COMBUSTION TURBINE (CT) HOT SECTION COATING LIFE MANAGEMENT

Final Report

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COMBUSTION TURBINE (CT) HOT SECTION
COATING LIFE MANAGEMENT

1.0 ABSTRACT

The integrity of coatings used in hot section components of combustion turbines is crucial to the reliability of the buckets. This project was initiated in recognition of the need for predicting the life of coatings analytically, and non-destructively; correspondingly, four principal tasks were established. Task 1, with the objective of analytically developing stress, strain and temperature distributions in the bucket and thereby predicting thermal fatigue (TMF) damage for various operating conditions; Task 2 with the objective of developing eddy current techniques to measure both TMF damage and general degradation of coatings and, Task 3 with the objective of developing mechanism based algorithms. Task 4 is aimed at verifying analytical predictions from Task 1 and the NDE predictions from Task 3 against field observations.
2.0 INTRODUCTION

The objective of this project is to improve the reliability, availability and maintainability (RAM) of combustion turbines (GTs) by developing advanced technology for assessing and managing the life of protective coatings on CT buckets and vanes.

In recent years, gas turbines (GTs) have become the equipment of choice for power generation by both electric utilities and independent power producers. Continuing advances in design concepts and in structural materials and coatings for GT hot-section components have enabled increases in rotor inlet temperature resulting in major efficiency gains. These high temperatures mandate the use of coatings on hot section components (buckets and vanes) to protect them from oxidation. Degradation of these protective coatings represents a major profitability challenge for turbine owners. Coating life usually dictates bucket refurbishment intervals – which typically are shorter than desired for baseload units. Downtime for coating inspection and replenishment requires dispatch of less efficient generating equipment or purchase of replacement power. Coating failure can lead to rapid, severe damage to the superalloy substrate, warranting bucket replacement. Replacement of a conventionally cast alloy bucket row can cost up to $3 million in the case of directionally solidified or single-crystal buckets with internal cooling. Unavailability costs can run up to $500,000 in lost revenues per day for a 500MW combined cycle plant. Bucket failures can also cause downstream damage in the turbine, causing prolonged outages and revenue loss. Moreover, losses to electricity customers due to disruption in supply can also be very substantial. A proper life management system for coatings represents a major step in preventing such major losses to the GT owner and to society at large.

The life management activities covered in this project for coatings directly impacts the objectives of increasing RAM of GTs. Accurate life management techniques optimize refurbishment intervals and operating practices, thereby avoiding unplanned outages. Currently, coating refurbishment intervals are dictated by empirical, fleet-specific (rather than unit-specific) manufacturer recommendations based on the concept of “equivalent operating hours (EOH).” The new technology described in this proposal will enable machine-specific calculations of coating remaining life and direct measurements of the same using non-destructive evaluation (NDE) techniques.

The project is intended to develop improved analytical and nondestructive evaluation techniques to assess the consumed life and/or estimated life of protective coatings on CT buckets and vanes, and then integrate these techniques with economic risk-based decision-analysis tools to optimize run/repair/replace decisions. The project is defined along four major technical tasks including:

- Task 1. Refinement and Validation of Hot Section Life Management Platform (HSLMP)
- Task 2. COATLIFE for Advanced Metallic Coatings and TBCs
- Task 3. NDE of Coatings
- Task 4. Field Validation of COATLIFE and NDE

This report summarizes results from these tasks.
3.0 EXECUTIVE SUMMARY

The results of the CT Hot Section Coating Life Management project have been reported in a series of four reports with the first report being issued in December 2002. The following is a summary of the key findings provided in each report. More detailed information on each topic can be found in the reports as listed below:

- General Electric 7FA+e Second Stage Bucket Analysis (EPRI/DOE Report No. 1004361)—Issued December 2002
- Eddy Current Inspection NDE System For On-Site Inspection (EPRI/DOE Report 1005025)—Issued March 2005.

Each of the tasks is summarized as follows.

Task 1: Refinement and Validation of Hot Section Life Management Platform (HSLMP)

Two reports were generated under Task 1 over the duration of the project. The first report focuses on the 7FA+e Second Stage Bucket and the second focuses on the Siemens Westinghouse W501C First Stage Bucket. Key findings for each are described below.

**General Electric 7FA+e Second Stage Bucket Analysis (EPRI/DOE Report No. 1004361)—Issued December 2002**

The results of this report are summarized in question and answer type format.

1. **What is the most notable finding from the design audit performed on the 2\textsuperscript{nd} stage blade?**

Creep is the predominant life-limiting factor that governs the reliability of the 2\textsuperscript{nd} row of buckets. There is strong evidence that premature creep cracking can lead to rupture of the shroud, breakdown the integrity of the continuous linkage, and thereby put at risk the entire row of buckets. As the firing temperature has been raised from 3350° F (1288° C) to 2420° F (1327° C), the original replacement interval prescribed by OEM has been reduced from 3 HGPI (72,000 hours) to 2 HGPI (48,000 hours). Results of the FA+e (PG7241) studies further reveal that the creep damage accumulated in the shroud is most critical in terms of determining the remaining life of these buckets. Direct
examinations of the shroud indicated that the creep limit is approached after 48,000 service hours. Given this capability to barely achieve the required hours of service, the design is considered to be marginal in terms of meeting the specified life criteria. Actual duty for the buckets is a critical unknown. Any events of over-firing or environmental impact (burning oil and corrosion attack) could further comprise the ability of specific rows to reach their replacement intervals.

2. **Is there a benefit of using DS material for the 2nd stage buckets?**

The creep analysis applying EA material properties shows the creep strain to be comparable or lower than that produced using DS material at the most critical locations in the shroud. Since creep limits for both EA and DS material in the transverse direction are essentially comparable, it is not evident how damage in the shroud area is significantly affected by using the more expensive alloy. Although using a DS material does improve the predicted creep life of the airfoil, the principal strain in shroud is not in the radial direction. Effectively, the benefits of the DS material are negated in what turns out to be the location of principal concern.

3. **Can further design changes to the shroud increase the present creep life?**

The most recent shroud appears to have nine span-wise cooling holes in conjunction with a substantial amount of material removed at both the leading and trailing edges. The likelihood of premature shroud creep rupture is likely to increase significantly for a 7FA+e blade if it employs the original, more massive shroud profile. The cutter tooth in the present style of shroud may stiffen the seal and help to resist shroud lifting, but it is also likely to worsen the creep strain in the fillet and notch regions. (Grinding the cutter tooth is probably beneficial given this is such a susceptible locations). Ultimately, it can be seen that the drive for more performance and efficiency by elevating the firing temperatures is overcoming the capabilities of the engineer to design ways of reducing the stress-strain in this critical region. Conventional methods of reducing mass, increasing the cooling and changing the material have only a nominal impact when the operating environment places the material in its extreme limits.

4. **What can and should be inspected in the 2nd row of buckets?**

As the shroud becomes more and more scalloped, it is made less flexible. Therefore shroud lifting is expected to be less visible than was possible for the original design. Also, creep in the trailing edge of the airfoil is expected be less significant when a lighter shroud is involved. Therefore, in-situ attempts to measure creep are likely to prove difficult based on the limited amount of permanent creep deformation expected to be present. It is therefore recommended to focus on the fillet and notch in the shroud, performing inspections to ensure that no creep cracks and/or evidence of corrosion attack are present at these critical locations.

5. **How might these results be applied to older sets of buckets with less scalloped shrouds?**
Because the corner piece at the leading edge on the suction side of the shroud can break off due to excessive creep damage it is recommended to re-shape the shroud with the newer scalloped profile if sufficient remaining life warrants such an effort. However, before considering such an effort, some estimate of the remaining life should be performed based on the operating history of the unit. As noted, creep consumption will be distinctive for rows that operate with different covers, at different temperatures, and with different duty cycle histories. The creep life expenditure should be assessed to avoid attempts to salvage and use buckets that do not have a reasonable chance of making the minimum maintenance interval.

6. What is the allowed tolerance for shroud gaps?

Compared to the 3rd stage buckets, the un-twist force produced at operating speed for closing the gap between neighboring contact surfaces produces a more stringent tolerance limit. The allowable gap size is predicted to be about 0.025" (0.64 mm) at 0 rpm. It should be noted that the original gap condition might change after a period of operation due to permanent creep deformation resulted from tip shroud tilting. It is therefore considered important to inspect the shroud gap of both newly installed and serviced blades to ensure a continuous link will be achieved at full operating speed.

7. Can operators manage the present component life?

As explained, creep damage accumulated in the 7FA+e 2nd stage bucket is more difficult to directly measure due to the small amount of radial creep deformation that is produced with the newest cover. It is therefore more reasonable to assess damage by tracking and monitoring the creep life consumed based on a record of the exhaust temperature and other relevant operating parameters. These projections can be readily produced by means of a damage-tracking matrix (DTM) prepared from results obtained with the HSLM platform. As such a damage tracking record would show running at part load for selected periods could prolong the life of these components. Conversely, over-fired operations will significantly reduce the life. In addition to producing a chronicle of damage accumulation, the results should further provide the operator with the type of detail required to calibrate the cause and effect of different operating scenarios, and provide the basis to establish a specification for managing these critical parts.

8. How might the maintenance interval be affected for simple cycle units?

Presently, two criteria govern the allowable service life of the 7FA+e 2nd row bucket; 3HGPI (2,700 factored starts) or 48,000 hours. For simple cycle/peaking units, it is unlikely that the buckets will approach their creep life in allowable hours before the number of factored starts requires their replacement. While TMF life consumption was not examined directly, it is reasonable to suspect that the factored starts err to the conservative when accounting for the true TMF/LCF damage that may occur, based on the temperatures and strains calculated for in the 2nd row. However, because creep plays such a significant role in the overall consumption of life for these buckets, if an operator of a simple cycle/peaking unit was interested in extending the service of buckets beyond the 2700 factored starts, the interaction of creep should be accounted for in any direct assessment of TMF/LCF. In other words, given the number of years
that a peaking unit is likely to operate before it reaches its limit of factored starts, the consumption of creep over this period should not be discounted or considered as a completely separate factor.

9. **How might the maintenance interval be affected for combined cycle units?**

As a rule, combined cycle units are expected to operate at base or partial loads. In either scenario, because of the duration of cycles, these units will accumulate greater creep damage than a peaking or cycling machine for the same number of start/stop cycles. The limit of 48,000 hours is therefore expected to control the replacement of the 2nd row buckets on combined cycle units. Predictions indicate that while this limit is achievable, there is presently little margin in predicted creep life for the 7FA+e buckets, whether they are made from either EA or DS material. This implies that for non-peaking units, or those typically used in combined cycle arrangements, both load and duration could dictate whether a row does or does not achieve this limit of 48,000 hours. When operated at part load, the aero-thermal results indicate that unlike the 3rd stage, this has a tendency to lower the temperature at the 2nd stage. As a consequence, this will reduce the rate of creep damage in the tip shroud. For operators who were interested in extending the service of their buckets beyond the 48,000 hours, it would therefore be critical to know precisely the number of the partial load cycles, their magnitude, and their approximate duration should they want to re-assess the potential life that a set of buckets might have been gained from running at reduced stage temperatures.

10. **How might damage tracking affect life cycle costs of these buckets?**

Based on the previous observations made, it is clear that a more precise measure of the damage caused during cycles of operation could directly influence the maintenance and replacement of these buckets for both simple and combined cycle units. For simple cycle units, conservatism in the present factors (meant to account for TMF/LCF damage) will continue to accelerate the frequency of their replacement and further minimize the potential creep life that might still be available in a row of buckets, e.g. a row replaced after 2,700 factored starts could still have 36,000 hours of residual creep life. For combined cycle units, the hours based limit of 48,000 might be reasonably extended, particularly where the units are frequently run at partial loads for extended periods. In both scenarios, cycle-by-cycle damage tracking offers the only means for operators to independently assess the actual life consumption caused by different start-stop scenarios, and/or the creep life consumed when operating at lower stage temperatures.

