COMBUSTION TURBINE (CT) HOT SECTION COATING LIFE MANAGEMENT

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Principal Authors:

R. Viswanathan
D. Gandy
K. Krzywosz
S. Cheruvu
E. Wan

Subcontractors

Southwest Research Institute
Turbine Technology International

Submitted by

R. Viswanathan
EPRI
3412 Hillview Avenue
Palo Alto, CA 94304
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1.0 ABSTRACT

The integrity of coatings used in hot section components of combustion turbine 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, three 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. This report is a record of the progress to date on these 3 key tasks. Two supporting tasks relating to field validation (Task 4) and economic analysis (Task 5) have not yet been initiated.

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2.0 INTRODUCTION

The objective of this project is to improve the reliability, availability and maintainability (RAM) of combustion turbines (CTs) by developing advanced technology for assessing and managing the life of protective coatings on CT blades 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 enable increases in rotor inlet temperature resulting in major efficiency gains. These high temperatures mandate the use of coatings on hot section components (blades and vanes) to protect them from oxidation. Degradation of these protective coatings represents a major profitability challenge for turbine owners. Coating life usually dictates blade 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 blade replacement. Replacement of a conventionally cast alloy blade row can cost up to \$3 million in the case of directionally solidified or single-crystal blades with internal cooling. Unavailability costs can run up to \$500,000 in lost revenues per day for a 500MW combined cycle plant. Blade 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 blades 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 five 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

Task 5. Economic risk-based Decision Analysis

This report summarizes results from the first three tasks. Tasks 4 and 5 have not yet been initiated at this time.

3.0 EXECUTIVE SUMMARY

In Task 1 the primary objective for this year was to audit the 7FA+e 2nd stage bucket design and use the results to evaluate the coating and creep life. By means of the HSLMP, a series of aero-thermal and structural analyses were performed to provide a precise and comprehensive understanding of the temperatures and stress that occur throughout the bucket. The effectiveness of using DS versus EA material was studied, as this is being offered as a potential means for improving the durability of present 2nd row designs. Also noted is that the aero-thermal analysis of the 2nd row bucket design was particularly involved, with several technical challenges that are discussed at length within this report.

Creep was shown to be the predominant life-limiting factor that governs the reliability of 7FA+e 2nd stage buckets. Strong evidence suggests that premature cracking can lead to rupture of the shroud, breakdown the integrity of the continuous linkage, and thereby risk the entire row of buckets. As the firing temperature has been increased from 2350F (1288C) to 2420F (1327C), the original replacement interval prescribed by the OEM has been reduced from 3 HGPI (72,000 hours) to 2 HGPI (48,000 hours). Direct examinations of the shroud indicate the creep limit is approached after 48,000 service hours. As a result, the present design is considered marginal in terms of specified life criteria.

A creep analysis was also performed to compare the use of EA versus DS materials for 2nd stage buckets. The study demonstrated the creep strain to be comparable or lower when using EA material properties versus DS properties at the most critical locations in the shroud. Since creep limits for both the 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 DS alloy. The DS alloy does improve the predicted creep life of the airfoil; however, the principal strain in the shroud is not in the radial direction.

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 design. As a result, it is considered more reasonable to assess the damage by tracking and monitoring the creep life consumed based on a record of the exhaust temperature and other relevant operating parameters. Such projections can be readily produced by means of a damage tracking matrix (DTM) prepared from the results of the HSLMP. As such, a damage tracking record would show running part load for selected periods could prolong the life of the component. Conversely, over-fired operations will significantly reduce the life.

Two governing criteria determine the service life of the 7FAe+ 2nd stage bucket: 1) 2700 factored starts or 2) 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. If an operator is 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. 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. For combined cycle units which operate at base or partial loads, these units will accumulate greater creep damage than peaking or cycling units 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 cycles. Predictions indicate that while this limit is achievable, there is presently little margin in predicted creep life for the buckets, whether they are made of EA or DS materials.

The objective of Task 2 is to develop the capability of COATLIFE to handle spallation life prediction for TBCs that are being used in advanced turbines manufactured by major domestic OEMs and to enhance COATLIFE to cover broader range of MCrAlY coatings for oxidation life prediction. The MCrAlY coating selected for the evaluation was CT102. The chemical composition of the CT102 coating is similar to the nominal chemistry of GE's proprietary coating GT33. Tasks 2.1 and 2.2 deal with the COATLIFE development for TBC and MCrAlY coatings, respectively. The COATLIFE model treats coating degradation mechanisms that are applicable to TBC spallation, and bond coat degradation resulting from loss of aluminum. Isothermal oxidation tests at three temperatures are being conducted on TBC-coated GTD-111 and IN-738 specimens with two different bond coatings.

Isothermal oxidation tests at three different temperatures, 1010°C (1850°F), 1038°C (1900°F), and 1066°C (1950°F) have been initiated on the TBC coated specimens. The specimens have been exposed for 3000 hours at 1010°C (1850°F) and 1038°C (1900°F), and for 2000 hours at 1066°C (1950°F) to date and the specimens revealed no external cracking. Considering the physical degradation mechanisms, a mechanistic model has been developed for predicting remaining service lives of TBCs. The predictive capabilities of the model are demonstrated by comparing the model predictions against the results published in the literature for APS TBCs. Preliminary results indicate that model predictions are in good agreement with the experimental data.

Cyclic oxidation tests on MCrAlY (CT102) coated specimens are conducted to determine the constants for the COATLIFE model. Cyclic oxidation testing at a peak temperature of 1066°C (1950°F) was completed. Testing at a peak temperature of 1010°C (1850°F) has been initiated and specimens have been exposed to 2750 thermal cycles to date. At both temperatures, the specimens initially gained weight due to formation of oxide scale with thermal cycles and then the specimen lost weight due to domination of scale spallation with increasing thermal cycles. The β -phase in the coating provides aluminum for the oxide scale formation. The loss of aluminum due to spallation of oxide scale results in coating degradation. Metallurgical evaluation of the specimens exposed at 1066°C (1950°F) was completed. Volume of fraction of β -phase and aluminum content in the coating were

determined as a function of thermal cycles. Results showed that the β -phase in the CT102 coating was completely consumed after 2000 one-hour thermal cycle exposures at a peak temperature of 1066°C (1950°F). The oxides formed on the specimens were also characterized. The results showed that the oxides formed on the coated specimens after thermal exposure varied with thermal exposure at the peak temperature of 1066°C (1950°F). The oxide scale formed on the coated specimens exposed for up to 1000 thermal cycle was predominantly alumina with dispersion of yttria particles, while the specimens exposed for over 1000 cycles exhibited mixed (aluminum, chromium, and yttrium-rich) oxides.

Considering the formation of chromium, aluminum, and yttrium rich oxides, the constants for COATLIFE were determined based on aluminum depletion in the coating. The calculated volume fraction of the remaining β -phase in the coating was in good agreement with the measured values of the specimens exposed at the peak temperature of 1066°C (1950°F). Using a preliminary set of model constants obtained from cyclic oxidation data for the coated specimens at 1010°C (1850°F), aluminum depletion and volume fraction of β -phase in the coating were calculated as a function of thermal cycles. These model predictions will be verified when the experiments results are available

The objective of Task 3 is to develop inspection technologies sufficient to quantitatively evaluate degradation of degraded coatings. Buckets exhibiting various levels of degradation have been acquired for development of the inspection criteria and for destructive analysis.

A Memorandum of Understanding paper was signed with Korea Western Power to secure as-coated and aged blade buckets in exchange for information obtained on furnished blade samples. EPRI gained access to five blade buckets – one as coated and four aged buckets with varying number of startups and operating hours. The three latest acquisitions represent first stage buckets from 7F frame, PG7211, equipped with GT 33+ duplex coatings. The two blades acquired in 2001 represent first stage buckets from 7FA frame, PG7231, equipped with GT 33+duplex coatings.

The eddy current testing of actual blade samples has demonstrated that it is not only capable of performing reliable testing of removed blade bucket but characterizing the blade condition as: 1) normal, 2) degraded coating due to β -phase depletion, and 3) repairable due to presence of thermal mechanical fatigue cracking.

Individual cutout section can be evaluated using plots of normalized impedance versus frequency or normalized impedance versus chord position. In general, normal coating/substrate conditions present the highest normalized impedance curves with positive slopes, followed next by almost flat curves representing β -phase depletion, and finally curves with negatives slopes representing presence of crazed cracking and possible corrosion of base metal. To review the entire airfoil condition, a colored contour map of normalized impedance values was plotted to show the affected area of interest. For aged blade buckets, more degradation was noted at the leading edge of the blade surface on the suction side.

Using the three-layer inversion program augmented by coating thickness information gathered from destructive sectioning results, it was possible to estimate conductivity values of the topcoat, bond coat, and GTD-111 substrate.

The progress made to date will be used ultimately to generate a calibration curve that would correlate conductivity of coatings to volume fraction of β -phase particles or percent wt. of aluminum remaining in the topcoat. This information will then be used to assess the useful remaining coat life.

4.0 TASK 1 REFINEMENT AND VALIDATION OF HSLMP

In Task 1 the primary objective for this year was to audit the 7FA+e 2nd stage bucket design and use the results to evaluate the coating and creep life. By means of the HSLMP, a series of aero-thermal and structural analyses were performed to provide a precise and comprehensive understanding of the temperatures and stress that occur throughout the bucket. The effectiveness of using DS verses EA material was studied, as this is being offered as a potential means for improving the durability of present 2nd row designs. Also noted is that the aero-thermal analysis of the 2nd row bucket design was particularly involved, with several technical challenges that are discussed at length within this report.

4.1 Experimental

For F-class machines, firing temperature has increased from 2350° F (1288° C) for the 7FA model (PG7221) to 2420° F (1327° C) for the 7FA+e model (PG7241). The output commensurate to this increase in temperature is from 159 MW to 172 MW for a simple cycle machine. Without the addition of new turbine stages, the gas temperature at each rotating stage must naturally increase as a consequence of raising the overall firing temperature. For the 2nd stage, this increase in operating temperatures has produced an unanticipated acceleration of creep damage that is tangibly impacting on the life cycle costs associated with the maintenance and replacement of these parts.

Creep has traditionally been recognized as the primary life limiting mechanism for the 7FA 2nd stage bucket. Although it operates at a lower temperature than the 1st stage, the requirement to have an integral shroud for the purpose of controlling bucket-disk frequencies, and to provide a platform for the sealing strip results in a higher creep strain at this critical region, and thus making the 2nd stage blade more vulnerable to creep rupture. As shown in Table 4.1, the life cycle has already been reduced to two (2) HGPI or 48,000 hours for hours-based 2nd stage buckets.^[1] As such, creep remains the principal issue that is presently setting the serviceable life of these parts, and/or is believed responsible for premature retirement of these buckets.

