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Researches on the Two-Dimensional Retarded Cascade*

(Part 3, Cascade Performances at High Inlet Angles)

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Two-dimensional cascade data at inlet angles higher than 65° have been presented which are required often for the designs of axial blowers. Cascade tests of NACA 65-series compressor blades were performed by the testing technique which had been established by the authors in the foregoing paper. Reliable data have been obtained in the ranges of inlet angle $\beta_1=65\sim 80^\circ$, solidity $\sigma=0.5\sim 1.33$ and camber $C_{10}=0.0\sim 2.0$. In the result, characteristics of cascade performance at high inlet angles are found. In the case of high inlet angles, it is necessary to take care in selecting a design angle of attack, because the range of angles of attack for effective operating region is very narrow. Limited blade loadings at high inlet angles are smaller than those at low inlet angles. Investigating various blade loading factors, defined by some researchers, Lieblein's equivalent diffusion ratio has been modified to apply also to cascades at high inlet angles. Some available data about the minimum loss coefficients and the blade efficiencies have been presented.

1. Introduction

Systematic experimental researches on a two-dimensional retarded cascade were carried out during the 1950's mainly in the U.S.A. and Gt. Britain with the development of CBT-engine for aircraft. And these results contribute to the design of an axial-flow compressor for industry. However, in the design of a blade row with inlet angles higher than 70° , a designer had no choice but to predict the cascade performance by extrapolating the data in the range of lower inlet angles or by modifying the isolated blade performance with the interference coefficient because of the scantiness of experimental cascade data. An exact prediction could not be expected in these ways. Therefore, the cascade performance at extremely high inlet angles has been demanded. For instance Serovy emphasized in his report "Recent Progress in Aerodynamic Design of Axial-Flow Compressor"⁽¹⁾, the need for blade design data for fluid relative inlet angles of 70° and above. And Turner⁽²⁾ compared several existing methods for selecting blade rows of a ducted fan with high inlet angles.

Although cascade data at considerably large inlet angles have been provided by Emery⁽³⁾, Shimo-

yama⁽⁴⁾ and Matsuki⁽⁵⁾ until now, these data leave doubts about the two-dimensionality of the test conditions. Moreover, it is inconvenient to employ them in blade design because they are restricted to the region of high inlet angles and discontinuous from the data at lower inlet angles.

In this paper, cascade tests of NACA 65-series compressor blades at high inlet angles are performed by the testing technique which was established by the authors as indicated in the foregoing paper⁽⁶⁾. Cascade performances are obtained in the range of inlet angles of $65\sim 80^\circ$ and pitch-chord ratios of $0.75\sim 2.0$, which are continuous from the existing performances of cascades with low inlet angles⁽⁷⁾.

2. Test program

The details of test apparatus and procedure have been described in Parts 1⁽⁸⁾ and 2⁽⁶⁾. Blade sections used in this experiment are NACA 65(216)-010, 410, 810, 1 210, 1 610 and 2 010 which are 10%-thick blades cambered to have isolated aerofoil coefficients of $c_{10}=0, 0.4, 0.8, 1.2, 1.6$ and 2.0 respectively. And performances of these blade sections are almost identical to those of NACA 65-(10 c_{10})10 series blade sections. Contours of these blade sections are shown in Ref. (9) etc. In order to investigate the performance at high inlet angles, tests are made over a range of angles of attack from negative to positive stall, for each combination of inlet angles $\beta_1=65^\circ, 70^\circ, 75^\circ, 80^\circ$ and pitch-chord ratios $t/l=0.75, 1.0$,

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1.5, 2.0. The angle of attack corresponding to negative or positive stall is determined by a double minimum loss of total pressure across the cascade. The test programs described above are summarized in Table 1. Cascade performances are obtained by traversing a special type of yaw-meter described in Part 2⁽⁶⁾ downstream of the cascade. And surface pressure distribution around a blade is obtained for blade sections of 410, 810 and 1210.

3. Discussion of results

The performances of cascades with inlet angles lower than 70° in this experiment agree with those obtained at NACA⁽⁷⁾. In this paper, therefore,

results at inlet angles of 75° and 80° will be shown. The turning angle $\Delta\beta$ and the total pressure loss coefficient ζ_p are represented against angle of attack α in Figs. 1(a)~(d) and Figs. 2(a)~(d).

In the region of high inlet angles, the characteristics of cascade performances are summarized as follows.