11. **If operated under an LTSA, what results from this study are still relevant?**

As originally noted, the LTSA typically ensures that the supplier will be adequately compensated by mandating sufficient frequency of inspections to ensure critical parts will operate reliably. Given greater control over the units, the LTSA provider is also inclined to experiment with alternatives to extend part life between replacements in an effort to reduce costs covered within the program. Both of these activities are clearly at work in regards to the 2nd row of buckets. As the consumption of creep life is accelerated and the frequency of their replacement is increased, the demand for
replacement parts can be expected to follow. Because a majority of these advanced units are going into service over the next several years, the present demand, already increased by the frequency of required replacement, is likely to be further increased by orders of magnitude. Operators serviced under an LTSA should therefore (a) be aware that increased intervals to ensure reliability is achieved at the expense of availability, (2) that their next set of replacements are of designs have been tested and proven on a fleet-wide basis, and (3) that if used parts are installed, an accounting of their previous creep life consumption is explicitly made.

The second report generated under Task 1 was on the Siemens-Westinghouse W501C first stage bucket. The report is identified below along with the key findings:

**Siemens-Westinghouse W501FC First Stage Bucket Analysis and Transition Piece Durability (EPRI/DOE Report No. 1005049)—Issued December 2003.**

1. Platform cracking appears to be the primary life limiting issue that dictates the repair and replacement interval for the W501FC 1st stage blade. Multiple cracking locations near platform indicate the distress is generic rather than an operational problem. Cracking on the pressure side near the platform cooling hole is most critical and could result in early retirement of the blade. Results from aero-thermal and stress analysis are consistent with field observation. It appears that predicted excessive compressive stress at base load is the result of highly non-uniform temperature distributions primarily attributed to the colder cooling air, shorter shank designs and stress concentration near the cooling hole.

2. Temperature near the inlet of platform cooling hole was predicted to be around 1400°F (760°C) at base load. Above the cooling hole, the interface temperature between TBC and bond coat was calculated to be 1540°C (838°C). Under this relatively low temperature level, the critical platform cracking is not likely caused by a time-dependent high temperature damage mechanism, such as oxidation or growth of the TGO (thermally grown oxide). The thermal stress introduced from high temperature gradient is suspected to be the primary contributor to the severe platform cracking.

3. At the inlet of platform cooling hole where cracking occurs, the strain range was calculated to be 0.6% and the corresponding TMF life was predicted to be about 150 cycles in a normal shutdown mode of operation. These are significant in terms of the rate of consumption for the TMF life. A comparable level of strain range was calculated at the interface between the TBC and the bond coat, above the inlet of platform cooling hole. It is very likely that once a TMF crack is initiated, either at the inlet of cooling hole or at the bond coat underneath the TBC, a through-crack across the platform thickness is immediately formed.

4. The effect of a unit trip on TMF life in terms of the initiation of platform cracking is not considered to be significant. At the leading edge of the airfoil, the effect of a unit trip on TMF damage is more pronounced. The transient thermal response at the platform is not severe as the leading edge. In addition, a TBC tends to reduce the transient thermal response and moderate the transient stresses. The TMF damage
at platform is most critical and it has been demonstrated that a trip has no substantial impact on crack initiation. A factor of 20 implied by OEM appears to be overly conservative in terms of providing an estimation of the damage at the platform caused by a full load trip.

5. Significant creep damage could also contribute to the premature cracking near platform cooling hole. Results from a creep analysis indicate that the peak creep strain would accumulate at the platform cooling hole where cracking occurs. As the creep damage was driven mainly by thermal stress, a significant rate of primary creep strain in the early stages of use and a very mild creep strain rate over the longer term is expected. The creep life (the point where creep might initiate cracks) was predicted to be about 29,000 hours. In conjunction with TMF damage, it is concluded that the creep damage may further reduce the life of this component.

6. For the W501F 1st stage blade, the bulk metal temperatures are generally lower compared to the similar GE 7FA+ design. This is attributed to the adoption of TBC, use of an external cooler for cooling air, and film cooling methods used around the airfoil. At the mid-span of airfoil, the interface temperature between TBC and bond coat was calculated to be below 1500°F (816°C). This demonstrates the benefit of applying a TBC over a metallic coating in terms of reducing TBC/bond interface and overall metal temperatures by 150°F~200°F (83°C~111°C).

7. A peak operating metal temperature of 1810°F (988°C) was predicted to occur at the TBC surface, near the leading edge of the squealer tip. Based on the preliminary TBC life model developed by EPRI/SWRI, the corresponding TBC life was predicted to be about 1,810 cycles or 11,000 hours for a cycle time of 6 hours per cycle. The effect of TGO growth on potential de-lamination or spallation appears less significant as compared to the fatigue damage for the 1st stage blade. Further calibration of the model is required to refine the prediction.

8. Bond coat oxidation has been identified to be the predominant life limiting parameter for TBC failures in aero engines. The information of service performance on land base combustion turbines is limited. Reportedly, quality issues have more impact on service performance on APS TBC nozzles and blades. Also, erosion and foreign object damage seems to be more critical in comparison to aero engines. Results from the design audit indicate that overall metal temperatures are relatively low for the 1st stage rotating blade. In general, bond coat oxidation might become more critical for long term service performance.

Task 2: COATLIFE for Advanced Metallic Coatings and TBCs
The results of Task 2 were described under the following report. Key findings from this report are highlighted below.

*Combustion Turbine Hot Section Coating Life Management—COATLIFE for Advanced Metallic Coatings and TBCs (EPRI/DOE Report 1011593)—Issued March 2005.*

The primary deliverable for this task was the development on an algorithm for advanced metallic coatings and TBCs which was delivered in the form of the COATLIFE. The following summarizes the primary findings of this task.

**Coating Algorithm Development**

The conclusions reached in the development of coating life algorithms for a NiCoCrAlY (GT33-like) coating are as follows:

- GT33-like coating forms predominantly alumina scales after a small number (600–800 one-hour cycles) of thermal cycles, but it forms a mixture of Al-rich, Cr-rich, and other rare-earth mixed oxides at a larger number (1500–2000 one-hour cycles) of thermal cycles. Spallation of mixed oxides eventually leads to pitting and localized oxidation. This cyclic oxidation behavior can be modeled by treating the oxidation and spallation kinetics of alumina.

- The oxidation life of GT33-like coatings can be predicted on the basis of a critical Al content for the formation of a continuous layer of alumina scale on the coating surface. Al contents in GT33-like coating remained high after the onset of pitting and localized oxidation because of the mixed-oxide formation that depletes Al, Cr, and other rare-earth or transitional metals in the coatings.

- Thermal fatigue cracks initiated in GT33-like coating at oxidation pits. Thermal fatigue cracking was more prevalent at 1850°F (1010°C) than at 1950°F (1066°C) because the longer oxidation life at the lower temperature permits a larger number of thermal cycles. TMF life of GT33-like coatings can be modeled in terms of the thermomechanical strain ranges and the Coffin-Manson equation for low-cycle fatigue.

- Life-prediction algorithms were developed for predicting the oxidation and TMF lives of GT33-like (NiCoCrAlY) coating on the basis of the underlying degradation mechanisms. These life prediction algorithms were incorporated into COATLIFE Spreadsheet Program Version 4.0.

- COATLIFE Spreadsheet Program Version 4.0 is capable of predicting the oxidation and TMF lives of a number of MCrAlY coatings, including that of GT33-like (NiCoCrAlY) coatings. The COATLIFE predictions were verified and validated against both laboratory results and field data.

- Life prediction by COATLIFE Spreadsheet Program Version 4.0 can be performed on the basis of coating usage history or NDE input of Al content or volume % of phase.
The conclusions reached in the development of coating life algorithms for TBCs are as follows:

- The prominent life-limiting failure processes in TBC are cyclic oxidation spallation and crack-related failure such as TMF.

- Cyclic oxidation degrades TBC performance by forming a TGO layer at the TBC/bond coat interface, while TMF causes nucleation and propagation of microcracks within the TBC, TGO, and in the vicinity of the TBC/bond coat. Both oxidation and TMF contribute to TBC spallation. This spallation process can be modeled as an oxidation-assisted low-cycle fatigue process with a critical alumina oxide (TGO) thickness as a failure criterion for the onset of TBC spallation or cracking.

- The critical oxide thickness for the APS TBC studied in this investigation was 20 μm, which corresponds to the TGO thickness at the onset of internal oxidation in the bond coat.

- TMF can cause the nucleation and propagation of microcracks into the bond coat and the substrate. The TMF life for this failure mode in a TBC system can be predicted on the basis of the TMF life of the bond coat and substrate alone.

- A generic TBC life model was developed for both APS TBC and EB-PVD TBC. The predictive capability of the TBC life model was demonstrated for APS TBC using both literature data and laboratory data generated in this program. The predictive capability of the model for EB-PVD TBC was illustrated via literature data.

- Life-prediction algorithms were developed for predicting the oxidation and TMF lives of APS TBC on the basis of the underlying degradation mechanisms. These life prediction algorithms were incorporated into COATLIFE Spreadsheet Program Version 4.0.

- COATLIFE Spreadsheet Program Version 4.0 is capable of predicting the oxidation and TMF lives of APS TBC. The COATLIFE predictions were verified against laboratory results and limited burner-rig data.

**TGO Thickness and TGO/Bond Coating Interface Cracking Results**

The TGO thickness at the bond coating /TBC interface increased with increasing exposure time. Variation of the bond coating and substrate chemical composition had no effect on the kinetics of TGO growth.

Delamination cracks were observed at the TGO/TBC interface. The extent and size of the cracking in all coating systems increased with the exposure temperature and time.

The TGO-induced interface cracking is implicitly assumed to be directly related to TBC spallation or external cracking.

The TBC failure mechanism depends on the exposure temperature and time. The failure mechanism may change from the TGO growth-controlled mechanism to bond coat oxidation and delamination after long-term exposure.
Task 3: NDE Of Coatings

The results of Task 3 were reported in the following document. Key findings from the effort are described as follows:

_Eddy Current Inspection NDE System For On-Site Inspection (EPRI/DOE Report 1005025)—Issued March 2005._

A state-of-the art F-SECT system has been assembled, tested, and evaluated on duplex and simplex metallic coatings of the 7FA and the 9FA blade buckets. It was also successfully field-tested on the first stage 7FA+e buckets with TBC. The assembled F-SECT system demonstrated its capability to provide both qualitative and quantitative information about coating conditions.

Capability of the F-SECT system to detect, discriminate, and characterize such coating conditions as normal coating, β-phase depletion, and cracked coating was demonstrated on service-aged buckets with GT33+ coating. In addition, it was possible based on the built-in inversion program to estimate coating layer thickness values and the associated conductivity values of NiCoCrA1Y bond coat and GTD-111 substrate from service-aged 7FA second stage bucket with GT 33+ coatings. Additional testing showed that F-SECT is capable of assessing a service-aged bucket coated with CoCrA1Y simple coating (GT 29). In addition to the loss of coating protection from oxidation, chromium depletion caused the GT29 coating to become magnetic. The magnetic characteristic of the coating was identified from the unique hook-pattern impedance plot obtained at lower frequency. By reviewing the normalized coil impedance plots over frequency, it was possible to detect and discriminate β-phase depletion of coating from the base metal corrosion.

In general, normal coating/substrate conditions present highest normalized impedance curves with positive slopes, followed next by almost flat curves representing β-phase depletion, and finally curves with negatives slopes representing the presence of TMF cracking and possible corrosion of base metal. To review the entire airfoil condition, a colored contour map of normalized impedance values showed the affected areas of interest. Most of the evaluation focused on the suction sides, and majority of the degradation noted was on the leading edge side of the blade surface.

Based on the compiled database representing 51 data points, the overall accuracy of the F-SECT system to estimate the useful remaining coating thickness was 95% with RMS error of 23 um. As for coating conductivity comparison to A1 wt% remaining, there was no unique correlation noted among the sample sets evaluated. This lack of correlation was attributed mainly to presence of TMF cracking, which affected the conductivity readings more than the coating degradation due to β-phase depletion.
Task 4: Field Validation of COATLIFE and NDE

The results of Task 4 were presented in the following document. Key findings from this report are summarized below. This task brought together each of the key elements of this program: component analysis, NDE, COATLIFE, and field evaluation.


COATLIFE-4 was verified for GT33+ type coating using the data obtained from the three service-run blades or blades removed from three different 7FA machines. The results of Blades 7, 57, and C were used for COATLIFE verification/validation. The operating metal temperature of these blades was determined using the local changes in the interdiffusion zone width.

