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Parody Martin, A., Marshall, M., Baillieu, A. et al. (2026) Investigation of high-temperature blade wear in ceramic abradable seals under representative gas turbine conditions through a novel heated incursion rig. *Wear*. ISSN: 0043-1648

<https://doi.org/10.1016/j.wear.2026.207008>

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Highlights

Investigation of high-temperature blade wear in ceramic abradable seals under representative gas turbine conditions through a novel heated incursion rig

A. Parody Martin, M.B. Marshall, A. Baillieu, J. Quince, C. Currie

- Novel high-temperature rig simulates blade tip wear mechanisms under high thermal loads.
- Elevated temperatures drive abradable adhesion and abrasive matrix degradation.
- Blade performance is limited by matrix plastic yielding near 1100 °C.

Investigation of high-temperature blade wear in ceramic abradable seals under representative gas turbine conditions through a novel heated incursion rig

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ARTICLE INFO

Keywords:

abradable seal systems
incursion testing
high-temperature wear
abrasive-tipped blades
wear mechanisms
surface topography

ABSTRACT

To optimise gas turbine aerodynamic efficiency, High-Pressure Turbine (HPT) stages use ceramic abradable seals that interact with abrasive-tipped blades. Evaluating the wear mechanisms of these systems requires simultaneous high-speed and high-temperature conditions, which are difficult to achieve in standard testing facilities. This study presents the development and validation of a High-Temperature Rig (HTR) that integrates high-speed rotor dynamics (100 m s^{-1}) with an induction heating system targeted at the blade to simulate engine incursion under high-temperature conditions on the blade tip side of high-pressure turbines. The achievement of engine-representative wear mechanisms in the HTR, benchmarked against an established room-temperature rig (SR), confirmed that ambient conditions yield a stable cutting interaction governed by brittle fracture. However, testing under representative temperatures showed a vulnerability in the electroplated c-BN abrasive coating. High temperatures cause the abradable material to adhere to the blade, reducing cutting efficiency and increasing normal contact forces up to 13 kN at high incursion rates. Topographical analysis using Positive Volume Variation (PVV) metrics demonstrated that this material congestion causes the abrasive layer to structurally collapse, transitioning the contact to pure friction. Results indicate that high-speed blade performance is limited not by mechanical grit wear, but by the thermal stability of the metallic anchoring matrix, which yields plastically when it reaches a friction-induced thermal limit near 1100°C .

1. Introduction

Tip leakage flows across the gap between the rotating blade tips and the stationary casing represent a primary cause of aerodynamic inefficiency in gas turbines [1, 2]. When the working fluid bypasses the blades through this clearance, energy transfer efficiency drops. To counteract these parasitic losses and sustain thrust, the engine must operate with higher fuel consumption [3].

Abradable seal systems mitigate these effects across compressor and turbine stages by allowing tip clearances to be reduced to levels that would otherwise be unsafe with rigid boundaries. These systems function by sacrificing material during rub events, thereby protecting the rotor while establishing the tightest possible operating gap [4]. In the compressor sections, metallic abradable coatings allow bare blades to carve a path during the initial engine run-in, creating a custom fit for regular operation [5, 6].

The High-Pressure Turbine (HPT) environment, however, imposes severe thermal constraints requiring ceramic-based abrasives, such as yttria-stabilised zirconia (YSZ) or magnesia alumina spinel (MAS) [7]. While these materials offer superior thermal stability, their high hardness poses a risk to the rotating components. To ensure the blades can cut these ceramic liners without sustaining damage, the tips are reinforced with abrasive materials [8]. The industry standard

for this application consists of cubic boron nitride (c-BN) particles embedded within an MCrAlY matrix, typically applied via electroplating [9, 10].

Verifying the tribological behaviour of these complex material systems at high temperatures remains a challenge, requiring experimental methods beyond standard characterisation [11]. Because standard tribometers cannot replicate the complex, high-speed, intermittent contact inherent to gas turbine operation [10, 12], the industry developed specialised high-speed wear test rigs to reproduce these specific conditions.

Early validation efforts at NASA and Pratt & Whitney Aircraft set the benchmark. Their facilities used compressed air turbines to drive rotors at speeds over 300 m s^{-1} , heating abradable specimens externally with oxygen-acetylene torches to reach 1300°C [13, 14]. Performance was typically quantified using the Volume Wear Ratio (VWR), a post-test metric. Building on this approach, modern commercial rigs—like those from Oerlikon Metco—now use high-velocity gas streams to heat samples to 1200°C , achieving tip speeds of 500 m s^{-1} with precise incursion control [7].

A thermal discrepancy persists in these setups, however. Most established rigs heat only the stationary abradable, meaning the rotating blade experiences high heat flux only during brief contact periods and cools via forced convection for the remainder of the rotation. This prevents the blade from reaching the sustained temperatures characteristic of an HPT. Previous research demonstrated that the degradation

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of the electroplated bonding system—and the release of c-BN particles—depends on the overall blade temperature, not just the abradable temperature [15]. Although small-scale rigs have been developed to heat both components, they are typically limited to low speeds (e.g., 50 m s^{-1}), which are insufficient to trigger the relevant high-speed wear mechanisms.

In addition, conventional high-temperature testing relies heavily on post-test analysis. While useful for measuring total volume loss, this approach misses the temporal evolution of wear. In compressor seal testing, in-situ instrumentation has proven vital, as wear is rarely linear. Researchers have successfully used dynamometers, high-speed imaging, and thermography to link specific rub events to immediate changes in wear behaviour [10, 16]. Applying such instrumentation to the harsher turbine environment remains a research challenge not fully addressed in current literature.

Current experimental capabilities present a trade-off: commercial rigs offer representative kinetic energy but lack blade-side heating and in-situ data, while setups providing thermal accuracy lack the necessary tip speeds. To address this gap, this paper presents the development and validation of a novel High-Temperature Rig (HTR) designed to evaluate abrasive-abradable interactions under these combined conditions. By integrating induction heating with high-speed rotor dynamics, the HTR enables the investigation of temperature-dependent wear mechanisms.

2. Methodology

This experimental investigation translates established room-temperature benchmarks to the complex thermo-mechanical environment of the HPT using the newly developed HTR. To ensure rig reliability and confirm that observed wear phenomena result from the intended thermal variables rather than rig-induced anomalies, the testing follows a robust strategy aimed at verifying the achievement of engine-representative wear mechanisms.

The experimental strategy comprises two primary phases: an initial benchmarking phase to verify the achievement of engine-representative wear mechanisms at room temperature in the novel HTR against the foundational performance of the existing Scaled Rig (SR), followed by a comprehensive high-temperature testing phase utilising the full capabilities of the HTR's induction heating system.

2.1. Scaled rig

The SR (Figure 1)—previously employed in multiple abradable studies [17, 18, 19, 10, 20, 16, 21, 22]—serves as the foundational rig for characterising abradable contact at room temperature. The mechanical design of this rig emphasises high-speed intermittent contact and extensive optical access for instrumentation. The rotor assembly, as illustrated in Figure 1a, features a cantilevered disc directly coupled to a GMN HSP120G - 21000/9 high-speed spindle through an HSK-140 rigid coupling. This spindle reaches 21 000 rpm and is supported by a 4.5 kW Hyfra Chilly 45 water-circulating chiller to manage thermal loads. The disc

holds a test blade and a counterbalancing dummy blade, achieving tip speeds up to 211 m s^{-1} , which is sufficient to achieve the brittle fracture removal mechanisms in MAS ceramic coatings [22].

The SR incursion mechanism (Figure 1a) is driven by a motorised Sigma Koki SGSP80-20ZF stage guide, which provides the linear movement of the abradable specimen toward the rotating disc. Controlled by a SHOT202 stage controller, this system allows for incursion speeds ranging from $0.1 \mu\text{m s}^{-1}$ to $2000 \mu\text{m s}^{-1}$. This flexibility is critical for matching the incursion rate, defined as the depth of material removed per blade pass, to specific engine manoeuvres. This derived parameter is a direct function of the stage radial feed speed, representing the linear incursion speed in $\mu\text{m s}^{-1}$, and the rotational frequency of the spindle in rpm. The mathematical relationship governing these variables is established as follows:

$$I_R = \frac{I_S \cdot 60}{\omega} \quad (1)$$

where I_R is the incursion rate, I_S is the incursion speed, and ω is the spindle rotational speed.

The SR instrumentation suite is optimised for real-time monitoring of the contact interface. A Kistler Type 9347C triaxial piezoelectric dynamometer is positioned beneath the abradable specimen, measuring normal and tangential forces during each strike. These signals are processed via a Kistler 5070A charge amplifier and digitised by a PicoScope 5000 Series oscilloscope at high sampling frequencies. Thermal data is captured using a FLIR A6751 SLS long-wave infrared (LWIR) camera, viewing the abradable sample through a zinc sulphide window in the containment as shown in Figure 1c. This camera is calibrated for temperatures between 150°C and 1300°C , providing insight into the frictional heat generated by the blade cutting along its width.

2.2. High-temperature rig

The High-Temperature Rig (HTR) detailed in Figure 2 represents the next step in abradability testing at the University of Sheffield, specifically designed to assess abrasive-tipped blade performance at elevated temperatures. While the SR focuses on the mechanical aspects of contact, the HTR introduces the thermal dimension required to simulate HPT conditions, where the metallic matrix of the abrasive coating softens and leads to grit dislodgement [15].

Unlike the cantilevered spindle of the SR, the HTR employs a two-point supported shaft to accommodate the larger 405 mm diameter Ti64 titanium alloy disc and manage the increased inertial loads. The shaft, 476 mm in length, is hosted between two bearing housing blocks, each containing two high-precision ICB H 7016 C.T.P4A.UL angular contact bearings in a back-to-back arrangement and non-contact GMN CF 6016 S10 labyrinth seals at the ends to prevent oil leakage and contamination. To ensure longevity at high rotational speeds, these bearings are preloaded with 100 N through a SKF KMT 15 locknut and lubricated via an air-oil mixture provided by a Bijur HyperFormance lubricating

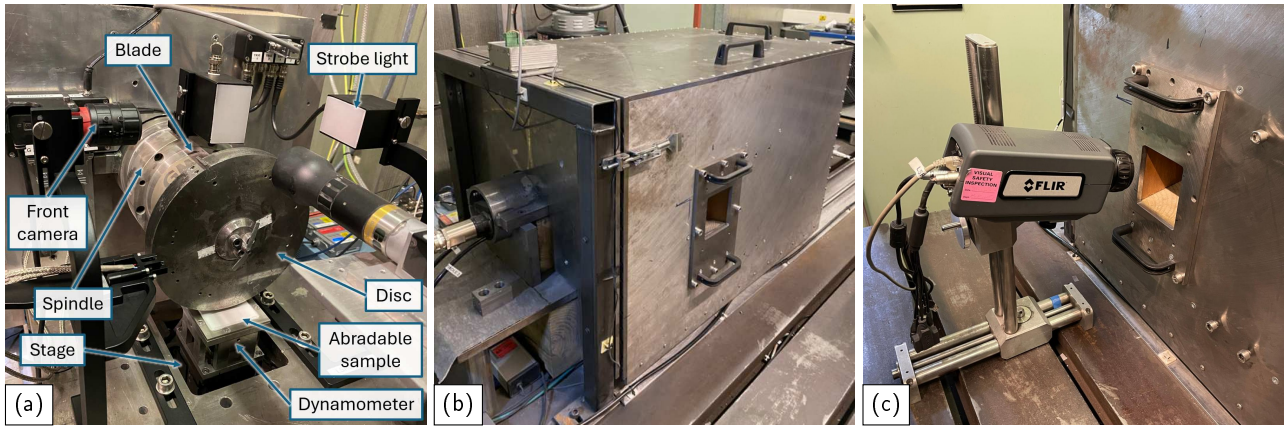


Figure 1: Scaled rig at the University of Sheffield detailing the (a) rig components, (b) safety containment, and (c) FLIR A6751 SLS long-wave infrared camera.