(1) The turning angle is reduced as the inlet angle increases, in the same manner as the case of low inlet angles. And the turning angle at an inlet angle of 80° does not become larger than 5° for the operating angles of attack corresponding to a small loss of total pressure.

(2) At the positive stalling angle of attack, the

Fig. 1 Cascade performances at $\beta_1 = 75^\circ$

Table 1 Cascade test program for NACA 65(216)-(10c₁₀)10 blade section

t/l	0.75	1.0	1.5	2.0
β_1				
65°	0-10, 4-10 8-10, 12-10 16-10	0-10, 4-10 8-10, 12-10 16-10	0-10, 4-10 8-10, 12-10 16-10, 20-10	0-10, 4-10 8-10, 12-10 16-10, 20-10
70°	0-10 4-10 8-10	0-10 4-10 8-10	0-10 4-10 8-10	0-10 4-10 8-10
75°	0-10, 4-10 8-10, 12-10	0-10, 4-10 8-10, 12-10 16-10	0-10, 4-10 8-10, 12-10 16-10	0-10, 4-10 8-10, 12-10 16-10
80°	0-10 4-10 8-12	0-10 4-10 8-10	0-10 4-10 8-10	0-10 4-10 8-10

turning angle does not fall in spite of a remarkable increase in the total pressure loss. Therefore, the lift coefficient at an extremely high inlet angle is not reduced after stalling. Figure 3 indicates lift and drag coefficients of cascades with blades cambered $c_{10}=0.8$ and with a pitch-chord ratio of 1.0 for various inlet angles. At $\beta_1=80^\circ$, the lift coefficient rises in spite of a remarkable increase in drag coefficient in this figure. The same phenomenon can be observed in the cascade test using blades of Göttingen 549-profile by Shimoyama⁽⁴⁾.

(3) As the camber of the blade increases, the turning angle and the loss of total pressure become larger for a given inlet angle, pitch-chord ratio and angle of attack. At large solidity, however, if the camber is larger than a certain limit, the turning

(a) $\beta_1=80^\circ$, $t/l=0.75$ (c) $\beta_1=80^\circ$, $t/l=1.5$ (b) $\beta_1=80^\circ$, $t/l=1.0$ (d) $\beta_1=80^\circ$, $t/l=2.0$ Fig. 2 Cascade performances at $\beta_1=80^\circ$

angle falls on the contrary and a remarkable increase in loss is recognized; that is, over-loading of blade occurs. The value of limiting camber for over-loading is reduced as the inlet angle increases. For example, in the case of $\beta_1=75^\circ$, $t/l=0.75$, blades with camber $c_{l0}=1.2$ (1 210-blade section) are under the over-loading as shown in Fig. 2(a). Therefore, a large cambered blade is unsuitable for a cascade with a high inlet angle and a large solidity.

(4) The higher the inlet angle β_1 and solidity of blades σ become, the larger the minimum total loss coefficient $\zeta_{p\min}$ is. When the inlet angle and solidity are higher than a certain value, $\zeta_{p\min}$ rises remarkably. A cascade with such an inlet angle and solidity is unusable. Figure 4 represents the minimum total loss coefficient for various inlet angles and solidities in a cascade with blades cambered $c_{l0}=0.8$. As is obvious from the figure, $\zeta_{p\min}$ becomes larger than 0.1 at $\beta_1=80^\circ$ and $t/l=0.75\sim 1.25$.

(5) Determine positive and negative stalling angle of attack by attaining a double minimum loss of total pressure, and define the effective operating range of angles of attack by the region between both stalling angles of attack. In a cascade with a large camber and a high inlet angle, the operating range becomes narrower as the inlet angle and solidity increase. Especially, the operating range is extremely narrow in the case of an inlet angle of 80° and a solidity higher than unity.

(6) The middle angle of attack in the operating range becomes smaller as the inlet angle increases and the solidity decreases.

4. Design angle of attack

In a cascade with high inlet angle, delicate care is necessary for selecting a design angle of attack,

Fig. 3 Lift and drag coefficient ($c_{l0}=0.8$)

because the effective operating range of angles of attack becomes narrower as the inlet angle increases as stated before. Until now, the following methods have been widely used to select the design angle of attack.

(1) The design angle of attack is determined on the basis of nominal condition which was introduced by Howell⁽¹⁰⁾. The nominal condition is defined as that pertaining to a cascade deflection which is 80% of its maximum.