Blade 7 experienced cracking at the leading edge but not at the trailing edge. Based on the interdiffusion zone width, the local temperature at the 75% airfoil height location was determined to be 1920°F (1049°C) at the leading edge and 1785°F (974°C) at the trailing edge. COATLIFE prediction indicates that the oxidation life has been totally consumed (170% life) at the leading edge, but only 47% life has been consumed at the trailing edge. A summary of the COATLIFE prediction of oxidation life of GT33+ coating against field data is presented in Table 3-1.

Blades C, 7, and 57 with GT33+ coating also experienced TMF cracking at different airfoil heights. A summary of the operation history of these blades is shown in Table 4-4. COATLIFE predictions indicate that the coatings are protective against oxidation but have exceeded their TMF lives.

Figure 3-1 shows the coating life diagram for 7FA Blade 7 at 25% blade height. The local temperature was 1660°F (904°C), and the TMF strain range was 0.8%. After 670 startups at 12.36 hours/cycle (8286 total operating hours), only 14.55% of the oxidation life had been consumed, but the coating has exceeded the predicted TMF life (94 cycles) considerably. Metallographic examination of the blade indicated that the TMF crack had penetrated into the base metal.

Figure 3-2 shows the coating life diagram for 7FA Blade C at the 55% blade height. The local temperature was 1680°F (916°C), and the TMF strain range was 0.65%. After 272 startups at 22.63 hours/cycle (6156 total operating hours), only 10.81% of the oxidation life had been consumed, but the coating had exceeded the predicted TMF life (217.2 cycles). Metallographic examination of the blade also indicated that the TMF crack had penetrated into the base metal and a small portion of the coating had spalled.
Figure 3-1
Comparison of the COATLIFE prediction of oxidation life for GT33+ against field data for the leading edge (75% blade height) of Blade 7 in a 7FA machine after 8286 hours and 670 startups: oxidation failure predicted by COATLIFE-4

Figure 3-2
Comparison of the COATLIFE prediction of oxidation life for GT33+ against field data for the trailing edge (50% blade height) of Blade 7 in a 7FA machine after 8286 hours and 670 startup cycles: COATLIFE prediction of 42.73% life consumed and a coating life of 1567.9 startup cycles.
Examination of service-run blades removed from the 7FA machine showed that the coating on the leading edge at the 75% height location on Blade 7 was severely degraded. The coating on the other two blades was in good condition.

TMF cracks were observed on the concave or convex section of all three blades.

The extent of TMF cracking in a blade depends on the quality of the coating. Thicker aluminide top layer and higher aluminum content in the NiCoCrAlY coating promotes TMF cracking.

The COATLIFE-predicted oxidation and TMF lives of GT33+ coated blades are in good agreement with the metallographic results.
Table 3-1
Verification of COATLIFE-4 predictions against field data for GT33+ in 7FA machines.

<table>
<thead>
<tr>
<th>Blade</th>
<th>Location</th>
<th>Operation Hours</th>
<th>Startup Cycles</th>
<th>Cycle Time hrs/ cycle</th>
<th>Metal Temp. °F</th>
<th>COATLIFE Prediction</th>
<th>Status</th>
<th>Field Observation</th>
</tr>
</thead>
<tbody>
<tr>
<td>7</td>
<td>75% BH LE tip</td>
<td>8286</td>
<td>670</td>
<td>12.36</td>
<td>1920</td>
<td>—</td>
<td>393.0</td>
<td>—</td>
</tr>
<tr>
<td></td>
<td>50% BH 1” from TE pressure side)</td>
<td>8286</td>
<td>670</td>
<td>12.36</td>
<td>1785</td>
<td>—</td>
<td>1567.9</td>
<td>—</td>
</tr>
<tr>
<td></td>
<td>25% BH 3” for LE-CX suction side)</td>
<td>8286</td>
<td>670</td>
<td>12.36</td>
<td>1660</td>
<td>0.80</td>
<td>4605.4</td>
<td>94.4</td>
</tr>
<tr>
<td>C</td>
<td>50% BH 1” from LE-CX</td>
<td>6156</td>
<td>272</td>
<td>27.63</td>
<td>1680</td>
<td>0.65</td>
<td>2515.4</td>
<td>217.2</td>
</tr>
<tr>
<td></td>
<td>50% BH 1” from LE-CX</td>
<td>2000</td>
<td>219</td>
<td>9.13</td>
<td>1730</td>
<td>0.50 (normal) 0.71 (trip)</td>
<td>3307.4</td>
<td>161.3</td>
</tr>
</tbody>
</table>

C = (F-32) X 5/9
1 inch = 25.4 mm
* Refurbished coating
4.0 Coating Life Algorithm Development

One of the primary goals of this program was to further develop and validate coating life algorithms for advanced metallic coatings (GT33+ like coating) and a TBC. The following discussion on coating life algorithm describes this effort and the results.

A coating life model named COATLIFE [1-4] was developed under prior EPRI sponsorship for predicting the oxidation life of overlay and diffusion coatings. This life prediction model is a computer program that simulates cyclic oxidation on a cycle-by-cycle basis and predicts the useful life of a coating based on a critical Al criterion. Figure 4-1 shows a schematic of the cyclic oxidation model and the methodology for predicting the usable life of a coating. The important features in the coating life model are (1) oxidation kinetics, (2) oxide fracture and spallation, (3) inward diffusion, (4) overall kinetics of cyclic oxidation and depletion of the oxide-forming element, Al, due to oxidation, spallation, and inward diffusion, and (5) a life-predicting scheme based on a critical concentration of Al for the formation of a protective oxide layer. Detailed descriptions of the coating life model are published in the literature [1-4].

![Figure 4-1](image)

*Figure 4-1*
Schematics of degradation mechanisms treated in the COATLIFE model.
From Chan et al. [1].

COATLIFE has been used to generate coating life diagrams for various overlay, diffusion, and duplex coatings. These results have been used to develop parameters in an Excel-based spreadsheet program called COATLIFE-2 [5], which allows the prediction of the remaining life of a combustion coating in a user-friendly manner. COATLIFE-2 has been updated to COATLIFE-3 [6], which incorporates a TMF life-prediction capability.
In this program, coating life algorithms were developed for NiCoCrAlY and TBCs. The modeling efforts for the NiCoCrAlY coating included determination of model constants for COATLIFE, generation of coating life diagrams and life-prediction algorithms, and the incorporation of these results into the COATLIFE Spreadsheet Program. For TBCs, the modeling efforts included development of a TBC life prediction model, determination of model constants, generation of TBC life diagrams and life-prediction algorithms, and the incorporation of the TBC-life prediction algorithm and coating life diagrams into COATLIFE.

4.1 NiCoCrAlY Life Prediction Algorithm Development

4.1.A Evaluation of Model Constants

Model constants in the coating life model, COATLIFE, were obtained for GT33-like coating on a GTD-111 substrate. The model constants evaluated include the parabolic kinetic constant for oxidation and the constants in the oxide spallation kinetic equation. A preliminary set of model constants was first evaluated based on the weight change data. Using this set of model constants, COATLIFE was used to predict the depletion of Al and β phase with thermal cycles and compared against experimental data. Discrepancies between computed and measured Al contents were observed. The discrepancies were resolved by performing EDS analyses of the composition of oxides formed at various times of cyclic oxidation. These results, which are presented in Section 1.2.3, indicated the formation of mixed oxides including alumina, yttria, and chromia after 1000 cycles. Consequently, the model constants were finalized, based on the experimental data of Al content in the GT33 coating after various times of cyclic oxidation at a maximum temperature of 1950°F (1066°C) or 1850°F (1010°C). Weight change data were not used because GT33 formed mixed oxides (alumina, yttria, and chromia) after 1000 cycles at 1950°F (1066°C) and after 2000 cycles at 1980°F (1010°C), making model constants derived from the weight change data unreliable for predicting Al depletion.

Figure 4-2 shows a comparison of the model fitting and measured Al content of GT33-like coating as a function of thermal cycles for a peak cycling temperature of 1950°F (1066°C). The computed β-depletion and weight change curves are compared against experimental data in Figures 4-3 and 4-4, respectively. Figure 4-4 indicates that the coated specimens showed more spallation than that predicted by the model. The discrepancy was partly due to experimental scatter and partly due to the formation of alumina, yttria, and chromia after 1000 thermal cycles. EDS analyses of the oxide scales indicated that a scale of mostly alumina oxides with dispersed Y-rich oxides (see Figures 4-5(a) and (b)) formed on GT33 in specimens after 600 thermal cycles. Figure 4-5(a) shows an SEM micrograph of the oxide scale, while Figure 4-5(b) presents EDS results that indicate that the oxide scale is comprised mostly of aluminum with some yttrium. In contrast, the oxide scale formed after 1500 thermal cycles consisted of a mixture of alumina, yttria, and chromia. Figure 4-6 shows the mixed oxides, and Figures 4-7(a), (b), and (c) show the compositions of the Al-rich, Cr-rich, and Y-rich oxides, respectively. The weight change curve computed by COATLIFE in Figure 4-4 was based on the formation and spallation of alumina. As a result, the model predicted less weight change than that observed in the experimental data since chromium and yttrium
have higher atomic weights than aluminum. Since COATLIFE uses the Al content and volume fraction of the β for predicting coating life, the contribution of weight change due to the contribution of weight change due to Al depletion is modeled accurately, while that due to other elements (such as Cr, Ni, and Y, etc.) is ignored. This has proved very successful for accurately predicting the life of other duplex and diffusion coatings [1-4].

Figure 4-2
A comparison of computed and measured Al content in GT33-coated GTD-111 specimens as a function of thermal cycles. The coated specimens were subjected to 1-hour thermal cycles between 77°F (25°C) and 1950°F (1066°C). The model was fitted to the experimental data of Al content.
Figure 4-3
A comparison of computed and measured volume percent $\beta$ in GT33-coated GTD-111 specimens as a function of thermal cycles. The coated specimens were subjected to 1-hour thermal cycles between $77^\circ F$ ($25^\circ C$) and $1950^\circ F$ ($1066^\circ C$).

Figure 4-4
A comparison of computed and measured weight change data of GT33-coated GTD-111 as a function of thermal cycles. The coated specimen was subjected to 1-hour thermal cycles between $77^\circ F$ ($25^\circ C$) and $1950^\circ F$ ($1066^\circ C$). Arrows indicate specimens whose oxide-scale compositions were determined using the EDS technique.
Figure 4-5
Microstructure and composition of the oxide scale formed on GT33-like coating after 600 one-hour cycles between 77°F and 1950°F: (a) alumina scale with dispersed Y$_2$O$_3$, and (b) EDS result shows a high Al content in the outer oxide scale (Location A).
Figure 4-6
SEM micrograph shows that three oxides (A, C, and Y) are present in the scale formed on the GT33-like coating after 1500 one-hour thermal cycles between 77°F (25°C) and 1950°F (1066°C).
Composites of Oxides A, C, and Y: (a) Al-rich Oxide A, (b) Cr-rich Oxide C, and (c) yttrium-rich Oxide Y.

Using the same procedure described above for 1950°F (1066°C), the model constants for NiCoCrAlY at 1850°F (1010°C) were determined, based on the cyclic oxidation data generated in this program. Figure 4-8 shows a comparison of the computed and
observed weight change curves. The observed weight changes are larger than those predicted by the model after 1000–2000 thermal cycles. The corresponding Al contents and volume fraction of $\beta$ phase are shown in Figures 4-9 and 4-10, respectively. In both cases, the computed values of the Al content and the volume fraction of $\beta$ phase are lower than the experimental data when the thermal cycle exceeds 2000. This finding indicates that Al and $\beta$-phase depletion occurs at lower rates than model prediction. Chemical analyses of oxides formed on the surface specimens indicated that the oxide layer contained a mixture of alumina, yttria, and chromia when the number of thermal cycle exceeded 2000 cycles. In addition, oxidation pits, which are indicative of localized oxidation, were observed in specimens after 3500 thermal cycles. The discrepancy between model prediction and experimental data was therefore due to the absence of a continuous alumina scale on the oxidized surfaces.

![Figure 4-8](image-url)  
Comparison of computed and measured weight changes data of NiCoCrAlY (GT33-like coating) at 1850°F (1010°C). Deviations of computed and measured weight changes for thermal cycles greater than 2000 cycles were caused by the formation of alumina, yttria, and chromia in the experimental data, rather than a continuous layer of alumina as assumed in the model.
Figure 4-9
Al depletion compared against experimental data for NiCoCrAlY (GT33-like coating) at 1850°F (1010°C).