Table 4.1 Life Cycles Associated with Different (F) Frame Types

	PG7191 (F)	PG7221 (FA)	PG7231 (FA+)	PG7241 (FA+e)
Output	150 MW	159 MW	167.8 MW	171.7 MW
Firing Temp.	2300°F (1260°C)	2350°F (1288°C)	2400°F (1316°C)	2420°F (1327°C)
Pressure Ratio	13.5:1	15.1:1	14.9:1	15.5:1
Exhaust Temp.	1100°F (593°C)	1087°F (586°C)	1101°F (594°C)	1114°F (601°C)
Replace Int. (Starts)	3 (HGPI)	3 (HGPI)	3 (HGPI)	3 (HGPI)
Replace Int. (Hours)	3 (HGPI)	3 (HGPI)	3 (HGPI)	2 (HGPI)
Note: The data presented in Table 1-1 is consolidated from several OEM publications and some discrepancies between the data shown in other publications may be noted.				

4.2 Results and Discussion

Why is this reduction in life cycle occurring despite attempts to modify the design and deal with the increase of temperature? To complete task 1.1, geometric data for a GE 7FA+e 2nd stage blade was collected with sufficient detail to proceed with Task 1.2 and 1.3. Pursuant to Task 1.2 and relying on the HSLMP, a series of aero-thermal and structural analyses were performed on the 7FA+e 2nd stage blade to provide a precise and comprehensive understanding of the temperatures and stress that occur in the airfoil and tip shroud. A converged solution was achieved by iterating between the heat conduction model and external heating/internal-cooling model. Pursuant to Task 1.3, these results were used to evaluate the oxidation and creep life, then contrast these findings against the maintenance interval and replacement criteria specified by the OEM. The effectiveness of using DS verses EA material was also studied.

4.2.1 Features of the Blade Design.

As shown in Figure 4.1, the 2nd row buckets are convectively cooled via nine (9) span-wise radial passages. The original 7FA design has only eight (8) passages. Both designs rely on the same three-hook, axial entry type of root attachment. A complete stage consists of 92 rotating buckets. Similar to the 3rd stage bucket, the 2nd stage relies on an integral shroud to form a continuous ring at full operating speed. Inter-locking between buckets is achieved by taking advantage of the un-twist at the tip that occurs under centrifugal force. The 7FA+e 2nd stage shroud features a deeper “scallop” type of cut-out at both the leading and trailing edges comparing to the original 7FA design. A newer style of “cutter tooth” is also found on the leading side of the seal strip. Based on the limited evidence gathered to date, it is suspected, but cannot be stated with absolute certainty, that an inconsistent variety of shroud designs might be operating in different new machines. This concern may be of particular relevance to operators with LTSAs who have been cautioned to closely monitor efforts by their provider to “test” alternatives designed to extend part life, and thus reduce the cost to perform the contracted program of service. [2] In any case, variations of shroud design play an important role in terms of their effect on the rate of consumption from creep, particularly in elevated gas temperature operating conditions. While this effort primarily focused on the most recent 7FA+e design the possible variety of covers should be considered before applying the results that are presented in this report to a specific unit.

Figure 4.1 – Details of the GE 7FA+e 2nd Stage Bucket That Was Examined

4.2.2 Aero-thermal Analysis Results

Two distinctive features are associated with simulating the flow about and through the 2nd stage bucket; (1) it is an intermediate stage, and (2) it has a cover with cooling holes that pass flow through to mix at the seal. A multiple stage model was therefore prepared to accurately predict the aero-thermal boundary conditions on a stage-by-stage basis through the turbine. Because cooling flow is purposely driven through the cover of the 2nd stage to reduce creep, tip leakage and the flow about the critical region of the cover also required additional special consideration. The hot gas path for a 7FA+e combustion turbine was analyzed with reference to the following ISO conditions: firing temperature of 2,420° F (1,327° C), an exhaust temperature of 1,114° F (601° C), an exhaust flow rate of 3,470,000 lbm/hour (1,580,000 kg/hour), an inlet total pressure of 222 psia (1.531 MPa), and an exit static pressure of 15.1 psia (0.104 MPa). Note that the inlet total pressure was estimated based upon a compressor pressure ratio of 15.5. The exit static pressure was estimated to be greater than the atmospheric pressure of 14.7 psia (0.101 Mpa) to account for the pressure loss appearing at the exhaust duct.

The gas path chosen for the throughflow analysis was principally described by the geometric data in which the gaging distribution (pitch and throat) was specified at hub,

mean, and tip diameters, respectively. The nozzles and buckets of the third stage were excluded from this step, as they are non-cooled. Depending upon the geometric arrangement of cooling holes, a mixing flow may occur along the radial span or near the tip-end region. For the nozzles of the first two stages, mixing occurs along the span only. In contrast to the 2nd stage, the first stage buckets have both tip-end holes to promote flow mixing near the end-walls and also trailing-edge slot holes to promote mixing along the span. The second stage buckets have tip-end holes only. The cooling flow parameters of the buckets were directly obtained from the solution provided by the cooling path flow (CPF) program, a special-purpose CFD program capable of analyzing the flow path of cooling holes. The total mass flow passing through cooling channels was estimated to be about 704,000 lbm/hour (320,000 kg/hour). The assumed cooling flow, about 20.2 percents of exhaust flow, agrees with the open literature, suggesting that the cooling flow in advanced gas turbines could reach as much as 20 percent of the overall mass flow. The radial clearance of seals was assumed as 0.030" (0.076 cm) at operating speed. An empirical equation was built into the program to evaluate the amount the leakage flow. The gross shaft power generated by the combustion turbine was computed to be 330.9 MW (139.1 MW from first stage, 110.1 MW from second stage, and 81.7 MW from last stage). Based on the rated output of 172 MW, the compressor consumes about half of the gross power.

Further examination of these results revealed that the maximum temperature at the turbine inlet could reach as high as 2,700 °F (1482 °C). As a result of the many advanced cooling strategies that have been applied to the FA+ design, the temperature drop across the first stage is substantially greater than that of the other two stages. After the hot gas proceeds from an upstream to downstream location the radial temperature profile becomes flatter. The flattening of the radial temperature profile is mainly attributed to two factors: (1) the fact that the mid-region of the vane is more efficient than the end-wall regions, and (2) the consequence of mixing main gas flow with tip-leakage flow and the cooling flow. To validate and calibrate the multi-stage version of the model, thermocouple rake data from a 7FA machine was used. Three rakes installed at nearly equal intervals around the circumference of the flow field after the last stage each provided measurements at five locations along the radial direction. Rake measurements corresponding to 141 MW and 72 MW loads were used to correlate with results obtained from the analytical models. The turbine exhaust pressure was assumed to be about 14.7 psia (0.101 MPa). When compared, the results from analysis agreed well with the rake measurements.

Following the overall procedure, an external gas flow and heat transfer analysis of the 2nd stage blade was performed based on the aero-thermal boundary conditions generated from the throughflow analysis. It is noted that accurate prediction of the convective heat transfer distribution around high-pressure and transonic gas turbine buckets still represents a significant challenge to the turbo-machinery community. Besides the complexity involved to model the turbulence that may be present, the problem is further complicated by what is referred to as the transition boundary layer, which may develop along the airfoil profile. Presented with these technical considerations, the quasi-3-D rotor viscous program developed by NASA known as RVCQ3D was applied. The program was selected for its ability to solve thin-layer Navier-Stokes equations on a blade-to-blade revolution using an explicit finite-difference technique.^[3,4] The two-dimensional CFD mesh produced to simulate

the flow path at mean diameter section is shown in Figure 4.2. It is a periodic C-type grid and has a mesh size of 60x338. The grid spacing away from the airfoil surface is extremely small, less than 1.5×10^{-4} inch, which is appropriate for an accurate prediction of film coefficients.

Figure 4.2 – CFD Model Developed Using NASA's RVCQ3D Program

An example of the predicted static pressure and Mach number at the hub, mean and tip section are shown in Figure 4.3. The static pressure (on the left) has a unit of psia. At the hub, the largest pressure occurs near the leading edge of pressure-side profile. The smallest pressure arises at two locations: at trailing edge and near the mid-span of suction-side profile. The Mach number distribution (on the right) is essentially opposite to the static pressure distribution. The maximum Mach number of 0.92 indicates that it is a transonic flow field. A distinguished wake region, featured with lower Mach number after the trailing edge, can be observed. Also shown is the variation of film coefficients predicted at the hub section. The stagnation point is located at the pressure side, approximately 0.2 inch away from leading edge. The maximum heat transfer rate, about 0.0009 BTU/in-in-Sec-°F, occurs near the stagnation point, where the thermal boundary layer is still thin. The lowest film coefficient occurs at both the pressure and suction sides near the trailing edge. For the mean section the largest pressure also occurs near the leading edge of pressure-side profile. The smallest pressure mainly occurs in the acceleration region on the suction side. The variation of film coefficients predicted at the mean section show that the stagnation point is located at the pressure side, approximately 0.2 inch away from leading edge. Excluding the trailing edge region in which large disturbances are present and cause the

predicted film coefficients unrealistic, a maximum heat transfer rate of about 0.0008 BTU/in-in-Sec-⁰F occurs near the stagnation point. The lowest film coefficient appears at both pressure and suction sides near the trailing edge. At the tip section, static pressure is shown on the left. The static pressure at the inlet region and for about half of the pressure side region is in the range between 56.8 and 59.0 psia. The smallest pressure mainly occurs in the acceleration region on the suction side. The Mach number distribution was shown on the right. A maximum Mach number of 1.12 indicates that the flow field at the tip section is transonic. A distinguished wake region, with a lower Mach number extending from the trailing edge to the exit boundary, can be observed. The computed film coefficient distribution at the tip profile shows the stagnation point is located at the pressure side, approximately 0.1 inch away from the leading edge. Excluding the trailing edge region in which large disturbances are present and cause the predicted film coefficients unrealistic, a maximum heat transfer rate of about 0.00075 BTU/in-in-Sec-⁰F occurs near the stagnation point. Again, the lowest film coefficient appears at both pressure and suction sides near the trailing edge. In this case, the re-laminarization of boundary layer flow did not produce any evident transition zone. The pressure-side film coefficients, however, were raised to the level close to the film coefficients in the suction side. The film coefficient becomes negligible at about 1.0 inch away from the trailing edge on both suction and pressure side.

Complementing the external flow conditions, according to the properties of gas extracted from the compressor, the inlet total pressure and temperature at all cooling passages were estimated to be 170 psi (1.172 MPa) and 750° F (399° C) under full load operations. The exit static pressure of the cooling passages was obtained from a computational fluid dynamic (CFD) solution of tip leakage flow. The results show its magnitude decreases from about 90 psia for the five upstream cooling holes to about 50 psia for the four downstream cooling holes. The mass flow rate of each passage was calculated in which the numbering sequence referred to in this report starts from leading edge towards trailing edge. The #7 and #8 passages were shown to accommodate the largest mass flow of 0.0178 lbm/sec (0.0081 kg/sec). The mass flow at the last passage, 0.0112 lbm/sec (0.0051 kg/sec), is substantially smaller than the other passages. The total mass flow in the cooling passages is about 0.137 lbm/sec (0.0622 kg/sec) per blade. The gas temperature in cooling passages depends upon (1) mass flow rate, (2) wall temperature, and (3) boundary conditions. In general, the smaller the mass flow rate, the higher the gas temperature. The highest temperature of 1000° F (538°C) occurs at the tip section of last channel.