(2) The design angle of attack is under the condition of maximum lift-drag ratio, which Carter called the optimum incidence and deflection⁽¹¹⁾.

(3) Defining the effective operating range of angles of attack by the region between positive and negative stalling angles of attack which correspond to a double minimum loss of total pressure, the middle angle of attack in the range is taken as design angle of attack⁽¹²⁾.

The design angle of attack based on term (1) or (2) is inadequate for the design of cascade at high inlet angle since the turning angle or the lift coefficient is not reduced for an angle of attack larger than the stalling angle in some cases. In this investigation, therefore, the design angle is determined based on term (3). Although such a definition appears somewhat arbitrary at first due to irregularity of stalling point, the plots of such design points are corrected so as to give consistent curves against solidity, blade camber and inlet angle. These design angles of attack are represented by arrow marks in Figs. 1 and 2.

Figure 5 shows a comparison between the design angles of attack obtained by this investigation and those given in other literatures⁽⁸⁾⁽⁵⁾⁽¹²⁾⁽¹³⁾ in the case of $c_{l0}=0.8$ and $\sigma=1.0$. In this figure, the result by Emery has been obtained for blades with thickness of

Fig. 4 Minimum total pressure loss coefficient ($c_{l0}=0.8$)

6% (806 blade section). One should note that the design angle of attack for the NACA carpet-plotting diagram⁽¹³⁾ is constant for the inlet angle, while the other design angles are reduced with the inlet angle. Namely, in the blade design using the NACA carpet-plotting diagram, the design angle of attack is determined from the camber of blade and the solidity only. The fact that the design angle is independent of the inlet angle can save trouble for design, and is rather a merit of blade design in the region of low inlet angles where the effective operating range of angles-of-attack is considerably wide. However, the design angle may be out of the operating range in the region of high inlet angles because the operating range becomes extremely narrow. An example is shown in Fig. 6 which is quoted from Ref. (7). Turning angles and total pressure loss coefficients are replotted against angle of attack for cascades with $c_{l0}=1.5$, $\sigma=1.0$, and various inlet angles. The design angle of attack obtained from the NACA carpet-plotting diagram is indicated by an arrow mark. As is obvious from the figure, in the case of $\beta_1=70^\circ$, the arrow mark is out of the region of small total pressure loss. Therefore the use of the NACA carpet-plotting diagram is apt to cause a serious error in the selection of compressor blades with high inlet angles. Especially, one must give up selecting the blade sections by means of extrapolation in the region of inlet angles higher than 70° .

5. Blade loading limit at high inlet angles

Several definitions of blade loading factors and loading limits have been presented by Howell, Carter, Wislicenus, Mellor, Lieblein and others. However, it is not satisfactory to apply these definitions to cascades at high inlet angles. In order to find a proper definition of blade loading factor which can be applied also to cascades at high inlet angles, the author investigated various blade loading factors

Fig. 5 Comparison of various design angles of attack proposed by some researchers

presented by some researchers. As the result, Lieblein's equivalent diffusion ratio⁽¹⁴⁾ was modified so as to be available for a wide range of inlet angles.

5.1 Lieblein's equivalent diffusion ratio⁽¹⁴⁾

Lieblein obtained the following relation by solving von Karman's momentum equation approximately. That is

$$(\theta/l)_2 = \varepsilon / \{1 - k_s \ln(v_{s, \max}/v_2)\} \quad \dots\dots\dots (1)$$

where, $(\theta/l)_2$ is the dimensionless momentum thickness of the wake referring to blade chord length, $v_{s, \max}$ is the maximum velocity along the suction surface of blade, ε is a factor which involves primarily the skin-friction coefficient on the blade surface and k_s is a factor which is closely related to the form factor of wake or boundary layer. ε and k_s can be obtained from the available experimental data, that is $\varepsilon = 0.004$ and $k_s = 1.17$ according to Lieblein's work.

The term $v_{s, \max}/v_2$, which may be called a suction surface diffusion ratio, can be represented as follows from the continuity of flow.

$$\frac{v_{s, \max}}{v_2} = \frac{v_{s, \max}}{v_1} \frac{\cos \beta_2}{\cos \beta_1} \left\{ 1 - \left(\frac{\theta}{l} \right)_2 \frac{H_2 \sigma}{\cos \beta_2} \right\} \quad \dots\dots\dots (2)$$

where H_2 is the form factor [displacement thickness/(momentum thickness)] of the wake, and $H_2 = 1.08$ according to Lieblein.