Figure 4-10
Computed volume percent of β phase compared against experimental data for NiCoCrAlY (GT33-like coating) at 1850°F (1010°C).
The material constants for GT33-like coatings are summarized in Table 4-2. Only the parabolic oxidation constant, \( k_p \), is temperature dependent. The \( k_p \) data were then fitted to an Arrhenius equation to obtain the \( k_p \) value for other temperatures. Parameters \( q, m, p, \) and \( T_c \) are material constants in the spallation model. Expressions for the oxidation and spallation models are described in earlier papers [1,2].

Table 4-1  
Material constants for GT33-like coatings.

<table>
<thead>
<tr>
<th>T, (^\circ)F ((^\circ)C)</th>
<th>Oxidation Constant</th>
<th>Spallation Constants</th>
</tr>
</thead>
<tbody>
<tr>
<td>( k_p ) (MG(^2)/CM(^4)/HR)</td>
<td>( Q )</td>
<td>( M )</td>
</tr>
<tr>
<td>1950 (1066)</td>
<td>1.25 ( \times 10^{-3} )</td>
<td>2 ( \times 10^{-9} )</td>
</tr>
<tr>
<td>1850 (1010)</td>
<td>9 ( \times 10^{-4} )</td>
<td>2 ( \times 10^{-9} )</td>
</tr>
</tbody>
</table>

4.1.B Coating Life Diagrams for NiCoCrAlY

After the model constants were determined, COATLIFE was used to compute the number of startup cycles to failure as a function of cycle time for thermal cycling at a prescribed peak temperature. Coating failure is defined on the basis of a critical Al content for the formation of a continuous alumina on the coating surface. The results were then presented in terms of a coating life diagram, which is a double logarithmic plot of the number of startup cycles to failure versus cycle time. Coating life diagrams for GT33 were computed for various temperatures ranging from 1500\(^\circ\)F (816\(^\circ\)C) to 1950\(^\circ\)F (1066\(^\circ\)C) at 50\(^\circ\)F (28\(^\circ\)C) increments. Figures 4-11 through 4-14 present the coating life diagrams of GT33 for 1950\(^\circ\)F (1066\(^\circ\)C), 1850\(^\circ\)F (1010\(^\circ\)C), 1750\(^\circ\)F (954\(^\circ\)C), and 1650\(^\circ\)F (871\(^\circ\)C), respectively.

For incorporation into COATLIFE, the linear approximations of the coating life boundaries were described in terms of a two-parameter expression given by

\[ N_s = 10^a \tau_c^b \]  Eq. 4-1

where \( N_s \) is oxidation life (i.e., number of startup cycles), \( \tau_c \) is the cycle time, and \( a \) and \( b \) are temperature-dependent constants derived from the coating life diagrams computed for various temperatures. Plots of \( a \) and \( b \) as a function of temperature are shown in Figures 4-15(a) and (b), respectively. These temperature-dependent coefficients were expressed as a function of temperature, and the results were incorporated into the COATLIFE Spreadsheet Program Version 3.0 (COATLIFE-3).
Figure 4-11
Computed coating life diagram for GT33-like coating at 1950°F (1066°C).

Figure 4-12
Computed coating life diagram for GT33-like coating at 1850°F (1010°C).
Figure 4-13
Computed coating life diagram for GT33-like coating at 1750°F (954°C).

Figure 4-14
Computed coating life diagram for GT33-like coating at 1650°F (899°C).
Figure 4-15  
Values of a and b as a function of temperature for GT33 coating: (a) value of a, and (b) value of b in the coating life expression $N_s = 10^a \tau^b$.  

(a)  

(b)
4.1.C Thermomechanical Fatigue Life (TMF) Relation for NiCoCrAlY

TMF life data developed in this program were used to develop the TMF life relation for NiCoCrAlY coatings on GTD-111DS. The TMF model used in this program has the form given by [7,8]

\[ \Delta \varepsilon_m = C N_f^{-\alpha} \quad \text{Eq. 4-2} \]

where \( \Delta \varepsilon_m \) is the mechanical strain range, \( N_f \) is the cycle-to-failure, \( C \) is the fatigue ductility coefficient, and \( \alpha \) is the fatigue life exponent.

Material constants in the TMF model, which are \( C \) and \( \alpha \), were evaluated by plotting the experimental data in a log-log plot of mechanical strain range versus cycles to failure. Least-squares regression analysis was then performed to obtain the values of \( C \) and \( \alpha \) for a particular coating/substrate combination. The value of \( C \) was then reduced to give the minimum coating life in the data set. The adjustment in the \( C \) value was necessary because of scatter in the TMF data. Finally, the \( C \) value was further reduced to that corresponding to one-half of the minimum life. This was done so that the predicted TMF life would be conservative. The TMF life equations and experimental data for NiCoCrAlY/GTD-111DS are presented in Figure 4-16. A comparison of the TMF life equation for NiCoCrAlY/GTD-111DS against those for NiCoCrAlY+/GTD-111DS and NiCoCrAlY/IN-738 is presented in Figure 4-17.

![Figure 4-16](image)

**Figure 4-16**

TMF strain-life relations developed based on experimental data of NiCoCrAlY-coated GTD-111DS.
A comparison of the TMF strain-life relation for NiCoCrAlY/GTD-111DS generated in this program against those of NiCoCrAlY+/GTD-111DS and NiCoCrAlY/IN-738 from previous EPRI programs [9,10].

4.1.D Coating Life Prediction Based On NDE Input

A set of procedures was developed for computing the remaining life of an MCrAlY coating using NDE data as input. One life equation was developed on the basis of linear depletion of the Al content, while another was developed on the basis of linear consumption of the $\beta$ phase in the coating. Both assumptions are currently used in COATLIFE for summing up damage accumulation under variable temperatures [6]. For linear depletion of Al content, the remaining life, $RL(T)$, of MCrAlY at temperature $T$ can be computed based on the current Al content via the expression given by

$$RL(T) = \left[ \frac{X_{Al} - X_{Al}^*}{X_{Al}(0) - X_{Al}^*} \right] N_f(T)$$

Eq. 4-3

where $X_{Al}$ is the current level of Al content determined by NDE, $X_{Al}(0)$ is the initial Al content in the pristine coating, $X_{Al}^*$ is the critical Al content, and $N_f(T)$ is the coating life for temperature $T$. 

Figure 4-17

A comparison of the TMF strain-life relation for NiCoCrAlY/GTD-111DS generated in this program against those of NiCoCrAlY+/GTD-111DS and NiCoCrAlY/IN-738 from previous EPRI programs [9,10].
For linear depletion of the $\beta$ phase, $RL(T)$ is given by

$$RL(T) = \left[ \frac{V_\beta}{V_\beta(0)} \right] N_f(T)$$

Eq. 4-4

where $V_\beta$ is the volume percent of $\beta$, and $V_\beta(0)$ is the initial volume percent of $\beta$ in the pristine coating.

Figures 4-18 and 4-19 show the correlations of Equations 4-3 and 4-4 against the experimental data of Al content and volume percent of $\beta$ phase in GT29+ coating, respectively. For this coating, the agreement between the experimental data and Equations 4-3 and 4-4 are excellent. Similar comparisons for GT33+, shown in Figures 4-20 and 4-21, indicate that Equation 4-3 gives a better agreement with experimental data for this coating. For GT33, Equation 4-3 is a lower bound of the experimental Al content, as shown in Figure 4-22, and is, therefore, conservative. Figure 4-23 shows that Equation 4-4 agrees well with the volume percent of $\beta$ phase in GT33.

Figure 4-18
Comparison of experimental Al content against model calculations for GT29+: (1) a linear fit to the experimental data, (2) COATLIFE, and (3) the linear damage (Al depletion) model used in COATLIFE for variable temperature conditions.
Figure 4-19
Comparison of experimental data of volume percent β phase against model calculations for GT29+: (1) a linear fit to the experimental data, (2) COATLIFE, and (3) the linear damage (β depletion) model used in COATLIFE for variable temperature conditions.

Figure 4-20
Comparison of experimental Al content against model calculations for GT33+: (1) a linear fit to the experimental data (2) COATLIFE, and (3) the linear damage (Al depletion) model used in COATLIFE for variable temperature conditions.
Figure 4-21
Comparison of experimental data of volume percent β phase against model calculations for GT33+: (1) a linear fit to the experimental data, (2) COATLIFE, and (3) the linear damage (β depletion) model used in COATLIFE for variable temperature conditions.

Figure 4-22
Comparison of experimental Al content against model calculations for GT33: (1) a linear fit to the experimental data, (2) COATLIFE, and (3) the linear damage (Al depletion) model used in COATLIFE for variable temperature conditions.
4.1.E COATLIFE – Version 3.5

Coating oxidation-life and TMF life equations for NiCoCrAlY coating on GTD-111DS were incorporated into COATLIFE 3.0. In addition, the graphical user interface (GUI) for COATLIFE 3.0 was revised to allow NDE input of Al content and volume percents of β phase and the computation of remaining coating life using the NDE data. Because of these additions, COATLIFE Version 3.0 was upgraded to Version 3.5. Figure 4-24 shows the revised GUI of COATLIFE Version 3.5 that includes columns for temperature, cycle time, and number of startup cycles, as well as additional columns for NDE input of Al content and volume percent of β phase. The radio button under each of these columns allows the user to select the mode of data input. The predicted coating life obtained from the selected data input is shown under the Failure Cycles column at the right of the window.

Figure 4-23
Comparison of experimental data of volume percent β phase against model calculations for GT33: (1) a linear fit to the experimental data, (2) COATLIFE, and (3) the linear damage (β depletion) model used in COATLIFE for variable temperature conditions.
As an example, Figure 4-25 shows the data input for computing the remaining life of GT33 GT33/GTD-111DS on the basis of the number of startup cycles for 24-hour thermal cycling at a peak temperature of 1800°F (982°C). The computed coating life (failure cycles), percent life consumed, the remaining life, and the coating status are shown under the corresponding columns in the upper-right portion of the window, while the predicted TMF data input and results are shown at the lower portion window. The coating life diagram for GT33/GTD-111DS for 1800°F (982°C) is presented in Figure 4-26, which shows the current coating condition as a pink square symbol, the Al oxidation life boundary as a red line, and the TMF life boundary as a pink horizontal line. For the conditions examined, the coating is currently safe since the accumulated number of startup cycles is well below the Al oxidation life and the TMF life boundaries.
Figure 4-25
Data input and predicted coating life for GT33/GTD-111DS subjected to thermal cycling at a peak temperature of 1800°F (982°C) and a cycle time of 24 hours for 200 startup cycles. The TMF strain ranges are 200 cycles at 0.55%. The predicted oxidation life, percent life consumed, and the remaining life are 728.08 cycles (or 17,474 hours), 27.47%, and 528.08 cycles, respectively, while the predicted remaining TMF life is 529.29 cycles.

Figure 4-26
Coating life diagram predicted for GT33/GTD-111DS at 1800°F (982°C).
The application of COATLIFE 3.5 for predicting the coating life of GT33/IN-738 is illustrated in Figures 4-27 and 4-28. Figure 4-27 shows the data input for thermal cycling at a peak temperature of 1750°F (954°C) and a cycle time of 1000 hours for 40 cycles. The predicted oxidation life is 26.423 cycles at 1000 hr/cycle or 26,423 hours, as shown in Figure 4-27. The coating life diagram, shown in Figure 4-28, shows that the oxidation life (red line) has been exceeded, meaning that the coating has failed by oxidation and Al depletion.

Figure 4-29 demonstrates the use of COATLIFE 3.5 for predicting coating life using NDE input data of volume percent of β phase. For this example, the input data include the temperature (1800°F (982°C)), cycle time (200 hours), and the volume percent of β phase (20%) remaining in the coating. The predicted coating life (failure cycles), percent life consumed, and the remaining life, which are shown at the right portion of the GUI, are 97.614 cycles, 50%, and 48.807 cycles, respectively. Thus, the remaining life of the coating is 9761.4 hours (48.087 cycles at 200 hours per cycle).