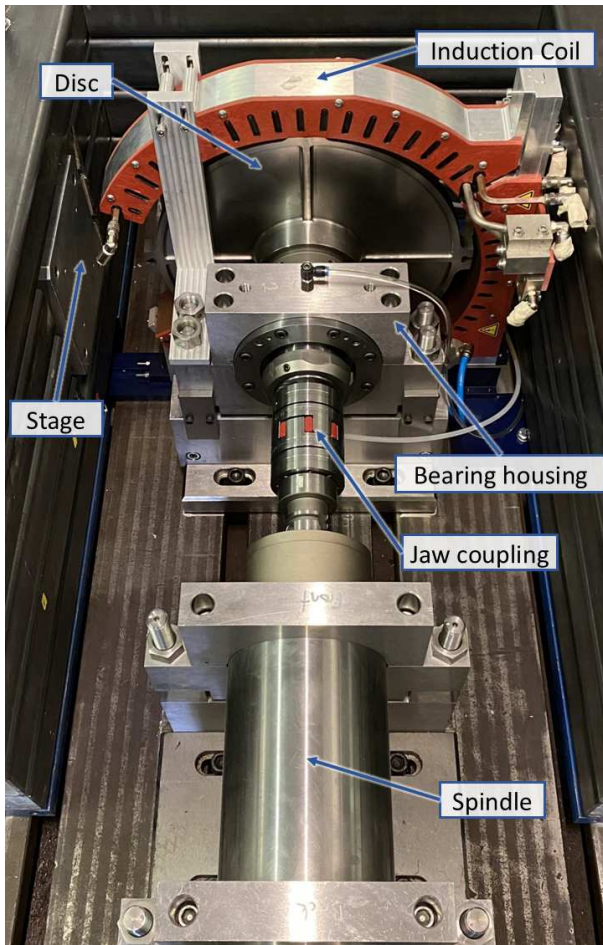


Figure 2: Configuration of the High-Temperature Rig (HTR) at the University of Sheffield.

system. The disc is secured using a shoulder and a SKF KMT 18 precision locknut.

The rotor is driven by a GMN HSP150G – 18000/9 spindle motor with a nominal power of 9 kW. Due to the windage generated by the disc design and the limiting space inside the

containment, the operating speed is limited to 12 000 rpm, resulting in a maximum linear speed of 256.98 m s^{-1} at the blade tip. A flexible KTR Rotex GS 19 coupling links the spindle to the drive shaft, absorbing minor misalignments and damping vibrations during high-speed incursions.

The incursion stage of the HTR is engineered for precision, particularly at the low incursion rates required to simulate thermal expansion and centrifugal growth. The system, depicted in Figure 3a, utilises a KEB 24.SM-200-42B0 servo motor coupled to a gearing assembly: a 30:1 Wittenstein LPK 070-M03-30-011 right-angle gearbox followed by a 50:1 Wittenstein LP 070S-MF2-50-1D1 planetary gearbox. This configuration yields a total reduction ratio of 1500:1, which is converted to linear motion via an EDrive Eliminator HD304 linear actuator with a 10 mm pitch lead screw and a KTR Rotex GS 19 flexible coupling. This high reduction maintains stable incursion rates as low as $0.02 \mu\text{m pass}^{-1}$, even under the substantial reaction forces generated when cutting efficiency decreases.

A key feature of the HTR is the induction heating system, which targets the rotating blade directly. This system consists of an UltraFlex HS-8 heating station and a custom-made C-shaped aluminium structure housing an S-shaped bent copper induction coil that surrounds the disc periphery. The coil is powered by a UltraFlex UPT-W15/150 power supply which delivers a 15 kW source operating in the 50 kHz to 150 kHz range.

To protect the system components, a dedicated cooling circuit featuring a 180 L water tank, a Hyfra Alpha 16-W industrial chiller, and a Grundfos CMBE 3-93 circulating pump ensures the induction equipment remains at a stable 15°C , utilising a constant distilled water flow of 6 lpm at a pressure between 3 bar and 6 bar. The chilling system diagram is detailed in Figure 4.

Prior to disc acceleration, the blade undergoes a static pre-heating phase for approximately 90 s, reaching temperatures above 760°C . This target temperature is established based on the heat conduction from the blade to the Ti64 disc, previously experimentally observed with the FLIR A6751

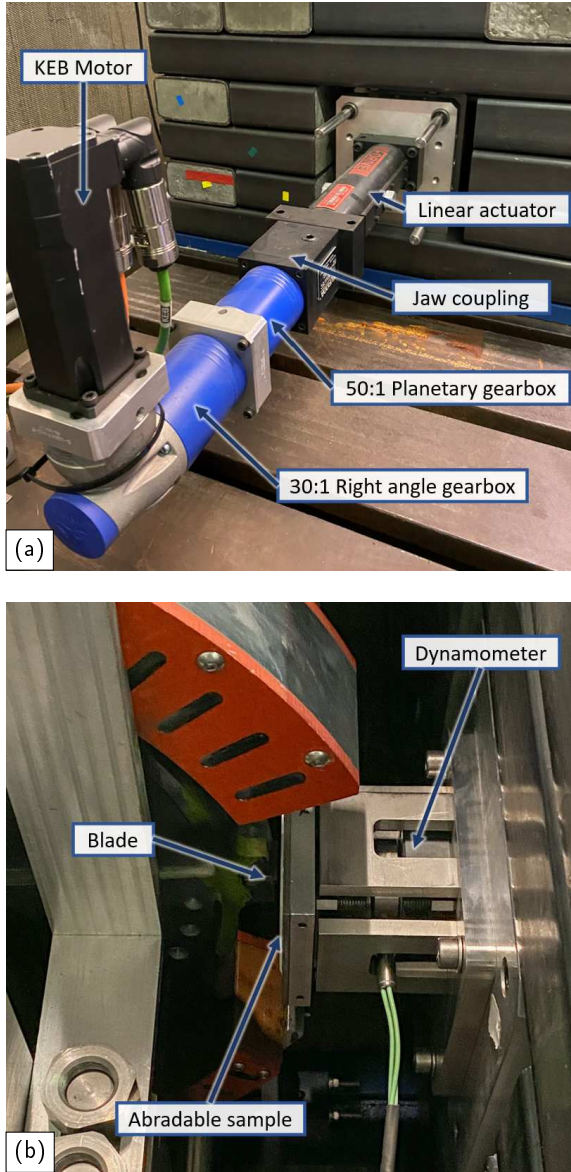


Figure 3: HTR incursion system displaying the (a) linear stage component diagram and (b) dynamometer with a mounted abrasion sample.

SLS infrared camera during system commissioning. The goal is to maintain the disc temperature below 400 °C, as the mechanical strength of the Ti64 alloy diminishes above this threshold.

Throughout the test, the induction system remains active to mitigate the forced convection cooling caused by the disc windage. Although the spindle motor allows for a maximum linear blade speed of 256.98 m s⁻¹, the high-temperature tests are undertaken at 100 m s⁻¹, with this corresponding to a spindle speed of 4669.58 rpm. This operational constraint is necessary because, at higher speeds, convective cooling as the blade is rotated surpasses the heating power delivered by the induction system.

Instrumentation in the HTR was adapted for the restricted space in the safety containment and the high-temperature environment. The rig retains a Kistler Type 9347C triaxial piezoelectric dynamometer (detailed in Figure 3b) for force measurement. Thermal monitoring of the blade surface is supplemented by a high-frequency infrared Impac IGA 740-LO pyrometer with a temperature range from 400 °C to 1400 °C. This pyrometer is placed perpendicular to the disc at 78 mm and records a spot size of 0.3 mm on the right side of the blade, approximately 0.5 mm down from the blade tip. Equation 2 defines the time window the pyrometer sees the blade, where t_{blade} is the blade thickness, d_{blade} is the diameter of the disc at the blade height and ν is the disc frequency in Hz. Considering the HTR geometry, the pyrometer sees the blade for 19 μ s when the disc spins at 4669.58 rpm (blade linear speed of 100 m s⁻¹). The device response time is 6 μ s, which is lower than this viewing window; therefore, the pyrometer can capture the rapid radiance variation as confirmed in Figure 5b.

$$\text{Pyrometer viewing window} = \frac{t_{blade}}{d_{blade} \pi \nu} \quad (2)$$

The pyrometer processes the radiation emitted by the blade into temperature according to an input emissivity value. The temperature is converted into voltage and read by the PicoScope Series 5000 digital oscilloscope. The analogue signal is digitised at 10 MHz, stored in the oscilloscope buffer, and later processed in MATLAB as detailed in Figure 5. The side of every blade targeted by the pyrometer is painted with LabIR HERP-HT-MWIR-BK-11 thermal paint, providing an emissivity between 0.865 and 0.923 for the pyrometer's measurable range [23]. An averaged emissivity value of 0.894 is utilised, introducing a relative error below 0.7 % on the temperature measurement.

Although the measured temperature is not at the blade contact surface where friction generates maximum heat, the measurement at the side of the blade provides a reliable overall blade tip temperature profile. This allows the determination of whether the blade experiences overheating due to poor cutting performance and enables performance comparisons between tests.

2.3. Test materials

The blade specimens utilised in this study are simplified representations of actual gas turbine blades as shown in Figure 6a and 6b. This approach simulates the contact mechanics of a gas turbine environment while optimising experimental costs. In all cases, the test blades are of a uniform thickness of 2 mm and a width of 20 mm. However, depending on the testing platform, two distinct blade lengths were employed: a standard 16 mm design, established in previous low-speed incursion testing, and a compact 14 mm variant designed to accommodate the tight spatial constraints of the induction coil in the HTR.

All blades were machined from a CMSX-4 nickel-based superalloy, and their tips were uniformly coated with the

High-temperature blade wear

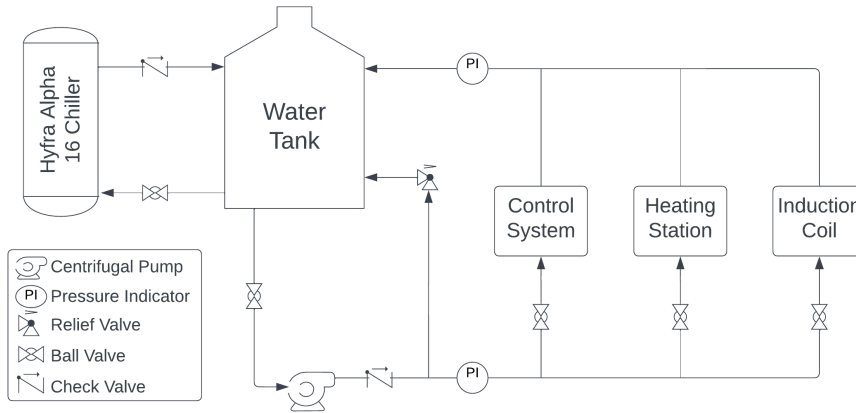


Figure 4: Diagram of the HTR cooling system.

