ABSTRACT

This paper deals with studies on the flow field around three types of linear cascades of low pressure turbine (LPT) airfoils with different chordwise loading distributions, while keeping the aerodynamic loading index almost the same. The purpose of the low-speed linear cascade study is to clarify the performance of newly designed two ultra high lift blade (UHL blade) and compare each of them to that of the conventional LPT blade (Base Model) with low solidity through the measurements of boundary layers accompanied by separation bubble for low Reynolds number conditions. Cylindrical bars on the timing belts work as wake generator to emulate upstream stator wakes that impact the boundary layer on the airfoil suction surface. Freestream turbulence is also enhanced by use of passive turbulence grid. In addition to the pneumatic probe measurements of the midspan loss characteristics of each of the cascades, hot-wire probe measurement is conducted over the blade suction surface to understand to what extent and how the interaction of incoming wakes as well as freestream turbulence affect the boundary layer and separation bubble. Computational Fluid Dynamics (CFD) analyses are also applied to the flow fields around the cascades, mainly using Large-Eddy Simulation (LES) with dynamic Smagorinsky subgrid scale model.

Nomenclature

- $C$ : chord length
- $C_{l}$ : axial chord length
- $C_{p}$ : static pressure coefficient
- $f$ : bar wake passing frequency
- $H_x$ : shape factor
- $N_x, N_y$ : data size, number of realizations
- $P_{in}, P_{out}$ : inlet and outlet stagnation pressure
- $Re_{c}$ : Reynolds numbers based on chord length and average exit velocity
- $RMS$ : rms value of velocity fluctuation based on time-averaged velocity
- $RMS_{e}$ : rms value of velocity fluctuation based on ensemble-averaged velocity
- $S_{0}$ : length of suction surface
- $St$ : Strouhal number of bar passing frequency
- $s$ : length from the leading edge

- $Tu_{in}$ : inlet turbulence intensity
- $t$ : airfoil pitch
- $U_{in}, U_{out}$ : inlet and averaged exit velocities
- $u_{r}, u_{t}$ : instantaneous and ensemble-averaged velocities
- $x$ : axial distance along the blade surface
- $Y_{x}$ : stagnation pressure loss coefficient
- $y_{r,t}$ : tangential direction, normal direction to the surface
- $\delta_{r,t}$ : displacement and momentum thicknesses based on time-averaged velocity
- $\delta_{r,t}$ : displacement and momentum thicknesses based on ensemble-averaged velocity

Subscript

1,2 : inlet, outlet
$x$ : axial direction

Abbreviation

FT : Freestream Turbulence
HL : High Lift
UHL : Ultra-High Lift

1. INTRODUCTION

Development of highly loaded low-pressure turbine (LTP) blades with high efficiency is a key element for competitive aero-engines under the circumstances of high-priced oil and intense demand for further reduction of fuel consumption. Because these highly loaded LTP blades are likely to suffer from significant aerodynamic penalty due to the occurrence of separation bubble on the blade suction surface, especially at high altitude cruising condition, they had been regarded as impractical blade in conventional design practices. In fact, as a number of relevant studies (for example, [1]-[5]) have revealed, the aerodynamic penalty associated with the separation bubble can be alleviated to some extent by taking advantage of the beneficial effects of inherent flow disturbances inside the engine such as wakes from upstream blades or freestream turbulence. In order to expand the design space of low pressure turbine blade much further, some researchers have proposed the attachment of separation control device on the blades, such as surface roughness, step, trench, dimple, jet, plasma actuator and so on. Although these devices look promising, they have to overcome some problems, for example
durability, cost, off-design performance or power supply, before being applied in actual aeroengines. Another approach to control the separation bubble is to manipulate chordwise aerodynamic loading so that the peak position of aerodynamic loading may locate near the leading edge side (front-loading) or near the trailing edge side (aft-loading). Howell et al. [6] investigated aft-loaded high-lift airfoils, giving rather a favorable estimation on those airfoils. Recently, Praissner et al. [7] made comprehensive measurements of midspan loss of several High-Lift LPT airfoils with different aerodynamic loadings derived from PAK B type airfoil. According to their findings, front-loaded airfoils surpass aft-loaded ones in many aspects. Superiority of front loading design to aft loading design in terms of midspan loss seems to be a widely accepted philosophy, apart from any 3D effects. Unfortunately, Praissner et al. did not provide any detailed information of separation bubble, and much remains unknown about the reason of the poor performance observed in the test cases using the aft-loaded airfoil. In addition, since Zhang et al. [8] reported that their aft-loaded airfoil exhibited better mispan aerodynamic performance than the front-loaded airfoil under the influence of wake passing, the present authors believe that further studies are still needed to deepen the understanding of what actually governs the midspan loss of front- or aft loaded airfoils.