Figure 4-30 illustrates the use of COATLIFE 3.5 for predicting coating life using NDE input data of Al content. For this example, the input data include the temperature (1800°F (982°C)), cycle time (200 hours), and the atomic percent of Al content (11 at.%) in the coating. The predicted coating life (failure cycles), percent life consumed, and the remaining life, which are shown at the right portion of the window, are 97.614 cycles, 103.85%, and -3.7544 cycles, respectively. The negative remaining life means the coating has failed by oxidation and Al depletion. For this reason, the computed percent life consumed exceeded 100%.

Figure 4-27
Data input and predicted coating life for GT33/IN-738 subjected to thermal cycling at a peak temperature of 1750°F (954°C) and a cycle time of 1000 hours for 40 startup cycles. The TMF strain ranges are 30 cycles at 0.35%, 5 cycles at 0.45%, and 5 cycles at 0.55%. The predicted coating life, percent life consumed, and the remaining life are 26.423 cycles (or 26,423 hours), 151.38%, and -13,577 cycles, respectively.
Figure 4-28
Predicted coating life diagram for GT33/IN-738 at 1750°F (954°C).

Figure 4-29
Data input of temperature (1800°F (982°C)), cycle time (200 hours), and NDE input of 20% β phase, together with the predicted coating life (97.614 cycles), percent life consumed (50%), and the remaining life (48.807 cycles) for GT33/GTD-111DS.
4.2 TBC Life Prediction Algorithm Development

4.2.A Failure Mechanisms

Several damage and failure mechanisms are possible in the TBC system when subjected to temperature and strain cycling. These failure mechanisms in air plasma spray (APS) TBC, shown schematically in Figure 4-31, include TMF [11–18], bond coat oxidation [11–18], and fracture in the TBC [13,14,18], oxide [13], or at the oxide/TBC interface [15]. Figures 7-32 (a) and (b) show cracking and spallation of TBC under isothermal and cyclic oxidation, respectively. For TBC failure, creep and stress relaxation in the bond coat [15,19], sintering in the TBC [19], roughness of the TBC/TGO interface [15,20], as well as interdiffusion of Al from the bond coat to the substrate are also important [19].
Figure 4-31
Degradation mechanisms in APS TBCs.

Figure 4-32
Cracking and spallation of TBC: (a) isothermal oxidation (502 hours at 1975°F (1079°C)), and (b) cyclic oxidation (395 one-hour cycles at a peak temperature of 1950°F (1066°C)). The arrow indicates a TBC crack formed by thermal cycling.

Extensive recent work [21–26] indicated that the failure mechanisms in EB-PVD TBC are similar to those in APS TBC. The formation of TGO and the corresponding compressive growth stresses at the TBC/bond coat interface remain one of the main causes of failure in the TBC. In addition, the formation of surface ridges in the bond coat [21,25], rumpling of the TGO, and void formation at the TGO/bond coat interface were identified as possible mechanisms for the formation of interface cracks, whose subsequent propagation leads to spallation of the TBC. As in the APS TBC, these time-dependent failure mechanisms are sensitive to the composition, microstructure, and processing condition of the bond coat, as well as the TBC [21-26]. Large variations in TBC lives are common. A $10 \times$ increase in TBC life through surface conditioning of the bond coat, prior to the application of the EB-PVD TBC, has been reported by Jordan and Gell [25].

Most of the above-mentioned damage mechanisms are time- and temperature-dependent processes whose synergism (for example, TMF and oxidation) leads to failure of the TBC by spallation and occasionally by cracking [27–29]. Both events can
lead to mechanical failure of the blades and cause severe damage to turbine components. An accurate prediction of time- and temperature-dependent damage accumulation and failure in the TBC is, therefore, important for the safe and efficient utilization of gas turbine engines.

4.2.B TBC Life Model

A life prediction model for thermal barrier coating (TBC) was developed to treat bond coat oxidation, sintering, and spallation of the TBC, as well as effects of coating thickness and substrate curvature on TBC spallation [4,30]. Figure 4-33 summarizes the essential features of this TBC life prediction model, which is called TBC LIFE.

![Diagram of TBC LIFE model](image)

Figure 4-33
Summary of the TBCLIFE code.
Life-Prediction Equation

Failure of a TBC is predicted on the basis of an isothermal stress-life equation to treat both oxidation and thermomechanical stress cycling of the TBC \[11,22\]. The TBC life expression is given by

\[
\Delta \sigma_{TBC} N_f^b = \sigma_{TBC}^*(t) \left[ 1 - \left( \frac{W_g}{W_g^*} \right)^c \right]
\]

where \(\Delta \sigma_{TBC}\) is the stress range in the TBC, \(N_f\) is the number of startups (cycles-to-failure or the TBC life), \(\sigma_{TBC}^*(t)\) is the time-dependent fatigue strength of the TBC, \(W_g\) is the weight gain, and \(W_g^*\) is the critical weight gain for TBC spallation.

In this approach, spallation of the TBC from the bond coat is treated as an oxidation-induced cracking process that is assisted by thermal-cycling-induced fatigue in the TBC/bond coat interface region. Spallation of the TBC is envisioned to proceed by the initiation and propagation of cracks at the TBC/bond coat interface or on planes adjacent to the interface. Both \(W_g\) and \(W_g^*\) can be expressed in terms of the thickness of the thermally grown oxide, TGO, leading to \[17,28,29\]

\[
\Delta \sigma_{TBC} N_f^b = \sigma_{TBC}^*(t) \left[ 1 - \left( \frac{\delta}{\delta^*} \right)^c \right]
\]

where \(\delta\) and \(\delta^*\) are the thickness and the critical thickness of the TGO formed between the TBC and the bond coat.

Mechanical Strain Range

The TBC is assumed to remain elastic during thermal cycling. Accordingly, the stress range in the TBC is given by

\[
\Delta \sigma_{TBC} = E_{TBC}(t) \Delta \varepsilon_{TBC} \text{ Eq. 4-7}
\]

with

\[
\Delta \varepsilon_{TBC} = \left( \alpha_{0,S} - \alpha_{0,TBC} \right) (T_2 - T_1) + \frac{1}{2} \left( \alpha_{1,S} - \alpha_{1,TBC} \right) (T_2^2 - T_1^2) \text{ Eq. 4-8}
\]

and

\[
\alpha = \alpha_0 + \alpha_1 T \text{ Eq. 4-9}
\]

where \(\alpha\) is the coefficient of thermal expansion; \(\alpha_0\) and \(\alpha_1\) are empirical constants; \(T_2\) and \(T_1\) are the maximum and the minimum temperature at the TBC/bond coat interface, respectively. The subscripts \(S\) and \(TBC\) represent properties of the substrate and the TBC, respectively.
Oxidation Kinetics

The TGO is assumed to exhibit parabolic growth kinetics that can be described as [4,11,14,17,28,29,30]

\[ \delta = (k_p t)^{\frac{1}{2}} \quad \text{Eq. 4-10} \]

where \( k_p \) is the parabolic rate constant, and \( t \) is time of oxidation at the maximum temperature of the thermal cycle. Furthermore, the parabolic rate constant is described by an Arrhenius equation given by

\[ k_p = k_{po} \exp \left[ -\frac{Q_{ox}}{RT} \right] \quad \text{Eq. 4-11} \]

where \( k_{po} \) is the parabolic oxidation coefficient, \( Q_{ox} \) is the activation energy for oxidation, \( R \) is the universal gas constant, and \( T \) is the absolute temperature.

Parametric Calculations

A parametric study of the various key variables in the TBC model has been performed, using APS TBC and MCrAlY bond coat as an example. The effect of the peak temperature on TBC life is illustrated in Figure 4-34, which shows reduced TBC life with increasing peak temperature at the TBC/bond coat interface. The TBC life decreases with decreasing TBC fatigue strength coefficient, as shown in Figure 4-35. TBC sintering leads to a decrease of the TBC life (see Figure 4-36). The amount of degradation depends on the time exponent, \( p \), of the power law relating the TBC elastic modulus and the fatigue strength to the time at the peak temperature of sintering. The TBC life is also predicted to decrease with decreasing radius of curvature, as shown in Figure 4-37.
Figure 4-34  
Effect of peak temperature on the calculated TBC life.

Figure 4-35  
Effect of the TBC fatigue strength coefficient on the calculated TBC life.
Figure 4-36
Effect of sintering and time-dependent fatigue strength on the TBC life.

Figure 4-37
Effect of substrate curvature on the calculated TBC life.

Model Validation Using Literature Data of APS and EPB-PVD Applied TBCs

Model constants in the TBC life model include physical, thermal, low-cycle fatigue, and oxidation properties. Literature data for APS TBC and EB-PVD TBC were first used to determine these model constants and to demonstrate the predictive capability of the TBC model. Subsequently, the APS TBC data generated in this program were applied to the TBC life model. TBC life modeling using literature data is summarized in this section, while TBC life modeling for the APS TBC data generated in the current program is described in the next section.

A correlation of the TBC life model to the TBC life data from the literature shows excellent agreement between calculated and measured TBC lives, as shown in Figure 4-38.
The experimental results shown in Figure 4-38 are burner-rig and furnace data at which the TBC spalled; these data are from the HOST program [14]. The TBC life model was also applied to EB-PVD TBC and compared to experimental data in the literature [23-25]. The calculated curves for oxidation-induced failure were obtained on the basis of a critical oxide thickness criterion of 6–8.5 µm [23]. The former led to a lower oxidation life (500 hrs at one hour cycle), while the latter gave a longer TBC life (2000 hrs at 1-hour cycles). The horizontal band represented by two dashed lines in Figure 4-39 corresponds to a TMF-induced or crack-induced failure mechanism. The occurrence of these two failure regimes (oxidation- and TMF-induced failure) are qualitatively in agreement with the TBCLIFE prediction shown in Figure 4-36 for $p = 0.45$ and $p = 0.5$. Model calculation indicates that for a given value of the parabolic rate ($k_p$) constant, the oxidation-induced failure boundary represents an upper bound in spallation life. Crack-induced failure results in premature TBC failure and should be suppressed. The TBC life in land-based gas turbines is likely controlled by the oxidation kinetics of the bond coat. Hence, the firing temperature, the thermal conductivity of the TBC, and the $k_p$ value of the bond coat are important factors that are expected to influence the durability of TBC-coated blades in land-based gas turbines.

![Figure 4-38](image)

Figure 4-38
Calculated TBC life compared with burner-rig and furnace data from the HOST program [14].
4.2.C TBC Life Algorithm Development for Current Coating Systems

The oxidation kinetics of the APS TBC/NiCoCrAlY bond coat with or without a platinum (Pt) interlayer addition on GTD-111DS and IN-738 substrates were characterized and compared. In all cases, a parabolic rate equation was used to describe the TGO thickness, $\delta$, as a function of time, $t$, of thermal exposure. The parabolic rate constant, $k_p$, was obtained by plotting the TGO thickness as a function of $t^{1/2}$. Linear regression of the experimental data, as shown in Figure 4-40, provided the value of the parabolic rate constant, $k_p$, at a given temperature, $T$. Comparison of the calculated and measured oxide thickness is shown in Figures 4-41 and 5-78 for 1950°F (1066°C) and 1850°F (1010°C), respectively. Based on the time at failure, the critical TGO thickness at TBC failure was determined to be about 20 μm (see Figure 4-41). In addition, the $k_p$ parameter was then plotted as a function of the reciprocal temperature to obtain the activation energy, $Q_{ox}$, for TGO formation (see Figure 4-43).
Figure 4-40
Plot of TGO thickness, $\delta$, as a function of $t^{1/2}$, where $t$ is the time of oxidation in hours. The solid line is a least squares fit to the experimental data.

Figure 4-41
A comparison of measured and calculated TGO thickness at various times of oxidation.

Figure 4-42
A comparison of measured and computed TGO thickness at various times of oxidation.
Figure 4-43
An Arrhenius plot of the parabolic constant, $k_p$, versus the reciprocal temperature.

