TURBINE BLADE AERODYNAMIC WALL SHEAR STRESS MEASUREMENTS AND PREDICTIONS

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ABSTRACT
The correct prediction of the aerodynamic wall shear stress is a critical test of a numerical code's ability to predict profile loss. Its measurement with heated thin film gauges is significantly easier than attempting a complete measurement of a turbine blade boundary layer. A modified form of previously published heated thin film gauge calibrations allows wall shear stress measurement in laminar incompressible flow with favourable pressure gradients and turbulent incompressible flows with small pressure gradients. In this paper, measurements are presented of the distribution of aerodynamic wall shear stress over the suction surface of a turbine blade in a linear cascade. Gauge voltage signal analyses show a laminar separation bubble between about 53% and 65% of suction surface length that is confirmed by surface flow visualization. By-pass transition is detected by downstream gauges. Wall shear stress measurements are presented at two cascade incidence angles and for tripped and natural transition. The commercial code FLUENT is used to predict the surface pressure distribution, the aerodynamic wall shear stress distribution in the laminar region and the turbulent surface shear distribution for the tripped boundary layer. Comparisons are made between measurements and predictions.

NOMENCLATURE

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<thead>
<tr>
<th>Symbol</th>
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<tr>
<td>k,</td>
<td>Critical trip wire diameter [m]</td>
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<tr>
<td>L_w</td>
<td>Effective sensor length [m]</td>
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<td>K</td>
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Greek

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<td>Dynamic viscosity [Kg/ms]</td>
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<tr>
<td>ν</td>
<td>Kinematic viscosity [m²/s]</td>
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<tr>
<td>θ</td>
<td>Cylinder angle [Deg.]</td>
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<td>ρ</td>
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<tr>
<td>τ_w</td>
<td>Wall shear stress [N/m²]</td>
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Suffixes

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<td>I</td>
<td>Inlet</td>
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<tr>
<td>T_w</td>
<td>To trip wire</td>
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<td>L</td>
<td>Laminar</td>
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INTRODUCTION

The long term aim of this work is the measurement of aerodynamic wall shear stress on gas turbine rotor blades as they operate under engine representative conditions and to use this data to validate viscous, compressible, unsteady codes. Many of the parameters that are used to validate codes are not necessarily a good measure of the codes accuracy in the prediction of loss. It is sufficient that a code predicts all parameters well - but not necessary.

One of the three divisions of loss identified by Denton (1993) is the profile loss. This is present because of the shear forces in the boundary layer and where the shear forces are greatest then so is the loss. Generally the shear forces are greatest on the surface, therefore the measurement of surface shear stress is a good measure of a codes ability to predict loss. Ideally, what is needed to be measured is the velocity everywhere in the boundary layer, but this is not possible at present given the thinness of the boundary layer and the difficulty of accessing the rotor passage boundary layer by optical or probing methods. In contrast to this, thin film surface gauges, which are the subject of this paper, have been successfully used for qualitative measurements in rotor passages by Tiedemann (1997) in conjunction with rotating instrumentation by Davies et al. (1997). The obvious step is therefore to calibrate these types of gauges to measure surface shear stress. This view is further endorsed by the pseudo-shear stress measurements of Hodson et al. (1994), Solomon and Walker (1995) and Ubaldi et al. (1996).

The work presented here builds upon the calibration theory of Davies and Duffy (1995) and the subsequent calibrations of Duffy et al. (1995) and Davies et al. (1997). This work allows for quantitative measurements in laminar, incompressible boundary layers with favourable free-stream pressure gradients and in turbulent, incompressible boundary layers with small free-stream pressure gradients. Gauges are mounted on the suction surface of a cascade running at engine representative Reynolds numbers. The measured data is compared with predictions from a commercially available CFD code. This was chosen because results from it are of more general interest than those from proprietary codes. The code does not allow for natural transition so the boundary layer is tripped near the leading edge for turbulence model validation. The inability of the code to predict by-pass transition is typical of current commercial and research codes. Two incidence angles are investigated over a range of Reynolds numbers. Both the hot film measurements and CFD predictions are seen as important steps towards the prediction of turbomachinery loss.