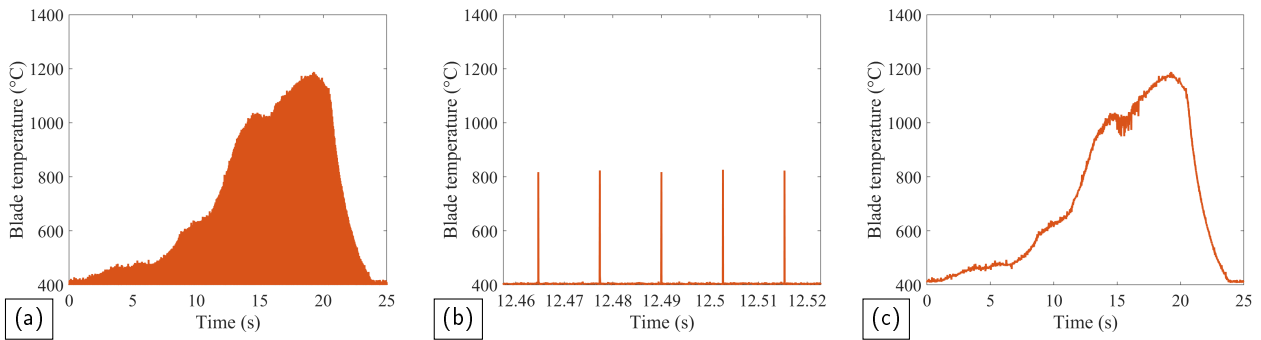


Figure 5: Blade temperature data captured via the high-frequency pyrometer within the HTR, detailing the (a) raw data points, (b) localised peaks, and (c) calculated peak envelope over time.

TBT429 abrasive lining (commercially available from Praxair Surface Technologies, Inc.) onto the top surface (20 mm × 2 mm). This coating consists of 60/80 US mesh single-crystal cubic boron nitride (c-BN) grits embedded within a NiCoCrAlY metallic matrix, deposited via the proprietary Tribomet electroplating process.

The abradable coating specimens, presented in Figure 6c consist of a primary abradable material made of magnesia alumina spinel (MAS). To ensure adequate adhesion, a multi-layered coating architecture was deposited over a 60 mm × 60 mm area onto a 316 stainless steel substrate (80 mm × 80 mm × 3 mm) machined for rig mounting. This coating system comprises a 150 μm thick CoNiCrAlY

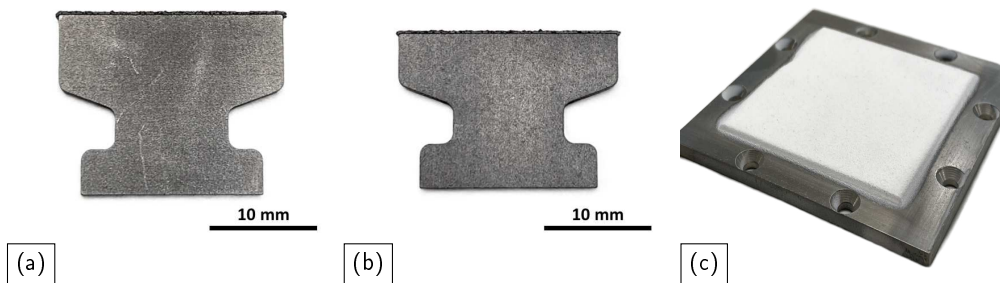


Figure 6: TBT429 c-BN tipped CMSX-4 nickel-based superalloy blades in the (a) long configuration for the SR and (b) short configuration for the HTR, alongside (c) a ceramic magnesia alumina spinel (MAS) abradable sample.

metallic bond coat, followed by a 200 μm thick 7 % wt. yttria-stabilised zirconia (YSZ) interlayer. The final MAS topcoat was subsequently applied to a thickness of either 800 μm or 1500 μm , dictated by the targeted incursion depth. The nominal hardness of the deposited MAS layers ranges from 55 to 66 on the Rockwell superficial hardness scale (15R).

2.4. Experimental strategy

The experimental strategy is structured into two testing phases designed to evaluate the cutting performance and wear mechanisms of the c-BN/MAS abrasable seal system. The first phase focuses on verifying the achievement of engine-representative wear mechanisms at room temperature in the newly commissioned HTR, benchmarked against the established SR at a representative Running-In and Handling (RiH) engine incursion rate of 0.3 $\mu\text{m pass}^{-1}$. The second phase leverages the HTR to perform a parametric study across a broader range of incursion rates under both room- and high-temperature conditions.

As first identified by Borel et al. [24] and subsequently demonstrated in various studies [25, 17, 20], representative engine wear mechanics are achieved on scaled test rigs operating with blade tip speeds in the range of 100 m s^{-1} to 400 m s^{-1} and incursion rates between 0.01 $\mu\text{m pass}^{-1}$ and 2 $\mu\text{m pass}^{-1}$. Previous research utilising the SR at the University of Sheffield demonstrated the simulation of engine performance with c-BN/MAS abrasable seal systems at 200 m s^{-1} and 0.3 $\mu\text{m pass}^{-1}$, up to an overall incursion depth of 500 μm [22]. In the present study, the requirement to maintain blade heating limits the HTR to a maximum tip speed of 100 m s^{-1} , while the overall incursion depth is extended to 1250 μm to thoroughly investigate the failure mechanisms of the c-BN blade tips. Furthermore, because operational rubs in the HPT are typically longer and more sustained, extending the incursion depth better simulates these standard HPT conditions and maximizes the volume of tribological data collected per test. Consequently, it is necessary to establish that the HTR produces comparable representative wear behaviours. That said, considering the highlighted previous studies, reducing the blade tip speed is expected to have negligible impact on the fundamental wear mechanics, as is extending the incursion depth. Specifically in the latter case, Parody Martin et al. [22] demonstrated that a steady-state cut occurs at 0.3 $\mu\text{m pass}^{-1}$ for the c-BN/MAS abrasable seal system at room temperature, with no indication of imminent blade tip failure at extended depths.

To verify the simulation of these representative wear mechanisms, tests are conducted on both platforms at an incursion rate of 0.3 $\mu\text{m pass}^{-1}$, which corresponds to an incursion speed of 99.47 $\mu\text{m s}^{-1}$ and 23.35 $\mu\text{m s}^{-1}$ at a spindle speed of 19894.36 rpm and 4669.58 rpm in the SR and in the HTR, respectively. The in-situ monitoring capabilities on both rigs enable the benchmarking of contact forces over the initial 500 μm incursion depth, alongside the evaluation of the extended 1250 μm range in the HTR. To account for differing rotor diameters and ensure an accurate geometric

benchmark, results are evaluated against the rub length parameter, a previously established governing variable in the wear of abrasable seal systems [17, 10]. The total rub length (RL_{total}) is calculated as the sum of the rub lengths of all individual passes, defined as follows:

$$RL_{total} = \sum_{i=1}^N 2 \cdot R_{disc} \cdot \arccos\left(\frac{R_{disc} - I_{D_i}}{R_{disc}}\right) \quad (3)$$

where $RL_{pass,i}$ is the rub length for pass i , which increases as the incursion progresses, I_{D_i} is the incursion depth at each consecutive pass, R_{disc} is the rotor radius, and N is the total number of passes executed during the test ($N = \text{Incursion Depth} / \text{Incursion Rate}$). Consequently, the baseline 500 μm incursion depth translates to a nominal cumulative rub length of 21.78 m for the SR and 31.62 m for the HTR. This normalises the differing rotor diameters, establishing a standardised geometric baseline to directly benchmark the dynamic contact parameters of the HTR against the established SR.

As outlined, the intention of performing tests on both the SR and HTR is to demonstrate that, at room temperature, the latter rig produces wear in line with an established benchmark, and that reducing the blade speed to 100 m/s and increasing the test depth to 1250 microns have no significant impact on the mechanisms present. This result will in turn be used to confirm that any changes seen when the induction heater is utilised on the HTR, are as a consequence of the addition of heat to the tribo-system, as opposed to something more fundamental in the rigs design or operating point. As such, tests are performed on each rig at their standard operating conditions, and the wear generated compared. This approach is also reflective of the fact that replicating test conditions across different scaled rig architectures—such as any of those highlighted in the Introduction—is unlikely to yield identical raw data outcomes, given no two experimental platforms are dynamically similar. It also aligns with the wider body of work on this topic, where results are routinely compared across rigs in the context of the wear mechanisms generated, provided of course test conditions fall within a range considered acceptable to be representative. That said, and for completeness, a wider comparator data set for both rigs is also included in the Appendix A, and referred to in the Discussion section.

Building upon this initial wear mechanism simulation phase, the experimental matrix is completed through a testing phase comprising thirteen tests executed on the HTR. These evaluate blade performance across four distinct incursion rates (0.3 $\mu\text{m pass}^{-1}$, 0.7 $\mu\text{m pass}^{-1}$, 1.0 $\mu\text{m pass}^{-1}$, and 1.5 $\mu\text{m pass}^{-1}$, derived from the spindle speed and stage linear speed presented in Table 1), simulating engine manoeuvres ranging from standard RiH to aggressive Maximum Take-Off (MTO) casing distortions. These parameters are tested under both room- and high-temperature conditions utilising the induction coil. The introduction of this thermal dimension is designed to simulate the thermo-mechanical

Table 1

Relationship between spindle speed and stage linear speed for setting the target incursion rate.

I_R ID	ω (rpm)	I_S ($\mu\text{m s}^{-1}$)	I_R ($\mu\text{m pass}^{-1}$)
1	4669.58	23.35	0.3
2	4669.58	54.48	0.7
3	4669.58	77.83	1.0
4	4669.58	116.74	1.5

stresses experienced in HPT environments, effectively reproducing the wear mechanisms and subsequent performance failures observed in actual gas turbine operations [11, 15].

2.5. Test protocol and conditions

Operating procedures across both the SR and the HTR were standardised to ensure consistency across all test campaigns. For safety and operational integrity, both test rigs are housed within a dedicated test cell featuring impact-resistant walls and independent containment enclosures, allowing for remote operation via a centralised control computer.

Testing begins by securing the blade specimen into a custom-machined clamp integrated into the rotor disc periphery. The abradable sample is mounted onto the motorised linear stage, with the triaxial piezoelectric dynamometer positioned immediately behind it to capture normal and tangential contact forces. To establish the initial zero-reference position, the linear stage is incrementally advanced until the abradable surface interfaces with the static blade tip. Once this datum is set, the stage is retracted by a predetermined clearance distance. This safety margin prevents premature contact caused by transient vibrations and centrifugal disc expansion during spindle acceleration. Prior to test initiation, optical and thermal instruments are focused and calibrated.

A custom-developed LabVIEW software architecture controls the entire experimental suite, coordinating synchronisation, data logging, and the connected subsystems.

The test execution sequence begins by accelerating the rotor to the specific rotational frequency required for the target linear blade tip speed. Once steady-state rotation is confirmed, the automated test sequence is triggered. The LabVIEW architecture commands the measurement suite to begin synchronised data acquisition while instructing the linear stage to advance toward the rotating disc. The linear approach velocity of the stage is strictly governed by the product of the prescribed test incursion rate ($\mu\text{m pass}^{-1}$) and the rotational frequency of the disc.

The abradable sample continuously feeds into the rotating blade tip, sustaining intermittent high-speed contact until the user-defined maximum incursion depth is achieved. Upon reaching this target depth, the linear stage executes a rapid retraction to its initial safe position, disengaging the contact interface. The spindle is then commanded to decelerate, and the control software finalises the data transfer before shutting down the sequence. To ensure operational safety, a mechanical emergency stop (E-stop) is integrated

into both platforms, capable of immediately disabling all active rig components.