An empirical correlation was established between $v_{s, \max}/v_1$ and the circulation parameter (dimensionless tangential force acting on a blade) $c_w = F_w / (1/2 \rho v_1^2 l) = 2 \cos^2 \beta_1 (\tan \beta_1 - \tan \beta_2) / \sigma$ at the minimum loss angle of attack. That is,

$$v_{s, \max}/v_1 = 1.12 + 0.61 \cos^2 \beta_1 (\tan \beta_1 - \tan \beta_2) / \sigma \quad \dots\dots\dots (3)$$

Substituting Eq. (3) into Eq. (2), and considering

Fig. 6 Cascade performance quoted from Ref. (7) and the design angle of attack obtained from the NACA carpet-plotting diagram⁽¹³⁾

$(\theta/l)_2 H_2 \sigma / \cos \beta_2 \ll 1$, Lieblein's equivalent diffusion ratio at minimum loss designated by the symbol D_{eq} is obtained in terms of inlet and outlet conditions as

$$D_{eq} = \frac{\cos \beta_2}{\cos \beta_1} \left\{ 1.12 + 0.61 \frac{\cos^2 \beta_1}{\sigma} (\tan \beta_1 - \tan \beta_2) \right\} \cong v_{s, \max} / v_2 \quad (4)$$

According to Eq. (1), the wake momentum thickness $(\theta/l)_2$ increases with $v_{s, \max} / v_2$ (that is D_{eq}), and the rate of increase of wake momentum thickness and therefore a large rise in loss may become marked for values of $v_{s, \max} / v_2$ greater than about 1.9~2.0. Thus, the blade loading limit for design use can be expressed as follows,

$$D_{eq} \leq 2.0 \quad (5)$$

This relation is widely used for the design of compressor blade because it can be extended to the blade row with change in axial velocity or change in radius of stream surface⁽¹³⁾. But, Eq. (5) is not applicable to a cascade at extremely high inlet angles. Next, the author will modify this relation so as to be available for a cascade at high inlet angles.

5-2 Diffusion ratio in the case of high inlet angles

All data used in this section are those for the design angle of attack determined at Section 4 (therefore at minimum loss). Lieblein's equivalent diffusion factors at minimum loss are plotted against the inlet angle in Fig. 7. In the region of inlet angles higher than 75°, the values of D_{eq} cannot be greater than 2.0 if blade camber c_{t0} becomes larger, and it appears that the limit of D_{eq} may be reduced with the inlet angle. Although Eq. (5) is not suitable for a cascade at high inlet angles, it can be modified as follows.

At extremely high inlet angles, the second term in the bracket of Eq. (2) is not ignored because

Fig. 7 Lieblein's equivalent diffusion factor at minimum loss

$(\theta/l)_2$ is considerably large and $\cos \beta_2$ is small. Since accuracy of approximation in Eq. (4) becomes poor, the suction-surface diffusion ratio $D_2 = v_{s, \max} / v_2$ is employed for the following analysis instead of D_{eq} .

Now, it is necessary to investigate the applicability of Eqs. (1), (2) and (3), since D_2 is evaluated from these equations by eliminating $(\theta/l)_2$ and $v_{s, \max} / v_1$. Among these equations, Eq. (2) should hold regardless of the inlet angle.

Figure 8 shows the relation between $v_{s, \max} / v_1$ and $c_u = \cos^2 \beta_1 (\tan \beta_1 - \tan \beta_2) / \sigma$. In this experiment, $v_{s, \max} / v_1$ is obtained from the surface pressure distribution around a blade using the next relation.

$$v_{s, \max} / v_1 = \sqrt{1 + \left\{ (p_1 - p_{s, \min}) / \left(\frac{1}{2} \rho v_1^2 \right) \right\}}$$

where p_1 and $p_{s, \min}$ are the upstream static pressure and the minimum local pressure on the suction surface, respectively. In Fig. 8, the relation of Eq. (3) is represented by a broken line and the results quoted from Ref. (7) are replotted and indicated by cross marks. It can be verified from the figure, that Eq. (3) holds also in the range of high inlet angles of 65° to 80°.

