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Experimental characterization of the cycling thermo-mechanical behavior of a reusable LOX-LNG regeneratively cooled thrust chamber

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Abstract

This paper presents the numerical life prediction of the Huracan LOX/LNG rocket engine's thrust chamber assembly developed as part of the Nyx Moon vehicle by The Exploration Company. The results are validated by means of an experimental test campaign conducted to evaluate the cycling thermo-mechanical performance of the hardware at the German Aerospace Center (DLR) in Lampoldshausen, with support from the European Space Agency (ESA).
A deep material characterization of the Additive Manufactured heat tread Inocnel718 involved in the thrust chamber has been used to finely calibrate nonlinear hardening models. High temperature low cycle fatigue and high temperature tensile tests are utilized to calibrate hardening models involved in the material constitutive numerical models responsible for the life prediction of the hardware. Experimental data have been involved to validate the thermal boundary conditions.
Results highlight the benefits of using additive manufacturing nickel-based alloys for combustion chambers and thermal barrier coating. Usage factors related to ratcheting and low cycle fatigue have been involved for life prediction to investigate the failure modes in the regenerative cooling channels.

1. Introduction

1.1 Nyx Spacecraft and Huracan Engine Development

The Exploration Company is actively developing, manufacturing, and preparing to operate a fleet of reusable orbital vehicles capable of in-orbit refueling. These vehicles are designed to support a wide range of missions, including operations in Low Earth Orbit (LEO) and round-trip flights to the moon. A core objective of the company is to create sustainable and cost-effective transportation infrastructure for space. The Nyx capsule for lunar missions will be mounted atop a service module equipped with a cryogenic propulsion system. This system utilizes bio-methane and liquid oxygen as propellant and oxidizer, respectively, aligning with the company's emphasis on sustainability and reusability.

For lunar operations, Nyx Moon (Figure 1) is being developed to handle cargo transport between Earth and the Moon, as well as missions within the cislunar space. These include berthing and docking with lunar space stations, executing propulsive lunar landings, and performing local 'hops' on the Moon's surface. Crucially, Nyx Moon is also designed to return safely to Earth, enhancing mission versatility. In-orbit refueling-either from dedicated fuel depots or from propellants produced on the Moon-is considered a pivotal technology to extend mission durations and reduce operational costs.

A central component of the propulsion system for Nyx Moon is the Huracan engine, a liquid rocket engine currently under development. This engine is being designed for reusability, system simplicity, deep-throttling capability,

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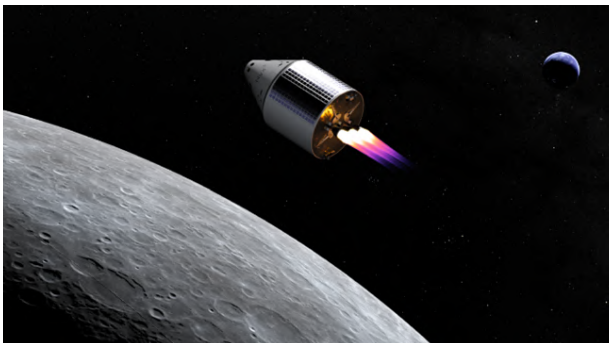

Figure 1: The Exploration Company Nyx Moon rendering module

and cost-effective manufacturing and operation. A key part of this effort is the development of the thrust chamber assembly (TCA), specifically the reusable main combustion chamber, which is being created in collaboration with the European Space Agency (ESA) and the German Aerospace Center (DLR) at its Institute of Space Propulsion in Lampoldshausen, Germany.

This paper outlines the first full-scale hot-fire thrust chamber assembly cycling hot firing test of Huracan engine at the European Research and Technology Test Bench P8 in DLR Lampoldshausen propulsion test site. Detailed attention is given to the thermal barrier coating effect on the predicted cycle to failure of the throat section of the combustion chamber. This comprehensive test campaign provides critical data on performance, reliability, and thermal behavior of the coated combustion chamber aiming to de-risk the use of thermal barrier coating in the frame of reusable regenerative cooled liquid rocket engine. A comparison with not coated combustion chamber is also provided.

1.2 Liquid Rocket Engine regenerative cooled combustion chamber

The thrust chamber of a bipropellant, regeneratively cooled liquid rocket engine is a crucial component in today's reusable launch systems, as it provides continuous propulsion under extreme operating conditions. One of the main engineering hurdles in designing such systems is effectively managing the intense heat loads generated during engine operation. To address this issue, regenerative cooling systems (RCS) are widely adopted. These systems perform two essential functions: they dissipate heat from the combustion-facing chamber walls and simultaneously raise the temperature of the incoming propellants, thereby enhancing the engine's thermal efficiency.

Repeated thermal cycling of the thrust chamber-especially during multiple engine ignitions-can lead to plastic deformation accumulation over cycles in the inner wall hot gas side. This fatigue behavior significantly impacts the service life of the component. Therefore, reliable life prediction is not only critical for maintaining flight safety and engine performance but also for supporting cost-effective refurbishment and reuse. Accurate predictions require integrating detailed thermal and structural analyses into the design process. Typically, the thrust chamber includes an inner liner and an outer shell composed of high-strength super-alloys for mechanical reinforcement. The inner wall and the close out wall are connected via ribs, forming the channels. This complex structure, under nominal operative high temperature conditions, generate complex internal strain fields that threaten the structural durability of the chamber.

Existing life prediction techniques often fall short in accuracy, primarily due to the absence of standardized and representative test specimens capable of mimicking the engine's true thermomechanical environment. Consequently, full-scale hot-fire tests are still the most trusted method for evaluating the operational lifespan of thrust chambers. However, these tests are both expensive and time-consuming. To reduce dependency on such experimental trials, numerical simulations have gained prominence. Although modeling material responses under high-temperature, cyclic loading introduces several uncertainties, finite element analysis (FEA) remains a widely used approach for fatigue life assessment.