The mass flow rate in the cooling passage plays an important role in the film coefficient, i.e. the larger the mass flow rate, the larger the film coefficient. As a result of the straight-through configurations of the passages, the film coefficient increases slightly from the hub to tip region. The film coefficients in the airfoil region for all cooling passages range from 0.0012 to 0.00135 BTU/in-in-Sec-F.

Figure 4.3 – Results of the External flow Analysis

The flow in the tip shroud region of the 2nd row bucket is unusually complicated due to the presence of a highly anisotropic flow and presence of a vortex under high rotational speed. The mixing of injected cooling flow at the shroud tip further complicates accurate prediction of these flows. Bearing in mind the role that creep plays in the life of the 2nd row bucket, it is essential to include the evaluation of tip leakage flow in order to obtain a reasonable prediction of the temperature distribution throughout the shroud. Pursuant to this requirement, a 3-D tip leakage flow model including tip cavities and portions of cooling flow channels was constructed using the commercially available code FLUENT. ^[5] Inlet and exit flow boundary conditions were again extracted from the results of the throughflow analysis. To handle the highly compressible flow with a rotational effect, the speed of the rotor was gradually increased to achieve converged solutions at full running speed. The predicted flow vectors and pressure distributions are shown in Figure 4.4. Flow circulations occur at both upstream and downstream cavities. The effect of interaction between ejected cooling flow and main leakage flow are also shown with the resulting heat transfer coefficients on the shroud tip surface. It can be seen that local heating was predicted in the leading edge, suction side of the shroud. This prediction is consistent with field observation that shroud tends to lift on the corner lip for the original 7FA design. It appears that the ejected cooling flow cools more favorably on the pressure side and down stream of the shroud when the gas and cooling flows are fully emerged. The calculated heat transfer coefficients in conjunction with the results obtained from external gas flow and internal cooling flow analyses were then used as the boundary conditions for the subsequent heat conduction analysis to predict metal temperature distributions throughout the blade.

Figure 4.4 – Results of the Tip Flow Leakage Analysis

4.2.3 Results of the Thermal and Stress Analysis.

The predicted temperatures previously described were applied as body forces to calculate thermal stress throughout the blade. The main source of thermal stress is from temperature gradients. The gradient across the wall in the airfoil is about 300°F (149°C). To calculate actual metal temperature distributions throughout the 2nd row bucket, a heat conduction finite element model was constructed using ANSYS. [6] All relevant structural features such as the nine cooling channels, the root, the shank, and the integral shroud were included in the model. Temperature dependent thermal properties for GTD111-DS material derived from EPRI's MCCT were used. Surface elements were identified to apply convective thermal loads that resulted from the external gas flow, internal cooling flow and tip leakage flow analyses previously described. The calculated surface temperatures were, in turn, applied as wall temperatures to the flow and heat transfer models. Solutions between the heat conduction and cooling flow were then iterated until converged solutions were obtained. While numerically intensive, this procedure accounts the interaction between the solid and fluid and thereby provides a more accurate prediction of metal temperature.

The steady state temperature distribution resulting for the 7FA+e 2nd stage blade is shown in Figure 4.5. A maximum operating temperature of 1658° F (903° C) was predicted to occur at the trailing edge near the mid-span. As the span-wise cooling does not extend all the way to the trailing edge, the resulting localized temperature at the trailing edge is approximate to the flow temperature. This indicates that the coating oxidation life of the 2nd row bucket blade would become of greater concern for the 7FA+e machine where the firing temperature is increased, thereby causing a corresponding increase in the stage flow temperature. Nevertheless, as will be discussed, the resulting stress remains low at the trailing edge despite the high metal temperature.

Figure 4.5 – Results of the Thermal Analysis

At the leading edge, a maximum temperature was calculated to be approximately 1420° F (771° C), occurring near the mid-span. The lower magnitude of metal temperature at this location is attributed to sufficient cooling and relatively low external heat transfer coefficients at the leading edge. The temperature inside the cooling hole near the mid-span was calculated to be approximately 1100° F (593° C), or substantially lower than the temperatures on the external surfaces. Although the temperature gradients through the walls are considerable, the resulting thermal stress is expected to be less severe than that for a bucket that relies on thin wall serpentine cooling, e.g. the 1st row buckets. At the shroud tip, the maximum temperature was predicted to be about 1620° F (882° C), occurring in the lip region at the leading edge, suction side. This high temperature was primarily due to the local heating resulted from tip leakage flow. At the notch of the Z-cut and the fillet underneath the shroud on the suction side, a lower, but still considerably high temperature near 1500° F (816° C) was predicted. High temperatures at these locations are critical because of the concern over creep.

As shown in Figure 4.6, compressive radial stresses ranging from 20 ksi (138 Mpa) to 50 ksi (345 Mpa) were predicted at the external surfaces of the airfoil. In the airfoil, a maximum tensile around 50 ksi (345 Mpa) was calculated in the 1st cooling hole near 25% bucket height. Compared to the 1st row bucket, these stress levels appear to be more moderate. This is considered to be a result of smaller temperature gradients and the thicker walls around the span wise cooling passages. At external surfaces of the airfoil, the tensile stress generated from mechanical loads offsets the compressive stress that resulted from thermal load. A maximum stress near 60 ksi (414 Mpa) was calculated at the outer surface near the trailing cooling hole. This stress level does not appear to be excessive since temperatures at this location are relative low, e.g. in the neighborhood of 1250°F (677° C). At the internal surface, the maximum tensile stress was computed to be around 85 ksi (586 Mpa) in the leading cooling hole near the hub. This stress is also of less concern due to the lower temperatures that are involved. An equivalent stress of 50 ksi (345 Mpa) was calculated in the fillet region near the juncture between suction side of the airfoil and tip shroud. This is the location susceptible to creep crack initiation, which leads to final rupture of the shroud. Stress was primarily attributed to the bending of the shroud segment. Compared to older designs, stresses at this location appear to be lower as result of using a larger fillet radii and reducing the amount of shroud material. The issue remains as to whether the reduction of stress is sufficient to alleviate the increase of gas temperature in terms of achieving the desired design life. A comparable stress of 50~60 ksi (345~414 Mpa) was computed at the notch of the Z-cut region which also makes this location vulnerable to creep damage.

Figure 4.6 – Results of the Stress Analysis

In regards to the shroud gap, a limit of 0.03" (0.76 mm) gap size is presently recommended by the OEM as a guideline for inspecting these rows. To verify this limit, a series of sensitivity studies were performed. The effects of thermal growth, bending and untwist were all taken into account by imposing thermal load, gas bending force and centrifugal load on the non-linear finite element model. A multiple load step procedure was used by gradually increasing the loading and operating speed. Starting with a nominal gap size of 0.006" (0.15 mm), the study revealed that an initial gap at 0 rpm will close at approximately 1,600 rpm. The elastic deformation of the shroud is shown in Figure 4.7. To assess the allowable tolerances between contact surfaces, the gap was incrementally enlarged until it remained open at full operating speed. The maximum allowable gap at 0 rpm was predicted to be about 0.025" (0.64 mm). These results are consistent with the criteria of 0.03" (0.76 mm) prescribed by OEM, and infer the applied loads and forces are in accordance with what the designer expected.

Figure 4.7 – Predicted Deformation of the Blade Shroud

4.2.4 Coating and Creep Life

Coating oxidation life is a function of the cycle time and the temperature of the metal. A coating life assessment was performed based on the COATLIFE-3 formulation developed by EPRI/SWRI, assuming GT29+ was used as the coating.^[7] At a maximum temperature of 1658° F (903° C) and a representative 120-hour cycle time, the coating oxidation life was estimated to be approximately 64,000 hours or 535 cycles. Since the OEM presently recommends a 48,000 hours interval to repair or replace the coating, the predicted results appear to be consistent and somewhat less conservative than this requirement.

Nevertheless, it should be noted that the actual coating life is strongly dependent upon the type of operations that are accumulated between maintenance overhauls. Part-load operations tend to increase coating life. Conversely, over-firing could significantly reduce the coating life. This variability recommends to operators that in order to accurately assess coating damage it should be tracked and the projected life periodically updated at each start-stop cycle. A separate study was made by applying a hypothetical GT29 coating on a GTD-111 DS base material. The coating life was dramatically reduced to 20,000 hours where the operating temperatures remained constant. It is apparent that the aluminum provided by the topcoat of aluminide for GT29+ contributes significantly to the integrity of

coating oxidation life. Results of this study however, raises a concern of adopting GT33 over GT29+, as presently recommended by OEM.

A creep analysis was performed by applying relevant thermal and mechanical loadings to the 2nd row bucket model. A separate creep damage assessment using GTD-111 EA material was undertaken to compare the creep damage between DS and EA materials. The purpose of this study was to evaluate the potential effectiveness of using more advanced directionally solidified materials in terms of improving the present life cycle. The same aero-thermal procedures were applied to calculate metal temperatures throughout the component. Identical thermal boundary conditions derived for DS material were assumed. In essence the study featured the difference in material response when subjected to the same thermal and mechanical loadings. The predicted creep strain distributions after 48,000 hours (two HGPI intervals) are shown in Figure 4.8. For the DS material it is apparent that a peak creep strain in the airfoil of about 0.2% occurs at the trailing edge. The lighter shroud has a positive influence by lowering the creep strain. The accumulated creep damage in the airfoil appears to be tolerable and within the proscribed creep limit. At the fillet near the airfoil-shroud juncture, a maximum creep strain near 0.5% was predicted on the suction side. In the earlier shrouds where more material was present, this location was susceptible to creep cracking, promoted by a significant problem of lifting for the shroud. In the notch region near the shroud contact surface, the peak creep strain was predicted to be in the range 0.7%~1%. This strain level is approaching the creep limit of 1.1%. As such, the potential for a crack initiation after 48,000 hours is appreciable. It is likely that the newer style of cutter tooth at the leading edge of seal might have an adverse effect on the strain levels in the notch region. As a result of designing a stiffer shroud with less material, the phenomenon of local shroud lifting is expected to be less visible than for the original design.

For the EA material, in the airfoil, a maximum creep strain of about 0.2% was predicted at the trailing edge near the tip shroud. This strain level is comparable to the results obtained using DS material. The DS material would provide longer creep life with slightly superior creep properties in DS directions. Nevertheless, a sufficient margin already exists for the EA material and benefits provided from DS material appear to be somewhat redundant. At the fillet near the airfoil-shroud juncture, the peak creep strain was calculated to be about 0.5%. Again, this strain level is close to the value generated from DS material. In the shroud notch, a lower creep strain of 0.5%~0.8% compared to DS material was predicted on the suction side. This apparent anomaly is significant since the notable strain level in the notch area may become most critical for the scalloped shroud which has the lip removed on the leading edge. A small amount of shroud lifting on the suction side with a negligible amount of tilting is expected for the bucket made from EA material. The notable shroud lifting observed for the less scalloped shroud is not expected to occur for the new shroud profile. It is worth noting that the tilt of the shroud tip becomes notable for the 2nd row bucket when DS material is involved.

