In additive manufacturing of metal products, predicting deformations induced during the process is indispensable for improving the quality of the products and reducing the post-process machining time. Finite element analysis (FEA) based on the inherent strain method using a voxel mesh is an effective prediction method due to its reasonable analysis time, and to efficiently implement the prediction, a method of easily and accurately defining the inherent strain value is required. In this study, an analysis method based on multi-layer model theory was proposed to evaluate the inherent strain applicable to electron beam melting. The inherent strains obtained by the analysis were $-0.835\%$ and $-3.42\%$ for 12Cr steel and Co alloy, respectively. As a verification result using the FEA based on the analyzed inherent strain, the out-of-plane deformation of the base plate and the thickness of the manufactured parts were evaluated with accuracies of $\pm 2.0\,\text{mm}$ and $\pm 0.5\,\text{mm}$, respectively.

**Key Words:** Additive manufacturing, Electron beam melting, Deformation, Inherent strain, Multi-layer model

1. **Introduction**

In additive manufacturing (AM) technology for fabricating metal products, powder-bed AM methods, such as electron beam melting (EBM) and selective laser melting (SLM), have a high degree of freedom in design, and the application of these techniques to various products is progressing. For practical applications, although providing reliable material properties and methods of inspecting defects such as vacancies are the main issues to be solved, deformation control during the manufacturing is also considered to be important. As the chamber volume in AM equipment has become larger, the manufactured products have become larger and more complicated. It has become difficult to suppress deformation even when using a support structure. Large deformation affects the manufacturing cost and the process stability. Shrinkage and bending of products can cause dimensional variations that are unacceptable for the design, and out-of-plane deformation of products and the base plate can give rise to interference with the powder rake, with possible interruption of the process. As one countermeasure, although it has been proposed to set a higher machining allowance and to increase the rigidity of the base plate, these measures can cause an increase in the number of extra post-process machining steps and the material costs. Therefore, such deformation must be intentionally controlled. To improve the product quality and reduce the number of machining steps, it is necessary to predict the amount of the deformation before the manufacturing and (if it is possible to suppress the deformation) to reduce the deformation by optimizing the process control and the support structure or (if it is not possible to suppress the deformation) to feed back a reverse displacement to the manufacturing drawing data. Thus, a highly accurate deformation prediction method will be indispensable.

Some previous studies have reported methods for predicting the deformation during the AM process. Initially, the deformation was predicted using thermo-elastic-plastic finite element analysis (FEA) in a powder-bed basic model with a single layer. After that, the manufacturing models became larger, and the deformation of multi-layer parts was analyzed in the powder-bed model and the direct deposition model. When performing such detailed analysis with a moving heat source, it is necessary to define the detailed conditions, such as the heat source model, the re-melting effect, the material properties at high temperature, the stress relaxation characteristics (if there is a preheating step in the AM), etc. In addition, in a model with a highly focused heat source in the powder-bed AM (EBM or SLM), the scanning distance of the heat source becomes very large. This is very cumbersome in terms of the required calculation time and the definition of the analysis conditions, and the analysis model has been limited to basic shapes, such as cubic model with one additive layer. In order to reduce the computational time, methods such as selective mesh coarsening (SMC), multi-scale analysis and additive FEA-Hybrid have been proposed; however the applicable models have been limited since one issue with SMC is the need to evolve the part geometry, and additive FEA-Hybrid requires a model with clear space division, as represented by wire arc additive manufacturing. Therefore, if the deformation of the entire product is to be predicted, the inherent strain method using a voxel mesh is considered to be effective in practice.

Then, how to determine the inherent strain value serving as the analysis input is important for achieving accurate analysis. Determining the value by thermo-elastic-plastic FEA, as described above, requires various kinds of information, such as the heat source conditions, the material properties of the molten metal and
the powder bed, the preheating temperature distribution (in case of EBM), etc. It is difficult to easily predict the inherent strain with high accuracy by thermo-elastic-plastic FEA. A method for inverse analysis utilizing simple experimental results has been proposed for SLM\textsuperscript{29,30}. In these previous studies, the inherent strain was determined by fitting analysis to reproduce the arched spring-back deformation that occurs when the manufactured part was separated from the base plate. Although this method involves understanding the deformation macroscopically under the assumption of a uniform inherent strain distribution, the information required for the evaluation is only the basic experimental data of the spring-back deformation and the elastic properties at the manufacturing temperature. This method is very simple and practical to implement; however, it is not effective in EBM where no spring-back deformation occurs due to the elastic strain relaxation during the high-temperature preheating process.