Within this framework, the present work present the numerical analysis results of the cycling experimental tests of the Huracan engine thrust chamber assembly with and without thermal barrier coating. A commercial finite element code is employed to simulate the chamber's response to cyclic thermal and mechanical loading, focusing particularly on ratcheting damage and low-cycle fatigue damages.

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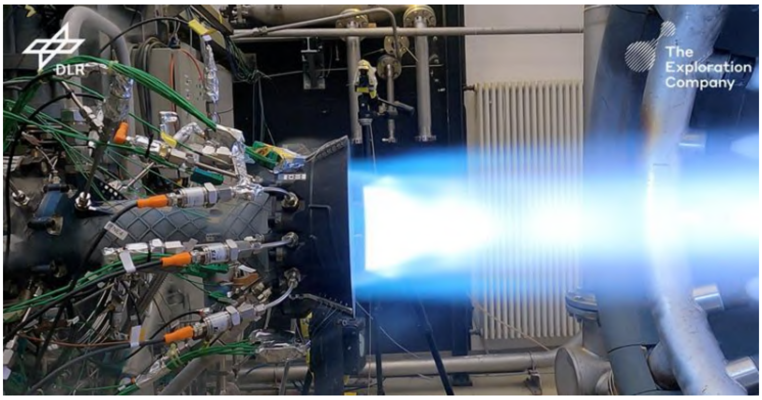

Figure 2: Huracan thrust chamber assembly third iteration hot fire test at nominal operative pressure

2. Martial and method

2.1 Test article

The test campaign described in the present work was conducted using the third iteration of the Huracan thrust chamber assembly (TCA). It took place at the P8 test bench at the German Aerospace Center (DLR) in Lampoldshausen, Germany. The P8 facility is a high-performance test stand capable of handling cryogenic propellants, and it is operated by DLR personnel. It features precise propellant mass flow measurement and rapid valve actuation, making it well-suited for advanced combustion system development. The work presented in this paper focuses on the cycling objective of the test campaign. The test article is presented in Figure 3

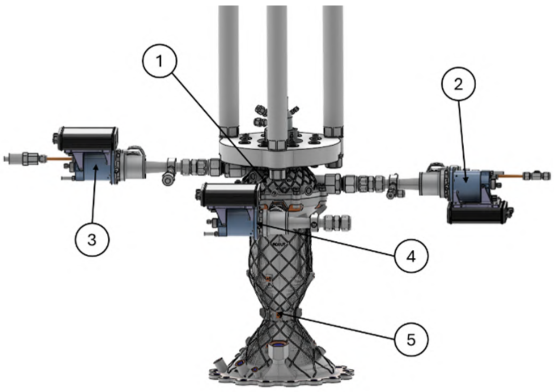

Figure 3: Test article involved in the third iteration of the Huracan thrust chamber assembly (TCA): Injector Head (IH), 2) Main Fuel Valve (MFV), 3) Main Oxidizer Valve (MOV), 4) Regenerative Cooling Valve (RCV), 5) Main Combustion Chamber (MCC)

The test article consists of an Inconel718 heat treated injector head mounted atop a regeneratively cooled Inconel718 heat treated combustion chamber. It is equipped with three main control valves: one on the liquid oxygen (LOX) line (MOV), one at the outlet of the regenerative cooling circuit (RCV), and one on the fuel side upstream of the injector head (MFV). A simplified Piping and Instrumentation Diagram (P&ID) of the propellant feed system is shown in Figure 4. In this configuration, the coolant exiting the regenerative cooling channels is coupled with the injector head and directed through a control valve.

2.2 Experimental cycling sequence and numerical cycling set up

The combustion chamber involved for the cycling test is coated with a therma barrier coating (TBC). In the frame of the present work, experimental data have been used to calibrate the predicting models for the Huracan TCA performances and thermal behavior. In particular, intrusive thermocouples have been employed in order to compute the heat flux and

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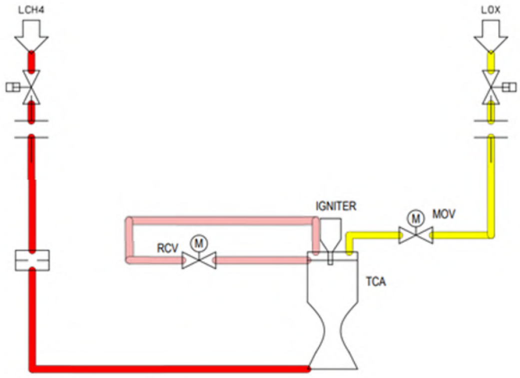

Figure 4: Propellant feed system (P&ID) of the cycling test.

correctly model the cycling thermal behavior. The cycling experimental sequence consists in 20 consecutive ignitions. The numerical analysis cycle is a simplified representation of the real cycle. The real single cycle behavior is divided in 5 loading steps to correctly model the different phases of the cycle consisting in (Figure 5): first portion of the start-up ramp, second portion of the start-up ramp, hot fire steady phase, shut-down phase and the post cooling. In particular, the star- up has been modeled via a multi linear slope in order to emulate the real start up sequence during the TCA cycling test.

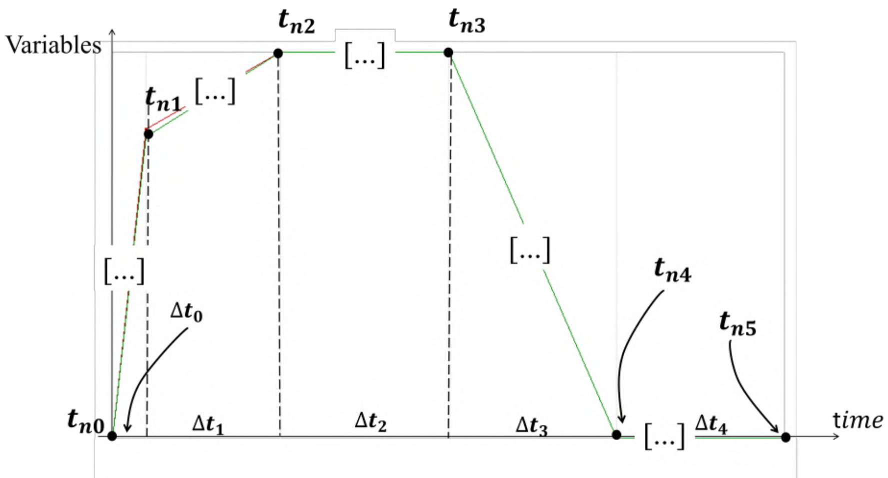

Figure 5: Schematic representation of the thermo-mechanical single cycle loading used for numerical simulation