Similar to DS material, the creep damage in the shroud is more pronounced and the strain is predominantly in the transverse direction. Since creep limits for both EA and DS material in the transverse direction are essentially the same, it seems that damage in the shroud fillet and notch will not be significantly affected using the more expensive alloy. Although using a

DS material does improve the predicted creep life of the airfoil, the principal stress acting on the shroud and driving the creep damage is not in the radial direction. Effectively, the benefits of the DS material are negated by the geometry of this critical location.

Figure 4.8 – Results of the Creep Life Analysis

4.3 Conclusions—Task 1—Refinement and Validation of HSLMP.

1. *What is the most notable finding from the design audit of the 2nd stage blade?*

Creep is the predominant life-limiting factor that governs the reliability of the 2nd 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 2350° 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 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 also 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 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 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?

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 (2700 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 7FAe+ 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 900 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.

4.4 References

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5.0 TASK 2: COATLIFE FOR ADVANCED METALLIC COATINGS AND TBCs

The objective of Task 2 is to develop the capability of COATLIFE to handle spallation life prediction for TBCs that are being used in advanced turbines manufactured by major domestic OEMs and to enhance COATLIFE to cover broader range of MCrAlY coatings for oxidation life prediction. Cyclic oxidation and isothermal exposure tests are being conducted on TBC-coated and MCrAlY-coated specimens to generate constants for the COATLIFE model. The exposed specimens are being characterized by optical and scanning electron microscopy to determine the changes in microstructure, thermally grown oxide thickness at the bond coating/TBC interface, and aluminum content and volume fraction of β -phase in the bond coatings. These results are required to validate the COATLIFE model.

The MCrAlY coating selected for the evaluation was CT102. The chemical composition of the CT102 coating is similar to the nominal chemistry of GE's proprietary coating GT33. Tasks 2.1 and 2.2 deal with the COATLIFE development for TBC and MCrAlY coatings, respectively. The COATLIFE model treats coating degradation mechanisms that are applicable to TBC spallation, and bond coat degradation resulting from loss of aluminum. Isothermal oxidation tests at three temperatures are being conducted on TBC-coated GTD-111 and IN-738 specimens with two different bond coatings.

Isothermal oxidation tests at three different temperatures, 1010°C (1850°F), 1038°C (1900°F), and 1066°C (1950°F) have been initiated on the TBC coated specimens. The specimens have been exposed for 3000 hours at 1010°C (1850°F) and 1038°C (1900°F), and for 2000 hours at 1066°C (1950°F) to date and the specimens revealed no external cracking. Considering the physical degradation mechanisms, a mechanistic model has been developed for predicting remaining service lives of TBCs. The predictive capabilities of the model are demonstrated by comparing the model predictions against the results published in the literature for APS TBCs. Preliminary results indicate that model predictions are in good agreement with the experimental data.

Cyclic oxidation tests on MCrAlY (CT102) coated specimens are conducted to determine the constants for the COATLIFE model. Cyclic oxidation testing at a peak temperature of 1066°C (1950°F) was completed. Testing at a peak temperature of 1010°C (1850°F) has been initiated and specimens have been exposed to 2750 thermal cycles to date. At both temperatures, the specimens initially gained weight due to formation of oxide scale with thermal cycles and then the specimen lost weight due to domination of scale spallation with increasing thermal cycles. The β -phase in the coating provides aluminum for the oxide scale formation. The loss of aluminum due to spallation of oxide scale results in coating degradation. Metallurgical evaluation of the specimens exposed at 1066°C (1950°F) was completed. Volume of fraction of β -phase and aluminum content in the coating were determined as a function of thermal cycles. Results showed that the β -phase in the CT102 coating was completely consumed after 2000 one-hour thermal cycle exposures at a peak temperature of 1066°C (1950°F). The oxides formed on the specimens were also

characterized. The results showed that the oxides formed on the coated specimens after thermal exposure varied with thermal exposure at the peak temperature of 1066°C (1950°F). The oxide scale formed on the coated specimens exposed for up to 1000 thermal cycle was predominantly alumina with dispersion of yttria particles, while the specimens exposed for over 1000 cycles exhibited mixed (aluminum, chromium, and yttrium-rich) oxides.

Considering the formation of chromium, aluminum, and yttrium rich oxides, the constants for COATLIFE were determined based on aluminum depletion in the coating. The calculated volume fraction of the remaining β -phase in the coating was in good agreement with the measured values of the specimens exposed at the peak temperature of 1066°C (1950°F). Using a preliminary set of model constants obtained from cyclic oxidation data for the coated specimens at 1010°C (1850°F), aluminum depletion and volume fraction of β -phase in the coating were calculated as a function of thermal cycles. These model predictions will be verified when the experiments results are available

5.1 Thermal Barrier Coatings—Task 2.1

Materials and Coatings--Experimental

Three shank sections of GTD-111 DS blades and three shank sections of IN-738 blades retired from Frame 5002 engines were procured for machining test coupons. The shank sections operate at a much lower temperature than the airfoil section of a blade and, as a result, the material at the shank section is not expected to degrade during service. The structure and properties of the material at the shank section represent the initial, as heat-treated condition.

Compositional measurements were made at selected locations on the GTD-111 and IN-738 shank sections using energy dispersive X-ray spectroscopy (EDS). The composition of blade materials is given in Table 5.1.

Table 5.1. Chemical composition of GTD-111 and IN-738 test materials (wt. %).

Blade	Al	Ti	Cr	Co	Mo	Nb	Ta	W	Ni
GTD-111	3.2	5.2	14.4	9.2	2.1	—	4.0	3.2	Bal
IN-738	3.8	3.6	16.0	8.3	1.9	1.1	2.1	2.2	Bal

About 150 cylindrical specimens (0.36 inch diameter and 1.5 inches long) were removed from the GTD-111 and IN-738 blade shank sections using an electro-discharge machining process. The specimens were ground and polished to remove the recast layer and Turbine Airfoils, Coatings, and Repairs (TACR) applied bond and yttria stabilized zirconia coatings. A low-pressure plasma spray process (LPPS) was used to apply CT102 coating to all specimens. The composition of the powder is given in Table 5.2. The composition of the powder is comparable to the nominal chemistry of GE's proprietary coating GT33. After application of CT102 all specimens were given a vacuum diffusion treatment at 1121°C

(2050°F) for two hours. After the diffusion heat treatment, an approximately 10 μm thick layer of platinum was applied by electroplating on half of the specimens. Siemens Westinghouse uses the platinum-plated CT102 as a bond coating for TBC-coated parts of advanced turbines. Following electroplating, the specimens were given a vacuum diffusion heat treatment at 1121°C (2050°F) for two hours. All CT102 and platinum-plated CT102 specimens were then given an aging treatment at 843°C (1550°F) for 24 hours prior to the application of the top ceramic coating, a standard yttria stabilized zirconia, by using an air plasma spray (APS) process. The chemical composition of the ceramic coating powder is given in Table 5.3.

Table 5.2. Chemical composition of CT102 bond coating powder (wt%).

Al	Co	Cr	Ni	Y
8.0	Balance	21.0	32.0	0.5

Table 5.3. Chemical composition of ceramic coating powder (wt.%).

Al_2O_3	Fe_2O_3	SiO_2	TiO_2	Y_2O_3	HfO_2	ZrO_2
0.13	0.02	0.27	0.09	7.69	1.85	Balance

Isothermal Tests

Multiple TBC-coated GTD-111 and IN-738 specimens with two different bond coatings are being aged in three different furnaces, which are maintained at three temperatures: 1010°C (1850°F), 1038°C (1900°F), and 1066°C (1950°F). One specimen from each substrate/coating system is being removed at predetermined intervals for metallurgical evaluation.

5.2 Results and Discussion

Microstructure of As Coated Specimens

Typical microstructures of the bond and ceramic coatings in the as-deposited condition are presented in Figures 5.1 through 5.4. Among all the specimens, thickness of the CT102 bond coating varied approximately 130 μm (0.005 inch) to 155 μm (0.006 inch). A thin layer of platinum is evident in Figures 5.3 and 5.4 between the CT102 and top ceramic (TBC) coating. The bond coating on all specimens was dense and exhibited a microstructure consisting of fine β -phase (NiAl) in a matrix of γ (Ni-Cr-Co solid solution). The outer surface of the bond coating on all specimens exhibited irregular surface topography, which is normal for the LPPS processed coatings. The rough surface is required for the adhesion of air plasma TBC. The thickness of the TBC on the specimens varied from about 250 to 375 μm (0.010 to 0.015 inch). Examination of the bond coating/TBC interface revealed a thin and discontinuous thermally-grown oxide (TGO) scale in isolated areas and a few micro-delamination cracks at the interface. These cracks were parallel to the interface and were

about 50 μ m (0.002 inch) long. Propagation of these cracks during isothermal or thermal cycling exposure could lead to TBC spallation. The microstructure of the TBC exhibited a layered structure resulting from the splatting of the powder particles during the deposition process. The pores/voids and microcracks were seen in the coating, which is normal for an APS-processed TBC.

Isothermal Exposure Testing

Isothermal exposure testing of the TBC-coated specimens at 1010°C (1850°F), 1038°C (1900°F), and 1066°C (1950°F) has been initiated. The specimens have been exposed for 3000 hours at 1010°C (1850°F) and 1038°C (1900°F), and for 2000 hours at 1066°C (1950°F) to date. Examination of these specimens revealed no external cracking.

Figure 5.1. Optical micrographs of as-coated TBC on an MCrAlY-coated GTD-111 specimen. The arrows point to delamination cracks at the TBC/bond coat interface.

Figure 5.2. Optical micrographs of as-coated TBC on an MCrAlY-coated IN-738 specimen. Arrows point to delamination cracks at the TBC/bond coat interface.

Figure 5.3. Optical micrographs of as-coated TBC on a platinum-plated MCrAlY coating on a GTD-111 specimen. Arrows point to delamination cracks.

Figure 5.4. Optical micrographs of as-coated TBC on a platinum-plated MCrAlY coating on an IN-738 specimen. Arrows point to delamination cracks at the TBC/bond coat interface.

MICROSTRUCTURE OF EXPOSED SPECIMENS

Metallurgical mounts were prepared from the samples removed after 500, 1000, 1500, and 2000 hours exposure at 1066°C (1950°F), after 1000 and 2000 hours exposure at 1038°C (1900°F), and after 2000 hours exposure at 1010°C (1850°F). Metallurgical evaluation of these samples for coating degradation and cracking at the TGO/TBC interface is in progress. The preliminary results showed that the TGO scale thickness increased with exposure time and temperature. The extent of delamination cracking at the TGO/TBC interface also increased with the exposure temperature and time.

TBC Life Algorithm Development

A life prediction model for 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. Figure 5.5 summarizes the essential features of this TBC lifing model, which is called TBCLIFE.