This paper deals with low-speed wind-tunnel experimental studies on the flow field around three types of linear cascades of LPT airfoils with different chordwise loading distributions at a low Reynolds number condition, while keeping the aerodynamic loading index almost the same. The purpose of the studies is two-fold, which is first to see whether the front-loaded airfoil exhibits better midspan aerodynamic performance than the aft-loaded airfoil even under the influence of flow disturbances, i.e., freestream turbulence and incoming wakes, then to clarify the causes of the poor aerodynamic performance through the measurement of unsteady behaviors of the separation bubble on each of the airfoil suction surfaces. Pneumatic probes measure the aerodynamic performance such as time-averaged cascade loss. Unsteady RANS (Reynolds-Averaged Navier-Stokes equations) and LES (Large-Eddy Simulation) analyses are also extensively carried out using a commercial code to enhance the understanding of the flow physics.

2. Airfoil Design

Figure 1 shows the cross-sectional geometries of the three airfoils tested in this study, along with their aerodynamic loading distributions (static pressure distributions). It should be noted that each of the airfoils had the same axial chord length (= 100 mm) and the corresponding cascades were designed so as to exhibit similar aerodynamic performances among the three, such as inlet and outlet flow angles (see Table 1). Accordingly Zweifel factors defined by Eq. (1), an aerodynamic loading index of cascade, were almost the same among the three cases.

\[
Z = 2 \frac{t}{C_x} \cos^2 \beta \left( \tan \beta_1 + \frac{U}{U_c} \tan \beta_2 \right). \tag{1}
\]

Also note that those Zweifel factors were about 1.23 times higher than that of the airfoils used by Hoheisel et al. [8].

From the definitions of several important parameters of separation bubble based of the loading distributions shown in the bottom of Figure 1, the peak load position (\(x_p\)), separation point (\(x_s\)), transition point (\(x_t\)) and reattachment point (\(x_r\)) for each of the airfoils were determined as shown in Table 2. Also displayed on the lower part of this table are the normalized surface lengths which indicate the same positions as on the upper part of the table.

![Figure 1](image)

**Table 1** Airfoil geometry and cascade configuration

<table>
<thead>
<tr>
<th>Axial chord length, (C_x)</th>
<th>100 mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>Inlet flow angle, (\beta_i)</td>
<td>47 deg</td>
</tr>
<tr>
<td>Outlet flow angle, (\beta_o)</td>
<td>-60 deg</td>
</tr>
</tbody>
</table>

**Table 2** Positions of peak load, separation, transition and reattachment determined from the loading distributions, normalized with axial chord length (upper), showing the same positions by use of the surface length from the leading edge (lower)

<table>
<thead>
<tr>
<th>(x_p/C_x)</th>
<th>(x_s/C_x)</th>
<th>(x_t/C_x)</th>
<th>(x_r/C_x)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Front</td>
<td>42%</td>
<td>60%</td>
<td>80%</td>
</tr>
<tr>
<td>Mid</td>
<td>50%</td>
<td>70%</td>
<td>83%</td>
</tr>
<tr>
<td>Aft</td>
<td>60%</td>
<td>73%</td>
<td>82%</td>
</tr>
</tbody>
</table>

