp. 163–180, 2008, doi: 10.1007/s11085-008-9091-4.
- [104] M. A. Nettleton and E. Raask, “The rate of evaporation of potassium sulphate,” *Journal of Applied Chemistry*, vol. 17, no. 1, pp. 18–21, 1967, doi: 10.1002/jctb.5010170105.
- [105] D. Gaskell, *An Introduction to Transport Phenomena In Materials Engineering*, 2nd ed. Momentum Press, 2012. doi: 10.5643/9781606503577.
- [106] D. W. Bonnel and J. W. Hastie, *Transpiration Mass Spectrometry of High Temperature Vapors*. Gaithersburg, Maryland: Department of Commerce, U.S. Department of Commerce, 1979.
- [107] S. Akwaboa, M. B. Silva, and D. E. Wolfe, “Comparison of Cyclic Oxidation Performance of APS and EBPVD Processed TBCs on IN738 with a Bond Coat of NiCoCrAlY Powder with 0.25% Hf,” *Proceedings of ASME 2009 International Mechanical Engineering Congress & Exposition*, pp. 1–6, 2009.
- [108] L. Chen, “Processing, microstructures and properties of thermal barrier coatings (TBCs) by plasma spraying (PS),” in *Thermal Barrier Coatings*, 1st ed., H. Xu and H. Guo, Eds., Woodhead Publishing, 2011, pp. 132–160. doi: 10.1533/9780857090829.2.132.

- [109] H. Zhao, C. G. Levi, and H. N. G. Wadley, "Molten silicate interactions with thermal barrier coatings," *Surf Coat Technol*, vol. 251, pp. 74–86, 2014, doi: 10.1016/j.surfcoat.2014.04.007.
- [110] R. Naraparaju, M. Hüttermann, U. Schulz, and P. Mechnich, "Tailoring the EB-PVD columnar microstructure to mitigate the infiltration of CMAS in 7YSZ thermal barrier coatings," *J Eur Ceram Soc*, vol. 37, no. 1, pp. 261–270, 2017, doi: 10.1016/j.jeurceramsoc.2016.07.027.
- [111] H. Haight, A. Potter, J. Sumner, and S. Gray, "New Technique to Map Hot Corrosion Damage: CMSX-4 Example," *Oxidation of Metals*, vol. 84, no. 5–6, pp. 607–619, Dec. 2015, doi: 10.1007/s11085-015-9590-z.
- [112] W. James and P. O'Dell, "Derated Climb Performance in Large Civil Aircraft," in *Boeing Performance and Flight Operations Engineering Conference*, 2005. Accessed: Mar. 26, 2024. [Online]. Available: <https://www.theairlinepilots.com/forumarchive/quickref/deratedclimbperformance.pdf>
- [113] S. L. Gong, L. A. Barrie, and M. Lazare, "Canadian Aerosol Module (CAM): A size-segregated simulation of atmospheric aerosol processes for climate and air quality models 2. Global sea-salt aerosol and its budgets," *Journal of Geophysical Research Atmospheres*, vol. 107, no. 24, pp. 1–14, 2002, doi: 10.1029/2001JD002004.
- [114] R. Donaldson, D. Fischer, J. Gough, and M. Rysz, "Economic Impact of Derated Climb on Large Commercial Engines," *2007 Boeing Performance and Flight Operations Engineering Conference*, pp. 8.1-8.11, 2007, Accessed: Mar. 26, 2024. [Online]. Available: https://www.smartcockpit.com/docs/Economic_Impact_of_Derated_Climb_on_Large_Commercial_Engines.pdf



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