The computational process was structured in two primary phases. In the first phase, a transient thermal analysis was carried out using thermal boundary conditions representative of a typical firing cycle. This simulation produced a time-dependent temperature distribution throughout the structure, capturing the evolving thermal gradients during ignition, steady-state operation, and the cool-down phase. The temperature data obtained from this thermal analysis was then used as input for the second phase: a sequential, non-linear quasi-static thermo-structural analysis. This was performed to evaluate the mechanical stress and strain response of the system over different cycles (Figure 6).

The numerical models is based on half symmetric throat section channel 3D generalized plain strain analysis. This model explicitly constrains the axial strain to remain constant, similar to the well known 2D generalized plain strain model but in the 3D domain, allowing a full 3D resolution of the stress and strain fields. The axial z strain direction is kinematically coupled between z-normal positive and negative surfaces of the structure. This is necessary to avoid a non-realistic fully free z axial deformation. The computational structured mesh involved 866 hexahedron 8-nodes bi-linear elements, for a total of 6530 nodes. This high spatial fidelity was critical for capturing steep thermal gradients and localized plastic deformation, especially near the hot gas wall side. Material behavior was modeled as nonlinear and temperature-dependent. The finite element solver was configured to account for large deformations, plasticity, and nonlinear material properties. The hardening models involved in the present study is a combined non linear kinematic hardening 2nd order Chaboche models combined with a multi-linear isotropic hardening Voce model.

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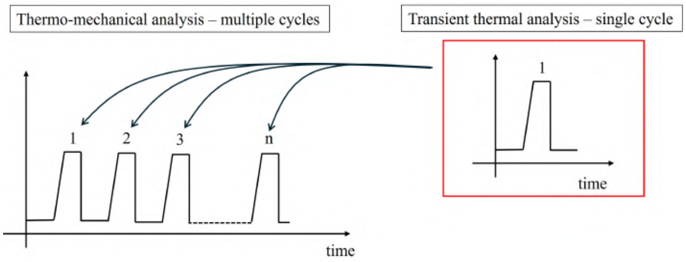

Figure 6: Numerical cycling sequence logic.

The TBC is considered as perfectly bonded with the Inconel718 combustionchmaber. Geometric and mechanical constraints (Figure 8) were carefully chosen to replicate the experimental boundary conditions. A rotational symmetry condition was enforced along the two r-z symmetry planes of the modeled half-chamber segment to account for axis-symmetry. The out of plain surface (normal with respect to the z axis) of the structure was fixed in the axial direction (uz = 0). The nodes on surface normal with respect to the negative z axis were hard coded to be coupled in the z-direction together with the surface normal with respect to the positive z axis. This coupling established a generalized plane strain condition, ensuring that axial displacements were consistent across symmetry z axis plane.[1,2]

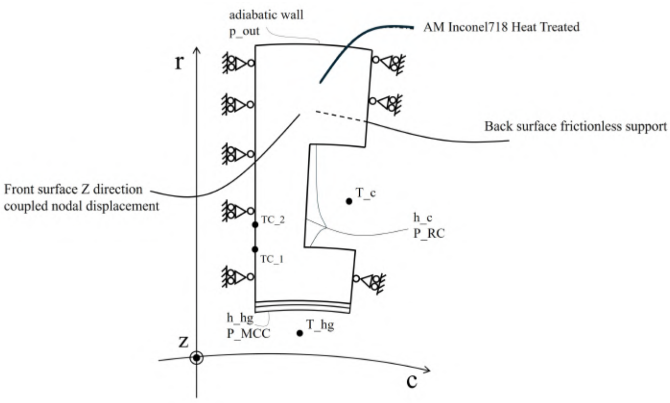

Figure 7: Numerical analysis boundary contrition setup.

The experimental characterized set of material properties for the in-house L-PBF additive manufactured heat treated Incoel718 are implemented to correctly model the thermo-mechanical behavior of the combustion chamber. All properties have been characterize with respect to the temperature range expected during the cycling TCA test. In particular, a series of low cycle fatigue tests have been performed with respect to different total strain range amplitudes and different temperatures to calibrate non-linear 2nd order Chaboche kinematic hardening model together with the isotropic Voce hardening model. By means of a combination of the aforementioned hardening models, both the Bauschinger effect and the softening-hardening behaviors of the nickel-based alloy can be taken into account to better predict the strain respond of the inner wall under multiple cycles.

2.3 MCC life prediction

The assessment of the fatigue life of the inner wall of the regenerative-cooled Huracan TCA was carried out through the application of a cycle-by-cycle cumulative damage model, grounded in the principles of Miner's rule.[6] This analytical framework is particularly suited for capturing the combined effects of low-cycle fatigue (LCF) and ratcheting, both of which are significant under the thermo-mechanical loading conditions typical of rocket engine operation.

To quantify the damage accumulated in each loading cycle *i*, the model evaluates the total strain variation in the hoop direction (circumferential orientation), expressed as

$$\Delta\varepsilon_{\text{hoop}} = \varepsilon_{\text{hoop,max}_{\text{cycle } i}} - \varepsilon_{\text{hoop,min}_{\text{cycle } i}}$$

This strain range represents the difference between the maximum and minimum hoop strain experienced during the cycle and serves as a critical input for fatigue life estimation.