3. Experimental Setup

3.1 Test Apparatus and Cascade

Figure 2 shows the test apparatus, showing the test linear cascade, wake generator and the position of the turbulence grid. The cascade consisted of 6 or 7 airfoils, including the instrumented airfoil to measure static pressure distributions, dummy airfoils, where the number of the airfoils depended on the airfoil type used in the experiment. The span length of all tested airfoils was 260 mm. The pitchwise periodicity was achieved by adjusting two guide plates downstream of the cascade. The wake generator,
composed of two timing belts and cylindrical bars of 3mm diameter, was placed upstream of the cascade, with the streamwise distance between the leading edge of the airfoils and the locus of the bar motion being 115 mm. The belt was driven by the inverter-controlled induction motor and the optical tachometer monitored a speed of the belt by counting the rotation number of the driving gear of 101.06 [mm] pitch-diameter. Besides, two types of turbulence grids, which are not shown in Figure 1, were set 740 mm upstream of the leading edge of the center airfoil of the cascade to enhance inlet freestream turbulence. The turbulence grid was not parallel to the cascade due to the mechanical constraint of the test apparatus, however, it turned out that almost uniform distribution of the turbulence was attained around the middle of the cascade including the three airfoils to be measured.

Figure 2  Test apparatus, showing LPT cascade, turbulence grid and wake generator

Figure 3  Test cascade and the target blade for the boundary layer measurement with the indication of the measurement location on the front-loaded airfoil

3.2 Instruments

Midspan aerodynamic performance of the cascade was measured using two miniature Pitot tubes. One of the Pitot tubes measured inlet total pressure $P_{in}$ at the place 72 mm upstream of the leading edge of the center airfoil. The other Pitot tube for the outlet pressure measurement was placed 15 mm downstream of the trailing edge of the airfoils in the axial direction. The probe head was aligned with the exit flow direction from the cascade, using a tuft as flow direction indicator. A PC-controlled traversing unit automatically changed the probe position over the measurement area covering two pitches.

Figure 3 also depicts the location of the hot-wire measurement. The measurement area extended from $x/C_{12} = 0.5$ to the blade trailing edge in the streamwise direction and from $y_{+} = 0.2mm$ to 10mm in the direction normal to the blade suction surface. Another probe positioning machine, equipped downstream of the cascade with minimal blockage, enabled the automatic and precise probe positioning along the normal lines to the airfoil surface. The velocity data were acquired by a single hot-wire probe (Dantec 5SP11), then transferred to the CTA (Constant Temperature Anemometer, Kanomax) unit and A/D converted with sampling frequency of 20kHz. The size of each of the realizations, $N_j$, was $2^{13}$ word. Note that the air temperature was also measured by a thermocouple, then sampled and stored into the PC for the compensation of the measured data for temperature drift.

From these velocity data, $u_k$ ($k = 1, \ldots, N_j$), time-averaged velocity $\bar{u}$ and ensemble-averaged velocity $\bar{\bar{u}}$ were calculated by the following equations, respectively.

$$\bar{u}(x, y_{+}; j \Delta t) = \frac{1}{N_j \Delta t} \sum_{j=1}^{N_j} u_k(x, y_{+}; j \Delta t),$$

(3)

where $\Delta t$ was data sampling interval ($= 50 \mu s$), $N_j$ was the number of the realizations used for ensemble averaging ($= 100$). The outer edge of the boundary layer in this study was defined as the location where the time-averaged streamwise velocity reached 98% of the maximum velocity $U_{ref}$ attained within the measurement line normal to the surface. Ensemble-averaged and time-averaged boundary layer thicknesses such as displacement thickness or momentum thickness were calculated using ensemble-averaged and time-averaged quantities, respectively. This policy was also applied to the calculation of ensemble-averaged and time-averaged shape factors $\bar{H}_{12}$, $\bar{R}_{12}$ as follows,

$$R_{12}(x; j \Delta t) = \frac{\delta_1(x; j \Delta t)}{\delta_2(x; j \Delta t)}$$

(4)

$$R_{12}(x) = \frac{\delta_1(x)}{\delta_2(x)}$$

(5)

In addition, the following rms values of velocity fluctuation, based on the time-averaged and ensemble-averaged velocities, were used.

$$RMS(x, y_{+}) = \frac{1}{N_j \Delta t} \sum_{j=1}^{N_j} \frac{\sum_{k=1}^{N_j} (\bar{u}(x, y_{+}) - u_k(x, y_{+}; j \Delta t))^2}{N_j}$$

(6)

$$RMS(x, y_{+}; j \Delta t) = \frac{1}{N_j} \sum_{j=1}^{N_j} \frac{\sum_{k=1}^{N_j} (\bar{u}(x, y_{+}; j \Delta t) - u_k(x, y_{+}; j \Delta t))^2}{N_j}$$