In this study, we propose a method that is applicable to EBM for evaluating the inherent strain based on a multi-layer model. 12Cr steel and Co alloy (MAR-M-509) powders were used for evaluating. The inherent strain was analyzed from the deflection observed in the experimental manufacturing. The validity was verified by the FEA based on the analyzed inherent strain.

2. Methodology

2.1 Inherent strain theory for multi-layer model

Figure 1 shows the manufactured model used for analyzing the inherent strain. Fig. 2 shows a schematic illustration for explaining the inherent strain theory for the multi-layer model.

The manufactured model was composed of a base plate, a support layer and a parts layer, the latter two being composed of multiple layers. Five bodies were arranged in parallel. Fig. 2(a) shows views from the X and Y directions. Fig. 2(b) is a schematic illustration with the dotted-line area enlarged and simplified to three layers, showing the dimensional relationship between the first layer and the N\textsuperscript{th} layer of the model. The support layer was modeled as a homogenized element. The curvature generated in the manufacturing was analyzed by the following equation. The YZ cross section shown in Fig. 2(b) was focused, and the force balance in this cross section and the moment balance around the neutral axis were evaluated.

First, the position of the neutral axis, which changes with the stacking, was calculated. Under the Bernoulli-Euler assumption, the relation between the total strain and the curvature due to a bending moment is given by:

\[
\epsilon_{x}^{\text{total}} = \frac{(z - z_{c})}{\rho_{x}} \tag{1}\]

The relation between the strains is described by:

\[
\epsilon_{x}^{\text{total}} = \epsilon_{x}^{\text{elast}} + \gamma_{x} \tag{2}\]

where \(\epsilon_{x}^{\text{total}}\) is the total strain in the X direction, \(Z_{c}\) is the position of the neutral axis, \(\rho_{x}\) is the radius of the curvature in the X direction, \(\epsilon_{x}^{\text{elast}}\) is the elastic strain in the X direction, and \(\gamma_{x}\) is the inherent strain in the X direction. The inherent strain was assumed to be uniform, since the process conditions (ex. beam scanning speed and heat input) of the parts layer are the same for all layers. From Eqs. (1) and (2), the elastic strain is expressed by:

\[
\epsilon_{x}^{\text{elast}} = \frac{(z - z_{c})}{\rho_{x}} - \gamma_{x} \tag{3}\]

The stress equation in the cross section is expressed as:

\[
\int \sigma_{x} \, dy \, dz = 0, \tag{4}\]

Fig. 1 Configuration of the manufactured model.
(a) Perspective view, (b) XY plane view, (c) XZ plane view.
and the relationship between the elastic strain and the stress is given by:

\[ \sigma_x = E_y \varepsilon_x^{\text{elastic}}, \]  

(5)

where \( \sigma_x \) is the stress in the X direction, and \( E \) is the Young's modulus.

From Eqs. (3) to (5), the stress equation for the \( N \)th layer shown in Fig. 2 is given by:

\[ \sum_{j=1}^{3} \int_{z_j}^{z_{j+1}} E_j (z - z_c) / \rho_x \, dy \, dz = - \sum_{j=1}^{3} \int_{z_j}^{z_{j+1}} g_x \, dy \, dz = 0, \]  

(6)

where \( E_j \) is the Young's modulus of each layer (\( j = 1 \) (the base plate), 2 (the support), 3 (the parts)), and \( Z_j \) is the vertical coordinate of each layer. The integration range in the Y direction is the entire cross section (150 mm width of the base plate and 5 mm × 10 mm width of the support layer and the parts layer). Since the thickness of the inherent strain layer (newly added layer in Fig. 2(b)) is 0.08 mm, which is 1% or less of the thickness of the base plate and the support layer, the \( g_x \) term of the equation (6) is approximated to zero.