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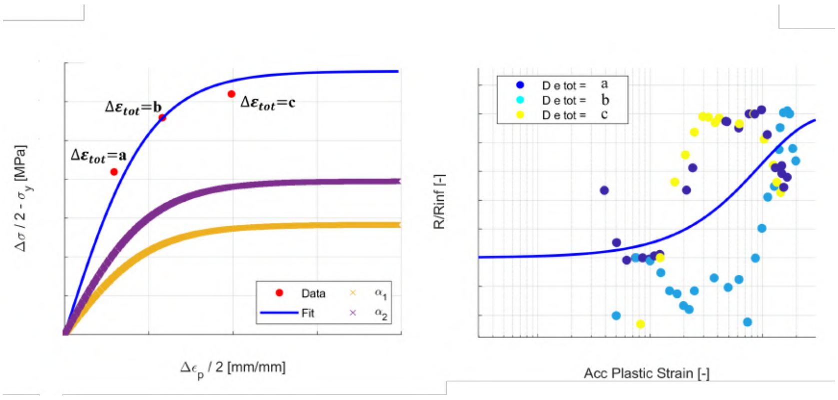

Figure 8: Example of L-PBF additive manufactured heat treated Inconel718 2nd order non linear kinematic 2nd order Chaboche model and isotropic hardening models calibration over experimental data at constant temperature

The calculated strain amplitude for each cycle was then used to interrogate the Coffin-Manson fatigue relationship, which characterizes the material's numer of cycle to failure based on experimental data. By correlating the measured strain range with this temperature-specific fatigue curve, the model determines the expected number of cycles to failure $N_{f_{\mathrm{LCF}}}$ for that given cycle.

By repeating this process for each cycle and summing the incremental damage contributions, the model predicts the total fatigue life of the component under the applied loading history, providing a detailed and physically grounded estimate of structural endurance.

$$\Delta\varepsilon_{hoop} = \varepsilon'_f(2N_{f_{LCF}})^c$$

where:

- ε'_f is the fatigue ductility coefficient;
- c is the fatigue ductility exponent;

The LCF damage[5,6] per cycle was calculated using Miner's rule as:

$$D_{\mathrm{LCF},i} = \frac{1}{N_{f_{LCF},i}}$$

The cumulative fatigue damage over n cycles is:

$$D_{\mathrm{LCF,total}} = \sum_{i=1}^{n} D_{\mathrm{LCF},i}$$

Ratcheting damage[5,6] was assessed by evaluating the progressive total (elastic and plastic) hoop tensile strain accumulation at the end of each cycle, thus after the shout down phase, according with J. Riccius et al.[5] :

$$\Delta\varepsilon_{\mathrm{hoop,ratchet},i} = \varepsilon_{hoop,\mathrm{end},i} - \varepsilon_{hoop,\mathrm{start},i}$$

where:

- $\varepsilon_{hoop,\mathrm{end},i}$ is the remaining strain after the considered cycle;
- $\varepsilon_{hoop,\mathrm{start},i}$ is the strain at the beginning of the considered cycle;

The ratcheting damage per cycle was calculated as follow:

$$D_{\mathrm{ratchet},i} = \frac{max(0, \Delta\varepsilon_{\mathrm{hoop,ratchet},i})}{\varepsilon_{\mathrm{max,tensile}}}$$

where:

- $\varepsilon_{\mathrm{max,tensile}}$ is the elongation to fracture of the tensile test at room temperature and equal to 0.25 mm/mm;

The cumulative ratcheting damage was given by:

$$D_{\text{ratchet,total}} = \sum_{i=1}^{n} D_{\text{ratchet},i}$$

The total damage per cycle is the sum of both LCF and ratcheting contributions:

$$D_{\text{total},i} = D_{\text{LCF},i} + D_{\text{ratchet},i}$$

The cumulative usage factor is:

$$D_{\text{cumulative}} = \sum_{i=1}^{n} D_{\text{total},i}$$

Failure was predicted when at lease one point of the structure satisfies the following criteria:

$$D_{\text{cumulative}} \geq 1$$

The TBC interface allowable mechanical load, according with the steady state computed maximum temperature, has been identified as the ultimate load computed from specific laboratory tests. TBC yield strength and ultimate strength are difficult to estimate with conventional laboratory tests. The ultimate allowable load - of the combustion chamber - to be analyzed is the one at the interface between the metal sub material and the bond TBC layer. This ultimate interface load can be derived from the so called 'detachment laboratory test', which consists in a specific standard procedure, performed with respect to a fully representative specimen of the thickness and the coating process, to estimate the ultimate detachment axial stress at the interface by axially pulling the TBC out from the substrate metal while measuring the axial force. To compute the ultimate allowable stress, the final detached area is measured via optical microscope. Thus, the ultimate allowable stress is available at the target steady state temperature of the nominal TCA cycle.

It is important to highlight that, even thought TBC manufacturing knowledge and applications are well known nowadays, this is not totally true for the life estimation mainly because of:

- TBC are brittle materials, so that the fracture behavior is hard to predict in terms of elongation to failure and maximum stress at the failure point;
- Because of the manufacturing processes, TBC tends to be porous with high scattering with respect to the experimental mechanical tests;
- Mechanical properties are not easy to test and correlate with numerical analysis, especially for fatigue analysis;
- The use of TBC for hardware in where the substrate metal material faces non-stationay high thermal strains is an additional challenge when compared to the conventional recommended steady state application for TBC (eg. turbine blades);

For the aforementioned reasons, high margin of safety have to be implemented in the design process of combustion chambers when TBC is employed. The principal TBC failure mode identified (and validated via post inspection of TCA cycing test campaign at P8 DLR Lampoldshausen test facility) for combustion chambers is the TBC delamination of the bond layer from the substrate material due to the high strains generated by the deformations of the inner wall during a nominal TCA cycle. The numerical analysis life prediction approach consists in comparing the computed frictional stresses at the interface between the substrate metal material of the inner wall of the combustion chamber (frictional stresses) with ultimate interface load computed from the detachment laboratory test at the target temperature.