EXPERIMENTAL SET-UP

All measurements were taken on the University of Limerick subsonic linear cascade shown in Fig. 1. The linear cascade blades are scaled models of the mid-span of the rotor blade designed by Santoriello et al. (1993), now operational in two single-stage turbine facilities at the VKI, Brussels and DLR, Göttingen. The blade incorporates many modern transonic turbine rotor blade design features with a high camber angle of 115°, low aspect ratio of 1.07 and pitch-to-chord ratio of 0.75. The 3-dimensional blade has a nominal 2-dimensional flow at mid-span to accommodate instrumentation and allow comparison to linear cascade measurements.

![Figure 1: Linear cascade experimental set-up.](image-url)
Ten Dantec 55R47 thin-film gauges were glued in two batches of five to the suction surface of two central aluminium linear cascade blades in a staggered array, as shown in Fig. 3, with 18mm spanwise spacing to avoid flow or thermal interference between gauges. From hot wire traverses, the exit velocity profiles of the three central cascade passages were found to be identical to within 5%, indicating good cascade periodicity. The five upstream gauges were all within the region of favourable pressure gradient. The first of the downstream gauges was placed at 62%SSL. The remaining four gauges were equi-spaced to 93%SSL, although the last gauge failed during testing and data for it is not shown in all the figures. The same two instrumented cascade blades were used for both 0° and -5.5° incidence tests. The off-design incidence was achieved by using a second pair of side-walls, shown in Fig. 1, with a cascade angle of 40° and using six rather than seven blades.

To avoid having to model transition with the CFD code, the boundary layer was tripped to validate the turbulence flow predictions. Schlichting (1979) has outlined various schemes for predicting the critical height and position of trip wires. Qualitative hot film data, presented later, was used to monitor the effect of the trip wires. Fage & Preston (1941) deduced the following relation for the tripping wire critical height.

$$k_{\text{crit}} = \frac{20\nu}{\sqrt{T_w / \rho}}$$

Inserting the known values of $T_w$ gives a height of 0.14mm at the lowest shear stress. As this equation relates to a flat plate boundary layer without a favourable pressure gradient, it was found necessary to use a thicker, 0.5mm trip wire. This was the minimum trip wire diameter that would give turbulent flow over all hot films along the suction surface length at the highest Reynolds number. Kramers’ (1961) relation for the distance downstream from the trip wire to transition is:

$$x_{Tr} - x_b = \frac{2 \times 10^4 \nu}{u}$$

This gives a wire position of approximately 9%SSL for turbulent flow over the first gauge. As will be shown, the wire placed here successfully tripped the entire suction surface boundary layer at the highest Reynolds number.

**CFD SOFTWARE**

The commercial CFD code FLUENT (1996) solves the governing conservation equations via the finite volume technique on a structured, non-orthogonal, curvilinear coordinate grid utilising a non-staggered storage system of discrete velocities and pressures. Various spatial
SIMPLEC algorithm. The governing conservation equations are solved in sequence using a line Gauss-Seidel technique accelerated by an additive-correction multigrid method and/or one-dimensional block correction. A suite of turbulence closure methods are available; the standard k-ε model, the Re-Normalisation Group k-ε model and the Reynolds Stress model. The effect of the wall in turbulent flow is modelled via the standard wall function, the non-equilibrium wall function or a two-layer zonal model.

Geometry creation and grid generation was achieved using GEOMESH (1996), the CFD code's preprocessor. A unique grid was used for the predictions of inviscid pressure distribution, the laminar boundary layer and the turbulent boundary layer. Fig. 4 illustrates the grid used for the pressure prediction. This coarse grid essentially gives an inviscid solution, since the wall cell centre lies outside the boundary layer and thus no viscous separation characteristics influence the overall flow field. The inlet and outlet portions of the grid are bounded top and bottom by cyclic boundaries which provide transfer of the flow field solution such that a continuous cascade is simulated.