Eqs. (6) is described by:

\[ \sum_{j=1}^{3} \int_{z_j}^{z_{j+1}} E_j (z - z_c) / \rho_x \, dy \, dz = 0, \]  

(7)

where \( E_j \) is determined by the physical property, \( Z_j \) is calculated by the thickness of each layer, \( \rho_x \) is the constant radius. Thus, the neutral axis can be calculated from Eq. (7).

Next, the radius of curvature generated during the process was calculated. The moment balance in the X direction around the neutral surface is:

\[ \int \sigma_x \, z \, dy \, dz = 0, \]  

(8)

From Eqs. (3), (5) and (8), the bending moment balance of the \( N \)th layer is expressed by:

\[ \sum_{j=1}^{3} \left\{ \frac{1}{\rho_x \, N} \int_{z_{j-1}}^{z_j} E_j \, z^2 \, dy \, dz + \varepsilon_{x, 0} \int_{z_{j-1}}^{z_{j+1}} E_j \, z \, dy \, dz \right. \]

\[ - \left. \int_{z_{j-1}}^{z_{j+1}} g_x \, z \, dy \, dz \right\} = 0, \]  

(9)

where \( \rho_{x,N} \) is the radius of curvature in the X direction at the \( N \)th layer, and \( \varepsilon_{x,0} \) is the strain at the neutral axis \((-z_c / \rho_x = 0)\). The first to third terms in Eq. (9) can be described as follows:

\[ \frac{1}{\rho_{x,N}} \sum_{j=1}^{3} \int_{z_{j-1}}^{z_j} E_j \, z^2 \, dy \, dz \]

\[ = \frac{1}{\rho_{x,N}} \left( E_1 \int_{z_{j-1}}^{z_{j+1}} z^2 \, dy \, dz + E_2 \int_{z_{j-1}}^{z_{j+2}} z^2 \, dy \, dz \right. \]

\[ + \left. E_3 \int_{z_{j+2}}^{z_{j+3}} z^2 \, dy \, dz \right) \]  

(10)

\[ \varepsilon_{x,0} \sum_{j=1}^{3} \int_{z_{j-1}}^{z_j} E_j \, z \, dy \, dz = 0 \]  

(11)

\[ \sum_{j=1}^{3} \int_{z_{j-1}}^{z_j} g_x \, z \, dy \, dz \]

\[ = E_3 \, g_x \, \int_{z_{j-1}}^{z_{j+2}} z \, dy \, dz. \]  

(12)
From Eqs. (9) to (12), the relationship between the radius of curvature and the inherent strain is expressed by:

$$
\frac{1}{\rho_{z,n}} \left[ E_1 \int_{t_{z-1}}^{t_z} z^2 \, dydz \right. \\
+ E_2 \int_{t_{z-1}}^{t_z+t_{1}\text{-layer}} z^2 \, dydz \\
\left. + E_3 \int_{t_{z-1}}^{t_z+t_{1\text{-layer}}} z^2 \, dydz \right]
$$

Thus, the radius of curvature ($\rho_{z,n}$) at the Nth layer can be calculated from the thickness of each layer ($t_1, t_2, t_3$), the thickness of the newly added layer ($t_{1\text{-layer}}$), the Young's modulus ($E_1, E_2, E_3$), and the uniform inherent strain ($g_s$).

Assuming that the deformation of each layer is accumulated elastically, the deflection ($\delta_{x,fin}$) of all layers is manufactured can be described by:

$$
\delta_{x,fin} = \frac{\sum_{N=1}^{m} \delta_{z,n} = \sum_{N=1}^{m} \frac{1}{\rho_{x,n}} dL^2 \frac{L^2}{2} \sum_{N=1}^{m} \frac{1}{\rho_{x,n}},}
$$

where $m$ is the total number of parts stacking, and $L$ is the length of the parts from the center line. If the deflection ($\delta_{x,fin}$) is determined from the experimental data, the uniform inherent strain can be calculated from Eqs. (13) and (14).

Table 1 shows parameters used in the calculation. The Young's modulus of the support layer was calculated using:

$$
E_2 = \rho E_3,
$$

where $\rho$ is the volume fraction of the solid (manufactured area) in the support area (thickness: 10 mm, Length: 101 mm). The deflection was evaluated from the experimental measurement at room temperature (1000 °C). The Young’s modulus of the support layer was calculated using:

2.2 Experimental methods

For the analysis of the inherent strain, the amount of deflection was evaluated by the following experimental manufacturing method.