The TBC interface damage for every cycle was calculated as follow:

$$D_{\text{TBC},i} = \frac{\sigma_{\text{frictional,max},i}}{\sigma_{\text{ultimate,detachment test}}}$$

where:

- $\sigma_{\text{frictional,max},i}$ is the frictional maximum stress of the i-cycle at the interface between the TBC bond layer and the Inconel718 inner wall of the combustion chamber;
- $\sigma_{\text{ultimate,detachment test}}$ ultimate interface load computed from the detachment laboratory test at the maximum temperature of the i-cycle;

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The cumulative TBC interface damage is given by:

$$D_{\text{TBC,total}} = \sum_{i=1}^{n} D_{\text{TBC},i}$$

TBC interface failure was predicted when at least one point of the structure satisfies the following criteria:

$$D_{\text{TBC,total}} \geq 1$$

3. Results

Figure 9 presents the normalized throttle command and the normalized temperature measured by the throat rib thermocouple (at a specific distance from the hot gas chamber wall) during a series of cyclic hot-fire tests of the thrust chamber assembly. Each cycle corresponds to a complete ignition and shutdown of the combustion chamber. The cycling test campaign of the Huracan TCA successfully achieved 20 consecutive cycles.

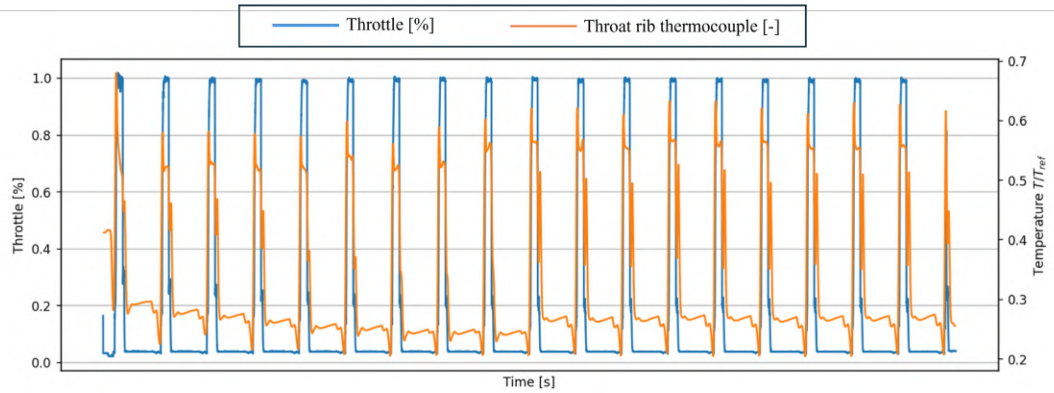

Figure 9: TCA combustion chamber time history of normalized throttle and throat thermocouple measurements during cyclic hot-fire testing.

The first cycle exhibits non-stationary thermal behavior, primarily due to the absence of pre-cooling prior to the first ignition. As a result, the chamber walls began at ambient temperature, leading to a different thermal transient compared to the subsequent cycles. Furthermore, an overshoot is observed at the start and end of each cycle. This behavior is attributed to the specific ignition and shutdown sequence employed.

Despite these initial transients, the test campaign demonstrated excellent repeatability. Both the pressure (as inferred from the throttle signal) and the temperature responses at the throat location showed consistent patterns across the cycles. This indicates stable operation and reliable thermal behavior of the cooling system throughout the test series.

A key observation is the systematic difference between the first ten cycles and the last ten. The test sequence was intentionally modified for the last 10 cycles, which affected the thermal boundary conditions - specifically, the chamber temperature at the end of each cycle. As a consequence, the subsequent ignition conditions in the second half of the test were influenced by a higher temperature in between cycles, resulting in a measurable upward shift in the thermocouple readings during the last ten cycles.

Finally, the last cycle deviates from the established trend due to propellant depletion, which triggered a redline condition. This caused an abnormal shutdown sequence, resulting in a truncated thermal response both during and after the firing.

Figure 10 presents the deviation of the combustion efficiency from the average value across all 20 hot-fire cycles. The deviations remain within a narrow range of ±0.5%, indicating stable and repeatable combustion behavior throughout the test campaign. A slight cycle-to-cycle variation is observed, with the most notable deviations occurring around cycles 2, 5, and 17. Despite these local fluctuations, no clear trend of degradation or improvement is apparent over time, which confirms the robustness of the combustion process. Overall, the variation is minimal and well within acceptable margins for high-performance propulsion systems. The final cycle combustion efficiency has not be plotted since the redline-triggered shutdown at the end of the test affected the combustion efficiency computation.

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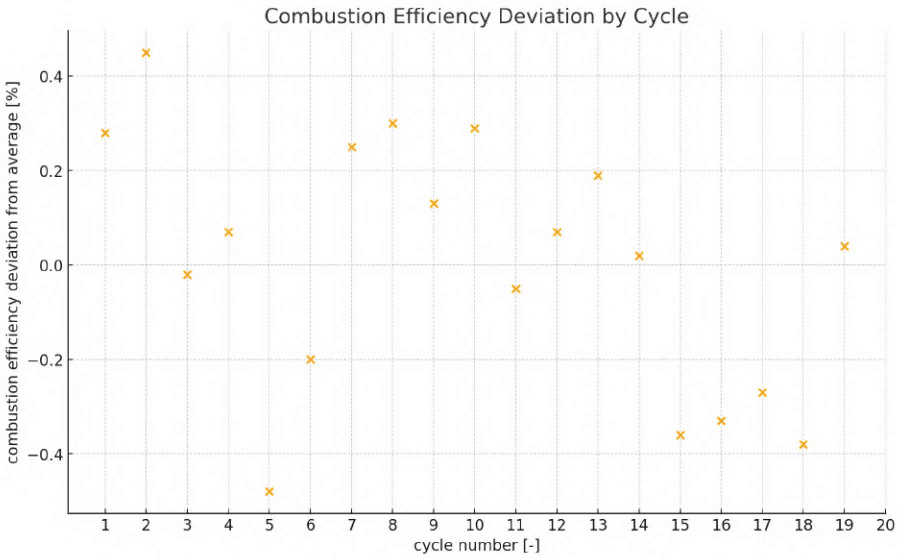

Figure 10: TCA combustion chamber combustion efficiency deviation from average across 20 ignition cycles.