Comparison of the calculated and measured oxide thickness indicated that all four coating systems examined in this study exhibited very similar bond coat oxidation kinetics, regardless of the substrate and the addition of Pt in the bond coat. Figures 4-44(a), (b), and (c) present the computed and observed oxide thickness as a function of time of oxidation for APS TBC on NiCoCrAlY/GTD-111DS, NiCoCrAlY/IN-738, and NiCoCrAlY + Pt/IN-738 systems at 1950°F (1066°C), respectively. The results indicated that, in all coating systems, oxidation is well described by a parabolic rate equation with an identical value of $k_p$. Similar results were obtained for these coatings tested at 1850°F (1010°C) also. Based on the time to bond coat internal oxidation (Figure 4-44(b)) and the time to TBC cracking (Figure 4-45), the critical TGO thickness at TBC failure was determined to be about 20 µm for all four coating systems examined in this project. The critical oxide thickness was independent of test temperature.
Figure 4-44
Comparison of computed and measured values of oxide thickness as a function of time of oxidation and the experimentally observed failure mechanisms for three TBC systems: (1) APS TBC/NiCoCrAlY/GTD-111DS, (b) APS TBC/NiCoCrAlY/IN-738, and (c) APS TBC/NiCoCrAlY + Pt/IN-738.
Figure 4-45
Determination of the critical oxide thickness at TBC cracking for APS TBC/ NiCoCrAlY/GTD-111DS: (a) TBC cracking was observed after 5000 hrs at 1900°F (1038°C), and (b) the onset of bond coat internal oxidation was observed after about 10,000 hrs at 1850°F (1010°C).

The oxidation kinetic constants and the critical TGO thickness were used in conjunction with the TBC life model to compute the number of startup cycles (TBC life) as a function of cycle time. Figure 4-46 shows a comparison of the calculated and measured TBC lives for APS TBC/NiCoCrAlY/GTD-111DS tested at a peak temperature of 1950°F (1066°C), which shows excellent agreement between model calculation and experimental data. Experimental data at 1-hour cycle time and one startup cycle (isothermal oxidation data) were both used to evaluate material constants in the TBC life model. The excellent agreement at both ends of the TBC life boundary was thus due to fitting of the model to the experimental data. On the other hand, the excellent agreement between model calculation and the experimental data for 24-hour cycle time represented an independent prediction since the experimental data were not used to calibrate the model and the prediction was made before the experiment was conducted.

Figure 4-46
Computed TBC life diagram compared with experimental data for APS TBC/NiCoAlY/GTD-111DS at 1950°F (1066°C).
Figure 4-47 shows a comparison of the predicted and measured TBC lives for all four TBC systems at 1950°F (1066°C). All four coating systems were predicted to exhibit essentially identical TBC lives because of similar bond coating oxidation kinetics, as predicted by the TBC life model. Figure 4-47 also presents a linear approximation of the TBC life boundary that is used to develop TBC life diagrams for incorporation into COATLIFE.

![Diagram](image)

Figure 4-47
Computed TBC life diagram compared with the experimental data of four TBC systems at 1950°F (1066°C).

The TBC model was also used to predict the coating life diagrams for various temperatures ranging from 1900°F (1038°C) to 1500°F (816°C) at 50°F (27.8°C) increments. Figures 4-48(a) and (b) show comparison of predicted and measured TBC lives for 1900°F (1038°C) and 1850°F (1010°C), respectively, together with the corresponding linear approximations for implementation into COATLIFE.

![Diagram](image)

Figure 4-48
Comparison of computed TBC life boundaries with experimental data: (a) 1900°F (1038°C), and (b) 1850°F (1010°C).
For incorporation into COATLIFE, the linear approximations of the TBC life boundaries were described in terms of a two-parameter expression given by

$$N_s = 10^a \tau_c^b$$  \hspace{1cm} \text{Eq. 4-12}$$

where $N_s$ is TBC life (that is, the number of startup cycles), $\tau_c$ is the cycle time, and $a$ and $b$ are temperature-dependent constants derived from the TBC life diagrams computed via the TBC life model for various temperatures.

Plots of $a$ and $b$ as a function of temperature are shown in Figures 4-49(a) and (b), respectively. As shown in Figure 4-49, the values of $a$ and $b$ both decrease linearly with increasing temperature.

![Figure 4-49](image)

**Figure 4-49**
Parameters of the TBC life boundary ($N_s = 10^a \tau_c^b$) as a function of temperature: (a) parameter $a$, and (b) parameter $b$.

TMF failure in gas turbine blades coated with TBC occurs in two ways: (1) formation and propagation of microcracks near the TBC/TGO interface causing spallation of the TBC, and (2) formation and propagation of microcracks into the bond coat and the substrate. Both TMF failure modes are treated in the current TBC life model. The TBC spallation failure model was treated via the methodology described in this section. For TMF cracks advancing transversely across the bond coat and into the substrate, most, if not all, of the TMF life of the TBC system is spent in crack nucleation and growth in the bond coat. Thus, the TMF life of a blade coated with TBC was treated on the basis of the TMF life of the bond coat, using the method described in Section 3.1.3 and the corresponding TMF strain ranges for the TBC-coated blade.

APS TBC life diagrams were incorporated into COATLIFE, and the software was upgraded to COATLIFE-4.0. Figure 4-50 shows the new flash screen for COATLIFE-4.0, while Figure 4-51 shows the graphical user interface (GUI). For illustration, a coating life prediction was made for APS TBC after 100 startup cycles at 1700°F (927°C) and a cycle time of 100 hours/cycle. The predicted TBC oxidation life due to TGO formation and growth, followed by TBC spallation and cracking, is 148.75 cycles (14875 hours). The life consumed is 67.23%, and the remaining life is 48.746 cycles (4874.6 hours). The predicted TMF life for TMF cracks advancing into the bond coat...
and the substrate is 668.9 cycles. The status of the TBC is safe and protective, as shown in the coating life diagram in Figure 4-52. In Figure 4-52, the oxidation life boundary (slanted line) corresponds to failure by TGO formation and growth as well as TBC spallation, while the TMF boundary (horizontal line) corresponds to failure by cracks advancing into the bond coat and the substrate.

The software was tested extensively, and no runtime errors were found. A User’s Manual for COATLIFE-4.0 was prepared [31]. A copy of COATLIFE-4.0 along with the User’s Manual was submitted to EPRI.

Figure 4-50
Flash screen of COATLIFE-4.0, which incorporates a life prediction capability for APS TBCs.

Figure 4-51
The graphical user interface (GUI) of COATLIFE-4.0 with TBC life prediction capability.
Figure 4-52
Predicted TBC life diagram showing the current status of an APS TBC after 100 startup cycles at 1700°F (927°C) and 100 hours/cycle.

4.2.D TBC Model Validation

The TBC life prediction was verified by comparing model prediction against cyclic oxidation data that were not used for determining model constants. These TBC data were those that were obtained by furnace tests with 24-hour cycle times and burner-rig tests with 1-hour cycles. Comparison to the predicted TBC lives and experimental data for furnace tests with 24-hour cycles is shown in Figure 4-53. As discussed in a previous section, the TBC remained intact without any signs of cracking or spallation after 373 one-hour thermal cycles. COATLIFE predicts a TBC life of 914 one-hour startup cycles at 1950°F (1066°C) peak temperature, as shown in Figure 4-53. For illustration purposes, Figure 4-53 also shows a prediction of the TMF life obtained on the basis of an estimated TMF strain range of 0.4%. The predicted TMF life is 1153.5 cycles, indicating that the TBC is also safe against TMF failure.

Figure 4-53
Coating life diagram for APS TBC for four different TBC/bond coat/substrate systems compared with TBCLIFE and COATLIFE.
Thus, COATLIFE prediction is consistent with burner-rig test data generated so far. A more rigorous test of COATLIFE will come when the TBC on the burner-rig test specimens begins to crack or spall. There is also a need for a rigorous analysis of the TMF strain range associated with the burner-rig tests.

Figure 4-54  
Verification of COATLIFE prediction against burner-rig tests: (a) COATLIFE prediction of a TBC life after 914 cycles, and (b) predicted coating life diagram for the APS TBC showing the TBC being protective after 373 one-hour thermal cycles.
4.2.E Coating Algorithm Development -- Conclusions

The conclusions reached in the development of coating life algorithms for a NiCoCrAlY (GT33-like) coating are as follows:

- GT33-like coating forms predominantly alumina scales after a small number (600–800 one-hour cycles) of thermal cycles, but it forms a mixture of Al-rich, Cr-rich, and other rare-earth mixed oxides at a larger number (1500–2000 one-hour cycles) of thermal cycles. Spallation of mixed oxides eventually leads to pitting and localized oxidation. This cyclic oxidation behavior can be modeled by treating the oxidation and spallation kinetics of alumina.

- The oxidation life of GT33-like coatings can be predicted on the basis of a critical Al content for the formation of a continuous layer of alumina scale on the coating surface. Al contents in GT33-like coating remained high after the onset of pitting and localized oxidation because of the mixed-oxide formation that depletes Al, Cr, and other rare-earth or transitional metals in the coatings.

- Thermal fatigue cracks initiated in GT33-like coating at oxidation pits. Thermal fatigue cracking was more prevalent at 1850°F (1010°C) than at 1950°F (1066°C) because the longer oxidation life at the lower temperature permits a larger number of thermal cycles. TMF life of GT33-like coatings can be modeled in terms of the thermomechanical strain ranges and the Coffin-Manson equation for low-cycle fatigue.

- Life-prediction algorithms were developed for predicting the oxidation and TMF lives of GT33-like (NiCoCrAlY) coating on the basis of the underlying degradation mechanisms. These life prediction algorithms were incorporated into COATLIFE Spreadsheet Program Version 4.0.

- COATLIFE Spreadsheet Program Version 4.0 is capable of predicting the oxidation and TMF lives of a number of MCrAlY coatings, including that of GT33-like (NiCoCrAlY) coatings. The COATLIFE predictions were verified and validated against both laboratory results and field data.

- Life prediction by COATLIFE Spreadsheet Program Version 4.0 can be performed on the basis of coating usage history or NDE input of Al content or volume % of phase.

- The conclusions reached in the development of coating life algorithms for TBCs are as follows:

  - The prominent life-limiting failure processes in TBC are cyclic oxidation spallation and crack-related failure such as TMF.

  - Cyclic oxidation degrades TBC performance by forming a TGO layer at the TBC/bond coat interface, while TMF causes nucleation and propagation of microcracks within the TBC, TGO, and in the vicinity of the TBC/bond coat. Both oxidation and TMF contribute to TBC spallation. This spallation process can be modeled as an oxidation-assisted low-cycle fatigue process with a critical alumina oxide (TGO) thickness as a failure criterion for the onset of TBC spallation or cracking.
• The critical oxide thickness for the APS TBC studied in this investigation was 20 μm, which corresponds to the TGO thickness at the onset of internal oxidation in the bond coat.

• TMF can cause the nucleation and propagation of microcracks into the bond coat and the substrate. The TMF life for this failure mode in a TBC system can be predicted on the basis of the TMF life of the bond coat and substrate alone.

• A generic TBC life model was developed for both APS TBC and EB-PVD TBC. The predictive capability of the TBC life model was demonstrated for APS TBC using both literature data and laboratory data generated in this program. The predictive capability of the model for EB-PVD TBC was illustrated via literature data.

• Life-prediction algorithms were developed for predicting the oxidation and TMF lives of APS TBC on the basis of the underlying degradation mechanisms. These life prediction algorithms were incorporated into COATLIFE Spreadsheet Program Version 4.0.

• COATLIFE Spreadsheet Program Version 4.0 is capable of predicting the oxidation and TMF lives of APS TBC. The COATLIFE predictions were verified against laboratory results and limited burner-rig data.
References


5.0 FIELD VALIDATION OF COATLIFE AND NDE

The primary objective of this project was to develop and validate the predictive capabilities of COATLIFE and the eddy current NDE methodology on field-operated coated turbine buckets. The results of this effort are provided below.

5.1 Field Validation

Gas turbine blades experience complex thermal and mechanical loading history during a typical operating cycle consisting of startup, steady-state operation, and shutdown. Temperature gradients and mechanical constraints during cycling result in fluctuating thermomechanical stresses, which can lead to thermomechanical fatigue (TMF) damage. The steady-state and cyclic operating conditions lead to degradation of blade material and coating. In a cyclic duty machine, TMF limits the blade service life, while degradation of the coating limits the blade service life in a base load machine. The COATLIFE software model treats both coating degradation and TMF life. The purpose of this section is to determine in-service coating degradation, validate the COATLIFE model using field data, and generate necessary metallurgical data for the correlation of NDE results.