For tests conducted under high-temperature conditions in the HTR, an additional thermal protocol is integrated to govern the blade tip temperature. Prior to spindle acceleration, the induction heating system statically pre-heats the blade to establish the initial temperature baseline. Once the required thermal profile is achieved, the disc is accelerated. Throughout the subsequent rotation and dynamic incursion phase, the induction coil remains continuously active, functioning as a supplementary heat source to offset the forced convection cooling generated by the disc windage.

The full experimental campaign was executed according to a systematically structured test matrix. Table 2 summarises the specific testing platforms, kinematic parameters, and thermal conditions for all individual tests conducted across the two experimental phases.

The specific distribution of the experimental matrix corresponds directly to the established testing stages: the engine-representative wear mechanism simulation phase comprises tests SRT1, SRT2, HTRT1, and HTRT2, while the high-temperature testing phase encompasses tests HTRT1 through HTRT13.

3. Results

The experimental results are structured into two primary sections, reflecting the established testing phases. Initially, the engine-representative wear mechanism simulation phase evaluates the room-temperature performance of the novel HTR against the established SR benchmark at the baseline $0.3 \mu\text{m pass}^{-1}$ incursion rate. Following this, the high-temperature wear phase details a comprehensive thirteen-test parametric study conducted on the HTR, assessing abrasive blade performance across a range of incursion rates under both ambient and elevated thermal conditions.

3.1. Engine-representative wear mechanism simulation

Macroscopic inspection of the post-test abradable specimens (Figure 7) shows clear geometric variations in the resultant rub tracks. As anticipated from the kinematic disparities between the platforms, the rub track lengths generated within the HTR (Figures 7c and 7d) are longer than those produced in the SR (Figures 7a and 7b). This difference is due to both the extended maximum incursion depth of the HTR tests and the increased rubbing distance in each pass caused by its larger disc diameter.

Despite these geometrical and kinematic differences, wear track morphology remained consistent across both platforms. The abradable surfaces showed the topographical features of a brittle fracture-driven material removal mechanism, evidenced by defined longitudinal grooves along the rub path. These grooving features appeared slightly more pronounced in the specimens tested within the SR.

Examination of the post-test blade surfaces revealed more transfer of abradable material onto the abrasive tips

Table 2

Summary of the experimental test matrix detailing testing platforms, kinematics, and thermal conditions.

Test ID	Test rig	Incursion rate ($\mu\text{m pass}^{-1}$)	Blade speed (m s^{-1})	Incursion depth (μm)	Thermal condition
SRT1	SR	0.3	200	500	Room temperature
SRT2	SR	0.3	200	500	Room temperature
HTRT1	HTR	0.3	100	1250	Room temperature
HTRT2	HTR	0.3	100	1250	Room temperature
HTRT3	HTR	0.3	100	1250	High temperature
HTRT4	HTR	0.3	100	1250	High temperature
HTRT5	HTR	0.7	100	1250	Room temperature
HTRT6	HTR	0.7	100	1250	Room temperature
HTRT7	HTR	0.7	100	1250	High temperature
HTRT8	HTR	0.7	100	1250	High temperature
HTRT9	HTR	1.0	100	1250	Room temperature
HTRT10	HTR	1.0	100	1250	High temperature
HTRT11	HTR	1.0	100	1250	High temperature
HTRT12	HTR	1.5	100	1250	Room temperature
HTRT13	HTR	1.5	100	1250	High temperature

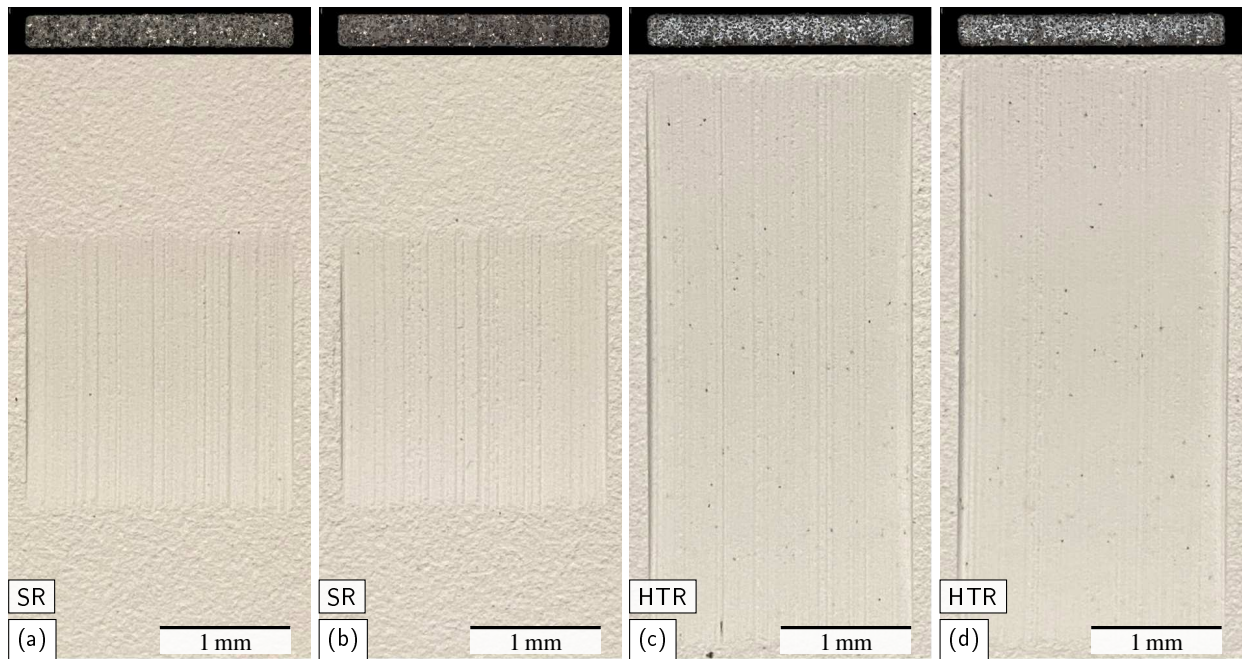


Figure 7: Post-test images of the MAS abrasable samples and TBT429 blades (top view) from the room-temperature wear mechanism simulation tests at a representative RiH incursion rate of $0.3 \mu\text{m pass}^{-1}$ for the (a,b) established SR benchmark (200 ms^{-1} , $500 \mu\text{m}$ depth) and (c,d) HTR baseline condition (100 ms^{-1} , $1250 \mu\text{m}$ depth).

during the HTR tests compared to the SR baselines. This can be attributed to the extended cumulative rub length, although the material adhesion remained strictly superficial. Despite this localised material adhesion, all tests achieved a clean cut through the ceramic coating. Visual inspections confirmed there was no blade damage or structural failure in either testing environment. These observations confirm that the modified rotor dynamics and extended incursion profiles of the HTR did not introduce anomalous wear phenomena.

The dynamic contact forces and thermal responses recorded during the wear mechanism simulation phase are presented

in Figure 8. In the SR tests (Figures 8a and 8b), the normal force exhibited a progressive increase up to approximately 0.25 kN following an initial accommodation phase. Simultaneously, the tangential force remained stable at a low magnitude of approximately 0.05 kN . This force evolution confirmed an efficient cutting mechanism governed by the brittle fracture of the abrasable ceramic, where the abrasive grits effectively evacuated the sheared material without inducing significant matrix friction. The maximum abrasable temperature correspondingly stabilised around 200°C , well within the safe operational limits of the tribological system.

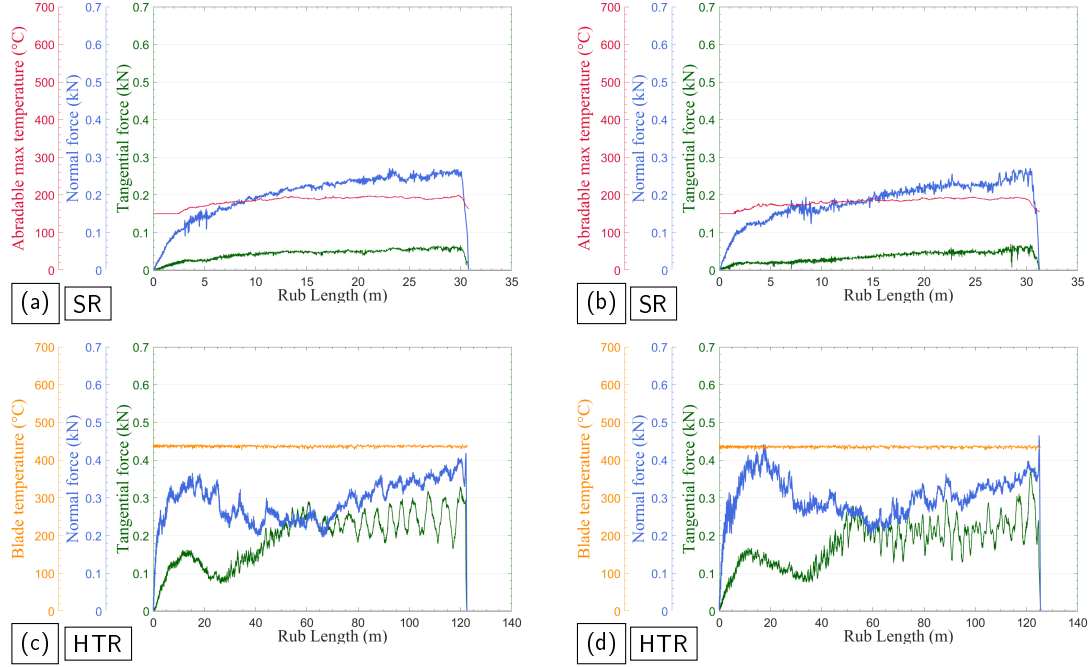


Figure 8: Force and temperature data plotted as a function of rub length for the room-temperature wear mechanism simulation tests at an incursion rate of $0.3 \mu\text{m pass}^{-1}$, displaying the tangential force, normal force, and maximum measured temperature for the (a,b) established SR benchmark (200 m s^{-1} , $500 \mu\text{m}$ depth) and (c,d) HTR baseline condition (100 m s^{-1} , $1250 \mu\text{m}$ depth).

Force profiles from the HTR tests (Figures 8c and 8d) displayed a non-linear progression linked to the extended $1250 \mu\text{m}$ incursion depth and longer cumulative rub length. Following an initial rapid increase approaching 0.4 kN , the normal force decreased before initiating a secondary rise characterised by oscillatory behaviour. This instability was mirrored and amplified in the tangential force, which experienced a relative increase in the latter half of the test, reaching peaks up to 0.3 kN .

Despite the increased workload and the onset of oscillatory contact mechanics, the high-frequency pyrometer focused on the blade tip did not register any readings throughout the extended rub length, as thermal emissions from the blade remained below the instrument's minimum detection threshold of approximately 435°C (adjusted for the applied emissivity). While the two testing platforms monitor different thermal targets—the stationary abradable in the SR and the rotating blade in the HTR—this lack of pyrometer activation is entirely consistent with the SR baseline, where the maximum abradable temperature barely reached 200°C . This correlation confirms that the low-temperature, efficient cutting nature of the contact is analogous between both rigs. Furthermore, the continuous absence of recorded thermal spikes indicated that the blade tip did not experience severe overheating at any point during the extended $1250 \mu\text{m}$ incursion. This stable thermal response aligned with post-test macroscopic inspections, which confirmed the preservation of the abrasive coating's structural integrity.