![Figure 4: 94x26 node grid for inviscid predictions.](image)

To obtain the wall shear stress in laminar flow, the code assumes a linear velocity variation between the wall and the adjacent cell centre. Consequently, the spacing between the wall and the adjacent grid line can significantly effect the accuracy of the computed wall shear stress. Based upon the theoretical boundary layer profile over a flat plate at zero incidence of Schlichting (1979), it is recommended that the grid point height adjacent to the wall, \( y_p \), should obey the criterion:

\[
y_p \sqrt{\frac{\mu \rho_{in}}{\mu}} \leq 1
\]

The final grid used in the laminar flow shear stress calculations utilised a value of 0.45 for the above criterion at 10% SSL. This gave rise to a wall adjacent cell centre 0.03mm above the blade surface. To avoid convergence difficulties and the propagation of numerical errors, the cell aspect ratio throughout the grid was kept below 10. This resulted in a fairly large laminar flow shear stress grid of 61x198 nodes. For the turbulent wall shear stress computations, the wall function technique was adopted. These functions are based on the assumption that a fully developed equilibrium turbulent boundary layer exists and therefore all the relevant flow properties can be obtained from the log law. The friction velocity, and hence the wall shear stress, is calculated by substituting the cell centre flow velocity, parallel to the wall, into the log law equation. The turbulent grid was generated such that each cell centre was located within the log-law region of the boundary layer. The resulting 50x101 node turbulent grid maintained the recommended wall adjacent cell centre wall unit value of approximately 30.

The power law interpolation scheme was used for the laminar flow calculations due to its good stability and convergence qualities. False diffusion effects, induced by the power law scheme, should be reasonably small since the flow was always well aligned with the grid. The quadratic upwind interpolation scheme was used for the turbulent flow calculations. All CFD calculations were carried out on a Silicon Graphics Indigo 2 workstation. A typical flow calculation took 300 iterations to converge and used approximately 6 minutes of CPU time.

### HOT FILM CALIBRATION

The semi-empirical calibration theory of Davies and Duffy (1995) links the aerodynamic wall shear stress to the voltage across a constant temperature hot film gauge.

\[
\tau_w = \left( \frac{(V_s^2 - V_o^2)}{d^{1/3}A'} \right)^{3} + \frac{b}{a \, dx} \frac{B'}{(V_s^2 - V_o^2)}
\]

Where:

\[
A' = \left( \frac{\rho \Pr}{\kappa^2} \right)^{2/3} 2^{1/3} \frac{\kappa W_{eff} L_{eff}^{2/3}}{K R_h \kappa^2 (T_L - T_w)} \left( \frac{T_{Max} - T_w}{T_{Max} - T_w} \right) ^{2/3}
\]

\[
B' = \frac{L_{eff} W_{eff} K R_h (T_L - T_w)}{2}
\]

By operating hot films in flow fields of known wall shear stress on a flat plate, circular cylinder, wedge and pipe, multi-point calibrations, plotting the variation of \((V_s^2 - V_o^2)\) over a range of \(\tau_w\), were used to determine the constants a and b as detailed by Duffy et al. (1995) and Davies et al. (1997). The known sensitivity of thermal sensor voltage to variations in ambient temperature were previously compensated for a priori using a hot wire analytical correction over a...
small temperature range. This technique, however, yields unrealistic values of shear stress as it under-compensates the air off voltage, $V_0$.

Significant variations in the thermal profile of a hot film operating between 10°C and 30°C were measured using an AGEMA Thermovision 880 infrared camera. The thermal dimensions $L_{eff}$, $W_{eff}$, temperatures $T_0$, $T_{air}$, and air-off voltage, $V_0$ were found to vary linearly with ambient temperature. Experiments were therefore designed to avoid the necessity of temperature correction by having long enough tunnel run times to measure $V_0^2$ and $V_{air}^2$ at the same temperature, shown in Fig. 5.

