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Researches on the Two-Dimensional Retarded Cascade*

(Part 1, Contraction Effect in the Cascade)

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In the two-dimensional retarded cascade, it is well known that the axial velocity increases through the blade row unless the boundary layer on the sidewall of the wind tunnel is sucked away. Increase of the axial velocity at outlet often causes a drop in the performance of cascade.

The first purpose of this paper is to find, among many factors that prescribe the cascade geometry, which factor affects most remarkably the two-dimensionality of flow. The axial velocity ratio, which represents the degree of two-dimensionality, depends mainly on the virtual retardation ratio $\cos\beta_1/\cos\beta_2$ at the center of blade span.

Secondly, the performances of a cascade with solid sidewall and one with porous sidewall for boundary layer suction are compared. The difference of the performance between both cascades increases as the inlet angle becomes larger. Furthermore, comparison between the effective operating ranges of both cascade arrangements shows a remarkable difference in the case of high inlet angles.

1. Introduction

In the two-dimensional retarded cascade testing, it is well known that the axial velocity increases through the blade row unless the boundary layer on the sidewall of the wind tunnel is sucked away. If a boundary layer develops on the sidewall, the flow cannot be made truly two-dimensional. Increase of the axial velocity at the outlet often causes a drop in performance of the cascade. This problem has been studied by many researchers^{(1)~(10)}, and further investigation seems, apparently, to be unnecessary. But, there are still some questions. One of the most important problems in the cascade testing is to find out among many factors which prescribe the cascade geometry and flow condition such as the shape of the blade section, aspect ratio, solidity, stagger and inlet angle, which factor affects most remarkably the increase in axial velocity at the outlet of cascade. Especially, the contraction effect in the cascade flow at high inlet angles is not yet clarified. The answer to this question may be given in this paper.

2. Nomenclature

Symbols used in this paper are illustrated as

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follows, referring to Fig. 1.

- a : coordinate, in the axial direction
- AR : aspect ratio of blade
- c_a : dimensionless force in the axial direction
- c_D : drag coefficient
- c_L : lift coefficient
- c_p : static pressure rise coefficient
- c_x : dimensionless force parallel to blade chord
- c_y : dimensionless force normal to blade chord
- D : drag force
- F : force acting on unit breadth of a blade
- L : lift force
- l : chord length of blade
- P : total pressure

Fig. 1 Coordinate system, cascade geometry and velocity diagrams

- P_t : loss of total pressure across the cascade
 p : static pressure
 Δp : static pressure rise ($p_2 - p_1$)
 q : dynamic pressure ($1/2 \rho v^2$)
 s : coordinate, in the spanwise direction
 t : space between blades (pitch of the cascade)
 u : coordinate, in the tangential direction
 v : flow velocity
 α : angle of attack
 β : flow angle referring to the axial direction
 $\Delta\beta$: turning angle ($\beta_1 - \beta_2$)
 γ : stagger
 ζ_p : total pressure loss coefficient
 λ : axial velocity ratio (v_{a2}/v_{a1})
 ρ : density of fluid

Subscripts

- 1: referring to the inlet of cascade
 2: referring to the exit of cascade
 a: axial flow component
 s: spanwise component
 th: referring to the ideal flow
 u: tangential component
 ∞ : referring to the vector mean value

3. Description of test apparatus and procedure

The test facility used in this investigation is the low-speed cascade wind tunnel in Kyushu University, which is specially equipped in order to carry out the cascade tests of high inlet flow angles. A schematic diagram of the facility is shown in Fig. 2. The working section of the tunnel has a span of 300 mm and a height of 530 mm. In order to have various inlet angles, it is possible to turn the frame of the cascade ①, by using the vertical screw ② and the guide rail ③, which support the frame, so that the center blade