3.2. High-temperature wear

Following the verification of engine-representative wear mechanisms in the HTR at room temperature, the experimental campaign shifted to the high-temperature wear phase. A total of thirteen tests evaluated abrasive tip performance across four distinct incursion rates: $0.3 \mu\text{m pass}^{-1}$, $0.7 \mu\text{m pass}^{-1}$, $1.0 \mu\text{m pass}^{-1}$, and $1.5 \mu\text{m pass}^{-1}$. This parametric sweep simulated engine operating conditions ranging from standard RiH clearance closures to aggressive mechanical interactions driven by MTO manoeuvres and casing distortions. Given the consistency and structural integrity observed during room-temperature testing at lower incursion rates, the number of repetitions was progressively reduced for higher rates to optimise sample usage. Repetitions at the most aggressive high-temperature condition ($1.5 \mu\text{m pass}^{-1}$) were omitted, as the severe blade failure observed in Figure 9m at these parameters induces unstable rotor dynamics and excessive contact forces that fall outside the designed operational envelope of the test rig.

Macroscopic inspections of the specimens tested under room-temperature conditions (Figure 9) indicated consistent cutting performance across the evaluated incursion rates. As observed in Figures 9a, 9b, 9e, 9f, 9i, and 9l, the abrasive tips executed a clean cut through the ceramic coating without sustaining macroscopic structural damage. Although the blades maintained their structural integrity, a progressive increment in localised abradable material transfer (pick-up) onto the blade tip was recorded as the incursion rate intensified, culminating at the $1.5 \mu\text{m pass}^{-1}$ condition (Figure 9l).

High-temperature blade wear

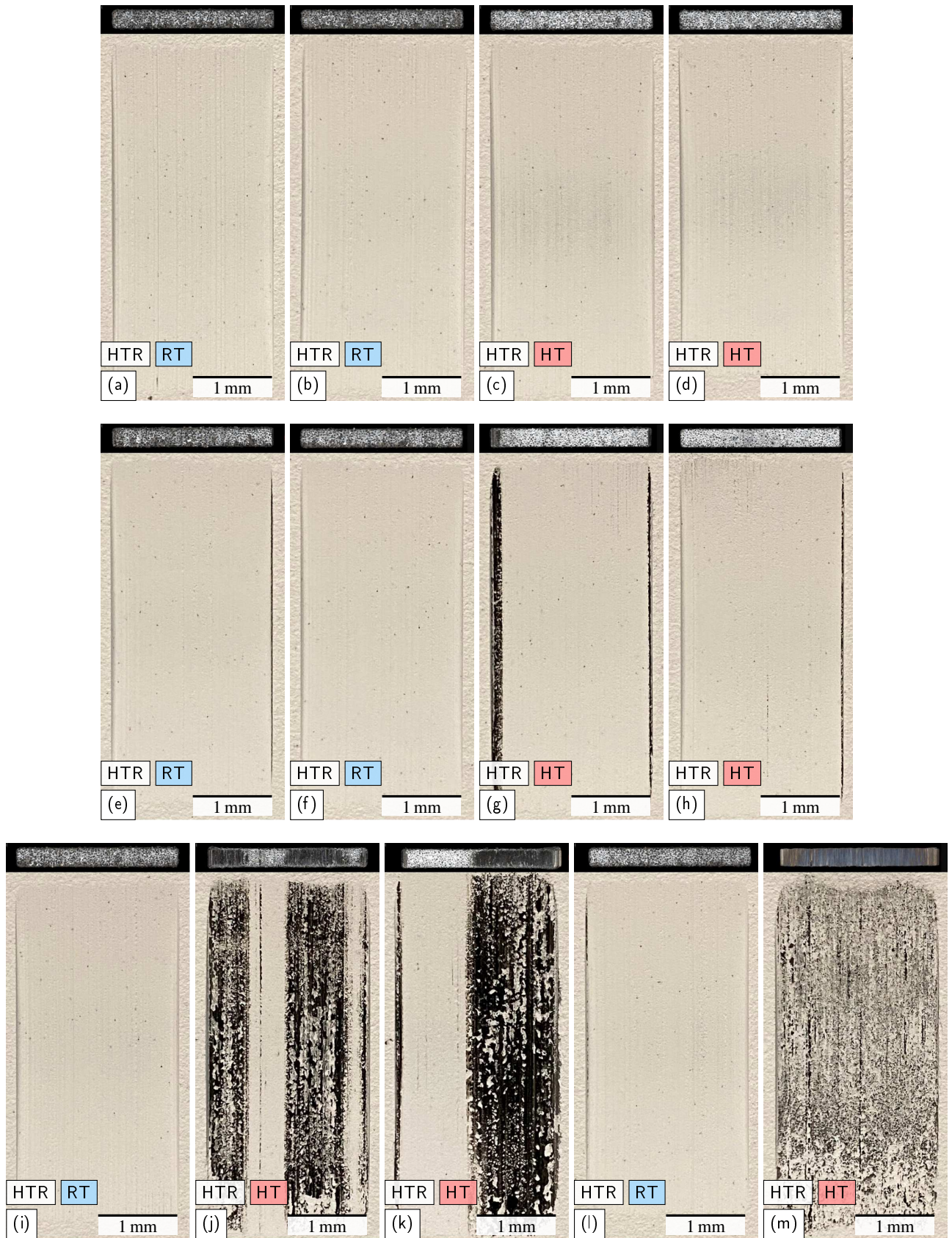


Figure 9: Post-test images of the MAS abrasable samples and TBT429 blades (top view) following room- and high-temperature tests conducted in the HTR (100 ms^{-1} , $1250 \mu\text{m}$ depth) across incursion rates of (a,b,c,d) $0.3 \mu\text{m pass}^{-1}$, (e,f,g,h) $0.7 \mu\text{m pass}^{-1}$, (i,j,k) $1.0 \mu\text{m pass}^{-1}$, and (l,m) $1.5 \mu\text{m pass}^{-1}$.

Activating the induction heating system altered the wear mechanisms, showing the abrasive coating's vulnerability to combined thermo-mechanical stresses. At the baseline RiH incursion rate of $0.3 \mu\text{m pass}^{-1}$ under hot conditions (Figures 9c and 9d), the blade exhibited a significant degree of material adhesion. This initial high-temperature material pick-up exceeded the maximum adhesion observed at the most aggressive incursion rate under room-temperature conditions. This material accumulation saturated rapidly at elevated incursion rates, initiating a progressive structural degradation of the abrasive tip. The onset of partial blade failure emerged at $0.7 \mu\text{m pass}^{-1}$ (Figures 9g and 9h), characterised by the localised detachment of the abrasive matrix strictly along the blade edges. As the incursion rate escalated to $1.0 \mu\text{m pass}^{-1}$ (Figures 9j and 9k), this damage propagated, resulting in larger regions of matrix loss.

Blade performance degraded further at the maximum $1.5 \mu\text{m pass}^{-1}$ incursion rate under high-temperature conditions (Figure 9m), leading to complete structural failure. This test resulted in metal discolouration on the blade surface, indicating thermal damage. Furthermore, the corresponding abrasible wear track presented a differing morphology compared to the preceding tests. This track finish indicates that once the abrasive layer detached, the bare metallic substrate rubbed directly against the ceramic, changing the contact from cutting to friction.

Dynamic contact forces and thermal responses from this thirteen-test phase are illustrated through four representative profiles in Figure 10. These specific profiles—contrasting the baseline RiH incursion rate ($0.3 \mu\text{m pass}^{-1}$) against an aggressive rate ($1.0 \mu\text{m pass}^{-1}$) under both room- and high-temperature conditions—encapsulate the mechanical transition from stable cutting to performance degradation. This approach establishes the basis for the subsequent aggregated evaluation of maximum forces and temperatures across all parameters, summarised in Figure 11. As a preliminary observation across all high-temperature profiles, the initial blade temperature inherently decreased below the minimum detection threshold of the high-frequency pyrometer during the rapid rotor spin-up phase immediately prior to engaging contact. However, at higher incursion rates, the blade temperature rapidly re-emerged above the detection threshold upon contact. This quick thermal recovery indicates that the preceding convective heat loss was likely confined to the superficial layer, a phenomenon that is further addressed in the Discussion.

Under room-temperature conditions, the cutting interaction remained stable regardless of the incursion rate. As depicted for the $0.3 \mu\text{m pass}^{-1}$ and $1.0 \mu\text{m pass}^{-1}$ cases (Figures 10a and 10c, respectively), contact forces stabilised at low magnitudes, and thermal emissions remained below the pyrometer's operating range. This stable mechanical behaviour closely mirrored the high-temperature baseline test at $0.3 \mu\text{m pass}^{-1}$ (Figure 10b). At this standard rate, neither the forces nor the operational temperatures deviated significantly from room-temperature testing counterparts,

correlating with the acceptable visual wear characteristics established previously.

Under high-temperature conditions, the dynamic response evolved as incursion rates increased above the baseline. This mechanical progression is captured in the $1.0 \mu\text{m pass}^{-1}$ profile (Figure 10d). The contact interaction was characterised by a steady, progressive ramp-up in both the normal force and the blade tip temperature throughout the incursion, reaching their maximum values prior to the controlled disengagement of the linear stage. Concurrently, the tangential force experienced an increase; however, its magnitude deviated from the sharp escalation observed in the normal force component. This divergence between normal and tangential forces indicates a transition in the contact mechanics, shifting from abrasive cutting to a friction-dominated rubbing regime under thermo-mechanical loads. This transition is corroborated in Figure 9j through the evident material transfer from the blade to the abrasible sample due to smearing. Although the individual time-domain profiles for the $0.7 \mu\text{m pass}^{-1}$ and $1.5 \mu\text{m pass}^{-1}$ conditions are omitted for brevity, continuous test data confirmed a consistent trend. Advancing the rate to $0.7 \mu\text{m pass}^{-1}$ induced a milder gradual ramp-up in forces and temperatures towards the conclusion of the test. At the maximum $1.5 \mu\text{m pass}^{-1}$ rate, the escalation of the three monitored variables initiated earlier, prior to the mid-point of the test, culminating in peak values that surpassed those recorded during the $1.0 \mu\text{m pass}^{-1}$ incursion.

The aggregated data presented in Figure 11 highlights the time-domain observations. Under room-temperature conditions, the system demonstrates overall stability, with maximum normal forces and blade temperatures remaining consistently low across all incursion rates. An exception is noted for test HTRT5, conducted at $0.7 \mu\text{m pass}^{-1}$, where a thin black line is observed on the right side of the abrasible coating (Figure 9e). This feature aligns with the precise location where the pyrometer focuses on the blade, thereby accounting for the elevated temperature recorded in Figure 11 for this specific room-temperature incursion. Excluding this isolated phenomenon, the room-temperature results confirm the presence of an efficient abrasive cutting mechanism. Furthermore, this stability is mirrored in the high-temperature baseline test performed at $0.3 \mu\text{m pass}^{-1}$.

High-temperature tests reveal a performance degradation at elevated incursion rates. Advancing to $0.7 \mu\text{m pass}^{-1}$ induces an initial escalation in mechanical and thermal responses, acting as a precursor to a critical transition at $1.0 \mu\text{m pass}^{-1}$. At this stage, the maximum normal force experiences an abrupt increase to nearly 10 kN, accompanied by a thermal surge that drives the blade temperature above 1000°C .