Figure 5.5. 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 [8, 9]. The TBC life expression is given by

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

where $\Delta\sigma_{TBC}$ is the stress range in the TBC, N_f is the number of start-ups (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 [10]

$$\Delta\sigma_{TBC}N_f^b = \sigma_{TBC}^*(t) \left[1 - \left(\frac{\delta}{\delta^*} \right)^c \right] \quad (2)$$

where δ and δ^* are the thickness and the critical thickness of the TGO formed between the TBC and the bond coat. TGO formation was described in terms of a parabolic oxidation process, while the TBC strength was treated via a time-dependent degradation process. Details of the TBC model are described elsewhere [3].

Parametric Calculations

A parametric study of the various key variables in the TBC model has been performed, using air-plasma-sprayed (APS) TBC and MCrAlY bond coat as an example. The effect of the peak temperature on TBC life is illustrated in Figure 5.6, 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 5.7. TBC sintering leads to a decrease of the TBC life, Figure 5.8. 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 5.9.

Figure 5.6. Effects of peak temperature on the calculated TBC life.

Figure 5.7. Effect of TBC fatigue strength coefficient on the calculated TBC life.

Figure 5.8. Effect of sintering and time-dependent fatigue strength on the TBC life.

Figure 5.9. Effect of substrate curvature on the calculated TBC life.

TBC Life

Model constants in the TBC life model include physical, thermal, low-cycle fatigue, and oxidation properties. These material constants are to be determined in an on-going experimental program that will generate the isothermal and cyclic oxidation data for an APS TBC. A preliminary 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 5.10. The experimental results shown in Figure 5.10 are burner-rig and furnace data at which the TBC spalled; these data are from the literature [11].

Efforts to evaluate and validate the TBC life model using laboratory and field data are currently underway in this program. The experimental effort involves performing both static oxidation tests at various temperatures and cyclic oxidation tests at various temperature ranges using one-hour cycles.

Figure 5.10. Calculated TBC life compared against burner-rig and furnace data of APS TBCs from the HOST program [11].

The TBC life model was also applied to EB-PVD TBC and compared to experimental data in the literature [5-7]. The calculated curves for oxidation-induced failure were obtained on the basis of a critical oxide thickness criterion of 6–8.5 μm [12]. 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 cycle). The horizontal band represented by two dashed lines in Figure 5.11 corresponds to 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 5.8 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 5.11. Computed TBC life diagram for EB-PVD TBCs compared against experimental data from Tolpygo et al. [12], Leyens et al. [13], and Jordan and Gell [14].

5.3 Conclusions—Thermal Barrier Coatings

- The degradation mechanisms TBCs are identified. The degradation of TBCs is manifested by formation and growth of a TGO scale at the bond coat/TBC interface and nucleation and propagation of microcracks within the TBC, TGO, and in the vicinity of TGO/bond coat interface (delamination cracks). Both oxidation and propagation of cracks can lead to TBC spallation.
- Considering the mechanisms, a mechanistic model has been developed for predicting remaining service lives of TBCs. The predictive capabilities of the model are demonstrated by comparing the model predictions against the results published in the literature for APS TBCs. Preliminary results indicate that model predictions are in good agreement with the experimental data.

5.4 MCrAlY Coating—Task 2.2

Materials and Coatings

Flat rectangular specimens were machined from the shank sections of GTD-111 buckets using an electro-discharge machining process for thermal cycling tests at two peak temperatures. TACR applied CT102 coating on these specimens using an LPPS process. The composition of the coating powder is given in Table 5.2. Since the composition of CT 102 powder is similar to GT 33 coating, the coating hereafter referred to as GT 33. Following application of the coating, the specimens were given a vacuum diffusion heat treatment at 1121°C (2050°F) for two hours and an aging treatment at 843°C (1550°F) for 24 hours.

Metallurgical Evaluation of Coated Specimens

For metallographic evaluation, a transverse section was removed from coated specimens. The sections were mounted in a conductive mounting media, polished using standard metallographic techniques, and examined under optical and scanning electron microscopes to characterize the coating structure and to determine the chemical composition of the coating either in the as-coated or post-exposed condition. The composition of the phases in the coating was determined using Energy Dispersive Spectroscopy (EDS) and the volume fraction of β -phase in the coating was determined using quantitative metallographic techniques.

Cyclic Oxidation

Cyclic oxidation tests were conducted using a facility designed and fabricated at Southwest Research Institute. The test facility consists of a furnace, a forced air cooling system, a coated superalloy frame for suspending test specimens, and a computer controlled moving arm that transfers specimens in and out of the furnace and to the cooling system. For cyclic oxidation testing, coated specimens were inserted into the furnace, which was maintained at a desired peak temperature and held at that temperature for 55 minutes prior to moving them into the cooling system. Following cooling the specimens for 5 minutes, to room temperature, the specimens were then reinserted back into the furnace. The tests were interrupted at predetermined intervals to weigh the specimens.

5.5 Results and Discussions

Cyclic Oxidation

Cyclic oxidation testing at a peak temperature of 1066°C (1950°F) was completed. Cyclic oxidation testing on the coated specimens at a lower peak temperature of 1850°F has been initiated and the specimens have been exposed for 2750 one-hour thermal cycles to date. The weight change results are presented as a function of thermal cycles in the section “Coating Algorithm Development.”

Microstructure of Exposed Specimens

Metallurgical mounts were prepared from all the specimens exposed at the peak temperature, 1066°C (1950°F) and the mounts were examined in optical and scanning electron microscopes for coating degradation. Typical microstructure of the coating as a function of thermal cycles is shown in Figure 5.12. The results showed that the β -phase in the coating decreased with increasing thermal cycles and the β -phase was almost consumed after exposure to 2000 thermal cycles. The volume of fraction of the β -phase and the aluminum content in the coating were determined in all exposed specimens. The results are presented Table 5.4. These results are used to develop COATLIFE algorithm.

FIGURE 5.12. Microstructure of GT 33 coated GTD-111 specimens showing variation of β -phase in the coating as a function of thermal cycles between room temperature and 1066°C (1950°F): (A) after 280 cycles, (B) after 1000 cycle, (C) after 1500 cycles, and (D) after 2000 cycles.

TABLE 5.4. Average aluminum content and volume of fraction of the β -phase in the coating as a function of thermal cycles between room temperature and 1950°F (1066°C).

SPECIMEN	CYCLES	AVERAGE AL CONTENT		AVERAGE β -PHASE IN THE COATING, %
		WT%	AT%	
X-17	0	8.3	16.1	40
X-17-6	280	8.0	15.5	38
X-17-1	600	7.3	14.3	32
X-17-4	1000	6.7	13.3	18
X-17-5	1500	5.3	10.7	8
X-17-2	1750	5.1	10.3	7
X-17-3	2000	4.7	9.6	1.1

COATING LIFE ALGORITHM DEVELOPMENT

Model constants in the coating life model, COATLIFE, were obtained for GT33 coating on a GTD-111 substrate. The model constants evaluated include the parabolic oxidation kinetics and the oxide spallation constants. 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 the results were 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 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 1066°C (1950°F). Weight change data were not used because of the formation of mixed oxides (alumina, yttria, and chromia) in GT33 after 1000 cycles, making model constants derived from the weight change data unreliable for predicting Al depletion.

Figure 5.13 shows a comparison of the model fitting and measured Al content of GT33 as a function of thermal cycles. The computed β -depletion and weight change curves are compared against experimental data in Figures 5.14 and 5.15, respectively. Figure 5.15 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 scale formed on the specimens that were exposed for up to 1000 cycles showed that it is mostly alumina with Y-rich oxide particles. Typical morphology of the oxide scale on the coated specimens and EDS spectra after 600 thermal cycles are shown in Figure 5.16. Figure 5.16 (a) shows an SEM micrograph of the oxide scale, while Figure 5.16 (b) presents EDS results that indicate 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 5.17 shows the mixed oxides and Figure 5.18 (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 5.15 was based on the formation and spallation of alumina. As a result, the model predicted less weight change than those 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 those due to other elements (such as Cr, Ni, and Y, etc., are ignored.

Figure 5.13. 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 one-hour thermal cycles between 25°C (77°F) and 1066°C (1950°F). The model was fitted to the experimental data of Al content.

Figure 5.14. A comparison of computed and measured volume percent β in GT33-coated GTD-111 specimens as a function of thermal cycles. The coated specimens were subjected to one-hour thermal cycles between 25°C (77°F) and 1066°C (1950°F).

Figure 5.15. 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 one-hour thermal cycles between 25°C (77°F) and 1066°C (1950°F). Arrows indicate specimens whose oxide-scale compositions were determined using the EDS technique.

(a)

(b)

Figure 5.16. Microstructure and composition of the oxide scale formed on GT33 after 600 one-hour cycles between 25°C (77°F) and 1066°C (1950°F): (a) alumina scale with dispersed Y_2O_3 , and (b) EDS result shows a high Al content in the outer oxide scale (Location A).

Figure 5.17. SEM micrograph shows three oxides (A, C, and Y) are present in the scale formed on GT33 after 1500 one-hour thermal cycles between 25°C (77°F) and 1066°C (1950°F).

Figure 5.18. Composites of oxides A, C, and Y: (a) Al-rich oxide A, (b) Cr-rich oxide C, and (c) yttrium-rich oxide Y.

A preliminary set of model constants for GT33 at 1010°C (1850°F) was obtained based on cyclic oxidation data generated so far in this program. Figure 5.19 shows a comparison of the computed and observed weight change curves. Again, the observed weight changes are larger than those predicted by the model after 1000-2000 thermal cycles. The discrepancy might indicate the formation of mixed oxides on GT33 and needs to be resolved by future EDS analyses of oxide compositions. The predicted Al depletion and volume fraction of β -phase as a function of thermal cycles are presented in Figures 5.20 and 5.21, respectively. These model predictions will be verified by the corresponding experimental data that are currently being generated.

Figure 5.19. Comparison of computed and measured weight changes data of GT33 at 1010°C (1850°F).

Figure 5.20. Predicted Al depletion for GT33 at 1010°C (1850°F).

Figure 5.21. Predicted β-phase depletion for GT33 at 1010°C (1850°F).

5.6 Conclusions—Task 3—Metallic Coatings

- *Microstructure of the NiCoCrAlY (GT 33) coating exhibited a duplex microstructure consisting of β -phase in a matrix of γ .*
- The β -phase in the coating was completely consumed after 2000 one-hour thermal cycles exposure at the peak temperature of 1066°C (1950°F).
- The oxides formed on the coated specimens after thermal exposure varied with thermal exposure at the peak temperature of 1066°C (1950°F). The oxide scales formed on the coated specimens exposed for up to 1000 thermal cycle was predominantly alumina with dispersion of yttria particles. The specimens exposed for over 1000 cycles exhibited mixed (aluminum , chromium and yttrium-rich) oxides.
- Considering the formation of chromium, aluminum, and yttrium rich oxides, the constants for COATLIFE were determined based on aluminum depletion in the coating.
- The calculated volume fraction of the remaining β -phase in the coating was in good agreement with the measured values of the specimens exposed at the peak temperature of 1066°C (1950°F).
- Using a preliminary set of model constants obtained from cyclic oxidation data for the (GT33 at 1010°C (1850°F), aluminum depletion and volume fraction of β -phase in the coating were calculated as a function of thermal cycles. These model predictions will be verified when the experiments results are available

5.7 References for Task 2.1

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6.0 TASK 3. NDE OF COATINGS

6.1 First Stage Blade Buckets with GT 33+ Coatings

During the third quarter of the reporting year, three first stage blade buckets from GE Frame 7211F engines were acquired from Korea Western Power. They consisted of one as coated (Blade 37) and two aged blade buckets (Blades 07 and 57). Since these buckets were acquired late, no significant work was accomplished with these samples. One exception is that Blade 37 was used to estimate the duplex coating and substrate conductivity values.