of the cascade is always situated in the center of the wind tunnel. The tunnel end piece, which introduces the inlet flow from the contraction section to the cascade test section, consists of triangle side plates ④ which are interchangeable corresponding to various inlet flow angles. The top and the bottom plates ⑤, ⑥ are movable parallel and normal to the axis of the wind tunnel, in such a way as to keep the prescribed condition of inlet flow constant for all cascade geometries. Boundary layers growing on these walls (④, ⑤ and ⑥) are removed from 10 mm-slits ⑦ upstream of the cascade by 50 mm, which are slotted on the side walls of the frame of the cascade and the top and the bottom plates. For the tests of high inlet angle cascades, it is necessary to control the inlet boundary layer using the porous bottom plates instead of the slit, lest flow separation should occur in the acute corner between the cascade and the bottom plates⁽¹¹⁾. The blade supporter which forms the side-wall of the cascade is a 100×1600 mm rectangular porous plate ⑧ which consists of a 30 mesh hammered brazen screen supported by a sheet of perforated metal with 5-mm-diameter holes. Blade setting holes are arranged with a spacing of 25 mm on this plate. In order to insure the condition of an infinite number of blades, deflectors ⑨ are set up at the upper and the lower sides of the cascade which also consist of porous plates. For the boundary layer removal from these slits and porous plates, sixteen 1 kW-centrifugal blowers are used as exhausters, and the suction of a boundary layer on an individual wall is controlled by adjusting the rotational speed of each exhauster.

The blade section used in the experiment is NACA 65(216) 810 profile. Its chord length is 100 mm and its aspect ratio is 3. The number of blades is 5 to 9 corresponding to various cascade geometries. The Reynolds number based on the chord length is about 2.1×10^5 .

Two series of cascade tests are made; i.e. solid-wall and porous-wall cascade tests. In the case of the solid-wall cascade tests, the boundary layer is removed from the 10 mm-slits upstream of the cascade to make the inlet flow velocity profile uniform, and all the blades of the cascade are made to operate under the same flow conditions by adjusting the gaps between the end blade and the top or bottom wall, and by controlling the direction of the deflector with the boundary layer suction. The tests of the porous-wall cascades are carried out according to the testing technique as described in the following paper⁽¹¹⁾.

The test program of each series is planned to provide sufficient information concerning the performance in a wide range of cascade geometries. For this purpose, tests of both the solid and porous wall

Fig. 2 Sketch of the test facility

cascades are carried out, for each combination of inlet angles $\beta_1=35^\circ, 50^\circ, 80^\circ$ and pitch-chord ratio $t/l=1.0$, and $\beta_1=65^\circ$ and $t/l=0.75, 1.0, 1.25, 1.5, 1.75, 2.0$. And each test is carried out at intervals of 2.5° of angle-of-attack from negative to positive stall, where the stall is defined as the state at which the total pressure loss becomes twice the minimum pressure loss.

The performances of cascades are obtained by measuring the downstream of cascade and the pressure distribution around the blade. Surveys of total pressure in the wake, exit flow velocity, static pressure rise and flow turning angle through the cascade are made by the traversing of Pitot, cobra and tuft probes in the pitchwise direction at the $1.0\sim 2.0$ chord length downstream from the trailing edge of the middle section of the blade span. The blade surface pressure distributions are measured at the twenty-three pressure holes which tap the middle section of the blade span. (One hole is at the leading edge, and eleven holes are located on the suction and pressure surfaces respectively.)

4. Axial velocity ratio in the solid wall cascade

In the solid wall cascade, a remarkable flow contraction occurs across the cascade. First, the effect of the cascade configuration and the inlet flow condition on the flow contraction has been investigated. The axial velocity ratio $\lambda=v_{a2}/v_{a1}$ at the center of the blade span can be considered as a measure of the contraction. Figure 3 shows experimental relations between the axial velocity ratio and the turning angle $\Delta\beta$ at the center of the blade span for various

Fig. 3 Relation between axial velocity ratio λ and turning angle $\Delta\beta$ in the solid wall cascade

inlet angles. λ varies linearly with $\Delta\beta$, unless flow separation occurs at the center of the span. The rate of the increase of λ against $\Delta\beta$ is greater as the inlet angle becomes higher. And as is apparent from the result of $\beta_1=65^\circ$ shown in Fig. 3, the pitch-chord ratio is not dependent on the relation between λ and $\Delta\beta$. A remarkable increment of λ is observed even for a small turning angle in the case of inlet angles higher than 65° . In this case, an aerodynamically two-dimensional flow is no longer expected.