The relation between the equivalent diffusion ratio D_{eq} and the wake momentum thickness $(\theta/l)_2$

Fig. 8 Relation between the circulation parameter and $v_{s, \max} / v_1$

Fig. 9 Relation between the equivalent diffusion factor and the wake momentum thickness

at the design angle of attack is shown in Fig. 9, where $(\theta/l)_2$ is calculated approximately from the following relation⁽¹⁶⁾. Namely,

$$(\theta/l)_2 \cong (\zeta_p/2\sigma)(\cos^3\beta_2/\cos^2\beta_1)$$

which corresponds to Eq. (9) substituting $[] = 1$ into it as described later. In the figure the relation of Eq. (1) substituting $v_{s, \max}/v_2 \cong D_{2,q}$ is represented by a broken line. Although considerable scatter is observed in experimental values, one can find the general tendency that the rate of increase in the wake momentum thickness for the equivalent diffusion ratio becomes remarkable as the inlet angle increases. And it appears that Eq. (1) is not applicable to the cascade data at high inlet angles. This tendency may be caused by the fact that in a cascade with an extremely high stagger angle the behaviour of the boundary layer or wake differs from the case of low stagger angle. Namely, the factors k_s and H_2 which represent form factors of the wake should be functions of the stagger angle. However, since a change in the form factor H_2 has little effect on the value of D_2 in Eq. (2), it seems to be quite proper to consider that $H_2 = 1.08$ and only the term k_s is a function of the stagger angle.

Investigating the values of D_2 at the design angle of attack in the range of $c_{l0} = 0.0 \sim 2.0$, $\beta_1 = 65^\circ \sim 80^\circ$ and $t/l = 0.75 \sim 2.0$, the authors obtained the following experimental formula in the region of high inlet angles.

$$k_s = 2.0 - 1.4 \cos \gamma \dots \dots \dots (6)$$

where γ is the stagger angle. According to Eq. (1), a limiting value of suction-surface diffusion ratio is reached for each value of k_s when $(\theta/l)_2$ approaches infinity. The limiting value is given by $D_2 = \exp(1/k_s)$. Assuming that an allowable value of diffusion ratio (blade loading limit) may be 90 per cent of this value, the allowable value is expressed as follows.

Fig. 10 Suction surface diffusion ratio at minimum loss

$$D_{2, \lim} = 0.9 \exp\{1/(2.0 - 1.4 \cos \gamma)\} \dots \dots \dots (7)$$

Figure 10 shows suction-surface diffusion ratios at the design angle of attack against the stagger angle for various cascade geometries. In the figure, the relation of Eq. (7) is represented by a thick solid line. It is obvious from the figure that the following expression should be proper as a measure of blade loading limit at high inlet angles.

$$D_{2,q} \leq D_{2, \lim} = 0.9 \exp\{1/(2.0 - 1.4 \cos \gamma)\} \dots \dots \dots (8)$$

However, it is troublesome to calculate the value of D_2 because D_2 is involved implicitly in Eqs. (1) to (3). Therefore, the following relation will be satisfactory instead of Eq. (8) for a practical purpose of the blade design.

$$D_{2,q} \leq D_{2, \lim} \dots \dots \dots (8)'$$

Because, considering that $D_{2,q} < D_2$, the relation of Eq. (8) holds always if the relation of Eq. (8') is satisfied.

6. Minimum total pressure loss coefficient and blade efficiency

From this investigation, it becomes clear that the minimum loss coefficient of total pressure loss ζ_p^* becomes larger qualitatively as the solidity, the inlet angle and the camber of blade increase respectively. Although it is difficult to obtain the quantitative relation between ζ_p^* and cascade geometry using experimental data, the minimum total pressure loss coefficient can be obtained from the wake momentum thickness $(\theta/l)_2$ of Eq. (1) using the energy equation of cascade flow. According to Lieblein's investigation⁽¹⁶⁾, it is given as

$$\zeta_{p0} = 2 \left(\frac{\theta}{l} \right)_2 \frac{\sigma}{\cos \beta_2} \left(\frac{\cos \beta_1}{\cos \beta_2} \right)^2 \left[\left(\frac{3H_2}{3H_2 - 1} \right) \times \left\{ 1 - \left(\frac{\theta}{l} \right)_2 \frac{\sigma H_2}{\cos \beta_2} \right\}^{-3} \right] \dots \dots \dots (9)$$

Figure 11 shows a correlation between experimental minimum loss coefficient ζ_p^* and the loss coefficient ζ_{p0} calculated from Eq. (9). In the figure the results from Ref. (7) are replotted in addition to our experimental data. As is evident from the figure, the correlation of $\zeta_p^* = 1.2 \zeta_{p0}$ is obtained from our results. In the calculation of Eq. (9), $(\theta/l)_2$ was obtained from Eq. (1) substituting $\varepsilon = 0.004$ and Eq. (6) into it. ζ_p^* and ζ_{p0} would have been identical if $\varepsilon = 0.0048$ were employed for the calculation.