At the most aggressive $1.5 \mu\text{m pass}^{-1}$ high-temperature condition, the normal force peaks near 13 kN. While the mechanical load continues to escalate, the corresponding temperature increase moderates, plateauing at approximately 1100°C . This suggests a thermal saturation point once

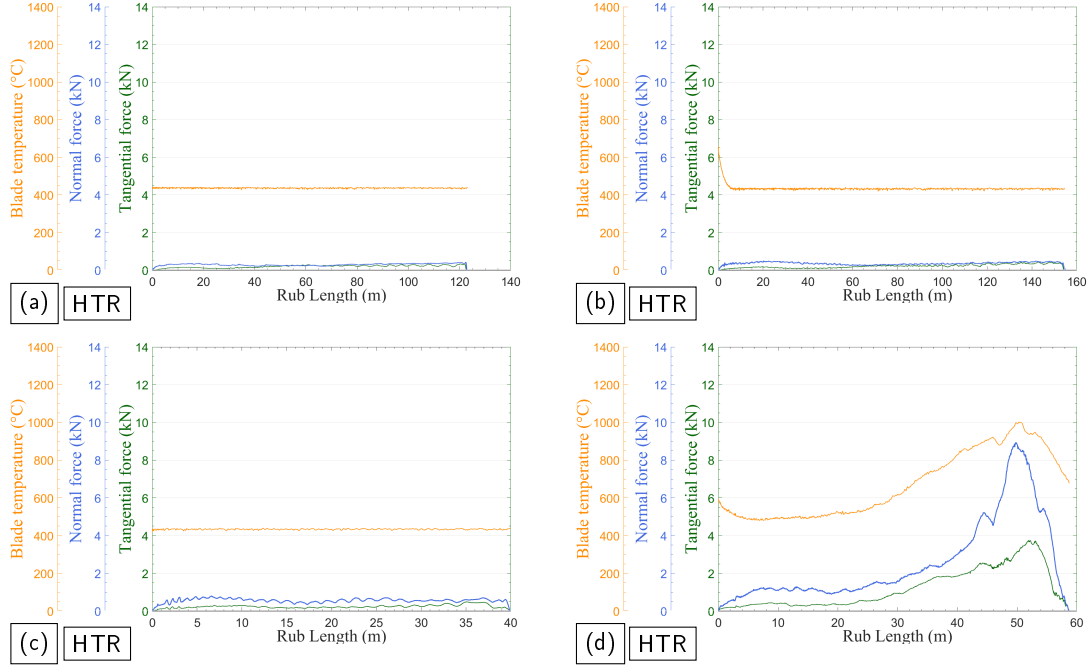


Figure 10: Normal force, tangential force, and blade temperature plotted as a function of rub length for tests conducted in the HTR (100 m s^{-1} , $1250 \mu\text{m}$ depth). Data corresponds to the (a) room-temperature and (b) high-temperature conditions at $0.3 \mu\text{m pass}^{-1}$, alongside the (c) room-temperature and (d) high-temperature conditions at $1.0 \mu\text{m pass}^{-1}$.

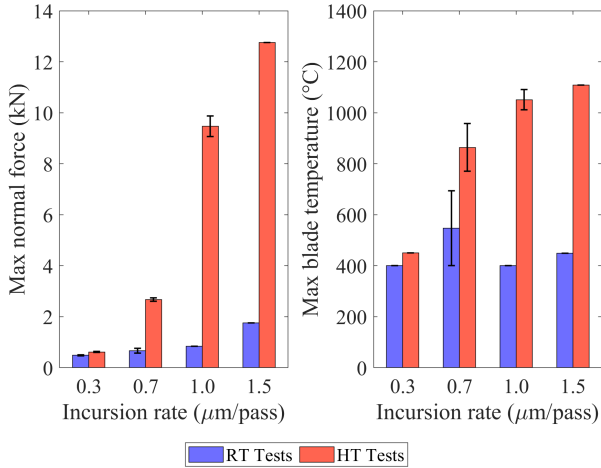


Figure 11: Comparison of the maximum normal force on the left and maximum blade temperature on the right, recorded during room-temperature (RT) and high-temperature (HT) incursion tests, plotted as a function of the incursion rate.

the system fully transitions into a friction-dominated rubbing regime, where total cutting failure generates excessive mechanical loads but diminishing incremental temperature gains.

4. Discussion

This experimental campaign extends mechanical room-temperature testing into the thermo-mechanical environment characteristic of the HPT. The transition to the newly developed HTR required an adaptation of the kinematic parameters, specifically reducing the blade tip speed to 100 m s^{-1} . Despite this velocity reduction and the augmentation of the incursion depth, the results presented herein demonstrate engine-representative wear mechanics. This confirms that the underlying cutting dynamics remain directly comparable between the SR and the HTR platforms.

For completeness, results are also compared to additional test data (detailed in Appendix A) for both rigs to verify the non-variability of the abrasable system behaviour to blade speed and incursion depth when moving between rigs. The tests referenced were performed at the same standard RiH incursion rate of $0.3 \mu\text{m pass}^{-1}$ but at a lower speed of 100 m s^{-1} in the SR and up to a shallower incursion depth of $500 \mu\text{m}$ in the HTR.

All tests presented a clean cut of the abrasable. Regarding force analysis, similar maximum forces are found when testing at a lower speed in the SR (Figure 12a), as previously pointed out in [17, 20]. Conversely, although very similar maximum normal forces are also recorded when testing at a shallower incursion in the HTR (Figure 12c), the maximum tangential force drops by approximately half when the incursion depth is decreased.

However, the HTR force graphs in Figure 10 depict that the maximum tangential forces prior to 31.62 m , which

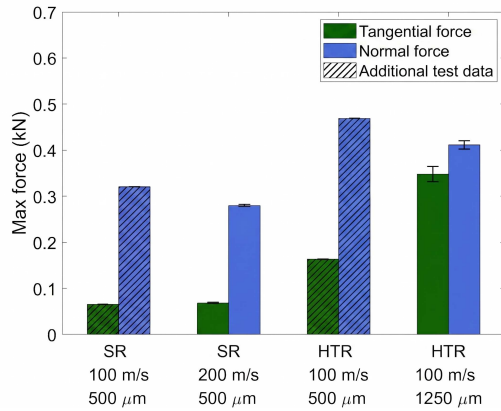


Figure 12: Comparison of the maximum tangential and normal forces between the additional tests (a,c) and the baseline conditions (b,d) for the SR and HTR, all performed at 0.3 $\mu\text{m}/\text{pass}$ and room temperature.

correspond to an incursion depth of 500 μm in the HTR, are similar to the maximum forces obtained from the shallower test. This indicates that longer rub tracks at deeper incursions hinder the evacuation of the cut material, thereby elevating the tangential force. Nevertheless, the normal force, which acts as a failure indicator, remains invariant regardless of the incursion depth parameter.

The HTR configuration results in an extended rub length, thereby increasing the volume of material removed per pass and prolonging the residency time in contact. This geometric factor accounts for the steady force progression observed during the room-temperature wear simulation tests. As the incursion progresses, the evacuation of fractured ceramic debris becomes increasingly hindered along the extended contact arc, leading to localised debris entrapment. This third-body interaction subsequently elevates the tangential forces and promotes material transfer. Nevertheless, the absence of thermal spikes and the maintenance of consistently low overall forces confirm that, under room-temperature conditions, the abrasive grits effectively clear the debris without experiencing a loss in cutting efficiency.

Testing with the induction heating system introduces an additional dimension to the frictional heating arising from the incursion contact. First, by introducing a static pre-heating that sets the fundamental thermal baseline, followed by a continuous supplementary induction heating that mitigates the convective heat loss due to the blade rotation. Consequently, this external heating source operates in conjunction with the inherent frictional heat generated during contact, collectively driving the interfacial temperatures characteristic of the wear process.

The results presented demonstrate that abrasive coating degradation depends heavily on temperature. At the baseline 0.3 $\mu\text{m pass}^{-1}$ incursion rate, the cutting interaction remained mechanically stable under both room- and high-temperature conditions. However, the application of external heat significantly amplified the degree of abrasible pick-up

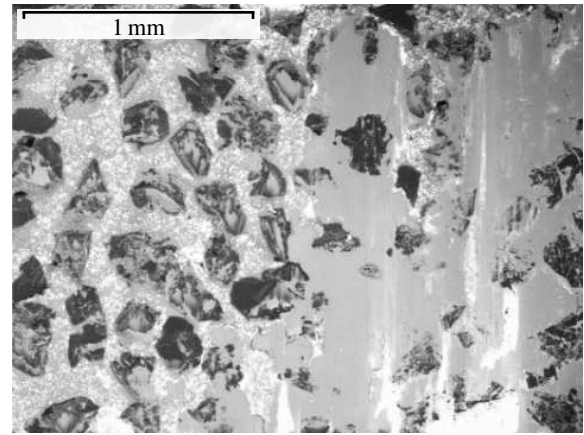


Figure 13: Detailed SEM image of the HTRT10 blade following incursion testing.

on the blade. The process of material transfer initiates when the ceramic abrasible is fractured by the grits and subsequently compressed underneath the blade tip. At elevated temperatures, this compressed material easily adheres to the hot metallic matrix and becomes trapped between the grits.

This abrasible material entrapment is further confirmed by the SEM-EDS analysis conducted on a section of the HTRT10 blade, as illustrated in Figure 13 and 14. SEM and EDS images were acquired by using a JSM-IT300 InTouch-Scope microscope and an Oxford Instrument Xplore AZtec detector, for the SEM and EDS imaging respectively.

The SEM image (Figure 13) reveals a non-failed but congested area on the left and a failed area on the right, where the c-BN grits have begun to dislodge and fracture. This process leaves behind a plastically deformed matrix with entrapped abrasible material. Regarding the EDS analysis presented in Figure 14, the Mg and O elemental mapping (Figures 14c and 14f), which represents the abrasible material constituents, demonstrates a widespread distribution across the entire analysed area. The presence of these elements in both the non-failed and failed portions highlights severe abrasible entrapment. Aluminium (Al) is omitted from this evaluation as it is additionally present in the abrasive matrix. Furthermore, the Ni and Co elemental mapping (Figures 14b and 14e) exhibits a homogeneous distribution in the failed region, which contrasts with the defined pattern surrounding the grits in the non-failed section. Finally, the B and N elemental mapping (Figures 14a and 14d) displays the well-defined shape of the grits in the non-failed region, whereas only traces are observed in the failed region. This indicates a mixture of fractured grits, fractured fragments adhered to the matrix, and buried fractured grits, as observed from the cross-sectional images in Figure 15, caused by the matrix softening due to high blade temperature.