In these experiments, the air temperature increases during the run, taking significant amount of time for the linear cascade blade, in this case, to reach thermal equilibrium with the gauge. The small changes in voltage during this period have a significant effect on the calibration constants and apparent shear stress. In all experiments the zero flow voltage $V_0$ is found immediately after switching off the air flow as shown in Fig. 5. Under these conditions there is no need for temperature correction.

$$C_1 = \frac{a^{1/3}A'}{(T_0 - T_m)}$$  \hspace{1cm} $$C_2 = \frac{bK}{2ah}$$ (7)

This gives a modified form of the hot film calibration Eq. (4) suitable for thermal equilibrium flows requiring no temperature compensation, with or without favourable pressure gradients:

$$\tau_w = \left(\frac{V_{air}^2 - V_0^2}{C_1(T_0 - T_m)}\right)^3 + \frac{C_2}{dx} \frac{dP}{dx}$$ (8)

The calibration plot of Eq. (8) to find $C_1$ and $C_2$ in laminar flow is shown in Fig. 6. Unlike the previous calibration constants $a$ and $b$, which were theoretically universal, $C_1$ and $C_2$ vary between gauge types. Davies & O’Donnell (1998) found a larger value of $C_1$ for an MTU hot film with larger dimensions than the gauges used here. The new constant $C_1$ from flat plate and pipe flow (zero pressure gradient flow) is entirely consistent with previous values of the non-dimensional calibration constant, $a$. The second constant, $C_2$, found on a circular cylinder and wedge with shear stress known from a Blasius boundary layer solution, is found to be constant despite the variation of $b$ and $h$.

The calibration constants differ in turbulent flows. $C_1$ has been evaluated for a zero pressure gradient pipe flow in Fig. 7. $C_1$ is only required for shear stress measurements in turbulent flows with pressure gradients, which are not considered in this work. To apply the calibration constants $C_1$ and $C_2$ evaluated here requires that the same gauges must be operated at an overheat temperature of 110K in air using an aluminium test-piece. Based on the accuracy of voltage, temperature and pressure measurement, it is estimated that the hot films measure the wall shear stress to ± 5% in thermal equilibrium flows where temperature correction is not required.
PRESSURE DISTRIBUTIONS

The pressure gradient term in Eq. (8) is evaluated by differentiating a fifth order polynomial approximation of the pressure distributions for each gauge over the Reynolds number range. The non-dimensional measured and predicted static pressure coefficient distribution is plotted in Fig. 8. The agreement between prediction and measurement is good up to 50%SSL. A local discrepancy at around 10%SSL is due to the boundary layer trip wire. In the adverse pressure gradient region, the presence of a laminar separation bubble causes a significant change in the experimental displacement thickness which is not accounted for in the coarse grid predictions. The coarse grid solution gives an indication of the success of the cyclic boundary conditions and the ability of the code to predict the overall flow field. The laminar pressure prediction gave identical results as the coarse grid up to the point of laminar separation. The prediction is not shown since the comparison with the experimental data is invalid after the point of laminar separation.

UNTRIPPED BOUNDARY LAYER RESULTS

The flow regime over each gauge must be known in order to apply the calibration, as $C_i$ differs between laminar and turbulent flow. Identifying the point of transition utilises the hot film gauges in their qualitative role. The simplest method is to monitor the fluctuation in instantaneous bridge voltage. Halstead et al. (1990) show voltage time traces that closely resemble those for laminar, separated and turbulent reattached flow shown in Fig. 9(a). The frequency spectra of Fig. 9(b) is analogous to the characteristic transitional traces of Johnson et al. (1987).

Time domain voltages are most efficiently condensed and presented as the Root Mean Square (RMS) values of Figs. 10 (a) and (b). It is important to note that the RMS is a comparative, rather than an absolute, measure of flow regime.
The low levels of RMS in Figs. 10(a) and 10(b) for the five gauges in the favourable pressure gradient region confirm laminar flow for both incidence angles. The RMS values in the adverse pressure gradient region are only significant in comparison to those upstream, and in conjunction with the identification of a separation bubble by flow visualisation in Fig. 2. Pucher & Göhl (1987) have associated the peak in RMS between 50%SSL and 70%SSL with turbulent re-attachment. Examination of Fig. 10 shows this peak moves upstream with increasing Reynolds number and decreasing incidence.