Horlock et al.⁽⁸⁾ attempted to predict the increment of the axial flow velocity, but it was much larger than that evaluated from the theories of boundary layer and secondary flow. This enormous contraction is mainly due to boundary layer separation initiated in the corner between the blade suction surface and the solid sidewall of the wind tunnel.

Taking note on that the increase of λ is mainly due to the flow separation, the authors investigated the relation between λ and the ratio of $\cos\beta_1/\cos\beta_2$ which was often called the Haller's factor and was used as a criterion of the wall stall of the compressor. In this paper, this ratio will be called the apparent retardation ratio, because it will represent a retardation ratio of flow velocity if the flow through cascade is aerodynamically two-dimensional. The relation between λ and $\cos\beta_1/\cos\beta_2$ in this experiment is given in Fig. 4. It is apparent that λ can be expressed as a function of $\cos\beta_1/\cos\beta_2$ but not of the inlet angle or the pitch chord ratio. In order to make sure the generality of this relation, the results obtained from other cascade wind tunnels by many researchers are replotted in the figure. Specifications of these cascades are indicated in Table 1. As is evident from Fig. 4, the correlation of λ and $\cos\beta_1/\cos\beta_2$ is different according to the test equipment. However, concerning one cascade tunnel, the correlation between two factors is independent of blade section, stagger angle, pitch-chord ratio, and it is verified that λ is a function of $\cos\beta_1/\cos\beta_2$ only.

	Symbol in Fig. 4	Span	Aspect ratio	Blade section	Inlet angle or stagger	Pitch-chord ratio	Reynolds number
Kawasaki ⁽⁵⁾	▲	450mm	3.0	BH2-1236-10	$\beta_1=42^\circ\sim 72^\circ$	1.0	$2.7\sim 4.3 \times 10^5$
	△	450mm	3.0	BH2-1236-10	$\beta_1=42^\circ\sim 72^\circ$	1.2	
Montgomery ⁽¹²⁾	◇	20in	4.1	NACA65-410	$\beta_1=48^\circ\sim 62^\circ$	1.0	3.0×10^5
Rhoden ⁽¹³⁾	— — —	18in	3.0	10C/20~40C	$\gamma=30^\circ\sim 38^\circ$	1.0	$3 \times 10^4 \sim 5 \times 10^5$
Erwin ⁽⁴⁾	○	5in	2.0	NACA65-1210	$\beta_1=60^\circ$	1.0	2.45×10^5
Emery	●	20in	4.0	NACA65-1210	$\beta_1=60^\circ$	1.0	
Takeya ⁽⁶⁾	■	300mm	3.0	NACA65 (216) 410	$\gamma=30^\circ$	1.25	2.5×10^5
	□			NACA65 (216) 1210	$\gamma=30^\circ$	1.0	
	▣			NACA65 (216) 1210	$\gamma=40^\circ$	1.0	
	▤			NACA65 (216) 1210	$\gamma=50^\circ$	1.0	
	▥			NACA65 (216) 12010	$\gamma=30^\circ$	1.0	
Authors.	Refer to Fig. 3	300mm	3.0	NACA65 (216) 810	$\beta_1=35^\circ\sim 80^\circ$	0.75~2.0	2.0×10^5