(7)
3.3 Uncertainty Analysis

Uncertainty associated with the pneumatic measurement was governed by the accuracy of the pressure transducers. Most severe cases in terms of the measurement accuracy were for low-speed flow conditions ($U_{in} = 4.9\text{m/s}$). As mentioned above, the accurate pressure transducer with $\pm 0.5$Pa was used for these cases. The standard procedure [9] determined that the uncertainty of the inlet velocity $U_{in}$ was about $\pm 1.7\%$. Uncertainty of the static pressure coefficient turned out to be $\pm 3.5\%$ around the peak region of the coefficient on the suction surface.

The uncertainty associated with the pneumatic measurement also determined the accuracy of the hot-wire probe measurements because the probe calibration relied on the velocity measured with the Pitot tube while any other errors such as the error due to the curve fitting or temperature drift remained small (less than 1%). Therefore the uncertainty of the hot-wire probe measurement was estimated to be about $\pm 2\%$.

3.5 Test Conditions

This study examined the flow fields with the fixed exit Reynolds number $Re_{e} = 5.7 \times 10^4$, where the Reynolds number was defined as follows,

$$Re_{e} = C_{d}U_{e}/\nu,$$  \hspace{1cm} (8)

Unsteady flow field affected by the bar wake passing was characterized by Strouhal number of the wake passing frequency $f$, defined as,

$$St = fC_{d}/U_{e}.$$  \hspace{1cm} (9)

The inlet turbulence intensity was enhanced by means of two passive-type turbulence grids. Table 3 shows the data of these grids, along with those without any grid, referred to as "No Grid".

Table 3 Turbulence grids used in this study

<table>
<thead>
<tr>
<th>Grid</th>
<th>TG04</th>
<th>TG16</th>
<th>none</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mesh size</td>
<td>4 mm</td>
<td>16 mm</td>
<td>-</td>
</tr>
<tr>
<td>Wire diameter</td>
<td>0.2 mm</td>
<td>2 mm</td>
<td>-</td>
</tr>
<tr>
<td>Turbulence level</td>
<td>1.1%</td>
<td>2.0%</td>
<td>0.8%</td>
</tr>
<tr>
<td>Integral length scale</td>
<td>3.8 mm</td>
<td>8.4 mm</td>
<td>-</td>
</tr>
</tbody>
</table>

4. NUMERICAL SIMULATION

4.1 Computational Grids

Figure 5 demonstrates an example of the computational grids used in this code, where the overview of the grid is shown on the upper part and the details of the grid systems near the bar and airfoil are on the lower part of this figure. This grid system was for the analysis of the front-loaded airfoil using LES along with moving bar effects, while similar grid topologies and grid point numbers around the airfoils were employed for other simulations. The grid system in Figure 5 consisted of two blocks, one was for the cascade flow analysis and the other was for the moving bars for the investigation of bar wake-airfoil interaction. For the sake of simplicity the bar pitch was set to be the same as that of the cascade and single passage analyses were carried out, where the spanwise length of the computational domain was 10% of the axial chord length with 50 equally-spaced grid points. In the cascade block an O-type sub-block with 800 grid points was allocated around the airfoil in the middle of the sub-block and the extension of this sub-block was 8% of the axial chord length from the airfoil surface. The nearest grid point to the surface was at 0.15 in wall unit. Likewise, an O-type sub-block with 120 grid points was created around the bar. The bar was cut into two parts, with each half being attached with the top or bottom horizontal grid lines. The remaining computational domain of the cascade and bar blocks was filled with several H-type sub-blocks. The wall unit value of the nearest grid point was 0.3. Total number of the grid points then amounted to about 9.6 million, where the cascade and bar blocks used about 8.4 million and 1.2 million grid points.