The preceding qualitative hot film analysis confirms that Eq. (8) with laminar calibration constants $C_1$ and $C_2$ may be applied to hot films up to 35%SSL in the untripped experiments. In the turbulent reattached region downstream of the separation bubble, the pressure gradient is small enough to ignore the second term in Eq. (8). Therefore only the first turbulent calibration constant $C_1$ is required.

Fig. 11(a) shows the predicted laminar separation at 53%SSL, which is in agreement with the flow visualisation results and the downward trend of the hot film data. Fig. 11(b) shows the increased wall shear stress for the -5.5° incidence case was captured by both the CFD and measurements. No change in $C_1 Re^\alpha$ with Reynolds number is predicted, although it is always measured.

The difference between the prediction and the measurements up to 25%SSL was initially thought to be due to inadequate grid resolution close to the surface. However, when the grid was adjusted such that the wall-adjacent cell centre was located 0.015mm above the suction surface, no change in predicted wall shear stress was found. Fig. 12 gives an example of the invariance of the velocity profile with grid refinement. Also illustrated is the characteristic separation point velocity profile at 53%SSL.

Previous measurement of the shear stress in the favourable pressure gradient region of the linear cascade blade by Duffy et al. (1995) were larger than those of Fig. 11. The method of calibration has been changed in regard to temperature as described previously.

**TRIPPED BOUNDARY LAYER RESULTS**

The suction and pressure surfaces of the instrumented passages were tripped to give a meaningful comparison with the fully turbulent
The wire effectiveness was ascertained from the qualitative hot film signals. A sample 50kHz voltage-time domain plot is shown in Fig. 13(a) with hot film bridge voltage fluctuations characteristic of turbulent boundary layer flow. A sample conversion to the power-frequency domain in Fig. 13(b) results in characteristic turbulent variations for a single gauge over a range of speeds.

The time domain signals from the gauges in the 0-40%SSL region for the three lowest test Reynolds numbers indicate flow re-laminarization due to the high favourable pressure gradient. The signals from the gauges around 65%SSL, again for the lowest three Reynolds number tests, are transitional, indicating re-laminarisation, laminar separation and by-pass transition. In contrast, all gauges at the highest Reynolds number give turbulent signals, therefore it was only at this highest Reynolds number that the tripping was successful.

As the second calibration constant $C_2$ has not been determined in turbulent flow, the gauges in the favourable pressure gradient region cannot be used to measure wall shear stress in the tripped case. Only the highest Reynolds number case is fully turbulent along the entire suction surface length and therefore comparable to the predictions where turbulence is induced from the leading edge. The turbulent calibration constant is therefore only applied to gauges downstream of 60%SSL at the highest Reynolds number.

Two turbulence models were utilised in the CFD prediction; the standard $k$-$\varepsilon$ model with the standard wall function and the Renormalisation Group $k$-$\varepsilon$ model along with a non-equilibrium wall function. Fig. 15 shows that both turbulence models give good agreement with the hot film data. The similar solutions obtained from the two turbulence models was not unexpected, since the RNG model tends to give results comparable to the standard $k$-$\varepsilon$ model in...
moderately strained flows.

The fully turbulent pressure prediction gave good agreement with the tripped data over the first 50%SSL. However, over the rear of the blade, less suction was predicted. Preliminary investigations into this characteristic has raised doubts about the success of the trip wire to give a fully equilibrium turbulent boundary layer over the remaining suction surface.

CONCLUSIONS
- The hot film calibration technique is simplified by thermal equilibrium experiments which avoid the necessity to temperature compensate a modified calibration equation.
- Measurements and CFD predictions of aerodynamic wall shear stress show discrepancies as great as 40% in the laminar suction surface boundary layer, although trends were well predicted.
- The start of laminar separation identified by both hot film voltages and paint flow visualisation was well predicted by CFD.
- Good agreement was found between measurement and prediction of the turbulent aerodynamic shear stress for the two turbulence models tested.

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REFERENCES