Table 1 Dimension of various cascade equipments in Fig. 4

So far as Fig. 4 shows, an aspect ratio does not appear to have a tendency to affect the relation of λ and $\cos \beta_1 / \cos \beta_2$. In other words, the axial velocity ratio for a given ratio of $\cos \beta_1 / \cos \beta_2$ is not necessarily larger as the aspect ratio becomes larger. The relation of λ and $\cos \beta_1 / \cos \beta_2$ is influenced most strongly by the inlet condition of the cascade. Because, the flow separation at the sidewall, which causes an increase in axial velocity ratio, is affected not only by the apparent retardation ratio but also by factors such as growth of boundary layer on the sidewall, Reynolds number and turbulence level of inlet flow, and roughness of the surface of the wind tunnel. Among these factors, growth of boundary layer at the inlet of the cascade seems to be the most important. From this standpoint, an experiment for $\beta_1 = 65^\circ$ and $t/l = 1.0$ is carried out in addition, in which the boundary layer on the sidewall is not removed from the 10 mm-slit (⑦ in Fig. 2) upstream of the cascade. The relation of λ and $\Delta\beta$ is given by a broken line in Fig. 3. Comparing the results with and without boundary layer removal from the slit, the increment of axial velocity ratio, $\lambda - 1$ is larger by about 15% in the case of the latter. Reneau et al.⁽¹⁴⁾ reported that the performance of a diffuser was strongly influenced by the growth of boundary layer on the wall of the inlet. In the case of a retarded cascade, the boundary layer affects the flow separation not only because of the small kinematic energy of fluid particles in it, but also because it causes a secondary flow in the cascade and the secondary flow may affect the separation. The secondary flow also has an effect on the turning angle of the main flow when the aspect ratio of blade is small. Pollard and Gostelow⁽¹⁵⁾ stated that the secondary flow produces a

Fig. 4 Relation between axial velocity ratio and deceptive retardation ratio in the solid wall cascade

twisting of the main stream flow for an aspect ratio as low as $AR=2$, but there is little twisting for $AR=3$. In our experiments, it is not necessary to consider the effect of secondary flow on the turning angle of the main stream, because of boundary layer removal from the slit upstream of the cascade, and also because of $AR=3$.

5. Comparison between solid and porous wall cascade

5.1 Working formulae

It is needless to say that the well-known formulae for the two-dimensional cascade⁽¹⁶⁾ cannot be applied to the solid wall cascade. Therefore, an approximate method will be shown here in which the lift and drag coefficients can be evaluated experimentally by downstream traversing.

Consider a hypothetical flow in which the flow conditions do not vary in the tangential direction ($\partial/\partial u=0$). Let the origin be the leading edge of the middle section of blade span, and take the a , u , s -axes in the axial, tangential and spanwise directions respectively, as shown in the upper right part of Fig. 1. Taking a control volume of a width of Δs as in the figure, the mass flow rate increases by $\rho(v_{a2}-v_{a1})t\Delta s$ in the control volume. This increment is due to the existence of inward spansise component v_s of a through flow which is caused by the contraction effect. If the increment of axial velocity across the infinitesimal distance da in the axial direction is dv_a , the following relation is obtained from the condition of continuity.

$$dv_a \Delta s = 2v_s da \quad \dots\dots\dots (1)$$

Therefore, the increase in the axial component of the momentum is

$$\begin{aligned} 2\rho t \int_0^{t \cos \gamma} v_a v_s da &= \rho t \Delta s \int_{v_{a1}}^{v_{a2}} v_a dv_a \\ &= \frac{1}{2} \rho t (v_{a2}^2 - v_{a1}^2) \Delta s \quad \dots\dots\dots (2) \end{aligned}$$

As to the increase in tangential component, it becomes

$$2\rho t \int_0^{t \cos \gamma} v_u v_s da = \rho t \Delta s \int_{v_{a1}}^{v_{a2}} v_u dv_a \quad \dots\dots\dots (3)$$

The integrand of the right hand of the equation can be replaced with $v_a \tan \beta$, if the flow direction makes an angle of β from the axial direction. Assuming that the direction of flow in the control volume is equal to that of the vector mean velocity approximately, namely $\tan \beta = \tan \beta_\infty = (v_{u1} + v_{u2}) / (v_{a1} + v_{a2})$, the increase of momentum in the tangential direction in the control volume can be written as follows

$$\begin{aligned} \rho t \Delta s \int_{v_{a1}}^{v_{a2}} v_u dv_a &= \frac{1}{2} \rho t (v_{a2} - v_{a1}) (v_{u1} + v_{u2}) \Delta s \\ &\quad \dots\dots\dots (4) \end{aligned}$$