4.2 Flow Solver and Boundary Conditions

The flow solver used in this study is a commercial software, ANSYS CFX 11. Large-Eddy Simulation (LES) using dynamic Smagorinsky subgrid scale model (DSM) was used as most reliable but time-consuming flow solver, which was mainly applied to the unsteady flow analysis of the aft-loaded airfoils in order to make a better understanding of complicated vortical motion of massively separated boundary layer. The second-order central difference scheme was used in space and the second-order backward scheme was employed in time. To make the analysis as time-accurate as possible, inner calculations during one time-step were repeated for 6 times at the maximum. Since the existence of large-scaled separation bubble on the suction surface was anticipated to worsen the convergence of the calculation, time-averaged Reynolds-Averaged Navier-Stokes (RANS) approach using Shear-Stress Transport (SST) two-equation model along with a transition model ($\gamma - Re_{e}$) was first executed to obtain an initial solution for the LES analysis. This approach using RANS was also employed for the unsteady flow analyses of front- and mid-loaded airfoils.

Figure 5 Overview of the computational grid for the analysis of bar wake/aft-loaded airfoil interaction (upper), with the close-up of the grid points around the bar and the airfoil (lower)
2 \rho C_x \left( P_0 - p(x) \right) \right] \right]^{1/2}.

(7)

However, it is clear from Figure 7 showing time-averaged $C_p(x)$ for the UHL condition obtained by DSM-based LES analysis provided much more accurate prediction than RANS.

5. RESULTS

The following discussions mainly deal with the experiments and simulations of the front- and aft-loaded airfoil cases. This is because the aerodynamic loadings shown in Figure 1 for these two cases are comparables each other, despite the similar level of Zweifel factors among the three.

5.1 Low Freestream Turbulence Condition

5.1.1 No Wake Condition

Static Pressure Distributions

Figure 8 shows the static pressure distribution of the aft-loaded airfoil measured under the no grid condition, compared with numerical results obtained by RANS and LES. As mentioned in Figure 1, there appeared sharp pressure recovery around $x/C_x = 0.8$, implying the existence of large separation bubble on the suction surface. It is clear that the RANS simulation yielded completely different flow field from the experiment. To make things worse, even LES analysis under the no wake condition ($St = 0.0$) failed to reproduce the flow field with no indication of reattachment of the separation bubble. The capability of CFD methods observed in the aft-loaded airfoil case was quite different from that observed in Figure 6, where even the RANS approach was able to make a reasonable prediction of the static pressure distribution of the front-loaded airfoil. This implies that the separation was considerably large so that the calculation was seriously affected by the separation or separation bubble. As discussed later, the LES analysis that took the bar-wake effects into account yielded a reasonable agreement with the measured static pressure distribution in term of its peak position, separation length and reattachment.

4.3 Code Validation

The validity of the code used in this study was checked through the comparison with the benchmark-like experimental data obtained by Funazaki et al. [4]. The airfoil in this case was the same as the airfoil of mid-loading used in this study. Figure 6 shows static pressure coefficient distributions calculated by the RANS with the transition model for three different solidity conditions, containing one of the test conditions used in the present study, front-loading. It appears that the present RANS simulations successfully reproduced overall characteristics of the static pressure distributions even for the highest loading condition (UHL), where the static pressure coefficient was defined as,

$$C_p(x) = \left( \frac{P_0 - p(x)}{2 \rho C_x^2} \right)^{1/2}.$$

Figure 7 Static pressure distribution calculated by LES with dynamic Smagorinsky model, compared with the experiment.

On the inlet boundary of the moving bar sub-block, all flow quantities, except inlet freestream turbulence intensity, were specified using the experimental data for $Re = 5.7 \times 10^4$ flow condition, while the mass flow rate was fixed on the outlet boundary of the cascade sub-block. Periodic condition was applied to the top and bottom grid lines, except for the locations of the bar in the bar block. Non-slip condition was specified on the airfoil and bar surfaces. The unsteady calculation was carried out in "Transient" mode of the solver. In this case the bar block slid along the interface grid line with a speed that corresponded to the specified Strouhal number, where Courant number was around 1.

4.3.1 Static Pressure

Figure 9 shows the calculated static pressure distribution for the UHL condition obtained by DSM-based LES analysis, compared with numerical results obtained by RANS and LES. As mentioned in Figure 1, there appeared sharp pressure recovery around $x/C_x = 0.8$, implying the existence of large separation bubble on the suction surface. It is clear that the RANS simulation yielded completely different flow field from the experiment. To make things worse, even LES analysis under the no wake condition ($St = 0.0$) failed to reproduce the flow field with no indication of reattachment of the separation bubble. The capability of CFD methods observed in the aft-loaded airfoil case was quite different from that observed in Figure 6, where even the RANS approach was able to make a reasonable prediction of the static pressure distribution of the front-loaded airfoil. This implies that the separation was considerably large so that the calculation was seriously affected by the separation or separation bubble. As discussed later, the LES analysis that took the bar-wake effects into account yielded a reasonable agreement with the measured static pressure distribution in term of its peak position, separation length and reattachment.