Two similarly aged first stage blades (Blades 5 and 9) from 7FA+ engines (Frame PG7231) acquired in 2001 were used primarily for coating characterization with one of the two purchased eddy current systems.

Table 6-1 outlines the operating histories of the acquired blade buckets.

Table 6-1. Operating Histories of First Stage Blade Buckets

Blade Number	Description	Operating Hrs	No. Startups	EOH*
5	7FA+ –Aged	6,156	272	11,596
9	7FA+ – Aged	6,156	272	11,596
	7F – Aged	8,286	670	21,686
37	7F – Recoated	0/8,113	0/546	0/19,033**
57	7F-Aged & Recoated	2,000/14,795	219/518	6,380/25,155***

* Denotes “Effective Operating Hours” = Operating Hrs + (20 x No. Startups)

** Before recoating, it logged 19,033 EOH but no additional hours after recoating.

*** It logged 25,155 EOH initially and additional 6,380 EOF after recoating.

GE’s frame 7F/7FA turbine designs utilized directionally solidified (DS) GTD-111 blades. The coatings used for these advanced GE engines include CoCrAlY (GT 29) and NiCoCrAlY (GT 33) bond coats followed by top aluminide overlay coatings. These duplex metallic coatings are commonly referred to as GT 29+ and GT 33+ coatings, respectively. Chemical composition of the GT 33+ coatings as determined by energy dispersive x-ray spectroscopy (EDS) is shown in Table 6-2.

Table 6-2.

Primary Chemical Composition of GT 33+ Coatings, Wt. %

GT 33+	AL	Cr	Co	Ni	Y
Topcoat	24.2	6.0	35.7	33.6	
NiCoCrAlY	10.5	18.6	32.6	37.2	0.4

The nickel-based GTD-111 substrate has among other things 3.09 wt.% aluminum. For eddy current NDE to work effectively, the individual layer conductivity values must be different not only from each other but also from the GTD-111 substrate. In general, a 10 percent or more change in the electrical conductivity can be detected reliably. Since aluminum is highly conductive, its presence or absence will affect the overall conductivity of individual layers. Based on the ideal breakdown of aluminum content shown by each layer, the eddy current technique is ideally suited to evaluate the conductivity of each layer, especially the top overlay coat.

6.1.1 7FA 1st Stage Bucket - Blade 5

Figure 6.1 shows the grid pattern and three respective transverse samples, A', B, and E, which were saved for eddy current testing. The remaining samples, A, C, D, and F, were destructively sectioned and individual coating layer thickness measured. This was done to establish some known input parameters so that the number of unknown variables could be reduced to solve for conductivity values that are associated with the top overlay coat, the bond coat, and the GTD-111 substrate.

Figure 6-1. Physical Appearance of 7FA+ First Stage Blade 5 and Locations of Individual Cutout Transverse Sections

6.1.2 Destructive Sectioning of Blade 5

Destructive sectioning and subsequent microscopic measurements of blade samples A1 through A6 and C1 through C6 provided information about sample B. Similarly, destructive sectioning and subsequent microscopic measurements of blade samples D1 through D6 and F1 through F6 provided information about sample E. The following steps were taken to obtain values of three different coating layers shown in Figure 6-2.

- Average of 10 measurements per grid sample for each of three layers
- Three coating zones identified as “X”, “Y”, and “Z” from top to bottom
- Optical microscopic measurements done at 200X magnification after using CuSO_4/HCL etchant

Within both “X” and “Y” layers, the main interests are those dark gray areas representing β -phase particles. Top two layers supply aluminum from these β -phase particles to form a protective alumina layer, which is nonconducting oxide scales. Loss of alumina scales result from the continuous oxidation process of the coating and the subsequent spalling off of these protective scales. As long as aluminum is present, it will continue to form the protective oxide scales. It has been reported that the critical aluminum content for an aluminide overlay coating is around 9.5 wt.% from the initial condition of 24.2 wt. %. This is equivalent to reduction of volume fraction of β -phase particles from 100% to approximately 39%. The inter-diffusion layer, Z, thickness is associated directly to exposed metal temperature - the width of Z increases with higher metal temperature. The current technology, however, is not able to reliably quantify the diffusion layer thickness due to the number of variables which already exist for duplex metallic coatings.

Figure 6-2. Cross Sectional Measurements of Individual Coating Layer Thickness by Optical Microscope at 200X

Table 6-3 shows the averaged grid sample readings for each of three layers from various grid locations shown in Figure 6-1.

Table 6-3. Averaged Measurements in Microns

<u>Grid Position</u>	<u>Outer Layer</u> “X”	<u>Second Layer</u> “Y”	<u>Inner Layer</u> “Z”	<u>Total Coating</u> “X plus Y”	<u>No of cracks *</u>
A1	95.5	99.1	33.5	194.6	20/6
A2	98.8	97.9	27.5	196.7	4/0
A3	96.8	106.7	0	203.5	14/4
A4	99.1	107.2	0	206.3	1/0
A5	92.2	106.7	0	198.9	3/0
A6	101.8	105.7	19.7	207.5	0/0
ΔA1-A6	97.4	103.9	13.5	201.3	
C1	108.9	103.5	32.1	212.4	5/0
C2	112.8	106.1	30.6	218.9	9/0
C3	106.0	95.5	21.1	201.5	11/0
C4	103.1	114.4	10.3	217.5	10/0
C5	97.9	99.5	4.9	197.4	1/0
C6	99.0	97.9	16.0	196.9	5/0
ΔC1-C6	104.6	102.8	19.2	207.4	
D1	101.0	103.6	24.7	204.6	4/1
D2	105.1	106.8	21.4	211.9	7/0
D3	109.6	105.5	19.0	215.1	15/2
D4	103.7	112.0	5.0	215.7	9/0
D5	99.5	104.4	1.1	203.9	3/0
D6	99.8	90.6	17.6	190.4	6/0
ΔD1-D6	103.1	103.8	14.8	206.9	
F1	No	Sample	-	-	-
F2	87.7	101.1	19.9	188.8	5/0
F3	81.9	119.5	0	201.4	15/0
F4	83.7	106.0	0	189.7	4/0
F5	105.9	102.7	21.3	208.6	0/0
F6	84.3	112.5	29.6	196.8	0/0
ΔF1-F6	88.7	108.4	14.2	197.1	
ΔB=ΔA-C	101	103.4	16.4	204.4	
ΔE=ΔD-F	95.9	106.1	14.5	202	

All measurements are in microns

* X/X = First number is total cracks, second number is number of cracks penetrating into substrate base metal (0.001 to 0.003-in).

Based on the above table, the estimated layer thickness of top overlay and bond coats for transverse section “B” came to 101 μ m and 103.4 μ m, respectively. Similarly for transverse section “E”, the respective coating thickness came to 95.9 μ m and 106.1 μ m.

For GT 33+, the top overlay coating thickness of around 100 μ m and NiCoCrAlY bond coat of around 100 μ m can be used as close estimates.

During the optical measurements of three different coating layers, a number of thermal mechanical fatigue cracking was also noted. They all initiated from the OD side and most penetrated through the duplex coatings and stopped at the diffusion interface. Some cracks, however, penetrated into the GTD-111 substrate. The last column of Table 6-3 show the number of cracks noted at specified grid locations. As noted, some of the cracks penetrated into the GTD-111 base metal. More cracks were concentrated on the leading edge side of the airfoil. Figure 6-3 shows cracks representative of such thermal mechanical fatigue cracks.

It should be noted that presence of fatigue cracks adds another variable dimension to the coating characterization. The coating conductivity, which is highest at the as-coated condition, decreases from the oxidation process due to β -phase depletion of aluminum. The presence of cracks also decreases the coating conductivity. However, as will be shown later, this crack-induced decrease in conductivity is more significant than decrease in conductivity due only to oxidation of coatings.

Figure 6-3. Examples of Thermal Mechanical Fatigue Cracks Found at Leading Edge

FIGURE 6-4 SHOWS DETAILS OF THE FATIGUE CRACKS THAT STOPPED AT THE DIFFUSION INTERFACE AND ONE THAT HAS PENETRATED INTO THE GTD-111 BASE METAL.

Figure 6-4. Details of Fatigue Crack Extension Under the Duplex Coatings

6.1.3 7FA 1st Stage Bucket - Blade 9

This blade has been saved for additional eddy current testing and coating characterization. No destructive sectioning has been done to date. Once all the eddy current testing and characterization is complete, the areas of interest will be sectioned for eventual comparison and validation of eddy current-based estimates of volume fraction of β -phase in the top overlay coating.

Figure 6-5 shows the details of grids used to obtain multiple eddy current measurements from the suction side of the blade bucket.

a) Trailing Edge Side of Blade 9

b) Leading Edge Side of Blade 9

Figure 6-5. Detail View of Grid Marks on the Suction side of Blade 9

Due to the presence of platform preventing the eddy current probe holder from accessing the blade surface, no eddy current measurements were obtained from A and B transverse grid locations.

6.1.4 7F 1st Stage Bucket – Blade 37

As indicated in Table 6-1, this is a re-coated blade with no additional service hours. Based on the repair practice, it was reported that following nominal thickness values were used in the recoating process: 4-10 mils (102-254 microns) for NiCoCrAlY bond coat; and 1.2-3.8 mils (30-97 microns) for top aluminized overcoat.

Figure 6-6 shows coarse grid marks used for multiple frequency eddy current measurements from the suction and pressure sides of the blade bucket.

Figure 6-6. Detail View of Grid Marks on the Suction and Pressure Sides of Blade 37

Both suction and pressure sides of transverse section C were used to estimate conductivity values of duplex coatings along with GTD-111 substrate.

6.2 Frequency Scanning Eddy Current Test System

This swept-frequency eddy current system was purchased from CESI of Italy. This is a PC-based frequency-scanning eddy current test (F-SECT) system. Figure 6-7 shows its application to actual blade bucket.

Figure 6-7. Frequency-Scanning Eddy Current Tester in Operation

The F-SECT system runs on a dedicated hardware and software to perform multiple frequency eddy current measurements in the 0.1 MHz - 10 MHz range. Usually, a set of 30 frequency scan data is acquired automatically in the range of 0.5 MHz - 10 MHz. Based on the built-in 2-layer and 3-layer inversion programs, the acquired eddy current data is used to estimate desired coating thickness and the associated electromagnetic parameters of interest for each coating layer. For non-magnetic materials, the electromagnetic parameter of interest is electrical conductivity. For paramagnetic materials, the electromagnetic parameter of interest is represented by the ratio of electrical conductivity to magnetic permeability.