Using the momentum theory, therefore, the axial and

tangential forces acting on unit breadth ($\Delta s=1$) of the blade can be represented by

$$F_a = \frac{1}{2} \rho t (v_1^2 - v_2^2) - \tilde{P}_l t - \left\{ \rho t v_{a1}^2 + \frac{1}{2} \rho t (v_{a2}^2 - v_{a1}^2) - \rho t v_{a2}^2 \right\} \\ = \frac{1}{2} \rho v_1^2 l \left\{ \frac{t}{l} (\tan^2 \beta_1 - \lambda^2 \tan^2 \beta_2) \cos^2 \beta_1 - \frac{t}{l} \zeta_p \right\} \quad \dots\dots\dots (5)$$

$$F_u = \rho t v_{a1} v_{u1} + \frac{1}{2} \rho t (v_{a2} - v_{a1}) (v_{u1} + v_{u2}) - \rho t v_{a2} v_{u2} \\ = \frac{1}{2} \rho v_1^2 l \left\{ \frac{t}{l} (1 + \lambda) (\tan \beta_1 - \lambda \tan \beta_2) \cos^2 \beta_1 \right\} \quad \dots\dots\dots (6)$$

where $\tilde{P}_l$ is the total pressure loss across the cascade. In order to evaluate a lift and a drag force, the resultant force of F_a and F_u must be split again into the normal and parallel components to the referring direction of β , that is

$$L = F_u \cos \beta + F_a \sin \beta \quad \dots\dots\dots (7)$$

$$D = F_u \sin \beta - F_a \cos \beta \quad \dots\dots\dots (8)$$

Substituting Eqs. (5) and (6) into Eq. (8), yields

$$D = \frac{1}{2} \rho t v_{a1}^2 (\tan \beta_1 - \lambda \tan \beta_2) \{ (1 + \lambda) \tan \beta - (\tan \beta_1 + \lambda \tan \beta_2) \} \cos \beta + \tilde{P}_l t \cos \beta$$

The second term in the right side of this equation represents the force due to fluid friction, while the first term appears even for an inviscid fluid depending on the direction of β . On the other hand, since the drag force should be evaluated only by the term involving the frictional loss, the referring direction β must be defined such that the first term of the equation can be equal to zero. Namely,

$$\tan \beta = (\tan \beta_1 + \lambda \tan \beta_2) / (1 + \lambda) \quad \dots\dots\dots (9)$$

The direction β represented by Eq. (9) is nothing but the direction of vector mean velocity v_∞ of inlet and exit flow velocity as is evident from Fig. 1. (that is, $\beta = \beta_\infty$)

$$v_\infty = \frac{1 + \lambda}{2 \cos \beta_\infty} v_{a1} \quad \dots\dots\dots (10)$$

Then it is reasonable that the lift and drag forces are made dimensionless referring to the vector mean velocity and chord length. Thus,

$$C_L = L / \left(\frac{1}{2} \rho v_\infty^2 l \right) = 2 \frac{t}{l} \frac{2}{\lambda + 1} \times (\tan \beta_1 - \lambda \tan \beta_2) \cos \beta_\infty - C_D \tan \beta_\infty \quad \dots\dots\dots (11)$$

$$C_D = D / \left(\frac{1}{2} \rho v_\infty^2 l \right) = \frac{t}{l} \left(\frac{2}{\lambda + 1} \right)^2 \zeta_p \frac{\cos^3 \beta_\infty}{\cos^2 \beta_1} \quad \dots\dots\dots (12)$$

The lift and drag coefficients can be easily evaluated from the downstream traversing measurement with these equations. In the real flow, however, the exit flow conditions vary in the tangential direction at the traversing position. Therefore, the following relations may be used.

$$\lambda = \frac{1}{t} \int_0^t (v_{a2}' / v_{a1}) du \\ \zeta_p = \frac{1}{q_1 t} \int_0^t (P_1 - P_2') du \\ \beta_2 = \tan^{-1} \left[\left\{ \frac{t}{2} \int_0^t \left(\frac{v_2'}{v_1} \right)^2 \sin 2\beta_2 du \right\} / \left\{ \int_0^t \left(\frac{v_2'}{v_1} \right)^2 \cos \beta_2' du \right\} \right]^{(11)}$$

where the prime denotes the local value.