Figure 8 Experimental static pressure distributions on the suction surface of the aft-loaded airfoil, compared with numerical results calculated by RANS and LES with and without wakes.
**Velocity and Loss Measurements**

Figures 9 and 10 demonstrate contours of time-averaged magnitude and r.m.s. (root-mean-square) of the velocity measured by a single hot-wire probe for the airfoils with front- and aft-loadings under no grid and no wake condition, where the abscissa is the normalized suction surface length and the ordinate is the distance from the surface. It should be mentioned that the measurement zones started at 50% $C_s$ from the leading edge for both airfoil cases, however, due to the difference in airfoil shape the area which was actually measured by the probe was different each other. Note that the velocity on each of the measurement lines normal to the surface was normalized with maximum velocity detected on the line. Also shown in these contours are the 50% velocity lines, designated 50%U. These lines can be regarded as centerline of the shear layer between the main flow and the separated flow, and the peak of the velocity r.m.s. appeared around these lines.

As already shown in Table 2, also confirmed by these contours, the flow separation occurred much earlier on the front-loaded airfoil surface than on the aft-loaded airfoil surface. After the separation onset, the separated zone for the aft-loaded airfoil rapidly grew so that the 50% velocity line reached its peak position (about 3 mm from the surface) around $s/So = 0.73$, while the peak height of the 50% velocity line for the front-loaded airfoil, appearing around $s/So = 0.68$, became 2mm.

Figure 11 displays the influence of the load peak position upon several important flow indices that are directly and indirectly related to midspan cascade loss, i.e., midspan aerodynamic loss coefficient $Y_p$, time-averaged momentum thickness at 98% $C_s$ position and maximum height of the separation bubble. The loss coefficient was defined by,

$$Y_p(y) = \frac{P_{in} - P_{in}(y)}{\frac{1}{2} \rho U_0^2},$$

where the outlet stagnation pressure $P_{in}(y)$ was measured 15% $C_s$ downstream of the trailing edge in the axial direction. The ordinate of the plot in Figure 11 indicates each of the values of those indices normalized by the values obtained for the airfoil of mid-loading. Interestingly, all three indices increased with the downstream shift of the relative load peak position. This suggests that the aft loading in the present design brought about heightwise growth of the separation bubble, resulting in shedding of large-scale vortices from the separation bubble (high momentum thickness) and evolution of turbulent boundary layer after the reattachment (high midspan loss). In the following, the results of time-resolved velocity measurements and unsteady CFD reveal the details of the flow fields around the airfoils, especially under the influence of the wake passing.

**5.1.2 Effects of Wake Passing**

**Time-Averaged Static Pressure Distributions**

Figure 12 shows time-averaged static pressure distributions of the front-loaded airfoil obtained from the experiment and unsteady RANS (URANS) analysis using the transition model, in comparison with the corresponding steady-state results. It is clear from these results that the incoming wakes considerably suppressed the separation bubble. The wake interaction slightly reduced the aerodynamic loading over the first half of the suction surface, which was due to the decrease in incidence caused by the movement of the wake generating bars. The numerical simulation reasonably reproduced the time-averaged static pressure distribution, especially over the first half of the suction surface, including the above-mentioned moving bar effects. However, the disagreement between the measurement and the simulation became obvious over the last half of the suction surface, possibly because the bar wakes in the simulation decayed faster than in the experiment.
Figure 1. Distributions of stagnation pressure loss coefficients calculated under no-grid and with-grid conditions for S-15 solidity case, compared with the experimental data.

Figure 2. Time-averaged static pressure distribution of front-loaded airfoil under the influence of wake passing, compared with steady-state results.