The manufacturing model is shown in Fig. 1. The manufacturing equipment was Arcam A2X, and the main manufacturing conditions are described in Table 2. The manufacturing was performed using two types of alloys with different high-temperature properties. The powder materials were Fe-based alloy and Co-based alloy, and the main chemical compositions are shown in Table 3. The Fe-based alloy and the Co-based alloy had basic compositions of 12Cr steel and MAR-M-509, respectively, and, in the following, are called 12Cr steel and Co alloy. To achieve stable manufacturing, we tried to use similar manufacturing conditions for the preheating temperature, the thickness of a single parts layer ($t_{1\text{-layer}}$), the heat input and the beam scanning pattern. The deformation of the manufactured specimens after cooling was observed using an optical non-contact measuring device (an ATOS-Core 300 3D scanner). The deformation of the manufactured parts was evaluated by observing the cross-sectional image after cutting the base plate.

2.3 FEA methods

In order to verify the inherent strain evaluated by the theory for multi-layer model, FEA based on the inherent strain method was performed.

Figure 3 shows the FE model. The element type was an eight-node hexahedral element, and the size of an element in the additive-manufactured area was 0.5 mm (X) × 0.5 mm (Y) × 1.0 mm (Z). The total number of elements was 365,040. The

| Table 2 | Experimental conditions for EBM. |
|-----------------------------|-----------------------------|
| parameter                  | unit | 12Cr steel | Co alloy |
| Machine spec.               | Arcam A2X |
| Accelerating voltage        | kV   | 60          |          |
| Atmosphere                  | mbar | 10³         |          |
| Particle size               | um   | 45–126      | 45–110   |
| Melting condition           |      |             |
| Preheat temperature         | °C   | Max. 1000   | Max. 1000|
| Melting heat power          | W    | 300         | 360      |
| Single Layer thickness      | mm   | 0.08        | 0.08     |
| Sequence                    | cross | cross       |

| Table 3 | Chemical compositions (wt%) of 12Cr steel and Co alloy. |
|-----------------------------|-----------------------------|
| C   | Co   | Cr   | Fe   | Nb   | Ni   | Ta   | V   | W   |
| 12Cr steel                  | 0.09 | 1     | 10.5 | Bal. | 0.08 | -    | -   | 0.2 | 2.5 |
| Co alloy                    | 0.6  | Bal.  | 23.4 | -    | 10.1 | 3.5  | -   | 7.0 |     |
number of layers was 20 (1 mm per layer) for the support and the parts layer. One layer of the FE model corresponds to about 12 layers in the experimental system where the thickness of a single layer was 0.08 mm. The analysis model was developed using the commercial software “MSC.MARC2013.1” with the updated Lagrange option.

The deformation of the EBM process is induced during the melting step and the cooling step. During the melting step, the out-of-plane deformation is dominant due to the temperature gradient in the parts. During the cooling step, the isotropic deformation is dominant due to the homogeneous temperature distribution. Therefore, the FEA was consisted from two steps. In the melting step, the inherent strain method was performed at the homogeneous preheating temperature (1000 ℃). In the cooling step, the thermal strain from the preheating temperature to R.T. was defined after the melting step.

In the inherent strain method, so as to express the actual manufacturing behavior, the rigidity recovery of the element and the generation of the inherent strain were defined sequentially. The example about the analysis of the 1st layer is shown in the following. When the inherent strain was input to the 1st layer to calculate the node displacement, the elements of the 2nd to 20th layers were deactivated, which means that the elements rigidity of the 2nd to 20th layers was zero. The nodes of the 2nd to 20th layers other than the shared nodes between the 1st and 2nd layers were not affected by the deformation or stress of the 1st layer. Thus, the deformation was occurred in the base plate, support and the 1st layer. Next, in the analysis of the 2nd layer, the deformed shape calculated by 1st layer analysis was used as the initial shape with residual stress and strain reset. The reset express the experimental situation where the high preheating temperature makes residual stress (elastic strain) relaxation immediately. In addition, the rigidity of the 2nd layer make a recovery. Then, the inherent strain input in the 2nd layer, and calculation of the nodal displacement were performed with no rigidity in the elements of the 3rd to 20th layers. Until the final layer (20th layer), these steps of the rigidity recovery, the stress (strain) reset and the inherent strain input were repeated.