Obtaining the forces parallel and normal to the blade chord from Eqs. (5) to (8), and dividing them by the product of inlet dynamic pressure and chord length, the dimensional forces c_X and c_Y are respectively,

$$c_X = \{ -c_L \sin (\beta_\infty - \gamma) + c_D \cos (\beta_\infty - \gamma) \} \times \cos^2 \beta_1 / \cos^2 \beta_\infty \quad \dots\dots\dots (13)$$

$$c_Y = \{ c_L \cos (\beta_\infty - \gamma) + c_D \sin (\beta_\infty - \gamma) \} \times \cos^2 \beta_1 / \cos^2 \beta_\infty \quad \dots\dots\dots (14)$$

In order to justify the accuracy of Eqs. (11) and (12), let us compare the cascade performance obtained from the downstream traversing measurement (DST) with that obtained from the measurement of blade surface pressure distribution (SPD). The comparison will be made for the dimensionless forces parallel and normal to the blade chord (c_X and c_Y), since it is difficult to find the vector mean direction in the case of SPD. c_X and c_Y are evaluated by substituting Eqs. (11) and (12) into Eqs. (13) and (14) in the case of DST. Figure 5(a) shows the comparison of c_Y against the angle of attack for cascades of various inlet angles and Fig. 5(b) that of c_X . Considerably good agreement between both methods is shown in these figures except in the case of $\beta_1 = 80^\circ$, and it is proved that the lift and drag coefficients in the case of $\lambda \neq 1$ can be evaluated from Eqs. (11) and (12) with considerable precision.

In the two dimensional case, the well-known formulae can be obtained by taking $\lambda = 1$ in the Eqs. (11) and (12). Representing the axial force coefficient and the static pressure rise coefficient as

$$c_a = F_a / (q_1 l) \text{ and } c_p = \Delta p / q_1 \quad \dots\dots\dots (15)$$

respectively, the following relation will be obtained

$$c_p = \frac{l}{t} c_a \quad \dots\dots\dots (16)$$

In the case of an inviscid flow,

$$c_{pth} = 1 - \cos^2 \beta_1 / \cos^2 \beta_2 \quad \dots\dots\dots (17)$$

5.2 Comparison of the cascade performances

Next, comparison between the performances of solid and porous wall cascades will be described. The turning angles for both cascades are compared against the angle of attack in Fig. 6 for various inlet angles. The turning angle across the porous wall cascade decreases with an increasing inlet angle,

while completely contrary results appear in the case of a solid wall cascade. So the difference between turning angles of both cascades becomes more striking with higher inlet angles. Especially the turning angle across the solid wall cascade is about twice as large as that across the aerodynamically two-dimensional cascade in the case of $\beta_1=80^\circ$. Kubota⁽⁶⁾ and Shaalan-Horlock⁽¹⁰⁾ tried to explain this discrepancy, which was due to an increase in axial velocity within the cascade, using the theory of potential flow. Namely, the change in axial velocity was reproduced by distributing sources within the blade passages, and a circulating flow was added around the blade so that Kutta's condition might be satisfied. This additional circulation corresponded to the change in turning angle⁽⁶⁾.

(a) Comparison of $\Delta\beta$ obtained by the measurement of downstream traversing and that of surface pressure distribution

(b) Comparison of C_x obtained by the measurement of downstream traversing and that of surface pressure distribution

Fig. 5

In the solid wall cascade, however, the performance of a blade section at the middle span rather falls notwithstanding the large turning angle, because the exit velocity is far larger than that expected from the turning angle. Figure 7 indicates the comparisons of static pressure rises across both cascades. Static pressure rise coefficients c_p are plotted against the turning angle for various inlet angles in this figure. As is obvious from Fig. 7, in the case of the porous wall cascade with the pitch-chord ratio equal to unity, the pressure rise coefficient almost agrees with the dimensionless axial force on blade c_a which is evaluated from measurements of the turning angle and total pressure loss. [see Eq. (16)]. On the other hand, the pressure rise coefficient across the solid wall cascade shows by far the lower value than that pre-

Fig. 6 Comparison of the turning angle in the porous and solid wall cascades ($t/l=1.0$)

Fig. 7 Comparison of static pressure rise coefficient in the porous and solid wall cascades ($t/l=1.0$)

dicted from the turning angle, and the coefficient is less than 0.2 for all turning angles.