As for the aft-loaded airfoil, Figure 8 also contains the information on the effects of wake passing upon the static pressure distribution. It turns out from the comparison between the measured data with and without bar wakes that the incoming wakes had relatively small influence upon the static pressure distribution on the airfoil surface, except for the area with separation bubble. The bar wakes induced slight increase in value of $C_p$ around the separated zone, followed by earlier transition of the separation bubble. It is quite interesting to see that the numerical simulation that took incoming wakes into account exhibited remarkable improvement in the prediction of the time-averaged measured data. This situation can be reconfirmed through the comparison of calculated velocity profiles with the measured ones, as shown in Figure 13. In this sense, LES with more sophisticated subgrid-scale model is one of the most preferable approaches to predict the flow field around highly loaded LPT airfoils. The reason for the large discrepancy between the experiment and the LES-based calculation for no wake condition is not clear. A plausible explanation can be that small but non-negligible free-stream turbulence, which was not considered in the calculation, considerably affected the separation bubble.

Figure 3. Comparison of calculated velocity profiles using LES with the measured ones for the aft-loaded airfoil (St = 0.8).

Figure 4. Ensemble-averaged shape factor on the time-surface distance for the front-loaded airfoil for St = 0.4 (measurement).
Discussion using Time-Space Diagram

Figure 14 and 15 are two types of time-space diagrams, displaying the interaction between the incoming wakes and the separated boundary layer on the front-loaded airfoil. Figure 14 shows the contours of ensemble-averaged shape factor on the time-surface length diagram for the case of wake passing Strouhal number $St = 0.4$, where time is normalized with the wake passing period and the surface length is normalized with the suction surface length. This figure contains one vertical dashed line at $s/S_0 = 0.61$. Composite contours of ensemble-averaged velocity and ensemble-averaged rms at $s/S_0 = 0.61$ calculated by Eq. (7) are shown in Figure 15, in conjunction with the information on time-varying edge velocity and the separation bubble represented by the shaded zone near the surface. Since the maximum and minimum values of the edge velocity were induced by inviscid effects of incoming wake deformation on the suction surface, so-called negative jet effect, the present study utilized the appearance of those two representative values in the time-space diagram like Figure 14 or 15 to indicate the front and the tail of the incoming wake, respectively.

By paying careful attention to the relationship between the contours of the shape factor and the dashed line with the name “Max”, it was found that the shape factor gradually increased before the arrival of the wake, then exhibited rather a abrupt decrease with time, while the local maximum of the shape factor shown by the solid line in Figure 14 lagged behind the wake movement. Since the temporal behavior of the wake-affected shape factor was directly related to the variation of the separation bubble height, it seems that the above-mentioned shift of the shape factor peak in the diagram could be caused by vortices shed from the separation bubble moving at a smaller advection speed than the main flow. This idea was supported by the evolution of the separation bubble (shaded region in which the flow velocity was less than 2m/s) and the appearance of high-valued rms regions with arrow-head like shape in Figure 15. It was also confirmed that the tail of the wake identified with the dashed line of “Min” corresponded to the valley or the local minimum region of the shape factor contour.

Figures 16 and 17 are the experimental data for the aft-loaded airfoil displayed on the two types of the time-space diagrams. Figure 16 contains the inclined lines showing the wake fronts and tails and the vertical dashed line corresponding to $s/S_0 = 0.67$ where the composite contours of ensemble-averaged velocity and rms shown in Figure 17 were obtained. In contrast to the data of the front-loaded airfoil, the separation bubble for the aft loading case was much thicker than that of the front loading case, and the separation bubble seemingly repeated growth and shrink in a sinusoidal manner, responding to the periodic wake passing. In addition, there was no clear indication of the shed vortices lagging behind the main flow.

Unsteady Behavior of Separation Bubble

While the ensemble-averaged velocity and rms data depicted on the two types of the time-space diagrams surely enhanced the understanding of the flow fields around the front and aft-loaded airfoils, it was expected that much more detailed information could be provided through the LES analyses along with some data-mining technologies to extract some important flow events from the vast size of the numerical data. Figure 18 is a series of snap shots of the wake-affecting flow field around the aft-loaded airfoil during one wake passing period visualized by iso-surface of
the Laplacian of pressure with the threshold of $1 \times 10^6$, colored with non-dimensional helicity. It is clear that elongated vortical structures became prominent inside the incoming wakes when they were largely deformed inside the passage. Besides, another type of flow visualization was made using spanwise vorticity as shown in Figure 19. This figure contains the pictures of the bar block showing the bars shedding vortices behind, however, the movement of the bars is not correctly illustrated here.