5.1.A Experimental Procedure

Material, Coatings, and Test Specimens

Following nondestructive examination (NDE), the EPRI NDE Center shipped three blades and mounts to SwRI for metallurgical evaluation and COATLIFE validation. Two service-run GE Frame 7FA blades (# 7 and 57) and six metallurgical mounts prepared from a transverse section removed from the 50% airfoil height of a Frame 7FA blade were received for evaluation. The six metallurgical mounts were identified as C-1 through C-6. The blades and the mounts are hereafter referred to as Blades 7, 57, and C. These blades operated on three different engines, which were fired with natural gas. The firing temperature of these Frame 7FA engines was reported to be 2350°F (1288°C). Blade 7 had seen 8286 hours of operation with 670 start-stop cycles. Blade 57 had seen 2000 hours of operation with 219 start-stop cycles after it was refurbished. The blade was refurbished after it had seen 14,795 hours and had experienced 518 start-stop cycles. Blade C had operated for 6156 hours with 272 starts-stop cycles. All three blades operated under similar conditions.

Metallography

The airfoil section of the blades was divided into squares or grids as illustrated in Figure 5-1. It was reported that NDE measurements were taken from all the squares marked on the blades. For NDE validation, three transverse sections at the 25%, 50%, and 75% blade height locations were removed from each blade. These locations correspond to transverse locations marked as A, C, and D on Blade 7, and B, E, and G on Blade 57. Each transverse section was cut into six or eight small sections. All of these small sections were mounted in a conductive mounting medium and polished using standard metallographic techniques. The locations of the mounts are illustrated in Figure 5-2. The
mounts received from Blade C are shown in Figure 5-3. All of these metallurgical mounts were examined under optical and scanning electron microscopes to characterize the coating structure and to determine the chemical composition of the coating. The mounts prepared from Blades 7 and 57 were also examined for TMF cracking. The aluminum content in the coating at different locations was determined by performing EDS measurements.

Figure 5-1
Photograph showing grids on the airfoil sections of Blades 7 and 57 where NDE measurements were taken. Note the smaller squares on Blade 57.

Figure 5-2
Photographs showing metallurgical sample locations in Blades 7 and 57.
5.1.B Results and Discussion

**Blade Materials and Coatings**

The metallurgical examinations showed that the blades were made of GTD-111DS. EDS measurements were made on the coating. The EDS analysis results of the coating are shown in Table 5-1. The results showed that these blades had over aluminized NiCoCrAlY coating (similar to GT 33+) The NiCoCrAlY coating on Blade 57 and Blade C had a higher aluminum content than the corresponding coating on Blade 7. It is well known that the MCrAlY and aluminide coating ductility is inversely related to the aluminum content in the coating and that higher aluminum lowers coating ductility and promotes TMF cracking [1].

Table 5-1
Semi-quantitative chemical composition of top aluminide and MCrAlY coating on the blades, wt. %.

<table>
<thead>
<tr>
<th>Blade</th>
<th>Coating</th>
<th>Al</th>
<th>Ti</th>
<th>Cr</th>
<th>Co</th>
<th>Y</th>
<th>Ni</th>
</tr>
</thead>
<tbody>
<tr>
<td># 7, CX @ 25%</td>
<td>Top Aluminide</td>
<td>18.5</td>
<td>-</td>
<td>15.5</td>
<td>24.6</td>
<td>-</td>
<td>Balance</td>
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<td></td>
<td>NiCoCrAlY</td>
<td>10.9</td>
<td>0.4</td>
<td>19.7</td>
<td>33.2</td>
<td>0.4</td>
<td>Balance</td>
</tr>
<tr>
<td># 57, CX @ 25%</td>
<td>Top Aluminide</td>
<td>21.5</td>
<td>-</td>
<td>9.9</td>
<td>31.3</td>
<td>-</td>
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<td></td>
<td>NiCoCrAlY</td>
<td>13.5</td>
<td>0.5</td>
<td>23.2</td>
<td>28.8</td>
<td>0.4</td>
<td>Balance</td>
</tr>
<tr>
<td># Blade C @ 50%</td>
<td>Top Aluminide</td>
<td>18.9</td>
<td>-</td>
<td>10.2</td>
<td>27.9</td>
<td>-</td>
<td>Balance</td>
</tr>
<tr>
<td></td>
<td>NiCoCrAlY</td>
<td>13.4</td>
<td>0.4</td>
<td>23.4</td>
<td>28.9</td>
<td>0.4</td>
<td>Balance</td>
</tr>
</tbody>
</table>
**TMF Cracking**

Several TMF cracks were seen on both blades and mounts C-1 through C-6. On Blade 7, TMF cracks were seen on the convex (suction) side of the airfoil near the leading edge. No cracks were observed on the concave (pressure) side of Blade 7 airfoil. The majority of cracks in this blade were shallow, and only a crack in the blade at the 25% height had extended into the substrate. All cracks were located near the blade’s leading edge. No TMF cracks were observed near the blade’s trailing edge. On Blade 57, TMF cracks were observed on both the convex and concave sides and were located between the leading and trailing edge of the blade. Several of these cracks at different heights extended into the substrate. The extent of TMF cracking at different airfoil heights on both blades is summarized in Table 5-2. It is evident from these results that the extent of cracking was more severe on Blade 57 than on Blade 7.

The morphology of cracking on both blades is similar. Typical morphology of TMF cracks is shown in Figures 5-4 and 5-5. TMF cracks were also observed on the convex and concave sides at the 50% airfoil height of Blade C. Some of these cracks in the airfoil had progressed into the substrate. Typical morphology of these cracks is shown in Figure 5-6.

On Blade 57 and Blade C, several grit particles were observed on the bond coat substrate interface. It is clear from the micrographs that the TMF propagated along the bond coating/substrate interface through the grit particles. In isolated locations, the crack propagated along the interface between the TMF cracks, as illustrated in Figure 5-6 (a). At a location on the convex side of the blade, the coating between two TMF cracks was spalled due to crack propagation along the interface between the cracks. This suggests that the presence of excessive contamination or porosity at the coating/substrate interface can lead to coating delamination and spallation. Hence, it is very important to control the quality of the coating for the durability of both metallic and thermal barrier coatings.

As can be seen in Figure 5-6, few cracks on Blade C had progressed into the substrate. In all three blades, the TMF cracks propagated approximately 60 µm maximum in length into the substrate.

**Table 5-2**

Summary of TMF cracking on Blade 7 and Blade 57.

<table>
<thead>
<tr>
<th>Blade ID</th>
<th>Airfoil Height, %</th>
<th>Number of Cracks</th>
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</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Convex side</td>
</tr>
<tr>
<td>7</td>
<td>25</td>
<td>6</td>
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<td></td>
<td>50</td>
<td>2</td>
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<td>68</td>
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<tr>
<td></td>
<td>75</td>
<td>19</td>
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</table>
Figure 5-4
Optical micrographs of TMF cracks at different blade heights on Blade 7.
Figure 5-5
Optical micrographs of TMF cracks at different blade heights on Blade 57.
Mount C-3 SS

Mount C-2 SS

Figure 5-6
Optical micrographs of TMF cracks at 50% airfoil height of Blade C.
Coating Quality

The microstructure of the coating on Blade 7 exhibited a duplex structure, consisting of $\beta$ phase $(\text{Ni}, \text{Co})\text{Al}$ in a matrix of $\gamma$ (solid solution of Ni-Co-Cr). The coating on the blades was dense, and a few pores/voids were observed in isolated areas on the airfoil section. Both NiCoCrAlY and top aluminide layers exhibited relatively uniform thickness at all three blade height locations examined. The NiCoCrAlY and top aluminide thickness at different locations varied from about 170–230 $\mu$m and 40–50 $\mu$m, respectively. Typical microstructure of the coating on Blade 7 is shown in Figure 5-7. It is evident from these micrographs that the coating/substrate interface was reasonably clean and free from grit or oxide particles. Overall, the quality of the coating on Blade 7 was good.

![Figure 5-7](image)

**Figure 5-7**
Typical microstructure of the coating at different locations on Blade 7.

Typical microstructure of the coating at multiple locations on Blade 57 and Blade C is shown in Figures 5-8 and 5-9, respectively. The NiCoCrAlY and aluminide thickness on Blade 57 at different locations varied from 90–100 $\mu$m and 70–100 $\mu$m, respectively. Similar variation in coating thickness was observed on Blade C. Blades 57 and C had a thicker top aluminide layer and thinner NiCoCrAlY layer compared to the coating on Blade 7.
The coating/substrate interface of both blades exhibited a significant amount of grit particles. In addition, extensive porosity was observed in the coating on Blade C. Comparison of micrographs presented in Figures 5-7 through 7-9 indicates that the quality of the coating on Blade 57 and Blade C was poor. The presence of higher aluminum content in the NiCoCrAlY coating and thicker aluminide coating lowers TMF life [1-4]. This is consistent with the more extensive TMF cracking observed in Blade 57 after 2000 hours and 290 cycles of service, compared to cracking observed on Blade 7 after 8286 hours with 670 cycles. The results of these service-run blades also indicate that the quality of the coating can significantly affect the TMF life of blades.

Figure 5-8
Optical micrographs of coating on Blade 57 showing the microstructure and coating thickness and the grit particles at the coating/substrate interface.
Coating Degradation

As expected, the coating at the 75% and 50% blade height location of Blade 7 showed evidence of degradation as a result of service exposure. As discussed in Section 1, the coating degradation during service results from transformation of the β phase into γ, coarsening of the β phase and an increase in the interdiffusion zone width below the NiCoCrAlY coating. The extent of coating degradation varied from location to location. Severe degradation was observed at the leading edge and on the concave (PS) side of the airfoil approximately 1.75 inches (4.5 cm) from the trailing edge of the blade. Coating at the leading edge at the 75% and 50% height locations degraded the most. No significant degradation was observed at the 25% blade height location.

The extent of coating degradation at the 75% and 50% height locations is shown in Figures 5-10 and 5-11, respectively. The results showed that that β-phase particles in the NiCoCrAlY and aluminide coatings were completely consumed on the suction side, approximately 0.125 inches (3 mm) away from the leading edge at the 75% blade height (see Figure 5-10(b)). The aluminum content in the β-phase depleted area is measured to be 3 wt. %. From the interdiffusion zone width, the local operating metal temperature at
this location is estimated to be 1920°F (1049°C). On the other hand, a significant amount of β phase was observed in the coating on the leading edge tip (see Figure 5-10(a)). However, it is evident from the micrograph that the β-phase particles in the coating on the leading edge have coarsened as a result of in-service degradation. The coating, approximately 1.2 inches (30 mm) away from the leading edge on the suction side of the airfoil, showed little or no evidence of coating degradation. Figure 5-10(d) shows typical microstructure of almost un-degraded coating on the suction side of the airfoil of Blade 57. Similar observations were made at the 50% airfoil height. Based on the interdiffusion zone width at the 50% height, the local metal temperature at the leading edge is estimated to be 1850°F (1010°C).

![Image](a) (b) (c) (d)

**Figure 5-10**  
Optical micrographs showing the variation of coating degradation at the 75% airfoil height of Blade 7. Note that the coating is completely degraded on the suction side near the leading edge (b) and that the coating showed no evidence of degradation on the suction side of the airfoil 1.75" from the trailing edge (d). Also note SS and PS on the micrographs denote suction and pressure sides, respectively.
Figure 5-11
Optical micrographs showing the variation of coating degradation at the 50% airfoil height of Blade 7. Note the degradation of coating completely at the LE (a) and note that the coating showed no evidence of degradation on the suction side of the airfoil, 1.75 inches away from the trailing edge.
The leading edge, on the convex side of the airfoil at 25%, was damaged due to foreign object impact. As a result, the extent of coating degradation at the leading edge could not be determined.

The microstructure of the coating on Blade 57 was in good condition. No evidence of coating degradation was observed at the 75%, 50%, and 25% airfoil height locations. This observation is consistent with the fact that the blade had operated for a relatively short time, 2000 hours. Typical microstructure of the coating on the airfoil approximately 1.75 inches (45 mm) away from the trailing edge is shown in Figure 5-12.

![Microstructure of the coating on pressure side (PS) of the airfoil at the 75% and 50% blade heights.](image-url)
The microstructure of the coating on Blade C at the 50% blade height is shown in Figure 5-13. Comparison of the micrographs shows a larger interdiffusion zone at the leading edge of the blade, indicating that the coating on the leading edge had degraded somewhat. A significant amount of β phase is observed in the top aluminide and NicoCrAlY coatings on the leading edge. As a result, the extent of degradation is not considered significant.