Therefore, the lift coefficient is also lower in a solid wall cascade than in a porous wall cascade. Comparisons of lift coefficients c_L evaluated from measurements of downstream traverse in two kinds of cascades are shown in Fig. 8. For reference, the lift coefficients in an ideal flow are shown by dot-dash lines (which is obtained from Eq. (11) by substituting $\lambda=1$ and $c_D=0$). As is evident from this figure, the lift coefficient increases with an increased inlet angle in the porous wall cascade, while it decreases in the solid wall cascade. The difference between both lift coefficients is larger in the case of high inlet angles. Lift coefficients in the cascades obtained from the measurement of pressure distribution around a blade indicate a similar tendency, but the figure will be omitted in this paper.

According to the facts described above, in the

Fig. 8 Comparison of lift coefficient in the porous and solid wall cascades

Fig. 9 Comparison of total pressure loss coefficient in the porous and solid wall cascades

solid wall cascade, a turning angle is apparently larger at the center of blade span, but the performance of the cascade becomes lower because of the contraction effect. Such a tendency is more remarkable in the case of higher inlet angles.

Lastly, comparisons between the losses of total pressure for the solid and porous wall cascades will be given. Total pressure loss coefficients ζ_p for various inlet angles are represented in Fig. 9 taking the angle of attack as abscissa. The minimum values of ζ_p for both cascades are not so different. In the case of the solid wall cascade, however, the positive and negative stalls begin to occur at larger angles of attack. Therefore, the effective operating ranges of angle of attack for both cascades show discrepancy. This discrepancy is more remarkable as the inlet angle becomes higher. Moreover, as the operating range is very narrow for a cascade with a high inlet angle, one should take care not to disregard the discrepancy of the stalling angle of attack. Serious error may result if this caution is ignored and the operating range is evaluated from the data of solid wall cascade in the case of extremely high inlet angles.

6. Conclusions

The summary of results in this experiment is as follows:

(1) In the case of a solid wall cascade with aspect ratio $AR=3$, the axial velocity ratio at the middle span increases linearly with the turning angle, and the rate of increase is higher with an increasing inlet angle.

(2) In a solid wall cascade, the axial velocity ratio λ is only dependent on the apparent retardation ratio $\cos \beta_1 / \cos \beta_2$. Relations between λ and $\cos \beta_1 / \cos \beta_2$ are different according to the cascade test facilities.

(3) A method is given by which the lift and drag coefficients are evaluated from measurements of downstream traverse in the case of the axial velocity ratio not being unity.

(4) Although the turning angle at the center of blade span of the solid wall cascade is apparently larger than that of the porous wall cascade, the performance becomes lower due to the contraction effect of flow. Such a tendency becomes more remarkable as the inlet angle becomes higher.

(5) The values of positive and negative stalling angles of attack in the solid and porous wall cascade are different. This discrepancy becomes more serious in the case of extremely high inlet angles.

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Errata (Vol. 15, No. 82, April, 1972)

Author	Page	Column	Line	For	Read
K. YAMAOKA et al.	507	right	Eq. (21)	$\dots M_v \frac{d^2 h_r}{dT^2} + \lambda_v \frac{dh_r}{dT} \dots$	$\dots M_v \frac{d^2 h_v}{dT^2} + \lambda_v \frac{dh_v}{dT} \dots$
				$\dots + \left\{ \lambda_{vs} \frac{dh_r}{dT} + \dots \right.$	$\dots + \left\{ \lambda_{vs} \frac{dh_v}{dT} + \dots \right.$
	511	left	5	retarding	regarding
	512	left	Eq. (1)	$\beta_e =$	$\beta_c =$
	516	Please exchange the picture $\left(\frac{1000 \text{ rpm}}{10 \text{ mm}^3/\text{stroke}} \right)$ for that of $\left(\frac{1000 \text{ rpm}}{25 \text{ mm}^3/\text{stroke}} \right)$ without change of the position of rpm, mm ³ /stroke.			