In the cooling step, the thermal strain from the preheating temperature to R.T. was input in the all the element. In consideration of the experimental conditions, namely, the homogeneous temperature distribution due to the slow cooling rate (approximately 200 ℃/h), the thermal strain was simultaneously input to the base plate, the support and the parts. No inherent strain due to the temperature distribution was added during the cooling step.

As the boundary condition, only the rigid body motion was restricted at the bottom of the base plate. Table 4 shows the material properties used in the FEA. The Young’s modulus, the Poisson’s ratio and the thermal strain were experimental data, the same as in Section 2.1. The inherent strain was evaluated by the method described in Section 2.1. It is assumed that the same inherent strain was generated in the X and Y directions. This is because the experimental scanning beam sequence was cross where the scanning direction rotates 90 degrees for each layer. The inherent strain in the Z direction was set to zero since it does not influence on the bending deformation.

![FE model for inherent strain method.](image)

| parameter      | unit | 12Cr steel 20℃ | 1000℃ | Co alloy 20℃ | 1000℃ | Base plate 20℃ | 1000℃ |
|----------------|------|-----------------|-------|---------------|-------|-----------------|-------|
| Young’s Modulus| GPa  | 217             | 64.3  | 235           | 106   | 192             | 76.6  |
| Yield stress   | MPa  | -               | -     | -             | -     | -               | -     |
| Poisson ratio  | -    | 0.3             | 0.3   | 0.3           | 0.3   | 0.3             | 0.3   |
| Inherent strain| g_x  | 0.835           | 3.42  | -             | -     | -               | -     |
|                | g_y  | 0               | 0     | -             | -     | -               | -     |
| Thermal strain | (1000 ℃ to R.T.) | % | 1.10 | 1.45 | 1.81 |
3. Results and discussion

3.1 Experiment results and analysis results

The experimental results for the evaluated deflection $\delta_{z,\text{fin}}$ in Eq. (13) are presented here.

Figure 4 shows a perspective view of the manufactured specimens, and Fig. 5 indicates a cross-sectional view of the TP-No.1 parts in the XZ plane. Fig. 6 shows the deformation distribution of the base plate obtained by the 3D scanner.

The base plate was bent by being pulled by the additive parts (Fig. 4). The upper surface of the parts was flat, and the bottom surface of the parts was bent along the base plate with almost the same curvature (Fig. 5). As shown in Fig. 6, the base plate exhibited saddle-shape deformation in both the 12Cr steel and Co alloy, and the saddle point was located almost in the center of the base plate. The deformation amount (contour range) was approximately 5-times greater for the Co alloy. The curvatures of the TP-No.1 parts were almost the same, and the deflection $\delta_{z,\text{fin}}$ of the TP-No.1 parts was evaluated using the displacement of the boundary between the base plate and the support layer, as shown in Fig. 5. The deflections of the 12Cr steel and Co alloy were 0.91 mm and 4.16 mm, respectively. Based on this result, the uniform inherent strains evaluated using Eq. (13) were $-0.835\%$ in the 12Cr steel and $-3.42\%$ in the Co alloy. The strain value of the Co alloy was 4.1-times greater than that of the 12Cr alloy.

3.2 FEA results

In order to verify the inherent strain theory for multi-layer model, FEA based on the analyzed inherent strain was performed.

Figure 7 shows the displacement contour in the out-of-plane direction (the Z direction) after manufacturing the 10th to 20th layers, followed by cooling. For both the 12Cr steel and the Co alloy, the displacement contour of the base plate slightly changed during the support layers (1st to 10th layers) manufacturing, but significantly changed during the parts layers (10th layer and after) manufacturing. The displacement tendency of saddle-shaped deformation was the same for the 12Cr steel and the Co alloy. For the 12Cr steel, the deformation during cooling looks larger compared to the Co alloy. This is due to the difference in contour range. The displacement for the Co alloy was approximately 5-times greater.