3.1 Numerical thermo-mechanical behavior of the coated chamber and TBC life prediction

In the present work, all analyses consider the total hoop stress and total hoop strain (including both elastic and plastic components) at the most highly loaded point of the structure. This critical point has been identified as the location on the symmetric section of the inner wall facing the hot gas side.[3–6]

In order to correctly simulate the thermo-mechanical cycling behavior it is of preliminary importance to set the starting temperature of the cooling channel equal to the ambient temperature. Thus, the mechanical behavior of the first cycle takes into consideration the generation of strains and stresses due to the thermal difference between the starting body temperature and the thermal imported cycle.

The cycling behavior of the throat section is presented in Figure 5. The starting point (t_{ref} in Figure 5) is the thermo-mechanical status of the analyses channel before the thermal and mechanical first cycle is applied. Once the cycle starts (t_0), the highest loaded point under analysis shows a relatively small positive total hoop strain and a negative compressive hoop stress. This effect is attributed to the shrinkage of the channel. During the first portion of the start up ramp, negative total hoop strain shows up together with negative hoop stress (t_1). The ramp-up behavior increases the absolute value of the variables under analysis until reaching the steady state condition at point t_2. The channel is under high compressive total hoop strains and compressive hoop stress. Consequentially, during the hot fire phase, the hoop stress is constant and the total hoop strain increases until reaching the maximum value at the end of the hot fire phase (t_3). It is important to highlight that the material, after a preliminary linear behavior during the first start-up phase yields. This is clearly visualized in Figure 11, from time t_1 to time t_2. When the shutdown sequence starts, the material linearly goes back to the starting position ending the cycle with the post cooling phase at t_5. However, at the end of the cycle high tensile hoop stresses are present in the inner wall middle hot gas side point (t_5). This shows that the plastic deformation during the start-up and the hot fire phase is not fully recovered. This is represented by the fact that the starting point of the first cycle (t_0) doesn't overlap with the last one, t_5.

From the second cycle on, all cycles exhibit the same behavior. In particular, the start point of the general n-cycle overlaps with the end point of the previous n-cycle. The thermo-mechanical behavior shows the total hoop strains and the hoop stress of the middle channel hot gas side point move along a linear line. This is due to the fact that the end point of the general n-cycle (t_{n5}) doesn't reach plasticity. Thus, when the next cycles starts (t_{n0}), the point under analysis goes linearly into compression and once reached the maximum negative total hoop strain at the end of the hot fire phase (t_{n3}), it recovers back to the starting position. This behavior is the same for every cycles after the first one. The point under investigation immediately enters into the plastic domain during the start-up and the hot fire phases of the first cycle (t_0 to t_3), yielding. However, all subsequent cycles are not able to increment the plastic strain (in both points, compression at t_{n3} and traction at t_{n5}), thus there is not cycling accumulation of total hoop plastic strain, resulting in a linear loading an unloading of the middle channel point on the hot gas side. This is due to the fact that, since the analyzed section is coated with a thermal barrier coating (TBC), the inner wall metal material face low temperature compared to the one expected with a bare inner layer. Heat treated Inconel718, as such relatively low temperature, exhibits high yield stress, thus no plasticity shows up.

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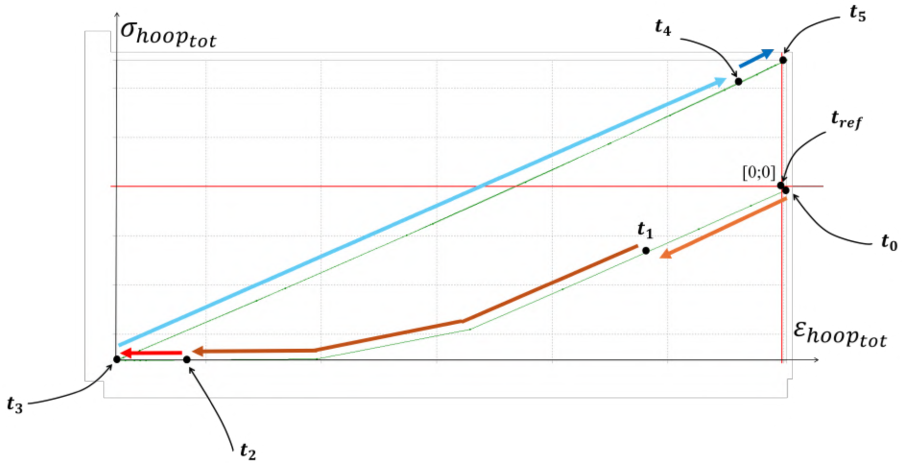

Figure 11: Throat section hoop stress and hoop total strain of the first cycle at the inner wall mid section point on the hot gas side - costed chamber configuration.

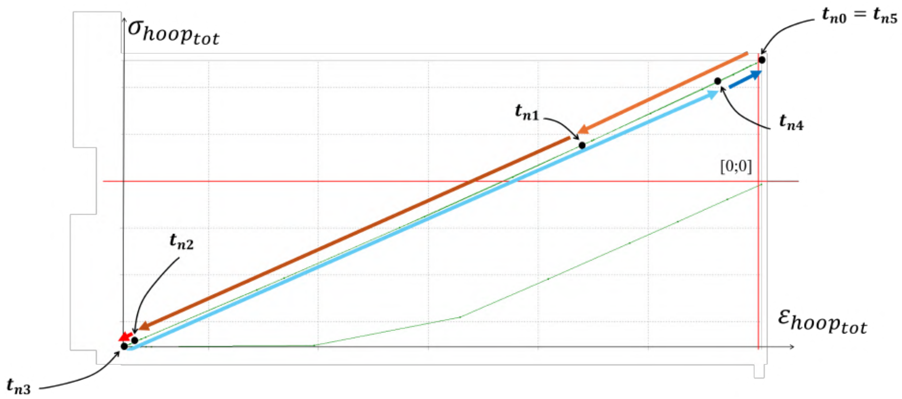

Figure 12: Throat section hoop stress and hoop total strain at the inner wall mid section point on the hot gas side - costed chamber configuration.