Figure 5-13
Microstructure of the coating on the suction side at the 50% airfoil height of Blade C.
**Chemical Analysis of the Coating**

EDS measurements were performed at several locations on the top aluminide, NiCoCrAlY, and overall coating (NiCoCrAlY+ aluminide) concave and convex sides at the 25%, 50%, and 75% height locations of Blades 57 and 7. The convex side of the leading edge at the 75% height was damaged due to foreign object impact, and as a result, no EDS measurements were taken on the leading edge at the 75% height. The results are presented in Table 5-3. EDS measurement results taken at or near the leading edge were not included in the table since no NDE tests were performed on the leading edge. The grid numbers listed in the table correspond to the XY positions marked on the blades in Figure 5-1. Consistent with the metallography results and service history, the coating on Blade 7 exhibited lower remaining aluminum than the coating on Blade 57. The coating at all locations where NDE measurements were taken exhibited a significant amount of aluminum (>10% Al in overall coating). The aluminum content in the coating dictates the remaining service life of a blade. Typically, the coating loses its protective capability when the aluminum content in the coating drops to about 3.7 [3,4]. These results show that the coating on the blades, where the NDE measurements were taken, has not significantly degraded.

**Table 5-3**

Chemical composition of the coating at various locations on Blades 57 and 7, wt. %.

<table>
<thead>
<tr>
<th>Grid #</th>
<th>Coating</th>
<th>Weight %</th>
<th>Average of 10 Frames</th>
</tr>
</thead>
<tbody>
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<tr>
<td>B-9-CX</td>
<td>Aluminide</td>
<td>21.51</td>
<td>9.93</td>
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<td></td>
<td>MCrAlY</td>
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<td>15.67</td>
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<td>Aluminide</td>
<td>22.05</td>
<td>9.8</td>
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<td>MCrAlY</td>
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<td>18.18</td>
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<td>B-7-CX</td>
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<td>Al+MCrAlY</td>
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<td>0.48</td>
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Table 5-3
Chemical composition of the coating at various locations on Blades 57 and 7, wt. % (Continued).

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<tr>
<th>Grid #</th>
<th>Coating</th>
<th>Weight %</th>
<th>Average of 10 Frames</th>
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Table 5-3
Chemical composition of the coating at various locations on Blades 57 and 7, wt. % (Continued).

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<th>Chemical composition of top aluminide and MCrAlY coatings (weight %)</th>
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| **Blade 57 @ 75%** | **Coating** | **Al** | **Ti** | **Cr** | **Co** | **Y** | **Ni** |
| Grid # | | | | | | | |
| G-9-CX | Aluminide | FOD | | | | | |
| | MCrAlY | | | | | | |
| | Al+MCrAlY | | | | | | |
| G-9-CV | Aluminide | 21.52 | 9.81 | 32.72 | Balance |
| | MCrAlY | 13.09 | 0.49 | 21.98 | 29.67 | 0.52 | Balance |
| | Al+MCrAlY | 15.53 | 0.35 | 18.54 | 30.54 | 0.28 | Balance |
| G-8-CX | Aluminide | 24.51 | 12.04 | 33.12 | Balance |
| | MCrAlY | 13.39 | 0.49 | 22.04 | 29.44 | Balance |
| | Al+MCrAlY | 16.6 | 0.37 | 18.81 | 30.48 | 0.4 | Balance |
| G-8-CV | Aluminide | 22.93 | 11.03 | 33.56 | Balance |
| | MCrAlY | 12.62 | 0.42 | 22.3 | 30.58 | 0.56 | Balance |
| | Al+MCrAlY | 14.43 | 0.38 | 20.21 | 31.02 | 0.34 | Balance |
| G-7-CX | Aluminide | 26.27 | 11.41 | 32.24 | Balance |
| | MCrAlY | 15.88 | 0.44 | 19.8 | 28.84 | 0.33 | Balance |
| | Al+MCrAlY | 17.58 | 0.41 | 18.81 | 29.52 | 0.3 | Balance |
| G-7-CV | Aluminide | 23.45 | 11.55 | 32.6 | Balance |
| | MCrAlY | 11.96 | 0.45 | 23.13 | 30.27 | 0.41 | Balance |
| | Al+MCrAlY | 15.08 | 0.37 | 20.05 | 30.53 | 0.38 | Balance |
Table 5-3
Chemical composition of the coating at various locations on Blades 57 and 7, wt. % (Continued).

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<th>Ti</th>
<th>Cr</th>
<th>Co</th>
<th>Y</th>
<th>Ni</th>
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Table 5-3
Chemical composition of the coating at various locations on Blades 57 and 7, wt. % (Continued).

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Table 5-3
Chemical composition of the coating at various locations on Blades 57 and 7, wt. % (Continued).

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Table 5-3
Chemical composition of the coating at various locations on Blades 57 and 7, wt. % (Continued).

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<th>Co</th>
<th>Y</th>
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<td>13.03</td>
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<td>33.55</td>
<td>0.58</td>
<td>Balance</td>
<td></td>
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</tr>
<tr>
<td></td>
<td>MCrAlY</td>
<td>10.95</td>
<td>0.31</td>
<td>19.95</td>
<td>32.34</td>
<td>0.27</td>
<td>Balance</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Al+MCrAlY</td>
<td>12.46</td>
<td>0.29</td>
<td>19.34</td>
<td>32.34</td>
<td>0.27</td>
<td>Balance</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
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Table 5-3
Chemical composition of the coating at various locations on Blades 57 and 7, wt. % (Continued).

<table>
<thead>
<tr>
<th>Grid #</th>
<th>Coating</th>
<th>Weight %</th>
<th>Average of 10 Frames</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Al</td>
<td>Ti</td>
</tr>
<tr>
<td>D-2-CV</td>
<td>Aluminide</td>
<td>14.72</td>
<td>1.62</td>
</tr>
<tr>
<td></td>
<td>MCrAlY</td>
<td>9.13</td>
<td>1.28</td>
</tr>
<tr>
<td></td>
<td>Al+MCrAlY</td>
<td>10.1</td>
<td>1.39</td>
</tr>
<tr>
<td>D-1-CX</td>
<td>Aluminide</td>
<td>18.58</td>
<td>12.56</td>
</tr>
<tr>
<td></td>
<td>MCrAlY</td>
<td>10.61</td>
<td>0.43</td>
</tr>
<tr>
<td></td>
<td>Al+MCrAlY</td>
<td>11.91</td>
<td>0.37</td>
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<tr>
<td>D-1-CV</td>
<td>Aluminide</td>
<td>17.06</td>
<td>0.88</td>
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<tr>
<td></td>
<td>MCrAlY</td>
<td>10.17</td>
<td>0.71</td>
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<tr>
<td></td>
<td>Al+MCrAlY</td>
<td>11.39</td>
<td>0.72</td>
</tr>
</tbody>
</table>

COATLIFE and NDE Validation

COATLIFE-4 was verified for GT33+ type coating using the data obtained from the three service-run blades or blades removed from three different 7FA machines. The results of Blades 7, 57, and C were used for COATLIFE verification/validation. The operating metal temperature of these blades was determined using the local changes in the interdiffusion zone width.

Blade 7 experienced cracking at the leading edge but not at the trailing edge. Based on the interdiffusion zone width, the local temperature at the 75% airfoil height location was determined to be 1920°F (1049°C) at the leading edge and 1785°F (974°C) at the trailing edge. COATLIFE prediction indicates that the oxidation life has been totally consumed (170% life) at the leading edge, but only 47% life has been consumed at the trailing edge. A comparison of COATLIFE prediction and metallographic results of the coating at the leading edge is presented in Figures 5-14 (a) and (b), respectively. The corresponding comparison for results of the trailing edge is presented in Figures 5-15(a) and (b), respectively. Similar predictions have been made for the other two 7FA machines, which had failure at Blade C and Blade 57. A summary of the COATLIFE prediction of oxidation life of GT33+ coating against field data is presented in Table 5-4.

Blades C, 7, and 57 with GT33+ coating also experienced TMF cracking at different airfoil heights. A summary of the operation history of these blades is shown in Table 5-4. The local temperature and TMF strain ranges computed by Turbine Technology International (TTI) for these locations are also shown in Table 7-4. COATLIFE
predictions indicate that the coatings are protective against oxidation but have exceeded their TMF lives. Figure 5-16(a) shows the coating life diagram for 7FA Blade 7 at 25% blade height. The local temperature was 1660°F (904°C), and the TMF strain range was 0.8%. After 670 startups at 12.36 hours/cycle (8286 total operating hours), only 14.55% of the oxidation life had been consumed, but the coating has exceeded the predicted TMF life (94 cycles) considerably. Metallographic examination of the blade (see Figure 5-16(b)) indicates that the TMF crack had penetrated into the base metal. Figure 5-17(a) shows the coating life diagram for 7FA Blade C at the 55% blade height. The local temperature was 1680°F (916°C), and the TMF strain range was 0.65%. After 272 startups at 22.63 hours/cycle (6156 total operating hours), only 10.81% of the oxidation life had been consumed, but the coating had exceeded the predicted TMF life (217.2 cycles). Metallographic examination of the blade (see Figure 5-17(b)) also indicates that the TMF crack had penetrated into the base metal and a small portion of the coating had spalled.
Figure 5-14
Comparison of the COATLIFE prediction of oxidation life for GT33+ against field data for the leading edge (75% blade height) of Blade 7 in a 7FA machine after 8286 hours and 670 startups: (a) oxidation failure predicted by COATLIFE-4, and (b) metallographic section showing β-depleted coating at the leading edge tip of Blade 7.
Figure 5-15
Comparison of the COATLIFE prediction of oxidation life for GT33+ against field data for the trailing edge (50% blade height) of Blade 7 in a 7FA machine after 8286 hours and 670 startup cycles: (a) COATLIFE prediction of 42.73% life consumed and a coating life of 1567.9 startup cycles (see Table 6-1), and (b) metallographic section showing the GT33+ coating at the trailing edge (50% blade height) being protective and in good condition.
### Table 5-4
Verification of COATLIFE-4 predictions against field data for GT33+ in 7FA machines.

<table>
<thead>
<tr>
<th>Blade</th>
<th>Location</th>
<th>Operation Hours</th>
<th>Startup Cycles</th>
<th>Cycle Time hrs/cycle</th>
<th>Metal Temp. °F</th>
<th>TMF Strain Range (%)</th>
<th>COATLIFE Prediction</th>
<th>Status</th>
<th>Field Observation</th>
</tr>
</thead>
<tbody>
<tr>
<td>C</td>
<td>50% BH 1&quot; from LE-CX</td>
<td>6156</td>
<td>272</td>
<td>27.63</td>
<td>1680</td>
<td>0.65</td>
<td>2515.4</td>
<td>217.2</td>
<td>TMF failure</td>
</tr>
<tr>
<td></td>
<td>50% BH 1&quot; from LE-CX*</td>
<td>2000</td>
<td>219</td>
<td>9.13</td>
<td>1730</td>
<td>0.50 (normal) 0.71 (trip)</td>
<td>3307.4</td>
<td>161.3</td>
<td>TMF failure</td>
</tr>
</tbody>
</table>

C = (F-32) X 5/9
1 inch = 25.4 mm
* Refurbished coating.
Figure 5-16
Verification of the COATLIFE prediction against field data of GT33+ coated GTD111 DS blade: (a) COATLIFE prediction of TMF failure at 25% BH of Blade 7 in a 7FA machine, and (b) metallographic section showing a TMF crack penetration into the substrate.
Figure 5-17
Verification of the COATLIFE prediction against field data of GT33+ coated GTD111 DS blade: (a) COATLIFE prediction of TMF failure at 50% blade height of Blade C in a 7FA machine, and (b) metallographic section showing a TMF crack penetration into the substrate and exposed blade due to coating spallation.
7.1.C Conclusions

- Examination of service-run blades removed from the 7FA machine showed that the coating on the leading edge at the 75% height location on Blade 7 was severely degraded. The coating on the other two blades was in good condition.
- TMF cracks were observed on the concave or convex section of all three blades.
- The extent of TMF cracking in a blade depends on the quality of the coating. Thicker aluminide top layer and higher aluminum content in the NiCoCrAlY coating promotes TMF cracking.
- The COATLIFE-predicted oxidation and TMF lives of GT33+ coated blades are in good agreement with the metallographic results.

References