Going into further detail, the healthy region depicted in Figure 15a comprises three distinct, horizontally interfaced layers: the blade substrate, an adhesion layer, and the matrix. The grits are partially embedded within the matrix and do

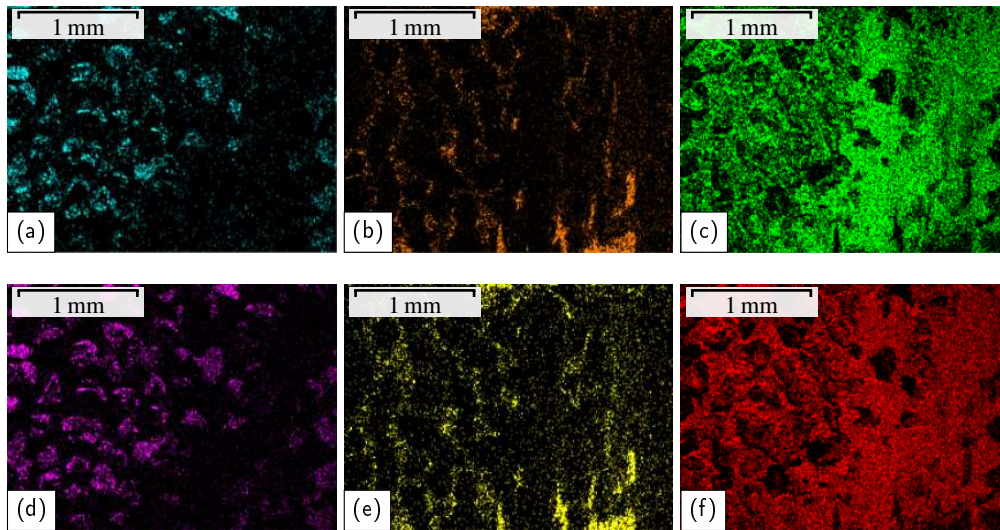


Figure 14: SEM-EDS elemental mapping of (a) B, (b) Ni, (c) Mg, (d) N, (e) Co and (f) O on the surface of the HTRT10 blade post test.

not reach the underlying adhesion layer, which securely bonds the matrix to the substrate.

Conversely, the damaged region (Figure 15b) reveals grits pushed downward and completely buried within the matrix. This penetration deforms the initially horizontal matrix-adhesion interface, forcing an upward flow of material in grit-free areas, although the blade substrate interface remains intact.

To evaluate the material transfer on the abrasible coating following the incursion tests, a FischerScope XAN 250 X-ray fluorescence spectrometer was employed to assess the elemental distribution along the rub track. The analysis focused on identifying surface material transfer from the blade, specifically Co, Ni and Cr, while differentiating them from underlying elements such as zirconium and yttrium that belong to the intermediate YSZ layer. As the detection capabilities of the system are hard-limited to elements heavier than aluminum, it does not detect any element present in the MAS topcoat (Al, Mg or O). For the data acquisition setup, a voltage of 30 kV with no primary filter was selected to optimize the detection of thin surface transfer layers without excessive X-ray penetration into the bond coat. Finally, to ensure the measurement area fitted perfectly within the narrow material transfer region and to gather a reliable signal, a collimator size of 0.2 mm was utilized alongside an acceptance quality mq value of 10 and an acquisition scan time of 180 s per test point. The experimental results are reported as relative elemental concentrations, expressed in percentages. These values are derived by applying the established spectrometer calibration to the raw counts per second (cps) measurements. Measurements were taken from the deepest region of the HTRT7 abrasible coating, comprising five measurements from each of the two distinct zones of the rub track: (1) the clean cut region and (2) the material transfer region as depicted in Figure 16.

Table 3

Elemental composition percentages across the four distinct zones of the HTRT7 abrasible coating rub track acquired via X-ray fluorescence spectrometry.

Region	Co (%)	Ni (%)	Cr (%)	Zr (%)	Y (%)
1	0.33 ± 0.16	0.67 ± 0.10	2.12 ± 0.25	89.02 ± 0.15	7.86 ± 0.12
2	19.45 ± 2.66	40.48 ± 4.03	21.30 ± 0.39	16.70 ± 3.87	2.06 ± 0.46

As presented in Table 3, the material transfer region demonstrates a high concentration of Co, Ni, and Cr, which are initially present only in the metallic matrix of the blade's abrasible coating. Conversely, the clean cut region shows a negligible content of these metallic elements. In this region, only Zr and Y from the intermediate YSZ layer of the abrasible lining are detected, as the spectrometer is unable to detect lighter elements such as Al, Mg, and O from the MAS topcoat.

As derived from these microstructural evaluations, the temperature-driven material loading promotes, in turn, further material entrapment and leads to progressive structural degradation. The accumulation of adhered ceramic diminishes the cutting efficiency of the blade, thereby requiring higher normal forces to penetrate and fracture the abrasible coating. These elevated contact forces generate additional frictional heat. When combined with the external thermal load from the induction coil, the overall blade temperature increases, softening the matrix and exacerbating material collection and ultimately leading to blade congestion. This phenomenon is analogous to the wheel loading of superabrasive grinding wheels, where inter-grit clearance is critical for maintaining material release capabilities [26, 27].

Heat accumulation combined with material congestion triggers the coating failures observed at the aggressive $1.0 \mu\text{m pass}^{-1}$ and $1.5 \mu\text{m pass}^{-1}$ incursion rates. The pronounced divergence between the normal and tangential

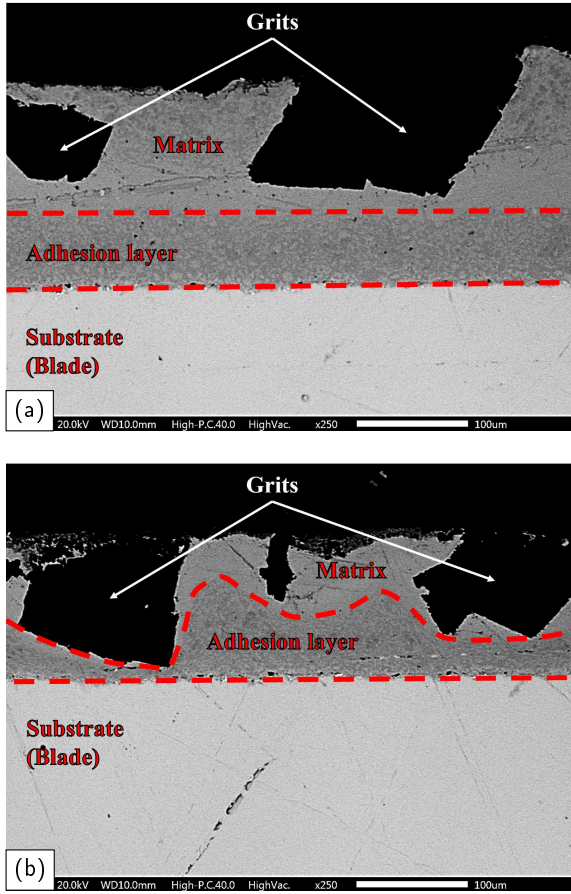


Figure 15: SEM cross-sectional images of the HTRT10 blade depicting the abrasive coating structure at regions where the blade (a) did not fail and (b) did fail after test.

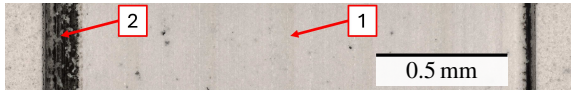


Figure 16: Detailed view of the deepest region of the HTRT7 abrasible coating rub track, highlighting the specific locations of the measurement points across the clean cut and material transfer regions.

forces recorded at $1.0 \mu\text{m pass}^{-1}$ serves as an indicator of this failure mechanism. As the thermal load increases, the strength of the electroplated matrix and its underlying adhesion layers is significantly reduced. Under the extreme normal forces demanded by the aggressive incursion, the weakened abrasive layer structurally collapses and can no longer cut.

As the abrasive tip fails, the contact mechanics transition entirely: the bare, softened matrix—and eventually the underlying metallic substrate—rubs directly against the ceramic coating. This degradation and blade failure is visually confirmed by the smearing and large material transfer at $1.0 \mu\text{m pass}^{-1}$ (Figure 9j and 9k), and the metal discolouration from thermal damage at $1.5 \mu\text{m pass}^{-1}$ (Figure 9m).

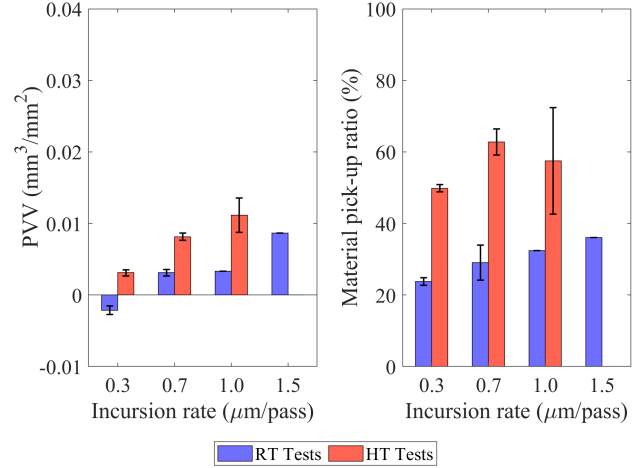


Figure 17: Comparison of the Positive Volume Variation (PVV) on the left and material pick-up ratio on the right, recorded during room-temperature (RT) and high-temperature (HT) incursion tests, plotted as a function of the incursion rate.

The degree of damage and subsequently remaining cutting capability of tested blades can be assessed through the 3D surface scanning methodology developed in a previous analytical study [22]. Two objective metrics are extracted exclusively from the non-failed regions of the abrasive tip: Positive Volume Variation (PVV), which measures the volumetric blade wear per area (where negative values indicate adhered material volume exceeds lost grit volume), and the material pick-up ratio, which represents the percentage of blade surface covered by adhered ceramic. For PVV evaluation, a 3D point cloud of the abrasive coating is captured across an approximate 20 mm^2 area, utilizing a horizontal and vertical resolution of $20 \mu\text{m}$ and 500 nm , respectively. The reference datum plane is established by determining the arithmetic mean surface height (S_a) from $0.1 \text{ mm} \times 0.1 \text{ mm}$ square areas manually selected from the bare metallic matrix. Vertical coordinates are subsequently normalized by subtracting (S_a), and the PVV is computed via trapezoidal numerical integration of the positive heights. To ensure reproducibility, the extracted data is subjected to a normalisation protocol comprising rotation, centring, and tilt correction.

As presented in Figure 17, the room-temperature tests numerically confirm an efficient cutting regime. At the lowest incursion rate, the PVV registers a negative value, demonstrating that the initial material adherence outweighs any negligible blade wear. As the incursion rate increases under room-temperature conditions, the volumetric blade wear exhibits only a slight, stable increase alongside the areal material pick-up.

The high-temperature environment completely alters this balance. Even at the mechanically stable $0.3 \mu\text{m pass}^{-1}$ baseline, the thermal conditions elevate the pick-up ratio to approximately 50% area coverage and push the PVV

into positive values, marking the onset of measurable volumetric wear. As heating accelerates at $0.7 \mu\text{m pass}^{-1}$ and $1.0 \mu\text{m pass}^{-1}$, PVV increases significantly. This blade wear, combined with high area coverage by adhered material, provides a numerical footprint of the thermo-mechanical degradation driving the contact transition. This occurs immediately prior to the structural collapse at $1.5 \mu\text{m pass}^{-1}$, which renders topographical analysis impossible.

In this friction-dominated rubbing regime, mechanical loads become excessive, as evidenced by the normal force approaching 13 kN at the maximum $1.5 \mu\text{m pass}^{-1}$ incursion rate. However, the plateauing of the maximum blade temperature near 1100°C at this condition suggests a point of thermal saturation. Once the system has transitioned from cutting to pure frictional rubbing, the heat generation reaches a steady-state limit governed by the yielding and rapid plastic deformation of the blade's structural materials. This thermal ceiling aligns with the critical threshold identified by Davenport et al. [15], who established that the MCrAlY anchor phase undergoes extensive high-temperature creep and loses its mechanical strength between 1100°C and 1200°C . While Davenport et al. observed this structural collapse by isothermally soaking specimens in a furnace at similar temperatures, the present results establish that such a severe ambient environment is not required for failure. Although the induction heating system provides an initial thermal load, convective cooling during the high-speed rotor spin-up ensures the blade engages the abrasible at temperatures below this yield threshold. Instead, the combination of an elevated thermal baseline and intense localised frictional heating—driven by third-body abrasion and material congestion—rapidly forces the contact interface to the critical 1100°C limit. Further increases in mechanical load therefore yield diminishing incremental temperature gains, as the input energy is primarily dissipated through the plastic deformation and smearing of the weakened matrix. These findings indicate that while the electroplated abrasive coating is highly efficient under pure mechanical wear regimes, its structural integrity is fundamentally limited by its thermal stability when subjected to the severe friction-induced heating characteristic of high-incursion HPT environments.