But, if $k_s = 1.17$ is used in the calculation of Eq. (1) according to Lieblein's work⁽¹⁶⁾, the calculating results may disagree with the experimental results. Figure 12 shows the relation between minimum total pressure loss coefficient and camber of blade for various inlet angles. In this figure, two kinds of calculating results of Eq. (9) are represented by solid lines and broken lines; the former indicates the calculation in which $(\theta/l)_2$ is evaluated from Eq. (1)

substituting $\varepsilon=0.0048$ and $k_s=2.0-1.4 \cos \gamma$ into it, and the latter indicates the calculation with $\varepsilon=0.0048$ and $k_s=1.17$. In the cascade at extremely high inlet angles (higher than 75°), experimental values of ζ_p^* rise as the blade camber increases. But, the calculating loss coefficients with $k_s=1.17$ are reduced on the contrary, while the other results (with $k_s=2.0-1.4 \cos \gamma$) show a similar tendency to the experimental results. From this fact, it can be confirmed also that the factor k_s changes according to Eq. (6) and the blade loading limit is reduced with the stagger angle.

Fig. 11 Correlation of total pressure loss coefficients obtained from Eq. (9) and the experiments

Fig. 12 Relation between the minimum loss coefficient and the blade camber

As the minimum loss coefficient ζ_p^* of total pressure is known, the blade efficiency η_b^* at minimum loss (at design angle of attack) can be obtained from the following relation.

$$\eta_b^* = 1 - \zeta_p^* / (1 - \cos^2 \beta_1 / \cos^2 \beta_2) \dots \dots \dots (10)$$

Examples are given in Figs. 13(a) and (b). Figure 13(a) shows the relations between η_b^* and c_{l0} for a cascade with $\sigma=1.0$ for various inlet angles, and Fig. 13(b) shows the relation between η_b^* and σ for a cascade with a blade cambered to $c_{l0}=0.8$. Blade efficiencies at $\beta_1=45^\circ$ quoted from Ref. (7) are replotted in these figures. From these figures, the following results can be obtained.

(1) In a cascade at a higher inlet angle than 45° , the blade efficiency is reduced as the inlet angle increases if the solidity is given.

(2) There exists the value of camber c_{l0} at which the blade efficiency is maximum for a given solidity and inlet angle. The value of c_{l0} decreases as the inlet angle becomes higher.

(3) As the solidity increases, the blade efficiency decreases. And the rate of decrease is more remarkable at higher inlet angles.

7. Conclusions

The results in this paper can be summarized as follows:

(1) Reliable data of NACA 65-series compressor blades at high inlet angles have been obtained in a wide range of cascade geometries.

(a)

(b)

Fig. 13 Blade efficiency at high inlet angles

(2) Characteristics of cascade performance at high inlet angles have been clarified.

(3) It is necessary to take care in selecting a design angle of attack in the region of extremely high inlet angles, because the range of angles of attack corresponding to small loss of total pressure is very narrow.

(4) In order to evaluate the limited blade loading of a cascade at extremely high inlet angles, Lieblein's equivalent diffusion ratio has been modified.

(5) The minimum loss coefficient of total pressure and the blade efficiency at the design angle of attack have been investigated.

In the following paper, based on these results, the author will describe methods of selecting the blade element for the design of axial-flow compressor blade rows. Lastly, the authors wish to thank Messrs. H. Okamoto and F. Mōri for their cooperation in the laboratory work, and he is also pleased to acknowledge a large supply of the test blade sections from The Kobe Technical Institute, Mitsubishi Heavy Industries, Ltd.