6.2.1 Measurement Procedure and Data Normalization

The following data acquisition and normalization steps are used for the F-SECT system.

Raw F-SECT data consist of the real and imaginary components of the receiver voltage at each test frequency: $\text{Re}(V)$ and $\text{Im}(V)$. Usually a set of 30 scanned-frequency measurements in the range 0.5 MHz -10 MHz is acquired at each measurement spot. The quality of raw F-SECT data depend on:

- Conductivity/permeability vs. depth profiles from the test sample
- Blade curvature
- Probe-to-sample coupling (lift-off and tilt) distance
- Instrument drive/gain settings
- Frequency characteristics of probe and cable length

Conductivity (σ) and permeability (μ) values represent the ultimate objective of the eddy current measurements, as they correlate with the structure of the coating/substrate system under investigation. To obtain desired σ values, other coating parameters, such as coating thickness for each layer, must be identified in order to minimize the number of variables for subsequent model based inversion analysis.

Surface curvature does not affect the measurements significantly unless the curvature radii are smaller than 10 times the probe diameter. To reduce other extraneous effects, such as lift-off, probe cable effects, temperature effect, etc., a special normalisation procedure has been devised to pre-condition the acquired raw data. It provides *normalised impedance R* that is compared directly with the modelled eddy current data.

Normalisation procedure relies on the results of measurements obtained from two reference calibration samples, A and C, having slightly different σ values (relative difference of about 10%). It should be noted that these two controlled samples do not necessarily reflect the actual coating/substrate conditions under investigation, but their σ values reflect the observed range of σ values for Ni-based superalloys.

Data normalisation is applied after acquiring five measurements per test area and the quality of the acquired data has been verified. The resulting output, expressed as *normalised impedance R vs. frequency*, is a scalar quantity. This scalar quantity is then compared against the model-based impedance output. The following explanations offer steps necessary to obtain the normalized impedance values.

- Use two reference samples A and C having homogeneous σ with slightly different values (approximately 10% difference).
- Obtain two reference frequency-scanning measurements, V_{Aa} and V_{Ab} , from sample A at lift-off distances “a” (standard distance) and “b” (increased lift-off).
- Obtain a third reference frequency-scanning measurement, V_{Ca} , from sample C at standard lift-off distance. Obtain a frequency-scanning measurement, V_{Da} , from coated test sample to be tested.
- From the vector orientation ($V_{Aa} - V_{Ab}$), the lift-off direction is determined. The projected vectors v_A , v_C and v_D in the orthogonal direction to lift-off are then calculated.
- The following formula is used next to obtain the normalized impedance value at measurement point D:

$$R_D = \frac{(v_D - v_A)}{(v_C - v_A)}, \text{ where } v \text{ is the lift-off free component of } V.$$

Referring to the Figure 6-8, the normalized impedance, R_D , is equivalent to the ratio between segments ad'/ac' , for each frequency included in the scan.

Figure 6-8. Process of Normalizing Impedance Value at Position D into Scalar Quantity

6.3 Data Analysis

Data analysis is performed first by establishing a model that would simulate the eddy current coil impedance response based on known excitation source, coil dimensions and geometries, and some known test sample parameters. Once this forward model is established, the model coil impedance curve is then compared to measured normalized coil impedance curve. Depending on the number of coating layers involved, a two-layer or three-layer iterative inversion program is executed next to minimize errors to estimate the parameters of interest, for example, σ value of each coating layer.

6.3.1 *Model-based Forward Analysis*

The normalization process described in Section 6.2 produce normalized coil impedance values that are free of lift-off and, to some extent, probe-cable effects. Under certain conditions, the normalized impedance value can be associated closely to the real part of the coil impedance based on the assumed excitation source of plane electromagnetic wave over semi-infinite conducting surface. Consequently, the eddy current forward model describes the interaction of a plane electromagnetic wave over a layered conducting surface structure as shown in Figure 6-9.

Figure 6-9. 3-Layer Model Configuration Showing Parameters of Interest

This model is used to evaluate the condition of duplex metallic coatings. The primary interest is to estimate the σ_1 value and correlate it to remaining volume fraction of β -phase particles found in the topcoat.

6.3.2 Three-Layer Inversion Program

Under service conditions, as the β -phase content reduces from 100% to 0%, the σ/μ ratio of NiCoCrAlY coatings generally decreases by 20-30%. This reduction is more than adequate to correlate the σ/μ ratio to volume fraction of β -phase content. It should be noted that GT 33+ coating did not exhibit magnetic properties. This was confirmed by placing a small permanent magnet at various locations of the airfoil to see if it sticks to the surface. Results were negative, indicating that GT 33+ coating is nonmagnetic. As such, the σ/μ ratio is reduced just to σ value.

To allow improved convergence of the three-layer inversion algorithm, several of the model parameters were fixed. Specifically, the values of L1 and L2 were fixed (based on the destructive sectioning results) to allow better estimates of σ values, σ_1 , σ_2 , and σ_3 .

6.4 F-SECT Data Analysis Results

Data analysis was conducted using the acquired normalized impedance data from cutout Sections B and E of Blade 5 along with Blade 9, which is kept intact for gathering additional eddy current data. In addition, one as-coated area of Blade 37 was also analyzed in order to compare the estimated topcoat σ values.

The analysed data is presented as both qualitative and quantitative test results.

6.4.1 Qualitative Analysis

This analysis basically allows review of the normalized coil impedance versus operating frequency curve at each measured location of the airfoil. Due to “skin effect”, more eddy current is concentrated at the top surface of the test sample at higher operating frequencies. Conversely, more eddy current penetration is allowed below the surface at lower operating frequencies. Consequently, topcoat information is gathered at higher frequency, while substrate information is gathered at lower frequency. Figure 6-10 shows an example of such plot obtained from transverse Section E of Blade 5. There are three conditions that are identified from this transverse Section E: 1) normal coating conditions; 2) β -phase depletion of coating; and 3) cracking of coating.

Due to higher aluminium content found in the topcoat, σ values are high, resulting in higher normalized impedance values. Conversely, as β -phase depletion increases, the associated decrease in σ cause the normalized impedance values to fall. When the β -phase content is nearly depleted, the normalized impedance values approach the value of the base metal over the operating frequency range. This resulted in the flattening of normalized impedance curves shown in the figure. As mentioned earlier, this particular service-aged blade was cracked. The bottom group of impedance curves with negative slopes shows continuous decrease in the normalized impedance values caused by presence of cracks.

It should be noted that normalized impedance value of “zero” refers to the condition where σ value of the blade sample at a given position is equal to a given σ value of the reference sample A. Basically, this condition exists when the measured area of the blade sample is depleted of β -phase aluminium and is free of any cracking in the coating.

Figure 6-10. Normalized Impedance Versus Frequency at 18 Different Airfoil Positions from Leading to Trailing Edge - Transverse Cutout Section E of Blade 5

This type of presentation provides a quick overview of the tested blade conditions. To determine where the problem is taking place along the transverse cutout section, another presentation based on the same data set is needed. This presentation is shown as Figure 6-11.

For this normalized impedance versus positional information, it is not necessary to display all 30-frequency data sets. Instead, we have selected 6-frequency data sets to illustrate those areas of interests. Positional or chord information represents an increasing number of arbitrary values at equal spacing starting from 1 at the leading edge to 18 at the trailing edge of the suction side.

The following specified positions are associated to those three coating conditions described above:

- 1) Normal coating conditions – Positions 9-11
- 2) β-phase depletion of coating – Positions 12-18
- 3) Cracking of coating – Position 1-8

Figure 6-11. Normalized Impedance Versus Positional Information at Selected Operating Frequencies - Transverse Cutout Section E of Blade 5

The cracked positional information presented in Figure 6-10 matches with the area identified to be cracked (see Table 6-3). Since cracks were found in grid sections D1-D3 and F2-F3, it is likely that cracks will also be present in grid sections E1-E3. These grid sections correspond to positional readings 1 through 8 of Figure 6-11.

Figure 6-12 shows results of EDS from three layers located along the edges of Section F (F2-F6) facing the transverse section E. Although it is slightly off, positions 9-11 showing increased normalized impedance values correspond to the area of high aluminium concentration in Figure 6-12 as positions 3-5 (they are very close to positions 6-10 of Figure 6-11).

Figure 6-12. Aluminum Concentration by Wt% Representing Cutout Section E

Similar qualitative presentations of normalized impedance values are shown as Figures 6-13 and 6-14 for transverse cutout Section B of Blade 5.

Since Section B was closer to the platform, it was wider and this allowed several more additional readings using the F-SECT system.

Unlike the cutout Section E, no normal coatings were found on this transverse section B. This was somewhat unexpected since lower temperature exposure is expected at this location in comparison to Section E, which is closer to 50% of the airfoil height and receives higher metal temperature. This lower temperature condition in Section B should have lead to less coating and airfoil degradation than in Section E.

Coating conditions showed β -depleted conditions and/or presence of crazed cracking. One area, position 4, showed possible base metal corrosion due to cracks penetrating into the base metal.

Figure 6-13. Normalized Impedance versus Frequency Plot for Cutout Section B of Blade 5

Figure 6-14. Normalized Impedance versus Position for Cutout Section B of Blade 5

Figure 6-14 showed that leading edge areas from position 1 through 11 contained cracks and thus affecting the integrity of coatings and possibly the base metal. As in Section E, more cracks are noted in the leading edge rather than trailing edge areas of Section B.

Figure 6-15 shows visual presentation of crazed cracking of coating found on Section B of Blade 5. As indicated earlier, some of these cracks penetrated into the GTD-111 substrate. It should be noted that the orientation of the observed cracks is along the length of the blade.

Figure 6-15. Crazed Cracking of Coating Attributed to Thermal Mechanical Fatigue

Similar plots of normalized coil impedance versus frequency and blade chord information were also gathered from Blade 9. Unlike Blade 5, no destructive sectioning has been done to this sample at this time. This allowed gathering of data from the majority of the entire airfoil surface. The normalized coil impedance values were obtained from the transverse sections C through N (see figure 6-5).

To visualize the condition of the blade in its entirety, it was more desirable to review the entire surface of the inspected blade bucket rather than reviewing individual transverse sections. 2D grid software that would allow mapping of X, Y, Z values was used to generate colored contour maps. For mapping purposes, “X” represented blade chord, “Y” represented blade height, and “Z” represented normalized coil impedance values.

Figure 6-16 shows the colored contour map, which represent 16 maximum chord positions and 12 blade height positions (C through N). Due to probe access limitations, the area next to the platform (sections A and B) was not included. The area of interest, however, is clearly delineated and easily located based on the graduated blue color presentations. Similarly, the area of normal coating condition is highlighted by the yellow color presentations.