Since the inner wall works in the elastic domain of the material, the metallic inner wall at the throat section doesn't represent the most critical area. Because of this reason, the failure point for the coated combustion chamber, has been identified to be the TBC bond layer interface with the metallic Inocnel718 inner wall at the throat section.

The Huracan engine TCA under investigation has been equipped with thermocouples in order to validate and improve the design models employed for the combustion chamber regenerative cooling system. The thermocouple holders introduce additional mass which locally modifies the stresses and strains distribution at the inner wall interface. This is mainly due to the over constrain of the close out wall which reflects in higher strain at the inner wall locations corresponding to the thermocouple holders at the throat section. Additionally, the thermal inertia of the close out wall is also effected by the additional mass introduced by the holders, leading to a localized different thermo-mechanical behavior with respect to the nominal regenerative cooling system geometry. Together with the aforementioned effect due to the external mass added on the close out, a local modification of the cooling channel geometry was implemented in order to accommodate thermocouples in the ribs. This particular modification generates a localized increase of temperature with respect to the nominal design away from the 4 instrumented throat regions. Since the strains are effected by the difference between the maximum and minium temperature within one cycle, a higher temperature during steady state nominal phase lead to higher strains. Because of the localized strains increases during the steady state nominal cycle phase, the frictional stress at the interface between the TBC bond layer and the inner wall also increases by a factor of 20 compared (Figure 13) to the nominal vales computed in the neighbor regenerative cooling channels not effected by the modification introduced for the instrumentation.

Because of the effects of the instrumentation in the throat, after 20 cycles explored during the cycling test, 4 TBC de-laminated areas have been found during the post test inspection (Figure 14). These areas perfectly correspond to the 4 instrumented sites, proofing the reliability of the TBC under cycling conditions in the nominal not instrumented

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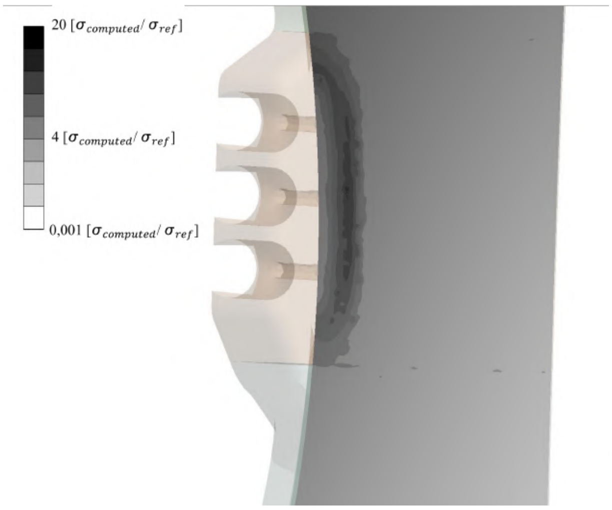

Figure 13: Throat section frictional stresses at the interface between the inner metallic wall and the bond TBC layer during nominal steady state hot fire phase.

regenerative cooling system areas.

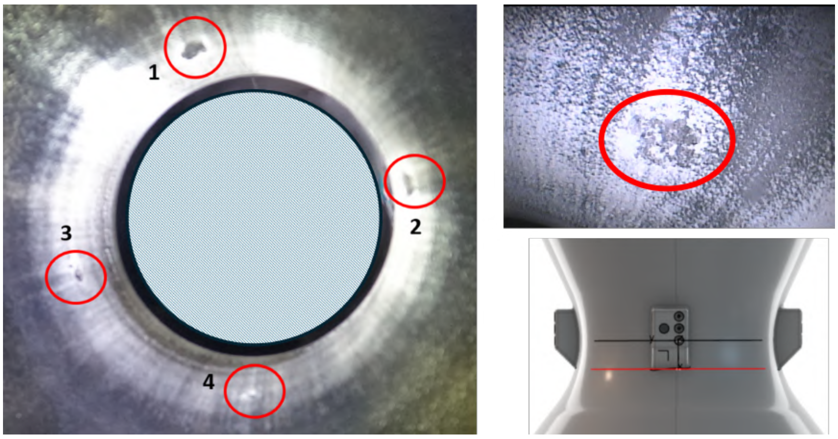

Figure 14: Throat section TBC de-laminated areas after 20 cycles.

The experimental results match with the analysis predicted by the TBC damage model. The local delamination was expected after a specific number of cycle. Figure 15 shows the communicative TBC damage at the mid section of the inner wall at the interface between the TBC bond layer and the metallic inner wall, for the nominal regenerative cooling system geometry. After a non-linear behavior, the damage assume a linear trend. The linear trend allows to extrapolate the cumulative damage to predict the number of cycle to failure of the nominal design, which occurs when the cumulative TBC damage reach the value of 1.

Thermo-mechanical behavior of the non-coated chamber and inner wall life prediction has been numerically explored to asset the reusability of the 3rd TCA iteration after the heat exchange model calibration during the coated TCA cycling test. The damage accumulation was decomposed into low cycle fatigue (LCF) and ratcheting contributions, and the results are presented in terms of total damage versus the number of loading cycles. The analysis reveals that LCF damage and ratcheting damages overlap in the initial phase of the life cycle, increasing sharply during the first few cycles and then approaching a steady state. This indicates the presence of higher plastic strain during early operation, which gradually stabilize. If the LCF damage tends to stabilize, the ratcheting damage accumulates progressively with each cycle. This suggests a continuous accumulation of inelastic strain. The total damage curve, being the sum of both mechanisms, shows an approximately linear increase after the initial cycles, highlighting ratcheting as the dominant damage mechanism over the engine's operational life. These findings emphasize the importance of controlling ratcheting deformation to extend component life and ensure structural integrity under cyclic high-temperature loading conditions.