4.1. Test rig appraisal

Ultimately, the development of the HTR has successfully enabled the simulation of the thermo-mechanical failure mechanisms observed in in-service gas turbines. Comparative testing isolates the contribution of the induction coil, demonstrating its role in increasing the blade tip temperature and triggering thermal failure. The role of the induction system was further investigated by measuring the cooling rate of the blade when spinning at 100 m s^{-1} for a prolonged period, following static pre-heating for the standard 90 s period. Averaged data from four repeat tests were determined to fit Newton's law of cooling with constant heat input [28, 29] as follows:

$$T(t) = T_s + (T_0 - T_s)e^{-bt} \quad (4)$$

where $T(t)$ denotes the temperature at a given time t relative to an initial state T_0 . The system converges towards a steady state temperature T_s , determined by $T_s = T_{amb} + \frac{P_{in}}{hA}$, where T_{amb} represents the ambient temperature, P_{in} the input power, h the convective heat transfer coefficient, and A the total surface area. The exponential decay resulting from energy loss is dictated by the constant b , defined as $b = \frac{hA}{mc_p}$, wherein m specifies the component mass and c_p its specific heat capacity. Using this approach, the steady state temperature T_s reached was determined to be $343.66 \pm 19.19^\circ\text{C}$ over the four tests performed, where it should be noted that this value represents an estimation given the pyrometer lower measurement limit of 435°C (adjusted for the applied blade emissivity). Furthermore, experimental data evaluation establishes the cooling coefficient b at 0.18 ± 0.02 , thereby completing the parameters of Equation 4. Notwithstanding the noted measurement limitation, the asymptotic nature of the data clearly highlights that the coil contributes to the heating of the blade both during static pre-heating and when rotating during a test, given the steady state temperature achieved.

Further, the rapid temperature recovery observed upon contact, especially at higher incursion rates, indicates that the core of the blade retains significant heat. Consequently, blade failure is not driven by a single heat source, but by the combination of the baseline temperature provided by the coil and the heat generated by friction during the rubbing process.

Since the system currently relies on this frictional heat generation to reach the critical matrix-softening temperature, more aggressive incursion rates and deeper rub depths are required to trigger a structural collapse. Therefore, blade heating temperature, incursion rate, and incursion depth must be approached as interdependent variables that collectively dictate thermo-mechanical failure. If the induction heating system is further developed to maintain a constant high temperature on the blade while it is spinning, these same matrix-softening failures would likely occur at lower incursion rates for a given incursion depth. Despite these current heating limitations, directly heating abrasive blade tips under representative incursion conditions provides a novel capability to simulate representative engine failure mechanisms in a laboratory setting.

The assessment of abrasible seal systems through incursion testing provides critical insights into their cutting performance and wear mechanisms in engine service. However, existing studies in the literature generally do not present explicit information regarding the operation of these abrasible systems under actual engine service conditions. Furthermore, experimental investigations of abrasible seal systems are conventionally conducted utilizing a single test rig architecture, which can limit the broader validation of the observed tribological phenomena [11, 7, 15]. To address this limitation, the present study employs an established test rig, which has been widely utilized in numerous previous incursion testing studies, to support and verify a newly developed incursion test rig [17, 18, 19, 10, 20, 16, 21].

Although no two experimental platforms are dynamically equivalent, demonstrating that near identical representative wear mechanics can be achieved across dynamically different rigs allows for a robust validation of the novel HTR, and enables the establishment of a clear baseline from which to judge the contribution of the heating coil.

To systematically address the deviations from real engine conditions, it is necessary to contextualise the testing parameters within the broader scope of scaled incursion testing. Regarding the unheated abrasible side and the localised blade heating mode, it is acknowledged that gas turbines operate with high ambient gas temperatures that heat both components. However, experimental evidence has demonstrated that during high-speed rubbing events, the abrasible material experiences a rapid temperature rise due to the intense frictional heat generated at the contact interface [30]. While this frictional interaction elevates the temperature of the abrasible lining, the rotating blade simultaneously experiences severe convective cooling due to windage. This forced convection prevents the abrasive coating from reaching the critical temperatures required to weaken the metallic matrix without a continuous external heat source onto the rotating blade. Therefore, the primary focus of the experimental design was to explicitly target the blade with induction heating to successfully achieve the matrix softening and grit pull-out mechanisms occurring in service. As highlighted by the presented results, the rig has achieved this aim. However, future consideration could also be given to heating the abrasible.

Regarding the kinematic deviations, the operational tip speed of 100 m s^{-1} is lower than the absolute maximum speeds encountered in actual high-pressure turbine stages. Nevertheless, as demonstrated in this work and other studies, scaled test rigs operating within the range of 100 m s^{-1} to 200 m s^{-1} successfully produce engine-representative wear mechanics [7, 11, 22]. This speed is sufficient to trigger the relevant high-speed brittle fracture of the ceramic abrasible, making it an acceptable dynamic baseline to accurately evaluate the transition to thermo-mechanical failure, with this point further highlighted by the wider comparator data set referenced in the preceding section, where no change in wear mechanics is observable in the blade tip speed range of 100 m s^{-1} to 200 m s^{-1} .

Finally, it must be emphasized that no scaled test rig can perfectly replicate a complete gas turbine engine. The fundamental purpose of these experimental platforms is to isolate and produce representative wear mechanics to investigate specific variables of interest, which is the methodological space all such rigs occupy [11, 15, 22]. Consequently, while the exact numerical failure thresholds cannot be directly extrapolated to an in-service engine without considering geometric and thermodynamic scaling factors, the fundamental progression of the wear mechanisms observed remains highly representative of the degradation experienced in actual operation, with the detailed test rig representing a significant step with regards to achieving blade tip heating on a scaled test platform.

5. Conclusion

The High-Temperature Rig (HTR) developed in this study successfully simulates the wear mechanisms on abrasible seals occurring in the complex thermo-mechanical environment of the High-Pressure Turbine (HPT). The primary innovation of the developed high-temperature test rig resides in the integration of an induction heating system capable of directly applying a thermal load and mitigate the windage cooling effect of the high-speed rotating blade tip during incursion. This novel configuration successfully bridges the gap between standard mechanical wear testing and in-service operating conditions, providing a validated laboratory platform capable of simulating the specific thermo-mechanical failure mechanisms and matrix softening observed in actual gas turbines.

The initial room-temperature wear simulation tests confirmed that the electroplated c-BN abrasive tips maintain an efficient, stable cutting mechanism governed by brittle fracture, effectively managing extended rub lengths and elevated incursion rates without experiencing a loss in cutting efficiency.

Testing under high-temperature conditions exposed a critical vulnerability in the abrasive coating system. Combining an elevated thermal baseline with friction-induced heating triggers abrasible material adhesion, progressive loss of cutting efficiency, and severe mechanical overloading. These factors cause structural collapse of the abrasive matrix and a transition to a friction-dominated rubbing regime. These results demonstrate that high-speed cutting performance is limited not by the mechanical wear of the c-BN grits, but by the thermal stability of the metallic anchoring matrix, which undergoes plastic yielding upon reaching a thermal saturation limit near 1100°C . Moving forward, the developed rig provides a validated experimental platform to further investigate the complex wear mechanisms present in turbine sealing systems at high temperature.

A. Additional test data

This appendix presents the supplementary data derived from the additional incursion tests referenced in the discussion section. Specifically, the dynamic contact force and temperature profiles (Figure 18) as well as the post-test macroscopic images of the abrasible samples and blades (Figure 19) are provided for the tests conducted up to an incursion depth of $500 \mu\text{m}$ at a blade tip speed of 100 m s^{-1} and an incursion rate of $0.3 \mu\text{m pass}^{-1}$. These results are detailed for both the SR and the HTR to substantiate the consistent evaluation of the abrasible system behaviour across the different experimental platforms.

Acknowledgements

This document is the result of a research project funded by Rolls-Royce Plc.

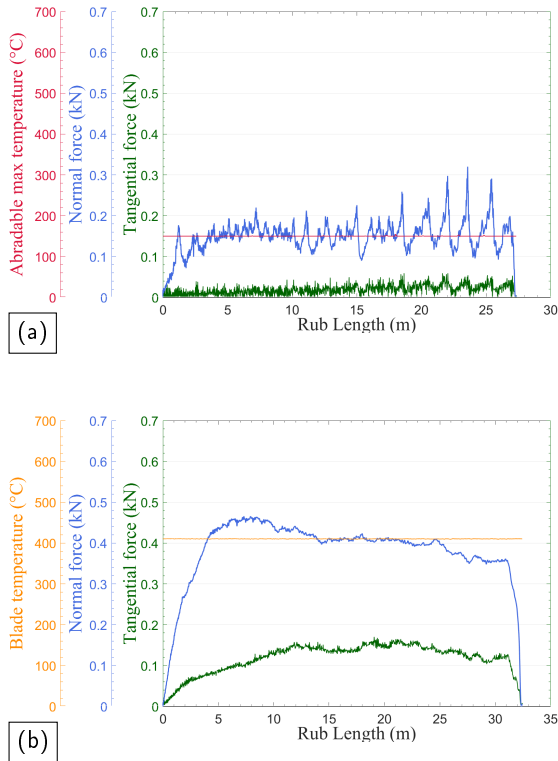


Figure 18: Additional data of incursion tests performed up to 500 μm at 100 m s^{-1} and $0.3\text{ }\mu\text{m pass}^{-1}$ in (a) the SR and (b) the HTR.

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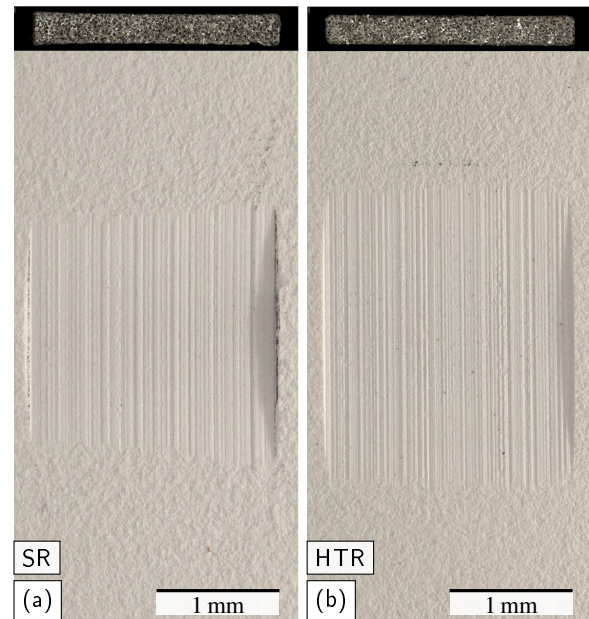


Figure 19: Additional post-test images of the MAS abradable samples and TBT429 blades from the incursion test performed up to 500 μm at 100 m s^{-1} and $0.3\text{ }\mu\text{m pass}^{-1}$ in (a) the SR and (b) the HTR.

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