In order to quantitatively evaluate the displacement of the base plate, Fig. 8(a) and (b) show the line profiles of the Z displacement along the edge of the base plate. Here, only the results for the Co alloy are plotted, since the deformation tendencies of the 12Cr steel and the Co alloy were almost the same, as shown in Fig. 7. The displacement up to the 10th layer was less than 1 mm for both...
the X and Y line profiles, whereas the displacement increased in the 11th to 20th layers. The increase of the displacement from one layer to the next became smaller as the number of layers increased. This is probably because the rigidity of the manufactured parts was increased by the layer stacking. At the cooling step, the displacement slightly changed with a maximum difference of -0.26 mm (at x=0). The displacement in the Y direction in Fig. 8 is asymmetric. This is clearly due to the asymmetrical rigidity of the support as shown in Fig. 1 where the support width at the both ends changes from 3 mm to 11 mm.

Figures 9 and 10 compare the results of the displacement and the thickness after cooling between the FEA and the experiment described in Section 3.1. Figures 9 (a) and 9 (b) show the displacement along the X and Y coordinates along the edge of the base plate. Since the reference point (i.e., constraint point) for the measurement was different between the FEA and the experiment, the displacement was evaluated using a biaxial coordinate system. These figures indicate that the deformation tendency was in good agreement between the FEA and the experiment. Regarding quantitative evaluation, in the X line profile of the 12Cr steel, the difference between the maximum and the minimum displacement was +1.2 mm with the FEA and +2.1 mm in the experiment. In the
Y line profile of 12Cr steel, the difference was +0.27 mm with the FEA and +0.62 mm in the experiment. On the other hand, in the X line profile of the Co alloy, the difference was +9.5 mm with the FEA and +7.5 mm in the experiment. In the Y line profile of the Co alloy, the difference was +1.2 mm with the FEA and +2.3 mm in the experiment. The analysis accuracy (the FEA value – the experimental value) was -0.9 mm to -0.35 mm for 12Cr steel, and -1.1 mm to +2.0 mm for the Co alloy. The relative accuracy (the FEA value / the experimental value) was 0.43 to 0.57 for 12Cr steel, and 0.52 to 1.3 for the Co alloy. The cause of this difference is considered to be some phenomena that were not defined in the FEA, such as the non-uniform distribution of the preheating temperature, the variation in the effective width of the support layer and the plastic strain induced in the base plate. Regarding the non-uniform distribution of the preheating temperature due to the surrounding powder bed, this distribution influences the rigidity of the base plate and can affect the base plate deformation during the manufacturing. Regarding the effective width of the support layer, the width reduction would influence the deformation of the base plate. The effective width of the experimentally manufactured support layer was reduced by several hundred micrometers due to the surface roughness peculiar to AM products. Considering for the design thickness (1 mm) of the support in the model, the reduction might not be ignored. The plastic strain in the base plate would be locally distributed due to heat input induced during manufacturing the support layer. The plastic strain in the base plate would influence the deformation of the base plate, although the heat affected zone would be local between the support and the base plate. Defining these factors would be effective for further improving the analysis accuracy; however, the current accuracy was on the same order as in previously reported studies that predicted the deflection of a base plate and manufactured parts. The effectiveness of the proposed method, that is, the validity of the uniform inherent strain, was thus verified.

Figure 10 shows a comparison about the parts thickness between the FEA and the experiment. Figure 10(a) indicates cross-sectional view of the FEA results at the final step (after cooling) in the TP-No.1 parts in the XZ plane. The upper surface of the parts was flat, and the bottom surface of the parts was bent along the base plate with almost the same curvature. The deformation tendency was similar with the experimental results shown in the Fig.5. Figure 10(b) shows a comparison of the thickness ($t_z$) of the manufactured parts with the FEA and the experiment. For the 12Cr steel, the thickness predicted with the FEA and the thickness obtained in the experiment were 9.6 – 9.9 mm and 9.5 – 9.9 mm, respectively. On the other hand, for the Co alloy, the thicknesses with the FEA and the experiment were 8.1 – 10.1 mm and 8.3 – 9.8 mm, respectively. The analysis accuracy (the FEA value – the experimental value) was 0 to 0.2 mm for the 12Cr steel and -0.1 to +0.5 mm for the Co alloy. The relative accuracy (the FEA value / the experimental value) was 1.00 to 1.02 for 12Cr steel, and 0.99 to 1.05 for the Co alloy. The FEA can predict the thickness with an accuracy of ±0.5 mm, which should be sufficient considering the powder size (maximum diameter 126μm) and the accuracy reported in a previous study. In order to improve the FEA accuracy further, it is necessary to define phenomena that are not described in the current FEA.
model, such as the flow of molten metal, dissipation of laser heat input to the powder bed beyond the design dimensions, and the in-plane distribution of the preheating temperature. The molten flow might affect the surface form of the manufactured parts. Laser heat diffusion can increase the parts thickness. In addition, a preheating temperature distribution would affect the rigidity balance and the curvature of the parts.