The snapshots at $t/T = 0.0$ in Figure 18 or 19 correspond to the instance after the bar wake hit the airfoil leading edge, with the wakes being largely deformed into bow-shape in the middle of the flow passage and about to overhang the separation bubble. The numerical results revealed that the separation bubble at this instance became largest during one passing period. At $t/T = 0.2$, large-scale vortex shedding started as if the shed vortex were being swept out from the suction surface, which resulted in the reduction of separation bubble. Similar situation still existed at $t/T = 0.4$, followed by the recovery phase of the separation bubble until $t/T = 1.0$. These findings almost corresponded to the measurement as shown in Figures 16 and 17.

**Loss Evaluation**

Figure 20 shows relationships between the maximum height of the separation bubble extracted from the time-averaged velocity and the time-averaged momentum thickness, which can be regarded as index of loss generated inside the boundary layer, for the three loading cases under several unsteady flow conditions. The momentum thickness for the aft-loaded airfoil was much larger than the other two loading cases, irrespective of wake passing Strouhal number. It appears that the higher the wake passing frequency was, the smaller the separation bubble became, eventually leading to smaller momentum thickness. In this sense, the wake passing is surely beneficial in reducing the boundary layer loss by suppressing the separation bubble. The reduction rate of the momentum thickness due to the wake differed among the three loading cases. The reduction rate was much larger for the aft-loaded or mid-loaded airfoil than for the front-loaded airfoil.

**5.2 Enhanced Freestream Turbulence Condition**

Figure 21 demonstrates time-averaged static pressure distributions around the mid-loaded airfoil under the influence of the wake passing and free-stream turbulence in comparison with the No Grid data with or without the bar wakes. An influence of the freestream turbulence was observed in these static pressure distributions over the suction surface before the suction peak, indicating the increase in aerodynamic loading near the leading edge for higher freestream turbulence condition. One possible explanation on this increase in the loading was the difference in degree of the separation bubble suppression caused by three different flow disturbances such as bar wake only, bar wake with TG04 and bar wake with TG16. Figure 22 provides an evidence for this statement. This figure compares the velocity rms contour under the influence of wake passing for no grid condition with that of TG16 turbulence grid condition. The freestream turbulence surely affected the separation bubble as indicated by the difference in the elevation of separation bubble. However, it is difficult to conclude that the loading increase observed in Figure 21 can be attributed to the above-mentioned difference in the flow disturbance, and further investigation is still required.
6. CONCLUSIONS

This study conducted detailed hot-wire probe measurements of the steady and wake-affected boundary layer on the three types of airfoils having almost the same Zweifel factor. Large Eddy Simulation using dynamic Smagorinsky model as subgrid scale model was also carried out to enhance the understanding of the complicated unsteady flow field. Important findings through this study are itemized as follows:

The numerical simulations using LES in conjunction with the dynamic model was superior to RANS simulations in predicting the flow field accompanied by large-scale separation bubble. The unsteady calculation that took accounted of the wake-generating bar movement yielded much better agreement with the measurements in terms of static pressure distribution and velocity profiles.

Among the three loading cases, the front-loaded airfoil exhibited better performances in terms of midspan loss, regardless of the existence of incoming wakes. It was found that the midspan loss or the momentum thickness at the trailing edge closely correlated with the maximum separation bubble height in this study.

Wake passing was surely useful to reduce the midspan loss, irrespective of the loading type. However, the response of the separation bubble to the wake passing were quite different between the front- and aft-loading cases. The wake-affected separation bubble on the aft-loaded airfoil varied in a sinusoidal manner in time, while the bubble of the front-loaded airfoil was featured with a kind of traveling mode, probably due to shed vortex moving downstream, being lagged behind the wake. The numerical simulation taking the bar wakes into account reproduced the above-mentioned experimental finding reasonably.

The impact of freestream turbulence was clearly seen as the difference in the elevation of separation bubble shear layer defined by the height of the 50% velocity line, even under the influence of bar wake passing.

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