The damage LCF and the ratcheting cumulative damage model shows a number of cycle to failure higher respect

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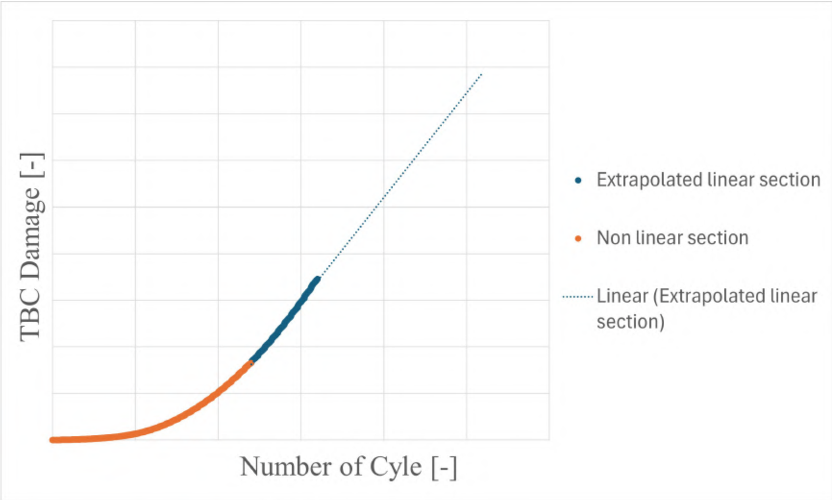

Figure 15: TBC damage over cycles - coated chamber.

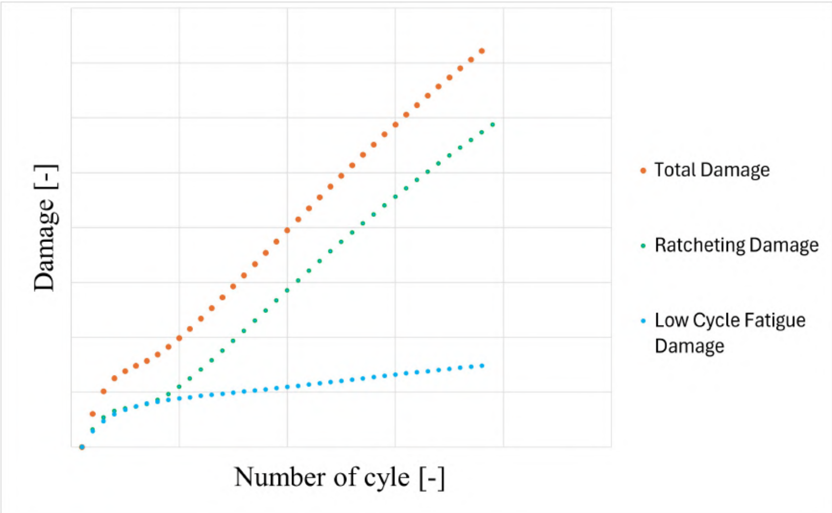

Figure 16: Low cycle fatigue and ratcheting predicted damage over cycles - uncoated chamber.

to the Huracan engine mission requirements. The damage of the throat section nominal geometry inner wall mid cross section on the hot gas side is presented in Figure 17 for few cycles.

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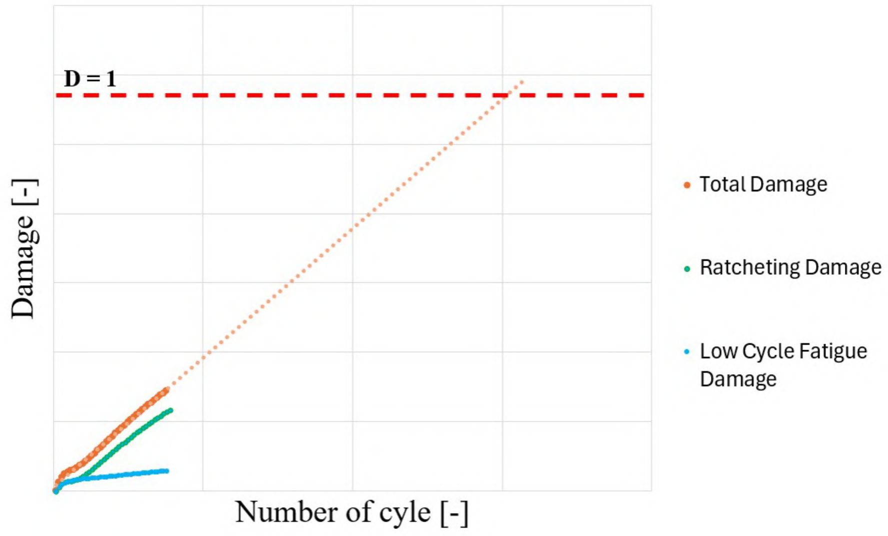

Figure 17: Extrapolation of low cycle fatigue and ratcheting predicted damage over cycles - uncoated chamber.

4. Conclusions

This study presents an integrated experimental and numerical characterization of the cycling thermo-mechanical behavior of a reusable LOX-LNG regeneratively cooled thrust chamber, developed for The Exploration Company lunar lender Huracan engine. Through a combined approach of high-fidelity finite element simulations and a dedicated hot-fire test campaign, the structural integrity and life-limiting mechanisms of the combustion chamber inner wall were assessed.

In coated configurations, the inner metallic wall remains predominantly in the elastic regime, with failure driven by TBC interface delamination only around the throat section instrumented area. In contrast, the uncoated chamber experiences a combination of low cycle fatigue (LCF) and ratcheting damage in the hot gas side of the regenerative cooling system, with ratcheting becoming the dominant life-limiting mechanism over time.

The cumulative damage approach employed accurately predicted the observed failure locations and cycle counts, confirming its applicability for reusable liquid rocket engine components. Importantly, the predicted number of cycles to failure in both configurations exceeds the current Huracan mission requirements, validating the reusability objectives.

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