Lastly, the physical meaning of the analyzed inherent strain is discussed. The calculated values (−0.835 % for the 12Cr steel and −3.42 % for the Co alloy) were respectively 0.88-times and 4.6-times the thermal strain from the melting temperature to the preheating temperature (1000℃) (−0.946 % for the 12Cr steel and −0.737 % for the Co alloy). For the Co alloy, shrinkage (i.e. compression strain) was greatly emphasized. In addition to thermal strain, plastic strain, creep strain and phase transformation strain are included as inherent strain. In this study, since transformation strain does not occur from the melting temperature to the preheating temperature, it is important to evaluate plastic strain and creep strain. Previous studies have analyzed the plastic strain induced during the melting process of EBM using thermo-elastic-plastic analysis. It has been shown that the plastic strain has anisotropy, and tensile plastic strain occurs in the beam scanning direction. The analysis also showed that high tensile stress is generated in the beam scanning direction, and it is expected that tensile creep strain would occur during the preheating process. Therefore, the plastic strain and the creep strain induced during the process would be tensile strain opposite to the emphasized compressive inherent strain. There would be other factors emphasizing the compressive strain, and here the focus is on the re-melting phenomenon peculiar to the AM process. In the AM process, considering the quality of parts, the beam scanning width is controlled by the line offset, and is defined so that the melt width overlaps with the neighboring scanning line. In addition, the melt depth is controlled by the beam focus or beam energy, and is adjusted to be larger than the manufactured thickness per layer (i.e., the stage offset in the build direction). Thus, even after the powder bed melts and solidifies, the manufactured parts repeatedly receive heat input from the subsequent sequence, and thermal and plastic strains would accumulate during the process. Under the current manufacturing conditions, the Co alloy was greatly affected by this repeated heat input, so it was inferred that the compressive strain was greatly emphasized. These considerations mean that the deformation and the stress induced in the AM process are affected not only by the material type but also by process parameters such as the line offset, heat input and beam focus. Regarding the influence of the heat input, some previous studies have reported that stress and deformation during the AM process can be controlled by changing the scan speed. In order to understand the physical meaning of inherent strain, it was suggested that it is important to evaluate not only the material properties but also the process conditions.

4. Conclusion

In order to evaluate the inherent strain that is needed for accurate deformation prediction during the EBM process, the method based on the inherent strain theory using the multi-layer model was proposed. The evaluation was carried out using two types of materials (12Cr steel and Co alloy). In order to verify the proposed method, the FEA based on the analyzed inherent strain was performed. The major conclusions are summarized below.

1. From the experimental manufacturing, the deformation tendency was indicated to be equivalent for both materials. The base plate was deformed to a saddle shape, and the deflection was evaluated by the shape of the parts and the base plate.

2. The uniform inherent strain were analyzed using the deflection as −0.835 % for 12Cr steel and −3.42 % for Co alloy, and a clear difference was observed depending on the material type.

3. The FEA based on the analyzed strain indicated that the out-of-plane deformation along the edge line of the base plate was evaluated with accuracies of ±0.9 mm for 12Cr steel and ±2.0 mm for Co alloy. The parts thickness was evaluated with accuracies of ±0.18 mm for 12Cr steel and ±0.47 mm for Co alloy.

4. The uniform inherent strains were 0.88-times larger for 12Cr steel and 4.6-times larger for Co alloy compared with the thermal strain defined from the melting temperature to the preheating temperature (1000℃).

5. This emphasis of the compressive strain for the Co alloy could be due to repeated heat input and accumulation of the thermal and plastic strains in the subsequent sequence. In order to understand the physical meaning of the inherent strain, it was suggested that it is important to evaluate not only the material properties but also the process conditions.

* Product names mentioned herein may be trademarks of their respective companies.

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