Discussion

M. MATSUKI and M. MINODA (National Aerospace Laboratory):

(1) The effective operating range in authors' cascade test is shown against the inlet angle in Fig. 5. But it seems that its range is too wide from the debaters' experiences. For instance, Append.-Fig. 1 shows the exit and turning angles of a rotating cascade with the values of $c_{10}=0.815$, $\gamma=61.9^\circ$ and $t/l=1.18$ (*1). (Solid lines indicate the result of two-dimensional cascade testing⁽⁵⁾ and I-marks show the critical point below which the two-dimensionality of the flow is obtained in the cascade test.) As shown in Append.-Fig. 1, the stall begins at $\alpha=6.0^\circ$ (namely $\beta_1=67.9^\circ$). On the other hand, the stalling point is $\alpha=14^\circ$ in Fig. 5 and it does not agree with the debaters' experiment.

(2) In Ref. (5), the debaters pointed out that it was difficult to obtain the two-dimensionality of the cascade flow in the region of high inlet angles. Clarify the authors' criterion of the two-dimensional flow. What is the amount of removed air from the porous wall of the cascade tunnel?

(3) Generally speaking, the two-dimensional cascade test should be carried out according to the testing techniques described in Ref. (6). However, the greatest trouble in the cascade test is to confirm the two-dimensionality of the flow for each cascade configuration, in the strict sense, in each test run.

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Therefore, it is desirable that the amount of the removed air and the axial velocity ratio be shown for each measured point in Figs. 1 and 2, for use of these cascade data or for convenience of the cascade testing.

Append.-Fig. 1 Results of rotating cascade tests (*1)

Authors' closure

(1) Because of the great three-dimensionality of the flow in the axial-flow blades, the stalling point of the rotating cascade varies case by case even if the geometry of the cascade is identical. And it does not always coincide with the stalling point of a two-dimensional cascade. In the authors' experiment (Fig. 11 in Part 2^(*)), for instance, the stall begins at $\alpha=10.9^\circ$ ($\beta_1=72.3^\circ$) in the case of $c_{l0}=0.8$, $\gamma=61.4^\circ$ and $t/l=1.29$, and it differs from Append.-Fig. 1. In another test, the stalling angle of attack of the rotating cascade is larger than that of a two-dimensional cascade.

According to note (*1), Append.-Fig. 1 shows the test results obtained near the tip of the second impeller in the 2-stages axial-flow blower, and the inlet angles were not measured directly but estimated approximately from the flow rates. Therefore, the inlet angle may be uncertain near the stalling condition. Furthermore, the stalling of the test blade begins near the hub. Therefore, a somewhat small angle of attack will be obtained near the tip of blade. In any case, it is necessary to consider the three-dimensional effects in the prediction of stalling point of the rotating cascade.

Further, the stalling lines of Fig. 5 represent roughly the stalling conditions to compare various test results. In the authors' experiment, the stalling point is $\alpha=12.0^\circ$ in the case of $c_{l0}=0.8$, $t/l=1.0$ and $\beta_1=70^\circ$ as shown in Fig. 2 of note (*3).

(2) The criterions of the two-dimensionality of the cascade flow defined by the authors, are indicated in note (*2). The total amounts of removed air from the porous wall to maintain aerodynamically two-dimensional flows, are different according to the cascade configuration. For reference, some examples of the ratio c_q of total removed air volume to the total air flow rate through the cascade are shown in

Append.-Fig. 2 The total amount of removed air from the slits and porous walls which is required for an aerodynamically two-dimensional flow

Append.-Fig. 2, taking the pressure rise coefficient on the abscissa.

(3) The cascade data in Figs. 1 and 2 are obtained under the two-dimensional flow condition with the criterions described in note (*2). In the case of $\beta_1=75^\circ$, for instance, the axial velocity ratio is maintained below the critical value of 1.02. Further, the turning angle is corrected by the method described at Section 4 in note (*2) in the case that the axial velocity ratio is not unity (even if the deviation from unity is very small), since it is very troublesome to keep the ratio just at unity. Therefore, it is meaningless to show the axial velocity ratio in Figs. 1 and 2.

The amount of the removed air required to obtain the two-dimensional flow changes remarkably depending on the aspect ratio of cascade, the condition of porous wall and the boundary layer thickness of the inlet flow. Therefore, the amount of the removed air is not shown in Figs. 1 and 2. To determine its amount, note (*2) and Append.-Fig. 2 will serve as references.

(*) Ikui, T., et al., *Bull. JSME*, Vol. 15, No. 84 (1972-6), p. 713.

(*) Ikui, T., et al., *Bull. JSME*, Vol. 16, No. 92 (1973-2), p. 262.