Figure 6-16. Colored Contour Map of Normalized Impedance Data at 10 MHz

As indicated, the X-axis shows chord distance from the leading edge to trailing edge in the increasing order of 1 to 16 at equally spaced distances. The Y-axis shows 12 equally spaced distances from the platform to blade tip (C through N). The worst degradation areas are noted at 3-6 (D6) to 6-6 (F6), while normal coating area is located at 12-10 (L10).

To see if there is any difference in the coating characteristics of 7F 1st stage bucket that has been recoated with no additional service hours, similar normalized coil impedance values were obtained from the suction and pressure sides of Blade 37.

These measurements were taken approximately 50% of the airfoil height from the platform. This transverse section has been identified as Section C of Blade 37. On the suction side, six equally spaced measurements were made from the leading edge to trailing edge. On the pressure side, only five equally spaced measurements were made from the leading edge to trailing edge.

Qualitative presentations of as-coated bucket on the suction side are shown as Figures 6-17 and 6-18. Similar presentations from the pressure side are shown as Figures 6-19 and 6-20.

Figure 6-17. Normalized Impedance versus Frequency Plot for Section C Suction Side of Blade 37

Figure 6-18. Normalized Impedance versus Position for Section C Suction Side of Blade 37

Figure 6-19. Normalized Impedance versus Frequency Plot for Section C Pressure Side of Blade 37

Figure 6-20. Normalized Impedance versus Position for Section C Pressure Side of Blade 37

In general, curves with positive slopes (see Figure 6-17 and 6-19) represent normal coating conditions. However, it appears that less coating was applied at position 3 of suction side, which corresponds to extrados side of the blade curvature (similar to location B3 of Figure 6-1). In contrast, more uniform coating is observed on the pressure side of Section C of Blade 37 (see Figures 6-19 and 6-20).

6.4.2 Estimates of Coating Thickness and Coating Conductivities

According to the developer of the F-SECT system, the measurement precision of coating thickness is about $\pm 25 \mu\text{m}$ for a coating thickness range of 50-300 μm . This is based on the expected σ difference of around 7-10% between the coating and substrate. Due to the upper frequency limit of 10 MHz, any coating layer thickness of less than 50 μm cannot be resolved accurately.

To determine the residual β -phase content, it is necessary to develop a calibration curve that would correlate the β -phase content (either volume fraction of β phase or wt.% of aluminium) to estimated σ values. Obviously, this would require obtaining information about the β -phase content of the topcoat and bond coat from a set of reference samples that have been sectioned and characterized destructively. In general, a linear relationship between σ and β -phase depletion is assumed. As such, to establish the calibration curve, the following two topcoat σ values need to be known: 1) as-coated σ value and 2) fully-depleted σ value.

The quantitative coating characterization is performed in two steps:

1. Estimating (by means of model based analysis) the thickness and the electromagnetic parameter σ/μ of the coating
2. Calculating an effective β -phase content (EBC) from the following expression

$$EBC = \frac{(\sigma / \mu_{test} - \sigma / \mu_{depl})}{(\sigma / \mu_{new} - \sigma / \mu_{depl})} \times 100$$

In the above formula, EBC can be correlated to either % volume fraction of β -phase remaining or wt.% of aluminium found in the topcoat.

To perform the first step, a 2-layer model or a 3-layer model can be used. The use of which model depends on the specific coating/substrate conditions found in test samples.

For GT 33+ coating condition, a 3-layer model fits the electromagnetic structure of the coating/substrate (see Figure 6-9). The model parameters of interests are L1, L2, σ_1 , σ_2 , and σ_3 . To minimize the number of variables and improve the iteration process to converge with minimum errors, L1 and L2 values were fixed based on destructive sectioning results. For the purpose of σ estimation, 100 μm and 200 μm were used for L1 and L2, respectively.

Table 6-4 show the normalized impedance, R, values and the corresponding σ_1 , σ_2 , and σ_3 values, which were obtained using the aforementioned 3-layer inversion program with specified constraints placed on L1 and L2. The same data set from the cutout Section E of Blade 5 was used in the 3-layer inversion program.

Table 6-4. Estimated Coating/Substrate σ Values for Normalized Coil Impedance Values – Section E of Blade 5

	1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18
R	-	-	-	-	-	-	-	0.10	1.61	2.27	2.54	0.60	0.48	0.13	-	-	-	-
	1.89	2.47	2.84	2.97	3.18	2.89	1.64								0.28	0.18	0.11	0.05
σ_1 (MS/m)	0.49	0.42	0.39	0.33	0.21	0.25	0.42	0.70	0.97	1.07	1.11	0.80	0.78	0.73	0.70	0.73	0.74	0.75
σ_2 (MS/m)	0.68	0.65	0.62	0.63	0.60	0.64	0.75	0.80	0.78	0.81	0.82	0.79	0.79	0.78	0.73	0.71	0.71	0.71
σ_3 (MS/m)	0.73	0.73	0.73	0.73	0.74	0.76	0.76	0.77	0.79	0.80	0.79	0.78	0.78	0.78	0.77	0.75	0.75	0.75
Error (E-03)	2.62	5.81	5.25	3.60	5.34	4.45	2.82	1.92	0.76	1.20	1.87	1.82	2.23	2.19	2.87	3.44	2.51	2.44

The last row shows the residual error values between measured and fitted data as condition for stopping the inversion process. Obviously, smaller the error, more reliable is the estimate. As expected, the normal coating conditions (positions 9-11) showed higher topcoat σ values, for example, 0.97 – 1.11 MS/m. In contrast, the areas of cracking showed lower topcoat σ values of 0.21 – 0.49 MS/m. The β -depleted areas without cracking showed topcoat σ values of 0.70 – 0.80 MS/m. Thus, by monitoring topcoat σ values, it is possible to discriminate different coating degradation conditions.

The averaged topcoat σ value was calculated to be 1.05 MS/m. By focusing on the normal coated areas and β -depleted areas, the following averaged σ values were also obtained for NiCoCrAlY bond coat (positions 9-11) and GTD-111 substrate (positions 8 -18), respectively: 0.80 MS/m and 0.77 MS/m.

Table 6-5 show similar σ values obtained from the cutout Section B of Blade 5.

Table 6-5. Estimated Coating/Substrate σ Values for Section B of Blade 5

σ	1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18	19	20
σ_1 (MS/m)	0.48	0.52	0.42	0.35	0.38	0.41	0.37	0.48	0.53	0.56	0.59	0.71	0.67	0.68	0.64	0.66	0.68	0.72	0.73	0.73
σ_2 (MS/m)	0.70	0.68	0.69	0.44	0.60	0.63	0.57	0.67	0.67	0.74	0.81	0.75	0.75	0.75	0.75	0.75	0.73	0.74	0.73	0.72
σ_3 (MS/m)	0.73	0.73	0.72	0.72	0.73	0.73	0.72	0.73	0.74	0.76	0.77	0.77	0.77	0.77	0.76	0.77	0.76	0.75	0.75	0.75
Error (E-03)	2.88	1.56	2.47	4.45	2.87	5.75	4.04	2.79	2.81	2.04	2.69	2.74	2.92	2.80	2.89	3.71	2.94	2.25	2.69	3.00

As mentioned earlier, there was no normal coating conditions found on this Section B. Based on Figure 6-14, positions 1-11 showed presence of cracking (averaged topcoat σ value of 0.46 MS/m). No cracking was noted at positions 12-20. The averaged topcoat σ value at positions 12-20 was calculated to be 0.69 MS/m, indicating that this is a β -depleted area. Within positions 12-20, the following averaged σ values were obtained for bond coat and substrate, respectively: 0.74 MS/m and 0.76 MS/m. Due to β -phase depletion, the bond coat σ value is pulled down from the expected value of around 0.80 MS/m. The base metal σ value of 0.76 MS/m was very close to the base metal σ value obtained from Section E (0.77 MS/m). Additional validation was conducted by estimating the σ values from Section L of Blade 9, which was identified earlier to have normal coatings.

Table 6-6 shows the estimated σ values.

**Table 6-6.
Estimated Coating/Substrate σ Values for Section L Suction Side of Blade 9**

σ	3	4	5	6	7	8	9	10	11	12	13	14	15	16
σ_1 (MS/m)	0.517	0.346	0.224	0.797	0.976	0.91	1.10	1.19	0.88	0.75	0.72	0.73	0.75	0.75
σ_2 (MS/m)	0.669	0.634	0.596	0.730	0.751	0.79	0.85	0.85	0.81	0.74	0.74	0.71	0.71	0.72
σ_3 (MS/m)	0.732	0.734	0.741	0.784	0.786	0.77	0.79	0.79	0.78	0.77	0.76	0.75	0.75	0.74
Error (E-03)	1.207	5.377	3.298	4.395	4.728	4.63	1.76	2.72	0.94	2.72	2.73	3.03	2.29	2.04

Normal coatings were found at locations 7-11 while cracked locations are confined to locations 3-5. All other remaining locations were affected by β -phase depletions. From the normal coatings, the following topcoat and bond coat σ values were estimated:

1.02 MS/m and 0.81 MS/m. The averaged σ value of GTD-111 was estimated to be 0.77 MS/m (averaged value from positions 6-16). These σ values were almost identical to values obtained from Blade 5.

6.5 Conclusions—Task 3—NDE of Coatings

This quarterly report summarized the effort and positive results that were obtained using the F-SECT system by CESI of Italy on both aged and as-coated blade buckets.

A Memorandum of Understanding paper was signed with Korea Western Power to secure as-coated and aged blade buckets in exchange for information obtained on furnished blade samples. EPRI gained access to five blade buckets – one as coated and four aged buckets with varying number of startups and operating hours. The three latest acquisitions represent first stage buckets from 7F frame, PG7211, equipped with GT 33+ duplex coatings. The two blades acquired in 2001 represent first stage buckets from 7FA frame, PG7231, equipped with GT 33+duplex coatings.

The eddy current testing of actual blade samples has demonstrated that it is not only capable of performing reliable testing of removed blade bucket but characterizing the blade condition as: 1) normal, 2) degraded coating due to β -phase depletion, and 3) repairable due to presence of thermal mechanical fatigue cracking.

Individual cutout section can be evaluated using plots of normalized impedance versus frequency or normalized impedance versus chord position. In general, normal coating/substrate conditions present the highest normalized impedance curves with positive slopes, followed next by almost flat curves representing β -phase depletion, and finally curves with negatives slopes representing presence of crazed cracking and possible corrosion of base metal. To review the entire airfoil condition, a colored contour map of normalized impedance values was plotted to show the affected area of interest. For aged blade buckets, more degradation was noted at the leading edge of the blade surface on the suction side.

Using the three-layer inversion program augmented by coating thickness information gathered from destructive sectioning results, it was possible to estimate conductivity values of the topcoat, bond coat, and GTD-111 substrate.

The progress made to date will be used ultimately to generate a calibration curve that would correlate conductivity of coatings to volume fraction of β -phase particles or percent wt. of aluminum remaining in the topcoat. This information will then be used to assess the useful remaining coat life.